Probabilistic Fracture Mechanics for Risk-Informed Activities
- Fundamentals and Applications -

Shinobu YOSHIMURA, Yasuhiro KANTO

Probabilistic Fracture Mechanics Subcommittee, Atomic Energy Research Committee,
The Japan Welding Engineering Society
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Preface

Fracture mechanics is an academic discipline that studies quantitative methods for characterizing whether a structure or structural element in which cracks have developed will fracture under a certain loading condition as experienced by the structure in use. If all factors affecting fracture phenomena—including the sizes and locations of cracks, the magnitude and frequency of the applied load, the fracture resistance of the material, and the usage environment of the structure, including its temperature and surroundings—are known with certainty, it is possible to make conclusive predictions as to whether a structure or structural element will suffer a fracture originating from a crack. However, in practice the various factors affecting fracture phenomena include statistical fluctuations and uncertainties, and so definitive characterization is not easy. Ordinarily, one takes these statistical fluctuations and uncertainties into account by making conservative estimates for each individual factor when characterizing fracture behavior. However, as these conservative estimations accumulate, a large degree of conservatism is introduced into the final characterization results, making it difficult to yield reasonable characterizations. To address these difficulties, one introduces probability theories into theoretical fracture mechanics and characterizes the fracture probability with which a structure will suffer a fracture; this is the subject of the academic discipline known as probabilistic fracture mechanics (PFM).

The “dangers associated with uncertainty” are commonly known as risks, a word that arises frequently in various aspects of modern daily life: financial risks, lung cancer risks, earthquake risks, etc. The ultimate risk faced by individuals is loss of life, but at a societal level, a variety of risks may be considered, including the incurrence of financial losses or a sudden inability to maintain a given lifestyle. Moreover, although the notion of risk typically conjures a reflexive instinct to flee, in fact whenever a risk is present, there is simultaneously some benefit associated with the risk, and this benefit must be properly weighed in the balance to determine the extent of the risk that one is willing to tolerate to take it. Thus, a key theme common to all introductions of risk paradigms is the notion that any action taken to achieve a certain benefit carries with it a certain associated risk; the extent of this risk must be clearly identified, and one must develop protocols for thinking concretely about questions such as how to minimize the risk and how to design systems or incorporate countermeasures in cognizance of the risk. For structures and structural elements, the possibility of fracture may itself be treated as a risk; alternatively, one may consider the larger risk of accidents endangering human lives that may be caused by a fracture. For these reasons, PFM may be thought of as furnishing a foundational tool for assessing risks in problems related to fracture phenomena in structures and structural elements.

The Japan Welding Engineering Society (JWES) was early to recognize the usefulness of PFM for assessing the soundness of structural elements for nuclear power
plants, and JWES has been conducting surveys and applied research regarding PFM-related techniques since 1987. In particular, from 1996 to the present, a PFM subcommittee has existed within the JWES Atomic Energy Research Committee with the objective of advancing “investigative studies of methods for applying probabilistic fracture mechanics to reliability assessments for structural elements in nuclear power plants.” The fruits of this research have been presented enthusiastically at academic conferences and in academic journals both within Japan and abroad, and the PFM subcommittee and its members not only constitute the core of PFM research and development in Japan but are also known internationally as one of the most advanced PFM research groups in the world.

To date, PFM analytical methods have been applied in multiple foreign countries to address challenges such as safety standards and the optimization of preventative measures. For example, in the United States, assessments via PFM-based analysis have been adopted since the mid-1980s as criteria for preventing brittle fractures associated with pressurized thermal-shock phenomena in nuclear-reactor pressure vessels in cases in which degraded fracture toughness (irradiation embrittlement) exceeds screening standards. In 2010, these criteria were subjected to a wide-ranging revision, and new standards based on PFM analysis and other techniques incorporating the latest insights were added. Meanwhile, the United States has also established policies for rational selection of test sites for in-service tests of nuclear reactor pipes based on risk information and the results of PFM analysis, and these have already been applied to actual plants. PFM analysis for in-service tests of this sort has also been applied to actual plants in Sweden, Spain, and other European countries.

However, in Japan—in contrast to the multiple foreign countries that have adopted regulatory standards based on PFM analysis and risk information, and despite guidance from Japan’s Nuclear Safety Commission and Nuclear and Industrial Safety Agency regarding future evolution toward the use of risk information—specific policies for making use of these methods have yet to reach the stage of realistic debate.

In fact, prior to the March 11, 2011 earthquake off the Pacific coast of Japan’s Tohoku region and the accident at the Fukushima Daiichi Nuclear Power Station triggered by the ensuing tsunami, research and development efforts and information exchanges regarding the use of PFM techniques were already underway in Japanese industry, government, academia, and academic societies, as part of a broader effort to make use of risk information. Trust in the reliability of nuclear power plunged after the accident at the Fukushima plant, and research and development on topics related to nuclear power seemed headed for something of a freeze; on the other hand, a comprehensive evaluation of safety at nuclear reactor facilities (so-called stress tests) was conducted to assess safety margins. In this context, it is important to evaluate the safety of all systems present in a plant, and thus PFM analysis—which evaluates the soundness of equipment and facilities using probabilistic metrics similar to those used
in methods of probabilistic risk assessment (PRA)—and policies to make use of PFM in the future are understood to be of great significance for ensuring safety at nuclear reactors and similar facilities. Thus PFM methods, which evaluate the safety of systems through quantitative assessments of year-by-year declines in the soundness and functionality of individual pieces of equipment, should be seen as crucial techniques that we must strive actively to develop, implement, and utilize in the future.

The objective of this book is to promote the spread of PFM techniques throughout Japan’s domestic industrial world, particularly the atomic energy industry. The book describes a variety of recent accomplishments and future developments in PFM research conducted by this subcommittee and the institutions that participate in this subcommittee, and is intended for a readership of structural material engineers and supervisors or managers responsible for protecting the operation of structural elements and plants. We hope this book will contribute in positive ways to the safety and logical soundness of the design, operation, and preventative maintenance of plants and structural elements.

This book is published in digital form on the website of the Japan Welding Engineering Society (http://www-it.jwes.or.jp/ac/), from which the most recent version may be downloaded free of charge. We encourage all readers to take advantage of this resource as well.

September 26, 2017

On behalf of the authors

Shinobu YOSHIMURA (The University of Tokyo)
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*PFM subcommittee member until 2013
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Chapter 1 What is PFM?
1.1 Fracture Mechanics and PFM

When a force of a given magnitude is applied to a structure or structural element in which cracks have appeared, will these cracks result in fractures? Fracture mechanics is an academic discipline devoted to quantitative methods for answering this question. In cases where various key pieces of information—including the size and location of cracks, the fracture resistance of the shape and materials of the structural element, the magnitude and frequency of the applied force, and data on the operating environment such as the temperature and surroundings of the structural element—are known with precision, the use of fracture mechanics allows highly accurate characterization of whether a structure or structural element will fracture. For example, in a brittle material such as glass, if the stress intensity factor $K$—a parameter measuring the magnitude of the stress field emanating from the tip of a crack (known as the singular stress field)—does not exceed the fracture toughness $K_{Ic}$, a physical quantity measuring the strength of a material to brittle fracture, then one can conclude that no brittle fracture will occur. On the other hand, if $K$ exceeds $K_{Ic}$, then brittle fractures will occur. Fracture mechanics has a long history and is actively utilized in many nations to characterize the safety and reliability of various types of machinery and structural elements in fields such as energy, shipping, electronics and machinery, and chemical plants. In Japan as well, the December 2002 revision to Japanese law governing electrical contractors included the introduction of a fitness-for-service standard dictating that, for nuclear power plants, “cracks detected by periodic inspections or through other means will be subjected to engineering analysis to determine appropriate future procedures and remedies;” with the system introduced in October 2003, the discipline of fracture mechanics finally began to play a prominent role in the spotlight.

Thus the field of fracture mechanics has an extensive history with a proven record of accomplishments, but subsequent research has advanced the field in several new directions. Among these is the emergence of probabilistic fracture mechanics (PFM). In actual structures and structural elements, the factors governing the appearance of fractures—including the location, magnitude, and growth rate of cracks, the fracture resistance (hardness) of materials, the magnitude and frequency of the applied forces, and the operating temperature and surrounding environment—are all subject to statistical fluctuations and uncertainties. In the past, fluctuations and uncertainties of this sort were all subsumed into a parameter known as the safety factor. As discussed above, in fracture mechanics one compares the stress intensity factor $K$ to the fracture toughness $K_{Ic}$; if the condition

$$K < K_{Ic}$$

(1.1-1)
is satisfied, then one concludes that no fracture will occur. However, in practice, both the stress intensity factor $K$ and the fracture toughness $K_{Ic}$ are subject to fluctuations; to take these fluctuations into account when considering the conditions required to prevent the occurrence of fractures, one introduces a factor $F$, greater than 1, and replaces Eq. (1.1-1) with the modified criterion.

$$K < \frac{K_{Ic}}{F} \quad (1.1-2)$$

$F$ is called the safety factor. However, if one makes conservative estimates of the fluctuations and uncertainties in each individual factor relevant for fracture phenomena—the location, magnitude, and growth rate of cracks, the fracture-resisting strength of the material, the magnitude and frequency of the applied force, the temperature and surroundings, etc.—and simply multiplies all of estimates together, the resulting $F$ will inevitably be rather large and the final result of the characterization will be excessively conservative. This suggests an alternative approach in which the various factors are treated as quantities possessing statistical fluctuations and uncertainties—known as random variables—with the interrelations among these quantities characterized by rigorous theoretical methods; in this case, the relevant question is not “Will fractures occur under certain specified conditions?” but rather “With what frequency (or fracture probability) will fractures occur in realistic systems subject to statistical fluctuations and uncertainties in characterization?” PFM is an academic framework that seeks to answer this question in quantitative ways.

### 1.2 PFM and PRA

In the field of nuclear engineering, techniques known as probabilistic risk assessment (PRA) or probabilistic safety assessment (PSA), which use probabilistic methods to characterize the overall safety of nuclear reactors, have been widely studied. PRA is discussed in detail in Section 3.10. In what follows, we outline the relationship between PRA and PFM, the subject of this document.

The development of PRA techniques has been underway in Japan since around 1992. In PRA, one asks what precipitating events (triggering events) might arise to put a plant into an irregular state, then conducts investigations to estimate the frequency with which these events can be expected to occur. Triggering events may be classified into internal triggering events arising within a plant, such as loss of coolant or supply water to a nuclear reactor, and external triggering events occurring outside a plant, such as earthquakes and tsunamis, volcanic eruptions, fires, and airplane crashes. Next, one uses an event tree to consider various combinations of successes and failures in the safety mechanisms established to prevent the spread of a triggering event in case one should occur. In this process, one uses a fault tree to
analyze the reliability of safety mechanisms and human operations, and determines the core damage frequency (CDF), the probability that a reactor will suffer major damage. All of this falls within the domain of what is termed Level-1 PRA. Going further, one analyzes the unfolding of events following reactor damage and determines quantities such as the likelihood of containment-vessel damage, which quantifies the probability that a large volume of radioactive material will be released from containment vessels, and studies the release and subsequent motion of nuclear fission products as events continue to unfold; this results in a determination of the source term, representing the quantity of nuclear fission products released into the environment in conjunction with damage to containment vessels. Everything thus far falls within the range of Level-2 PRA. Finally, one considers the nature of the damage to containment vessels and the quantity of fission products released to assess health risks to the general public, taking into account factors such as the spread of fission products through the environment (via atmospheric diffusion or through the food chain) and the exposure of the general public. This defines the extent of Level-3 PRA.

The advantage of PRA is that it not only allows quantitative determination of CDFs and other risk measures but also (in the process) enables quantitative characterization of other relevant factors, including the likelihood that events leading to reactor damage will arise in given scenarios, a sense of which elements contribute most significantly to driving the development of events, and information regarding the extent to which risks are affected by the presence or absence of particular safety mechanisms. On the other hand, the drawback of PRA is its dependence on factors such as the scope of the PRA, the extent to which relevant systems are properly modeled, the maturity of the methods used, and the completeness of the databases referenced in the process, all of which introduce a degree of incompleteness or imprecision into the process. These factors must be carefully understood and taken into account when assessing the results of PRA.

The frequency of occurrence of triggering events and the probability of instrument malfunction, quantities that constitute the basis of PRA analysis, are typically evaluated from statistical data. However, for triggering events that have occurred infrequently (or not at all), the frequency of occurrence and the instrument malfunction probability cannot be determined from statistical data. In contrast, PFM allows quantitative characterization of fracture probabilities for structural elements such as cooling-system pipes and nuclear reactor vessels. For this reason, PFM is located at the upper-stream to PRA, and analytical results of PFM studies are used as one source of input data for PRA analysis.
1.3 Fracture Probability and Safety Factors

1.3.1 Deterministic methods

To explain the significance of probability theory in fracture mechanics, we will use the safety factor as an illustrative example. Within linear fracture mechanics, which is used to characterize brittle fractures, the following relation is used as a condition for assessing fracture-prevention measures:

\[ K_I < K_{lc} \quad (1.3-1) \]

Where

\[ K_I = \sigma \sqrt{\pi a} \quad (1.3-2) \]

Here, \( K_I \) is the stress intensity factor for the opening fracture mode (mode I), determined by the crack length \( a \) and the load stress \( \sigma \) acting in the direction perpendicular to the crack surface, and \( K_{lc} \) is the fracture toughness, a measure of the material’s resistance to brittle fracture. Eq. (1.3-1) says that, even if a crack arises in a structural element, no brittle fracture will result as long as \( K_I \) is less than \( K_{lc} \). However, in practice, it is difficult to determine with accuracy the dimensions of cracks arising in structural elements or the loads acting on them, and consequently Eq. (1.3-1) is not easy to evaluate accurately.

For example, techniques used to measure crack length a include non-destructive testing methods such as ultrasound or eddy-current measurements. Measurements produced by such methods inevitably include errors originating from measurement errors in test instruments and human errors on the part of instrument operators. Moreover, for structural elements involving complex shapes, non-uniform material compositions, residual welding stresses, and other complications, it is very difficult to determine with accuracy the stresses acting on the regions where cracks exist.

Thus, within conventional characterization methods that seek to determine all relevant factors with certainty (known as deterministic approaches), fluctuations and uncertainties in the various individual factors are taken into account by introducing a safety factor \( F \) (a number greater than 1) to ensure that Eq. (1.3-1) is satisfied, and thus that fractures are prevented, even if \( K_I \) increases slightly or \( K_{lc} \) decreases slightly; in this case, Eq. (1.3-1) is replaced by the following condition for determining when brittle fractures will be prevented:

\[ K_I < \frac{K_{lc}}{F} \quad (1.3-3) \]

In deterministic methods of this sort, large statistical fluctuations or uncertainties in characterization will force the use of large values for the safety factor \( F \), whereupon the results of the overall evaluation will be excessively conservative.
1.3.2 How to think about fracture probabilities within PFM

We next present a simple example to illustrate how fluctuations and uncertainties in factors relevant to fracture phenomena are taken into account within the PFM framework.

In probabilistic settings, the benchmark that measures the degree of safety is the reliability \( R \). For the particular case of brittle fractures, \( R \) is the probability that \( K_I \) is less than \( K_{ic} \):

\[
R = P(K_I < K_{ic}) = 1 - P_f
\]

Here, \( P_f \) is the fracture probability. The stress intensity factor \( K_I \) and fracture toughness \( K_{ic} \) may be thought of as quantities with statistical fluctuations, that is, random variables, with probability distributions (probability density functions) \( f(K_I) \) and \( f_c(K_{ic}) \), depicted schematically in Fig.1.3-1. In this figure, the horizontal axis corresponds to the range of possible values that \( K_I \) and \( K_{ic} \) may assume, and \( f(K_I) \) and \( f_c(K_{ic}) \) are normalized to integrate to 1 over the entire range of possible values. The fracture probability \( P_f \) may be expressed as follows:

\[
P_f = \int_{K_{ic}}^{\infty} \int_{0}^{K_{ic}} f(K_I) f_c(K_{ic}) dK_I dK_{ic}
\]

Fig.1.3-1 Schematic depiction of the probability density functions for \( K_I \) and \( K_{ic} \)
The significance of Eq. (1.3-5) may be understood as the logical result of the following step-by-step progression. First, the innermost integral

\[ S(K_{lc}) = \int_{K_{lc}}^{\infty} f(K_{1}) dK_{1} \]

measures the probability that, when the fracture toughness is fixed at the value \( K_{lc} \), the stress intensity factor satisfies the condition \( K_{I} \geq K_{lc} \). This corresponds to the hatched region of Fig.1.3-2. Then the product of \( S(K_{lc}) \) with the probability that the fracture toughness takes the value \( K_{lc} \) measures the joint probability that the fracture toughness takes the value \( K_{lc} \) and the condition \( K_{I} \geq K_{lc} \) is satisfied. Consequently, the integral of this product over the full range of values \( K_{lc} \geq 0 \), that is,

\[ P_{f} = \int_{0}^{\infty} S(K_{lc}) f_c(K_{lc}) dK_{lc} = \int_{0}^{\infty} \left[ \int_{K_{lc}}^{\infty} f(K_{1}) dK_{1} \right] f_c(K_{lc}) dK_{lc} , \]

measures the probability that the condition \( K_{I} \geq K_{lc} \) is satisfied, taking into account fluctuations in both \( K_{I} \) and \( K_{lc} \). In other words, the quantity \( P_{f} \) defined in this way is the probability that a brittle fracture will occur. Fig.1.3-3 depicts this relationship schematically.

---

**Fig.1.3-2** Schematic depiction of the conditional probability of finding \( K_{I} \geq K_{lc} \)

**Fig.1.3-3** Schematic depiction of the probability that a brittle fracture will occur
1.3.3 Relationship between fracture probability and safety factor

Next, we consider the relationship between the probability of fracture occurrence, \( P_f \), and the safety factor \( F \), using the following simple example for purposes of illustration.

First, we assume that the stress intensity factor \( K_I \) and the fracture toughness \( K_{IC} \) are statistically independent and that the fracture mechanism is captured by the criterion of Eq. (1.3-1) (often known simply as the criterion). Next, for simplicity, we assume that, of the three variables appearing in the fracture criterion of Eq. (1.3-1), \( K_{IC} \) and \( \sigma \) are constants, whereas the crack length \( a \) is the sole random variable subject to statistical fluctuations and distributed according to the following exponential probability distribution:

\[
f_a(a) = \frac{1}{\bar{a}e^{-\frac{a}{\bar{a}}}}
\]  

(1.3-6)

Here, \( \bar{a} \) is the mean crack length. In this case, because \( K_{IC} \) and \( \sigma \) are constants, the threshold crack length at which fractures will be induced may be expressed via Eq. (1.3-2) in the functional form \( a_c (= K_{IC}^2/\pi\sigma^2) \); then, from Eq. (1.3-5), the probability that \( a \) exceeds \( a_c \) is given by

\[
P_f = \int_{a_c}^{\infty} f_a(a)da
\]

(1.3-7)

This equation is easily solved to yield the following expression relating the safety factor \( F \) to the fracture probability \( P_f \):

\[
P_f = e^{-F^2}
\]

(1.3-8)

where

\[
F \equiv \frac{K_{IC}}{\sigma\sqrt{\bar{a}a}} = \frac{\sigma\sqrt{\bar{a}a}}{\sigma\sqrt{\bar{a}}} = \sqrt{\frac{a_c}{\bar{a}}}
\]

(1.3-9)

Eq. (1.3-9) is plotted in Fig.1.3-4. As shown, if \( K_{IC} \) and \( \sigma \) are constants and \( a \) alone is a random variable with an exponential probability distribution, choosing a safety factor of \( F = 3 \) corresponds to a fracture probability of roughly \( P_f = 10^{-4} \), that is, to suppressing fractures to the level of one in ten thousand.
For simplicity, we have here considered a case in which only the crack length $a$ is a fluctuating quantity with a given statistical distribution. However, in actual phenomena, the parameters $K_{IC}$ and $\sigma$, along with many other quantities, exhibit statistical fluctuations; with multiple random variables, Eq. (1.3-7) involves a multidimensional integral and cannot be solved as easily as we have done here. In such cases, one must instead turn to methods of numerical analysis such as Monte Carlo simulation, a topic discussed in the following section.

1.4 Monte Carlo Simulation for PFM

In this section we discuss the rudiments of Monte Carlo methods, which are widely used in various fields of reliability engineering and which play an important role in PFM as well.

1.4.1 Basic notions

If $x$ is a given random variable, we may subdivide the values obtained by sampling $x$ multiple times into some number of intervals; by aggregating the number of samples falling within each interval, we obtain discrete knowledge of the statistical fluctuations of $x$. The graph obtained by plotting on the horizontal axis the intervals

---

![Graph](image)

**Fig.1.3-4 Relationship between fracture probability and safety factor**

Note: This plot is for the case in which only the crack length exhibits statistical fluctuations governed by an exponential distribution, with other parameters fixed at constant values.
into which \( x \) values are subdivide and the number of samples falling within each interval on the vertical axis is called a histogram, and the function \( f(x) \) that describes the probability distribution of \( x \) values is called the probability density function; it is normalized as follows:

\[
\int_{-\infty}^{\infty} f(x) \, dx = 1
\]  

(1.4-1)

The probability density function \( f(x) \) may be thought of as a normalized version of the histogram. For our purposes, \( f(x) \) may be thought of as the probability density function of a stress or a load. The probability that \( x \) lies at or below some value \( c \) [this probability is denoted by \( P(x \leq c) \), and here we will also use the notation \( F(c) = P(x \leq c) \)] is given by integrating \( f(x) \) over the interval \([-\infty, c] \):

\[
P(x \leq c) = F(c) = \int_{-\infty}^{c} f(x) \, dx
\]  

(1.4-2)

\( F(c) \) is referred to as the cumulative distribution function; it increases monotonically from 0 to 1 as the argument \( c \) increases. Fig.1.4-1 shows the relationship between the histogram, the probability density function \( f(x) \), and the cumulative distribution function \( F(x) \). For the histogram, the vertical axis units are the number of sample occurrences, whereas, for the plot of \( f(x) \), the units of the vertical axis are \( 1/\)the unit of the random variable \( x \); for the plot of \( F(x) \), the vertical axis is dimensionless.

![Fig.1.4-1 Histogram, probability density function \( f(x) \), and cumulative distribution function \( F(x) \)](image)

When treating problems involving fractures in structures, if \( c \) denotes the fracture threshold, then the fracture probability is simply \( P(x > c) \), the probability that \( x \) is greater than \( c \), and is given by
This equation may be rewritten in the form

\[ P(x > c) = \int_{-\infty}^{\infty} I_c(x) f(x) dx \]  

(1.4-4)

where we have introduced the step function

\[ I_c(x) = \begin{cases} 1 & (x > c) \\ 0 & (x \leq c) \end{cases} \]  

(1.4-5)

To carry out this calculation using Monte Carlo methods, we generate random values of \( x \) according to the actual probability density function \( f(x) \), and then use the following approximation to assess the fracture probability:

\[ P(x > c) \approx \frac{\sum_{i=1}^{N} I_c(X_i)}{N} \]  

(1.4-6)

Here, \( N \) is the number of samples and \( X_i \) is the value of the \( i \)th sample of \( x \). Eq. (1.4-6) is an approximation whose accuracy increases for larger values of \( N \). The sample values \( X_i \) may be generated using a uniform random number generator and the inverse of the cumulative density function.

### 1.4.2 Importance-sampling methods

In the design of structures, one presumably wishes to keep the fracture probability as small as possible, whereupon the quantity \( P(x > c) \) for a given fracture threshold \( c \) should be tiny, and \( f(x) \) generally takes a form like that shown schematically in Fig.1.4-2. Thus, from Eq. (1.4-3), the area under the tail of this distribution must be extremely small, requiring an enormous number of samples in Eq. (1.4-6). A technique commonly used to address this problem is the method of importance sampling. In this method, one introduces a hypothetical probability density function \( g(x) \) that is peaked at \( x = c \), as shown in Fig.1.4-2, and chooses samples of \( x \) distributed according to \( g(x) \). Then rewriting Eq. (1.4-5) in the following transformed way allows \( P(x > c) \) to be studied with good accuracy using relatively few samples:

\[ P(x > c) = \int_{-\infty}^{\infty} I'_c(x) g(x) dx \]  

(1.4-7)

where \( I'_c \) is defined by
\[ I'_c(x) = \begin{cases} \frac{f(x)}{g(x)} & (x > c) \\ 0 & (x \leq c) \end{cases} \quad (1.4-8) \]

A Monte Carlo method improved in this way is known as an *importance-sampling Monte Carlo method*; in this case, the quantity \( P(x > c) \) is approximated, in analogy with Eq. (1.4-6), in the form

\[ P(x > c) \approx \frac{\sum_{i=1}^{N} I'_c(x_i)}{N} \tag{1.4-9} \]

The use of importance-sampling Monte Carlo methods yields results that, in some cases, require only 5,000 samples to achieve accuracies essentially equivalent to those of ordinary Monte Carlo methods with 500,000 samples.

![Fig.1.4-2 Probability density function f(x) and importance function g(x)](image)

We next introduce the notion of *stratified sampling* as a modified form of importance sampling. As an example, consider a crack in the shape of the surface of a half-ellipse, defined by the dimensionless depth ratio \( a/t \) (with \( t \) being the wall thickness) and aspect ratio \( a/c \) (with \( c \) being the crack half-length). The values of both ratios lie between 0 and 1, but not all such values lie in the appropriate range of values for an initial crack (this range is known as the *sample space*). Fig.1.4-3 depicts the sample space for initial crack samples. The hatched region is bounded by cracks of uniform depth over their full perimeter, and parameter values lying within this region correspond to shapes that are geometrically impossible. Similarly, the dashed line indicates the range parameter values for which there is 100% certainty of a mechanical loss of coolant accident (LOCA) at the time of initial crack formation, and we would like to exclude this region from our sample space as well. Initial cracks correspond to parameter values lying within the remaining regions of the plot, and should trace a trajectory like those indicated by the curves shown in the figure.
The probability of pipe rupture at a given time \( t \) (that is, the probability that the time to pipe rupture, \( T \), is less than or equal to \( t \)) may be determined by Monte Carlo methods via the following equation:

\[
P(T \leq t) = \frac{N(t)}{n}
\]  

(1.4-10)

Here, \( n \) is the total number of pipes sampled (the sample size) and \( N(t) \) is the number of pipes that ruptured before time \( t \). Thus, in this case, \( n \) initial cracks must be sampled from the sample space described above; however, in reality, relatively small cracks, which occur with relatively high probability, are unlikely to lead to rupture, whereas large cracks, which have a high likelihood of leading to rupture, are unlikely to arise among the \( n \) initial crack samples. Consequently, direct use of Eq. (1.4-10) to obtain a reliable converged value for the fracture probability would require an enormous number of samples and would be computationally inefficient. Instead, we *stratify*: we divide the sample space into \( m \) subregions (cells) and obtain the probability of rupture before time \( t \) in the form

\[
P(T \leq t) = \sum_{m=1}^{M} \frac{N_m(t)}{n_m} \times P_m
\]  

(1.4-11)

where \( M \) is the total number of cells, \( n_m \) is the number of samples obtained from the \( m \)th cell, \( N_m(t) \) is the number of samples (out of \( n_m \) total samples for cell \( m \)) that led to rupture by time \( t \), and \( P_m \) is the probability that the initial crack fell within cell \( m \).
Fig.1.4-4 shows the initial-crack sample space subdivided into $M$ cells. For the purposes of our analysis, the important point here is the following: The region of Fig.1.4-4 lying between the two dashed curves is the uncertain region, in which one cannot be certain whether a fracture will ensue, and by choosing large numbers of samples in this region, we can improve the efficiency of our analysis. This method of stratifying the initial-crack sample space into $M$ cells is essentially equivalent to postulating a new function $g(x)$ to describe occurrences of $x$, and Eq. (1.4-11) may be thought of as a special case of Eq. (1.4-9).

1.5 Standard Analysis Flow of PFM

1.5.1 Overall analysis flow

Fig.1.5-1 shows one standard analysis flow for PFM. The first step is to construct a damage analysis model that allows one to trace the full trajectory of the process leading to damage of a device (the damage scenario), starting from the point in time (after the plant goes into operation) at which cracks first develop, proceeding as the cracks develop further as the device is used, and culminating in damage suffered by the device. In passing, we note that, although for simplicity we have thus far used only the single, blanket term “fracture,” in fact, fractures take many forms. We use the term leak to refer to situations in which a crack develops gradually and stably, eventually extending throughout the full thickness of wall, at which point the internal coolant material leaks to the exterior. We will use the term break for situations in which a crack, after developing stably for some time, begins to develop rapidly under
certain conditions, causing a sudden rupture of the entire cross section, that is, situations in which fractures arise. We will use the general term *failure* to refer collectively to leaks and breaks.

![Standard flowchart for PFM analysis](image)

We next construct a random variable space that accounts for various fluctuations and uncertainties, including the following: fluctuations in the size of the initial cracks to arise; fluctuations in the magnitude of the forces that apply during operation; fluctuations in the frequency with which loads are applied during operation; fluctuations in the speed with which cracks grow; the probability that cracks will be overlooked during pre-service inspections (PSIs) or in-service inspections (ISIs); fluctuations in the fracture resistance of materials; and uncertainties in determining whether coolant material has leaked from a crack that penetrates through a structural element. After completing this preparatory step, we will proceed to actual simulations. In these simulations, we select values for each variable by taking into account the fluctuations (probability distributions) in that variable; we refer to a set of values for all variables as a *sample*. For each sample, we conduct an analysis to trace through a scenario that results in failure, and we determine at what point and for what reasons the failure arises. We construct a large number of samples, up to 100 million, and execute this analysis for each sample; at each time point, we compute the fraction of all samples for which failure ensues. This fraction defines the *failure probability* at that time point.
1.5.2 Types of random variables

When numerical values of interest associated with a given phenomenon (such as, for example, the strength of a material in tensile-strength tests) are accompanied by statistical fluctuations, we refer to them as random variables. The field of fracture mechanics contains a wide variety of parameters that may be thought of as random variables, including the following:

1) Initial number of cracks present and their locations, shapes, and dimensions

2) Detection of cracks during pre-service inspections (PSIs) or in-service inspections (ISIs)

3) Speed of crack growth due to fatigue, creep, or stress/corrosion ruptures

4) Fracture-resistance properties such as fracture toughness

5) Magnitude and frequency of loading.

We first note that the probability of the existence of defects\(^1\) is well characterized for welded regions. For example, in the Marshall report (“An Assessment of the Integrity of PWR Vessels,” 2nd report by a study group under the chairmanship of D. W. Marshall, United Kingdom Atomic Energy Authority, 1982), a well-known pioneering study of PFM assessment conducted on an actual operational facility, the rate at which defects exist at welded regions was reported to lie between 0.4/m\(^3\) and 40/m\(^3\). However, these values have been subject to continual revision. The most recent PFM analyses of pressure vessels at U.S. nuclear reactors proposes, based on studies of fractures in actual plants, that the existence probability for cracks depends on the dimensions of the crack, with small defects present in greater numbers compared to the findings of the Marshall report, while large defects are less common than what was reported in that report. [Erickson Kirk, M. T., et al., “Technical Basis for Revision of the Pressurized Thermal Shock (PTS) Screening Limits in the PTS Rule (10 CFR 50.61): Summary Report,” NUREG-1806, U.S. Nuclear Regulatory Commission, 2006].

Regarding the shape of initial defects, one frequently assumes either a three-dimensional crack in the shape of the inner surface of a half-ellipse or the interior of an ellipse, or alternatively a two-dimensional crack. As for the corresponding dimensions, one may take the ratio of crack depth to wall thickness, or the crack aspect ratio (relative to the half-length of the crack), to be a random variable whose probability density is expressed in the form of a certain function; measured data may then be introduced into the analysis simply by taking this function to be the distribution of the measured data values. In addition, defects that arise in welded portions of structures during equipment manufacture may or may not be detected by
non-destructive tests conducted during pre-service or in-service inspections; the probability with which such defects are detected is another important factor that significantly affects the failure probability.

1.5.3 **Variables for analyzing crack growth**

Defects that arise during manufacture and which fail to be detected during pre-service inspections may subsequently proceed to grow during plant operation. This growth may be caused by a variety of factors, including fatigue crack growth, creep crack growth, environmental fatigue crack growth, ductile crack growth, and stress corrosion ruptures. To date, many fracture-mechanics studies have sought to clarify the various mechanisms at work here. Among these, we will here consider Paris’ law, commonly used in studies of, for example, fatigue crack growth:

\[
\frac{da}{dN} = C(\Delta K_{eff})^n
\]  

(1.5-1)

Here, \(a\) is the dimension of the crack, \(\Delta K_{eff}\) is the effective range of the stress intensity factor (the difference between the maximum and minimum values assumed by the stress intensity factor, in correspondence with the load amplitude, during the course of one load cycle), and \(C\) and \(n\) are constant material parameters. In this case, the material parameters \(C\) and \(n\) are treated as random variables.

Ultimately, the criteria used to make assessments of equipment failure include criteria relevant for brittle fractures [e.g., Eq. (1.1-1)], as well as net-section-stress criteria and tearing-modulus criteria relevant for unstable ductile fractures. In brittle-fracture assessments and in assessments of unstable ductile fractures based on net-section-stress criteria, it is common to consider probabilistic fluctuations in fracture-toughness values \(K_{lc}\) and flow stresses \(\sigma_f\). In the environment of a nuclear plant, the fracture toughness and yield strength of the materials used to construct the reactor vessel are heavily affected by the gradual phenomenon of neutron irradiation embrittlement. The effects of embrittlement are frequently incorporated directly into PFM analysis in the form of increased values for the ductile/brittle transition temperature \(RT_{NDT}\) or lower values of the fracture toughness. However, the phenomenon of embrittlement is affected by the quantities of specific ingredients, such as copper or nickel, contained in materials, and thus these are taken into account as secondary random variables. Structural elements in nuclear plants are subjected to mechanical loading, thermal loading, and seismic loading. Because these depend on operational history and seismic environment, the frequency and timing of the loading must be treated probabilistically.
1.5.4 PFM methods for assessing parameters of fracture mechanics

The mechanical foundations of PFM analysis are the characterization of fracture-mechanical parameters and their comparison with the fracture toughness of the relevant materials. The most general method for determining the fracture-mechanical parameters of three-dimensional cracks is the finite-element method. However, in PFM analysis, where Monte Carlo methods often constitute the basis for probabilistic analysis, it is essentially impossible to incorporate finite-element analysis directly. Instead, one uses the finite-element solutions obtained for various values of parameters such as crack dimensions and load stresses to construct simple assessment formulas, and then incorporate these into PFM analysis codes.

Our discussion here has assumed that all factors relevant for fracture-mechanics analysis are treated as random variables. However, in present-day practice, it is in fact difficult to treat all factors as random variables due to lack of data and the computational cost. For this reason, it is common to treat some of the relevant factors as deterministic quantities.

1.6 PFM and Risk-Informed Evaluation

In recent years, the occurrence of various types of accidents has brought the terms risk and risk management to a position of prominence throughout society as a whole. The term risk refers to a quantity defined by the product of the damage caused by a single occurrence of an accident and the frequency of occurrence (the occurrence probability) of that accident. Thus, accidents which cause only minimal damage on each single occurrence but which have a high frequency of occurrence pose a high risk; similarly, accidents that occur infrequently but for which any single occurrence results in significant damage also correspond to a high risk. In modern society, any action taken for the purpose of deriving a certain benefit inevitably gives rise simultaneously to some possibility (risk) of loss. Another way to say this is that the only way to reduce the risk associated with a given action to exactly zero is to avoid taking the action altogether, thus simultaneously losing all benefit. Taking things one step further, if we choose action B as a substitute for action A, there will be some new risk associated with action B, and thus the challenge of making appropriate decisions in our lives requires that we carefully weigh the risks and benefits of action A against those of action B.

However, although the possibility of benefit and the possibility of loss both arise in conjunction with specific activities, in fact the loss will not become apparent unless an accident occurs, and for this reason risks are frequently not taken sufficiently seriously or (in the opposite extreme) overemphasized. Furthermore, to ensure rational and appropriate decision-making in the context of day-to-day activities, it is not sufficient to assess benefits and seek to improve these alone; instead, it is of
crucial importance that one accurately identifies and assesses all anticipated risks and then takes appropriate steps to minimize these to the greatest possible extent.

In nuclear power plants, the most fundamental risk is thought to be the core damage frequency (CDF). Probabilistic risk assessment (PRA) is a method for quantitative evaluation of CDF. In PRA, one constructs a list of all possible events that may arise in a nuclear plant, quantitatively evaluates the expected occurrence frequency of each event and the contribution it will make to damaging the reactor, and then assesses the plant’s risk level based on the CDF value. In this case, the occurrence frequency of accidents arising within plants is determined by the occurrence frequency of the first event to occur (known as the triggering event) and the probability of malfunction in the systems designed into the plant to mitigate the consequences of accidents. In general, the occurrence frequency of triggering events and the probability of malfunction in the surrounding systems are evaluated on the basis of statistical data constructed from previous instances in which the event in question occurred. However, for events that have happened infrequently (or not at all), the frequency of occurrence and the likelihood of instrument malfunction cannot be computed from statistical data.

In contrast, for structural elements in nuclear power plants, PFM allows direct theoretical evaluation, from the perspective of fracture mechanics, of the failure probability itself, based on inputs such as the year-to-year variation in the values of material-property parameters, the plant’s operational history, and actual data from periodic plant inspections. The results of PFM analysis are extremely important inputs for improving the accuracy of PRA assessments. In addition, damage to principal structural elements, even damage so minor as to exert barely any impact on CDF values, that arises during the operation of a plant may lead to a variety of secondary emergencies and frequently results in long-term plant shutdown. Consequently, unplanned plant shutdowns caused by accidents of this sort should also be properly assessed as one type of risk, and plants should be appropriately maintained and protected at all times in ways that minimize their occurrence.
Chapter 2    PFM Analysis Codes
2.1 Introduction

Many software codes have been developed for several targets of the PFM problems. In this chapter, major PFM analysis codes are summarized mainly used in the activities of the JWES PFM subcommittee. Table 2.1-1 shows the list of the software codes explained in the following sections. The more detailed explanation can be found in the Appendices A-J. Examples described in Chapter 3 are analyzed by using these software codes.

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<th>LICENSE</th>
<th>ENV.</th>
<th>TARGET</th>
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2.2 PASCAL Series

2.2.1 PASCAL 3

To maintain the integrity of the major components of nuclear power plants during their service lives, it is important to consider the aging degradation of material, along with defect initiation and growth. As one of the most important components in aging light water reactors (LWRs), the structural integrity of the reactor pressure vessel (RPV) is assessed for a non-ductile fracture. In particular, the neutron irradiation embrittlement of RPV steels generates the most concern in relation to the structural integrity assessment of an RPV for the long-term operation of aging LWRs. A pressurized thermal shock (PTS) is the most important condition for the integrity assessment. For the RPV integrity assessment during a PTS event, a fracture mechanics approach is applied considering several factors such as the neutron irradiation embrittlement of RPV steels, fracture toughness decrease, and postulated crack. The assessment procedure for these factors is provided in the current code [1,2]. However, the procedure is given by a deterministic approach, with a conservative evaluation and a margin term. The probabilistic fracture mechanics (PFM) method has recently been highlighted to rationally incorporate the uncertainties arising from the material properties, defect distribution, inspection quality, etc., unlike the conventional deterministic method. In fact, a regulation on the fracture toughness requirements against PTS events with a probabilistic approach has been defined in the U.S. [3,4]. This approach consists of the PFM analysis, thermal-hydraulic analysis, and probabilistic risk analysis. Based on the approach in the U.S., the applicability of the probabilistic approach to the domestic code for the RPV integrity assessment against PTS events has been studied, and R&D related to the development of a PFM analysis is ongoing in Japan.

In general, the failure probability of a component is obtained through the PFM analysis by inputting the material strength data, crack size, load, etc. as parameters with probabilistic distributions. We have been investigating the application of this PFM analysis method to the domestic regulation, codes, and standards. In the meantime, JAEA is developing analytical tools for the PFM analysis of an RPV under transient loading such as PTS. The PFM codes developed are called the PASCAL3 (PFM analysis of structural components in aging LWRs) code [5-10].

2.2.1.1 Overview of PASCAL 3 code

The latest version of PFM analysis code PASCAL3 (PASCAL version 3) was developed at JAEA [9] based on the RPV structural integrity assessment methods of the Japanese regulation and codes. This code evaluates the conditional probability of failure of an RPV containing a flaw under transient conditions such as PTS. The main flow chart of the probabilistic analysis in PASCAL3 is shown in Fig. 2.2-1.
In the PASCAL3 code, the geometry of a crack (depth, length, and location within the RPV wall) is first determined by sampling from the crack size distribution (crack sampling). Next, the embrittlement-related parameters (chemical composition, neutron fluence, reference temperature of nil-ductile transition \( \text{RT}_{\text{NDT}} \), and fracture toughness) are sampled from appropriate distributions. The fracture toughness shift caused by the neutron irradiation embrittlement of the RPV steel is predicted by the correlation method provided in the Japanese codes [1,2]. Although these codes provide only deterministic analysis methods for assessing the structural integrity of an RPV, some probabilistic treatments are incorporated into PASCAL3.

A fracture analysis is performed based on the sampled flaw condition for a selected PTS transient. The applied mode I stress intensity factor \( (K_I) \) and the fracture toughness \( (K_{IC}) \) at the crack tip are compared at each transient time. The crack that is initiated is then
evaluated to determine whether it penetrates the RPV wall. Crack sampling is repeated based on the total sampling number. Finally, the conditional probabilities of crack initiation and fracture are calculated as the ratio of the number of failed cracks to the number of total samples.

2.2.1.2 Analytical examples obtained by PASCAL 3

(1) Sensitivity analysis

As research on the applicability of the probabilistic evaluation method for the assessment of RPV integrity in a plant life management evaluation, JAEA set the appropriate input data for a domestic plant, and PFM analyses were performed using PASCAL3 [11]. In addition, sensitivity analyses was performed focusing on different parameters and conditions for the standard PASCAL3 analysis method based on the previous investigation and relevant survey results. The parameters chosen were the initial reference temperature and the residual stress for a butt weld. Each parameter was examined on the conditional probability of fracture (CPF) when a PTS transient occurred. The standard analysis conditions for the sensitivity analyses are listed in Table 2.2-1. The other parameters and methods have already been studied in a previous paper [12], including the fracture toughness, embrittlement prediction equation, weld residual stress caused by the cladding, and margin term.

<table>
<thead>
<tr>
<th>Item</th>
<th>Parameter</th>
<th>Random variable</th>
</tr>
</thead>
<tbody>
<tr>
<td>RPV geometry</td>
<td>ID 2000 mm, Thickness 200 mm, Cladding 5 mm</td>
<td>-</td>
</tr>
<tr>
<td>Crack geometry, orientation and location</td>
<td>Semi-elliptic surface crack in axial weld</td>
<td>-</td>
</tr>
<tr>
<td>Crack dimension</td>
<td>Depth: PNNL distribution, Aspect ratio: Log-normal</td>
<td>Yes</td>
</tr>
<tr>
<td>Average neutron fluence</td>
<td>2, 5, 10 (×10^{19} n/cm², E &gt; 1 MeV)</td>
<td>Yes</td>
</tr>
<tr>
<td>Std. dev. neutron fluence</td>
<td>0.131 times the average</td>
<td>-</td>
</tr>
<tr>
<td>Initial RT and std. dev.</td>
<td>-50°C, 10°C</td>
<td>Yes</td>
</tr>
<tr>
<td>Embrittlement prediction</td>
<td>JEAC4201-2007</td>
<td>-</td>
</tr>
<tr>
<td>Chemical composition (average wt.%)</td>
<td>Cu: 0.14%, Ni: 0.80%</td>
<td>Yes</td>
</tr>
<tr>
<td>Std. dev. chemical composition</td>
<td>Cu: 0.04, Ni: 0.02</td>
<td>-</td>
</tr>
<tr>
<td>Fracture toughness K_{fc}</td>
<td>PASCAL Weibull type</td>
<td>Yes</td>
</tr>
<tr>
<td>Crack arrest toughness K_{fa}</td>
<td>ORNL Weibull type</td>
<td>Yes</td>
</tr>
<tr>
<td>Crack growth in cladding</td>
<td>None</td>
<td>-</td>
</tr>
<tr>
<td>Stress intensity factor calculation</td>
<td>Influence function developed by CEA</td>
<td>-</td>
</tr>
<tr>
<td>Crack growth model</td>
<td>Independent growth at the deepest and surface points</td>
<td>-</td>
</tr>
<tr>
<td>Warm pre-stress</td>
<td>Yes</td>
<td>-</td>
</tr>
</tbody>
</table>
In-service inspection | None | -

*Note: PASCAL Weibull type means that the fracture toughness curve was modeled as a Weibull-type distribution using $K_{IC}$ data available in Japan and incorporated into PASCAL3.

A probabilistic analysis was performed to change the initial RTNDT. Fig. 2.2-2 shows the CPF for small break loss of coolant accident (SBLOCA) transients, with initial RTNDT values of -50°C, -25°C, and 0°C. When the initial RTNDT was higher, the CPF became higher. The CPF increased by about 300 times when the initial RTNDT was changed from -50°C to 0°C.

![Fig. 2.2-2 CPF for different initial RTNDT values.](image)

To determine the effect of the weld residual stress distribution by butt welding, a residual stress distribution of FAVOR [13] was used as the third-order polynomial approximation by the least squares method. In Fig. 2.2-3, the CPF is compared with the case without considering the residual stress distribution. In general, the CPF is increased by considering the residual stress distribution. For example, at a fast neutron fluence of $10\times10^{19}$ n/cm², the CPF is increased by 2.5 to 5 times as a result of considering the residual stress. At $f = 2\times10^{19}$ n/cm², the CPF values of cases with and without considering the residual stress for the large break loss of coolant accident (LBLOCA) are lower than $10^{-10}$.

![Fig. 2.2-3 CPF for different residual stress distributions.](image)
Fig. 2.2-3 CPF with/without residual stress in butt-weld.

(2) Comparison between deterministic and probabilistic analyses

Based on the survey and the analysis results for the input data sets for domestic plants, some input data sets for probabilistic integrity analyses by PASCAL3 as well as deterministic integrity analyses were defined. Typical values such as the chemical composition of RPV steel and the initial value of reference temperature $R_{\text{NDT}}$ were shown in the previous paper [11]. The irradiation conditions were set with reference to a 3-loop type PWR plant. As typical PTS events compliant with JEAC4206 [1], three types of transients were applied: SBLOCA, main steam line break (MSLB), and LBLOCA. The LBLOCA was practically not a “pressurized” event. However, it was selected as a very severe transient as a result of only the thermal shock.

Deterministic fracture mechanics analyses for the domestic model plants were performed to obtain the stress intensity factor ($K_I$) value as a function of the temperature for each transient. The fracture toughness ($K_{Ic}$) curves are defined in JEAC 4206-2007, and the $R_{\text{NDT}}$ values are determined by the initial value and the shift value calculated using the embrittlement prediction method based on the mechanisms prescribed in JEAC 4201-2007 [2] with a margin term of $2\sigma_A$ (−20°C). When the maximum values of $K_I$ during the transients were compared, it was found that the highest was for the MSLB, the lowest was for the SBLOCA, and the LBLOCA had an intermediate value. For example, a $K_I$ curve and $K_{Ic}$ curve at $f = 10 \times 10^{19}$ n/cm$^2$ are shown in Fig. 2.2-4. $K_I$ for the MSLB reached the maximum value at a temperature of 150°C and was approximately 20 MPa m$^{1/2}$ higher than the maximum value for the SBLOCA. From the analysis results, the temperature margin ($\Delta T_m$) values are calculated as the temperature difference between the $K_I$ value and the $K_{Ic}$ curves as a function of the neutron fluence values. When the WPS effect is considered, the $\Delta T_m$ value is defined as the temperature difference between the $K_{Ic}$ curve and the point where $K_I$ is at the maximum value.

![Fig. 2.2-4 $K_I$ curves for PTS transients and $K_{Ic}$ curve at $f = 10 \times 10^{19}$ n/cm$^2$ [11].](image-url)
Probabilistic fracture mechanics analyses were performed for domestic model plants to obtain the CPF for each transient. As an example of the results for model plants with different wall thickness, the CPF is shown in Fig. 2.2-5 if there is a crack in the weld metal. For all of the model plants, an increase in the fast neutron fluence from $2 \times 10^{19}$ n/cm$^2$ to $10 \times 10^{19}$ n/cm$^2$ showed an increase in the CPF values of more than 20 times. The probabilities are the highest for the SBLOCA, and are the lowest for the LBLOCA. For the case of a postulated crack in the base metal, the CPF as functions of the neutron fluence are also obtained. For all of the cases, the CPF of the base metal are higher than those of the weld metal. This is because the initial $R_{NDT}$ value is much higher in the base metal than in the weld metal. In reality, the possibility of a crack existing in the base metal can be much lower than that for the weld metal. Therefore, it should be noted that the difference in the crack existence probabilities for the weld and base metals should be considered when making comparisons with these values.

Based on the analysis results for each of the transients obtained above, the conditional probability of crack initiation (CPI) is plotted as a function of $\Delta T_m$ to investigate the correlation of the fracture mechanics assessments by the deterministic and probabilistic analyses. Fig. 2.2-6 shows the relationship between the $\Delta T_m$ and CPI values for all of the model plants. The variation in the CPI values on the curve (the difference between the maximum and minimum CPI values at the same $\Delta T_m$) is within a factor of 10. For every plant, a good correlation is observed. On the other hand, the relationship between $\Delta T_m$ and the CPF, as shown in Fig. 2.2-7, tends to have larger scatters than that between $\Delta T_m$ and the CPI. The variation in the CPF values on the curve has a range of approximately 50 times. From these results, it is suggested that the margin with regard to the probability can be estimated from the deterministic margin determined from the temperature difference between the $K_t$ and $K_{IC}$ curves, regardless of the type of transients and the location of a postulated crack (base metal or weld metal). This finding is very useful
for us to promote the step of introducing a probabilistic approach into the current deterministic approach in Japan.

2.2.1.3 **Summary on PASCAL3** As a study to develop probabilistic structural integrity assessment methods, through establishing the standard analysis condition for the assessment of the RPV structural integrity during PTS events, sensitivity analyses of several parameters have been performed. In addition, for some model plant cases, we investigated the margin and conditional fracture probabilities for an RPV by postulating the existence of a crack using the deterministic and probabilistic analysis approaches. Conclusions related to the PFM analyses are that higher initial RTNDT provided higher CPI and CPF, and the CPF tended to increase when considering the residual stress distribution. From the model plant analyses, firstly the temperature margin (ΔTm) values were determined from the deterministic analyses and probabilistic analyses were performed to obtain the CPI and CPF values for each case. It was found to be small with a low neutron fluence and LBLOCA, which corresponded to the crack arrest being more plausible under
the conditions. When the $\Delta T_m$ and CPI values for each case were compared, a good correlation was obtained. This means that the CPI value could be estimated from $\Delta T_m$, which was obtained from the deterministic analysis.

ACKNOWLEDGMENT

This study was performed under a contract between the Japan Nuclear Energy Safety Organization (JNES) and JAEA.

2.2.2 PASCAL-SP

2.2.2.1 Outline of PASCAL-SP  PFM analysis codes for piping in nuclear power plants have been developed to evaluate the failure probability of piping at Japanese BWR plants. JAEA has been developing PASCAL-SP (PFM Analysis of Structural Components in Aging LWR - Stress Corrosion Cracking at Welded Joints of Piping). PASCAL-SP code basically conforms to approaches of Nuclear and Industrial Safety Agency (NISA) [14] and Codes for Nuclear Power Generation Facilities - Rules on Fitness-for-Service for Nuclear Power Plants - of the Japan Society of Mechanical Engineers (JSME FFS) [15]. The evaluation methods in PASCAL-SP are as follows; The crack initiation model of Harris et al. [16] on SCC is applied. A user can also set initiate cracks any time at any position without using the initiation model. The crack initiation model is extended referring to the report of NISA [14]. The position of crack initiation at inner surface of piping is determined using a normal distribution model made by statistical treatment of the detected crack data observed in PLR piping at Japanese BWR plants. Crack growth in piping thickness direction follows the notifications of NISA and the JSME FFS Codes. Some cracks observed in PLR piping at Japanese BWR plants grew from base to weld metal. The report of NISA provides that crack growth is evaluated by 2 steps. Cracks in heat affected zone (HAZ) grow by the growth rate diagram of Type 304 and those in weld metal grow by that of Type 316. The relationship between the position of crack initiation and crack depth in reaching weld metal, which is brought by data analysis of cracks observed in PLR piping at Japanese BWR plants, determines the switch of the growth rate diagram from Type 304 to Type 316. The SCC growth rate diagrams of type SUS304 and type SUS316 in the JSME Codes are used. In JAEA, statistical treatment for the diagrams of crack growth rate was developed. According to the setting of analytical conditions, plural cracks on planes parallel to cross section of piping may exist. The rule on crack merge follows the JSME Codes. Crack growth under unsteady load brought by seismic motion is evaluated as fatigue growth. The crack growth rate by fatigue follows the JSME FFS Codes. Statistical treatment for the fatigue growth rate is also applied in JAEA by referring to the JSME Codes.

Fig. 2.2-8 shows the outline of the evaluation procedure in PASCAL-SP. After random variables expressing for initial condition with scatter and uncertainties are sampled, plant operation is simulated. The plant operation includes crack initiation and growth by
SCC and fatigue crack growth by seismic stress and transient events. In-service inspections are done periodically. Accuracy of flaw detection and sizing for the in-service inspection are modeled by the data of Ultrasonic Test & Evaluation for Maintenance Standards (UTS) project [17] by Japan Power Engineering and Inspection Corporation (JAPEIC) and Japan Nuclear Energy Safety organization (JNES). The piping is evaluated as leak when cracks grow through the wall crack and leak is detectable. The evaluation method proposed by Shinokawa et al. [18] is introduced to calculate the leak rate from the through-wall crack. Failure judgment is done during crack growth evaluation. Limit load method and 2 parameter method provided in the JSME FFS Codes are applied to the failure judgment. The procedures above are repeated many times to evaluate failure probabilities (Monte Carlo method). Large sampling calculation by Monte Carlo methods generally takes a long time. Hence, a function of parallel calculation in PC cluster environment has been introduced. It decreases calculation time. For example, a calculation which takes 7,000 seconds in single PC ends at 1,000 seconds in 8 parallel calculations.

There are some evaluation models for residual stress distribution in PFM analysis. For example, in PC-PRAISE [16], some deterministic and probabilistic models are available. However, the models are based on the data from experiments in USA. Hence, a simplified probabilistic model using parametric FEM analyses based recently on our welding experiments has been developed.

In order to conduct fragility evaluation, PASCAL-SP has also been developed from the viewpoints that 1) to be consistent with fragility evaluation of seismic PRA for unaged components, the seismic stress is represented as a probabilistic variable following log-
normal distribution, 2) to be consistent with fragility evaluation of seismic PRA for unaged components, both the epistemic and aleatory uncertainties are treated for probabilistic variables, and 3) Current seismic PRA does not consider the existence of cracks for fragility evaluation, while PASCAL-SP calculates failure probability by considering cracks. To obtain fragility curve corresponding to seismic PRA, a failure evaluation method for pipe without crack is introduced to PASCAL-SP. A crack growth prediction method was introduced into PASCAL-SP that was proposed by considering the effects of the excessive seismic loading.

2.2.2.2 Analytical examples obtained by PASCAL-SP

(1) Uncertainties of weld residual stress distribution

Parametric PFM analyses concerning uncertainties of residual stress distribution using PASCAL-SP were performed [19]. The standard deviation of residual stress at a point in the database (Fig. 2.2-9) represents uncertainties at the point. The difference of the standard deviation at each point leads that of the extent of uncertainties. The value of the standard deviation at each point is varied for the sensitivity analyses of the uncertainties of entire residual stress distribution.

![Fig. 2.2-9 Contents of residual stress distribution database](image)

Parametric PFM analyses concerning the magnification to the standard deviation at each point were performed. The variation standard deviation means the distribution of uncertainties of entire residual stress distribution. The magnification is varied from 1 to 3 times in the parametric PFM analysis. Table 2.2-2 shows the outline of an analytical condition. The parametric FEM analyses about heat input and welding speed in 108 cases...
by 2-dimensional axisymmetric model (Table 2.2-2 in [20]) brought the database for 250A piping. Crack size at initiation was set as the size of crack which could not be detected by UT in the UTS project [17].

Table 2.2-2 Example of analytical conditions of PASCAL-SP

<table>
<thead>
<tr>
<th>Piping geometry</th>
<th>250A (thickness 15.1 mm)</th>
</tr>
</thead>
<tbody>
<tr>
<td>Operation stress</td>
<td>98 MPa (Design stress intensity)</td>
</tr>
<tr>
<td>Residual stress distribution</td>
<td>Database made by FEM analyses</td>
</tr>
<tr>
<td>Material</td>
<td>Type 316L stainless steel</td>
</tr>
<tr>
<td>SCC Evaluation Model</td>
<td></td>
</tr>
</tbody>
</table>
  • Crack initiation: One crack (depth 0.7 mm, total length 15.5 mm) at time 0  
  • Crack growth rate: Rules on Fitness-for-Service (JSME) |
| In-Service Inspection | No Inspection |
| Failure Judgment | Rules on Fitness-for-Service (JSME) |
| Flow Stress      | Weibull distribution (Shape Parameter 1.89, Scale Parameter 36.9, Location Parameter 327.5MPa) |
| Earthquake       | No Earthquake |

Fig. 2.2-10 shows conditional break probabilities versus operating time when the magnification to the standard deviation is varied. The conditional break probabilities were calculated under the conditions listed in Table 2.2-2. The magnification to the standard deviation at each point largely influenced the conditional break probabilities, resulting in increasing the probability as the magnification.
(2) Effect of partial welding

When weld defects are observed during an inspection after welding, repair welding is performed after removing the defects. However, partial repair welding can potentially complicate the weld residual stress distribution. Based on analysis results that evaluate the weld residual stress produced by repair welding after pipe butt-welding, structural integrity assessments related to stress corrosion cracking using the PASCAL-SP were performed by JAEA [21].

Partial repair welding complicates the weld residual stress distribution as shown in Fig. 2.2-11 [21]. To evaluate the SCC growth behavior under the complicated weld residual stress, the welded region was divided into certain areas in the circumferential direction as shown in Fig. 2.2-12. It is important to note that, at this moment, there is no method for calculating the stress intensity factor using an influence function method for a semi-elliptical surface crack existing in such an inhomogeneous weld residual stress distribution. Therefore, in PASCAL-SP, the mean residual stress calculated as the average value in areas where SCC belongs to was used for the evaluation of SCC propagation based on the influence function method [22]:
Fig. 2.2-12 Axial residual stress after partial welding

An outline of the analytical conditions is listed in Table 2.2-2. It should be noted that the probability that a crack would occur in partial repair welded region was the highest because the highest tensile residual stress at the inner surface caused by repair welding occurred in this region. The standard deviation of the residual stress based on a database for 250A piping welds was used, as described previously [19]. Fig. 2.2-13 shows the conditional cumulative failure probabilities for a large leak, and a break as a function of the operating time. The leak rates used to indicate a large leak was $1.89 \times 10^{-2} \text{ m}^3/\text{min}$ (5 gal/min). The probability of a large leak or a break before repair welding is higher than that after partial repair welding. In particular, the cumulative break probability of the piping before repair welding is one order of magnitude higher than that after repair welding. These results indicate that partial repair welding favorably affects the breakage of piping welds. As shown in Fig. 2.2-11, the tensile stress within the repair-welded region increases in comparison with that of the steady butt-welded region. However, the compressive stress in the regions adjacent to the repair-welded region is produced as a result of the repair welding, as shown in Areas 3 and 5 in Fig. 2.2-12. This decrease in the residual stress caused by partial repair welding can suppress SCC propagation in the circumferential direction. When SCC is initiated in Area 4, high-tensile residual stress is used for SCC propagation until the SCC reaches Area 3 (and/or 5). After that point, SCC is slowed because of the lower stress derived as the average of the residual stresses in Areas 4 and 3 (and/or 5). The decrease in the tensile stress caused by the compressive residual stress has the effect of suppressing SCC propagation. Thus, it can be concluded that the cumulative probability of a large leak or break in the piping is decreased after partial repair welding.
2.2.2.3 Summary on PASCAL-SP  JAEA has been developing the PASCAL-SP code to evaluate failure probabilities of piping focusing mainly on the SCC. This code has functions to follow evaluation methods in Japan. The simplified probabilistic model for residual stress distribution considering the uncertainty has been proposed to incorporate into PASCAL-SP. This model utilizes the residual stress database obtained from FEM analyses. The parametric PFM analysis using PASCAL-SP showed that the uncertainties of residual stress distribution largely influenced the break probability. The break probability increased with increasing the uncertainties of residual stress. PFM analysis based on the PASCAL-SP code considering the inhomogeneous residual stress distributions produced by partial repair welding was also performed. Although higher tensile residual stress in the partial repair-welded region is shown in partial repair welded region, partial repair welding has a favorable influence, which leads to a lower break probability for piping welds.

ACKNOWLEDGMENT

This study was performed under a contract between the Nuclear and Industrial Safety Agency of the Ministry of Economy, Trade and Industry of Japan and the JAEA.

REFERENCES


2.3 REAL-P and REAL-B

2.3.1 Introduction

A computational code system, “MSS-REAL”, has been developed for structural integrity evaluation of sodium-cooled fast reactor components such as vessels and piping [1]. This system consists of a couple of modules, REAL-A, REAL-B, REAL-D, REAL-F and REAL-P, as shown in Fig.2.3-1, to provide a set of tools for structural integrity evaluations ranging from design code-based assessments to more in-depth phenomenological approaches including structural reliability evaluations [2-4]. The modules, REAL-P and REAL-B provide probabilistic tools and probabilistic fracture mechanics (PFM) plays a significant role.

The tools for structural evaluation in the MSS-REAL system have been developed to materialize the System Based Code concept [5-8]. This concept has been proposed to allow allocating margins in structural design in an optimized way. Designing to a target reliability is one the approaches in the concept. For that purpose, tools for structural reliability evaluation is indispensable. The immediate application of the system is fast reactor components, but the methodologies could be applied to similar products in other technological areas.

Recently, efforts for implementing structural reliability evaluation to mechanical engineering have become active, and a new Japanese Industrial Standard, JIS B 9955 (2017) [9], has been published. This standard provides a general principles for structural reliability evaluation of mechanical products, covering products including chemical plants and nuclear plants as well as cranes and other mechanical properties. Guidelines for more specific applications are also under development. For example, guidelines for passive components of fast reactors will soon be published from Japan Society of Mechanical Engineers [8]. The MSS-REAL system is in line with these developments.

2.3.2 REAL-P

REAL-P has been developed for structural reliability evaluation. It incorporates the PFM approach. The main features of REAL-P are as follows:

1) Probabilistic structural integrity evaluation based on the rules of the FR code [10] is possible using Monte Carlo method.

2) More realistic probabilistic structural integrity evaluation on a life-cycle basis, simulating degradation mechanisms, taking into account of various factors in a plant life cycle, such as material, design, fabrication, installation, inspection, operation, inservice inspection, repair and replacement can be performed. This is also based on Monte Carlo method. This method has a basis in a conventional fracture mechanics approach but has been extended to have a variety of new functions. Such new functions involve the evaluation of crack initiation and propagation, and the evaluation of crack depth distribution in a structure subjected to cyclic thermal stress; also included is the function for realistic
simulation of the effect of inservice inspection, modeling the difference of effect of inspection between a case where a fixed location is inspected successively and a case where location for inspection is changed at every inspection. Further explanation of those is given below.

The above two examples of new functions that have been implemented to REAL-P are briefly described below:

**Example 1: Evaluation of crack depth density distribution**

An example of evaluation of crack depth distribution is shown in Fig.2.3-3 (see [11], for the detail). This figure illustrates an example of a result of crack depth density distribution evaluation of cracks observed in a structural specimen which was subjected to hot and cold sodium alternatively that produced cyclic thermal stress in the specimen. A crack depth density distribution obtained by evaluation using REAL-P is compared with an observed distribution. The principle of this evaluation is as follows: First, we consider a joint probability density function $f(x_1,\ldots,x_n)$, where $x_1, \ldots, x_n$ are basic random variables. Then, a portion of the joint probability density function that corresponds to a specific crack depth $a$ is integrated, and the density $g(a)$, density of cracks whose depth is $a$, is obtained.

\[
g(a) = \int_{(x_1,\ldots,x_n) \in \mathcal{S}} f(x_1,\ldots,x_n) \, ds
\]

where, $\mathcal{S} = \{(x_1,\ldots,x_n) | h(x_1,\ldots,x_n) = a\}$

Example 2: Modeling of sampling methods in inservice inspection

There are two kinds of sampling methods for inservice inspections; one is random sampling and the other is the inspection of a fixed location. REAL-P deals with both kinds of sampling methods based on the following modeling: In the case of the inspection of a fixed location, an assumption is made that a crack exists in the location to be inspected. Probability of not detecting the crack while inspection is calculated by Eq. (2.3-2). In this equation, it is assumed that the ratio of the portion that is inspected in the location is $\alpha$.

\[
P = (1-\alpha) + \alpha(1-P_{d_1})(1-P_{d_2})(1-P_{d_3})(1-P_{d_4}) \cdots \cdot (1-P_{d_n})
\]
P : Probability that a crack is not detected
P_{dn} : Probability of detection at the n\textsuperscript{th} inspection
\alpha : Ratio of inspected portion in a location

For random sampling, the portion inspected is different at every inspection. The whole area is covered by times of inspection. In this case, a probability of not detecting a crack is expressed by Eq. (2.3-3).

\[ P = \frac{1}{N} \left( 1 - P_d \right) \left( 1 - P_{\alpha N+1} \right) \left( 1 - P_{\alpha 2 \times N+1} \right) \cdots \]
\[ + \frac{1}{N} \left( 1 - P_d \right) \left( 1 - P_{\alpha 2 \times N+2} \right) \left( 1 - P_{\alpha 2 \times 2 \times N+2} \right) \cdots \]
\[ + \frac{1}{N} \left( 1 - P_d \right) \left( 1 - P_{\alpha 3 \times N+3} \right) \left( 1 - P_{\alpha 3 \times 2 \times N+3} \right) \cdots \]
\[ \cdots \]
\[ + \frac{1}{N} \left( 1 - P_d \right) \left( 1 - P_{\alpha N} \right) \left( 1 - P_{\alpha_{4 \times N}} \right) \cdots \]

\[ (2.3-3) \]

N : Number of times of inspection

For example, when 100\% of a location is inspected in 4 times (N=4), the ratio of the location inspected at a single inspection is 25\%. In this case, Eq. (2.3-3) becomes Eq. (2.3-4).

\[ P = \left( 1 - \frac{1}{4} (P_{d1} + P_{d2} + P_{d3} + P_{d4}) \right) \]

\[ (2.3-4) \]

2.3.3 REAL-B

REAL-B is for reliability evaluation based on simplified evaluation methods such as First Order Reliability Method (FORM). This corresponds to recent requirements in design to implement reliability evaluation principles. In the design procedure, performing numerical calculations using methods such as Monte Carlo method could be computational burden. For example, REAL-B provides tools for calculating partial safety factors that can directly be implemented in design codes. It is also pointed out that simplified methods is of help to quantitatively access the uncertainties associated with the structural reliability evaluation [12].

2.3.4 Concluding remarks

In order to implement structural reliability evaluation into the design and maintenance of fast reactor components such as vessels and piping, a material and structure reliability evaluation system MSS-REAL has been developed. This system is composed of multiple
objects and intended to meet a wide range of needs from designers and engineers. One of the basic philosophies of this system is to simulate as realistically as possible mechanisms and factors that affect the integrity of structure during its life cycle. For example, the density distribution of cracks that initiate and propagate due to cyclic thermal stresses can be reproduced fairly accurately. Technologies for realistic life-cycle reliability evaluation can be a basis of margin exchange which is one of the key elements of the System Based Code concept.

REFERENCES


Fig. 2.3-1  Structure of MSS-REAL

- MSS-REAL
  - REAL-A  Statistical analysis of material data
  - REAL-B  Structural reliability evaluation by simplified methods (Under development)
  - REAL-D  Creep-fatigue evaluation
             Determination of design allowables
  - REAL-F  Interface with finite element analysis
             (Stress classification, etc)
  - REAL-P  Life-cycle structural reliability evaluation
             By Monte Carlo method
Fig. 2.3-2 Functions of MSS-REAL

<table>
<thead>
<tr>
<th>Item</th>
<th>Contents</th>
</tr>
</thead>
<tbody>
<tr>
<td>Material</td>
<td>SUS304</td>
</tr>
<tr>
<td>Examination method</td>
<td>Fatigue testing under Sodium environment</td>
</tr>
<tr>
<td>Temperature</td>
<td>$600^\circ\text{C} - 300^\circ\text{C}$</td>
</tr>
<tr>
<td>Number of cycles</td>
<td>$2000$ cycles</td>
</tr>
</tbody>
</table>

Fig. 2.3-3 An example of crack depth distribution evaluation
2.4 PEPPER

2.4.1 PEPPER

2.4.1.1 Features of PEPPER  PEPPER (Probabilistic Evaluation Program for Pipes aiming Economical and Reliable design) is a nonlinear PFM code applicable to reliability evaluation of pipes having a single crack. For PFM analysis when handling multiple cracks, PEPPER-M described later in 2.4.2 is applicable.

PEPPER was developed for application to fast reactors initially used in high temperature conditions. In fast reactor piping, plastic and creep deformation not only at a local portion around a crack but also at a general portion of a pipe are assumed. Therefore, in PEPPER, crack growth evaluation using not only a stress intensity factor (fatigue crack growth and SCC (Stress Corrosion Cracking) crack growth) as a linear fracture mechanics parameter but also J-integral (fatigue crack growth) and creep J-integral (creep crack growth) as non-linear fracture mechanics parameters can be dealt with. Circumferential and axial surface and embedded cracks can be evaluated in PEPPER.

For fracture evaluation, in linear conditions, a linear fracture mechanics, a limit load, an elastic-plastic fracture mechanics and a two-parameter method can be selected while in nonlinear conditions, a limit load evaluation method and a two-parameter evaluation method can be selected.

A general PFM analysis code targets ‘initial cracks’, whereas PEPPER can perform failure probability (leak and break probabilities) evaluation taking into account a probability of cracks in service.

2.4.1.2 Functions of PEPPER  PEPPER has the following functions.

1) Capable to address initiation and growth of a single crack during operation.
2) Capable to take into account fatigue, SCC and creep crack growth.
3) Capable to perform integrity assessment during an evaluation period with the Rules on fitness-for-service, based on ISI (In-Service Inspection) results.
4) Capable to apply a linear fracture mechanics, a limit load, an elastic-plastic fracture mechanics and a two-parameter method for fracture evaluation.
5) Enables to calculate a conditional probability of pipe fracture caused by an earthquake.
6) Performs the stratified Monte Carlo simulation to crack geometry and crack position (only for embedded cracks) in order to improve the calculation efficiency.
7) Avoids a recurring random number issue when the number of samples is huge enough, by using Mersenne Twister for random numbers.
2.4.1.3 Failure evaluation model  The calculation flow of failure analysis by PEPPER is shown in Fig. 2.4-1.

![Fig. 2.4-1 Basic flow of evaluation](image)

In PEPPER, assuming a crack in a piping evaluation portion, a failure behavior as a result of crack growth is evaluated. Crack growth behavior is evaluated using the following equations.

Fatigue crack growth:
\[
\frac{da}{dN} = C\Delta K^m, \quad \frac{da}{dN} = C\Delta J_f^m
\]  \hspace{1cm} (2.4-1)

SCC crack growth:
\[
\frac{da}{dt} = CK^m
\]  \hspace{1cm} (2.4-2)

Creep crack growth:
\[
\frac{da}{dt} = C\mathcal{J}_c^m
\]  \hspace{1cm} (2.4-3)

Where \( K \) is a stress intensity factor, \( J_f \) is a J-integral, \( J_c \) is a creep J-integral, and \( C \) and \( m \) are material-specific parameters. For deterministic evaluations, based on the material test results, a slope \( m \) is set on a double logarithmic graph of a relation between fracture mechanics parameters (\( K \), \( \Delta K \), \( \Delta J_f \), and \( J_c \)) and \( \frac{da}{dN} \), or \( \frac{da}{dt} \), and then \( C \) is decided to envelope a data group on the conservative side. For stochastically handling, \( m \) is usually fixed in the same as the deterministic evaluations, assuming that \( C \) has a probabilistic distribution. Therefore, at the same time as giving an initial crack size for one sample, \( C \) is also sampled by the Monte Carlo method to calculate the growth of the crack using Eqs. (2.4-1) - (2.4-3).

For determining fracture of a cracked portion, a linear fracture mechanics, a limit load, an elastic-plastic fracture mechanics and a two-parameter method are used. For material strength, which is a determination criterion in the crack evaluation, a plane strain fracture toughness \( (K_{ic}) \) and a flow stress \( (\sigma_f) \) are used. In deterministic
evaluations, fracture strength assuming on the conservative side based on the material test results is used in the evaluations. If handled stochastically, it is assumed that these material strengths exhibit a probabilistic distribution. At the same time giving one initial crack size, fracture strength is sampled by the Monte Carlo method to determine the failure mode described below.

Initial crack is assumed to be a semi-elliptical (fully circumferential crack is also possible) or elliptical (embedded defect) crack. Initial crack size is defined by providing two out of the following three parameters ((1) is always required) by a probability density function.

1. Probability distribution of crack depth, \( a/t \)
2. Probability distribution of aspect ratio, \( a/c \)
3. Probability distribution of crack length, \( c \)

In order to perform fracture mechanics assessment considering these probability distributions, initial crack sizes are sampled by the Monte Carlo method. Two types of cracks: surface crack and embedded crack, can be addressed as initial crack geometry. The direction of a crack shall be subject to circumferential or axial direction. The circumferential direction of the surface crack is directed to an outer surface and an inner surface.

With the Monte Carlo method, each of \( N \) samples is evaluated and the failure probability at the time of occurrence if \( n \) samples among \( N \) is determined by the following equation.

\[
P = \frac{n}{N}
\]  

(2.4-4)

If the probability function of the evaluation conditions of interest by the Monte Carlo method is of normal or log-normal distribution, the shape of function around the center can be easily approximated even with normal Monte Carlo sampling. However, in order to precisely approximate the distribution of both end regions (regions with low frequency of occurrence), it is necessary to perform a large number of samplings as a whole. For example, with a probability density function shown in Fig. 2.4-2, it is assumed that the probability of the interval \( X_1 \) to \( X_2 \) (integral value) is \( 10^{-5} \). Assuming that 100 samples are required to simulate the distribution shape of the interval, the total number of samples \( (N) \) is required as follows.

\[
N = \frac{100}{10^{-5}} = 10^7
\]

(2.4-5)

Thus a huge volume of calculation will be required to calculate the failure behavior of each sample for each cell. In order to solve this issue, PEPPER uses the stratified Monte Carlo sampling method for sampling crack geometries.
In the stratified Monte Carlo sampling, when a probability density function as shown in Fig. 2.4-2 is given, in order to efficiently indicate a function shape of the portion of the section $X_1 - X_2$ first, sampling is limited to the section $X_1 - X_2$ as in Fig. 2.4-3 and simulation is performed with the probability of this section being 1.0. Then a probability (integral value) of the section $X_1 - X_2$ of the probability density in Fig. 2.4-3 is multiplied to this result ($n/N$). In this way, the entire section is divided into several layers, sampling is performed with the probability in the respective divided sections being 1.0, and by multiplying the existing probability of the corresponding layer to perform efficient evaluation.
In PEPPER, stratified sampling is possible for two parameters \( a/t \) and \( a/c \), or \( a/t \) and \( c \), and further \( e/t \) (center position of a buried crack) of initial crack size distribution. In this approach, assuming a cell with parameters \( a/t \) and \( a/c \) as shown in Fig. 2.4-4, for example, sampling is performed for initial crack size for each cell, crack growth analysis and determination of fracture mode is performed, and all the cell results are integrated to provide a failure probability.

In the middle of repetition crack growth analysis with age, it is determined whether or not break or leakage may occur using the crack size at that point of time. Material strength as an evaluation condition is given as a random variable. In addition, crack depth that can be considered as penetration can be handled as a probability density.

Crack stability evaluation is mentioned later in Section 2.4.1.4, and a method for considering a pre-service inspection (hereinafter, PSI) and in-service inspection (hereinafter, ISI) in Section 2.4.1.5 below.

Fracture mechanics parameters \((K, J_f, J_c)\) to be applied to crack growth analysis is a function of crack size and applied stress. Though stress range and its generation timing may be irregular during plant operation, the applied stress is handled as follows for the purpose of simple calculation:

1) Stress range assumes a determined value for each transient event.

2) Timing of transient is provided by random sampling assuming an average frequency for each transient or Poisson distribution.

Fig. 2.4-4 Cell concept of initial defect sizes
The crack growth due to applied stress cycle is given by deterministic calculation as in the following equation. In this evaluation, crack growth rate as evaluation conditions and largeness of stress cycle are given as random variables.

\[ a_i = a_0 + \Delta a_i \quad (2.4-6) \]
\[ c_i = c_0 + \Delta c_i \quad (2.4-7) \]

Where \( a_i \) is a crack depth after crack growth, \( a_0 \) is an initial crack depth, and \( \Delta a_i \) is the amount of crack growth in depth direction. Similarly, \( c_i \) is a crack length after crack growth, \( c_0 \) is an initial crack length, and \( \Delta c_i \) is the amount of crack growth in length direction.

Applied load to be used in fracture assessment may be defined separately from the load to be used in crack growth evaluation. Basically, the maximum load that is applied during transients is usually used.

2.4.1.4 Loading method of transient event

In PEPPER, for each transient event, a maximum value, a minimum value and an average frequency of applied stress in the transient event are entered. Loading sequence of transient events may be different between handling each transient event occurred at constant intervals and that occurred by the Poisson distribution. In PFM code, it is possible to handle either of these two types for each transient event.

For the Poisson distribution, generation intervals of each transient event are determined by the following equation.

\[ f(t) = \lambda \exp(-\lambda t) \quad (2.4-8) \]

Where \( t \) is the next timing of occurrence of transient event (year), and \( \lambda \) is an average frequency (year/times).

Procedures for determining the loading sequence of transient events to be evaluated in crack growth analysis are shown in Fig. 2.4-5. Where, six types A-F of transient events are assumed as transients (Fig. 2.4-5(1)).

a. Sequence at starting operation

At entering the transient events, the initial card shall be a transient event associated with startup/shutdown. For this reason, the transient event defined with the input data first is handled to be one occurred in year 0. The remaining five types of transient events are sampled by the Monte Carlo method so that in the first year of occurrence, the average frequency follows the Poisson distribution and are rearranged from the earliest occurrence (Fig. 2.4-5(2)).
b. Sequence in crack growth analysis

1) After performing crack growth calculation by the first entered transient event, it is sampled when the pertinent transient event may occur next time (Fig. 2.4-5(3)).

2) By interrupting the next transient event of 1) in the sequence arranged in (a) above, rearrangement is performed (Fig. 2.4-5(4)).

3) As a result of 2), using the transient event that occurs the earliest, crack growth analysis is performed. It is sampled when in years the transient event will occur next time in the same as in 1) and sorted in the same as in 2).

4) The above process 3) is repeated.

<table>
<thead>
<tr>
<th>Transient</th>
<th>Time</th>
<th>Timing of occurrence</th>
</tr>
</thead>
<tbody>
<tr>
<td>A</td>
<td></td>
<td>0</td>
</tr>
<tr>
<td>B</td>
<td>0.4</td>
<td>0.1</td>
</tr>
<tr>
<td>C</td>
<td>0.1</td>
<td>0.2</td>
</tr>
<tr>
<td>D</td>
<td>0.3</td>
<td>0.4</td>
</tr>
<tr>
<td>E</td>
<td>0.5</td>
<td></td>
</tr>
<tr>
<td>F</td>
<td>0.2</td>
<td></td>
</tr>
</tbody>
</table>

The number and frequency (timing of occurrence) of thermal transients are read from an input card.

<table>
<thead>
<tr>
<th>Transient</th>
<th>Time</th>
<th>Timing of occurrence</th>
</tr>
</thead>
<tbody>
<tr>
<td>A</td>
<td></td>
<td>0</td>
</tr>
<tr>
<td>C</td>
<td></td>
<td>0.1</td>
</tr>
<tr>
<td>F</td>
<td></td>
<td>0.2</td>
</tr>
<tr>
<td>D</td>
<td></td>
<td>0.3</td>
</tr>
<tr>
<td>B</td>
<td></td>
<td>0.4</td>
</tr>
<tr>
<td>E</td>
<td></td>
<td>0.5</td>
</tr>
</tbody>
</table>

Transients are rearranged from the earliest based on the timing of occurrence.

<table>
<thead>
<tr>
<th>Transient</th>
<th>Time</th>
<th>Timing of occurrence</th>
</tr>
</thead>
<tbody>
<tr>
<td>A</td>
<td></td>
<td>0.45</td>
</tr>
<tr>
<td>C</td>
<td></td>
<td>0.1</td>
</tr>
<tr>
<td>F</td>
<td></td>
<td>0.2</td>
</tr>
<tr>
<td>D</td>
<td></td>
<td>0.3</td>
</tr>
<tr>
<td>B</td>
<td></td>
<td>0.4</td>
</tr>
<tr>
<td>E</td>
<td></td>
<td>0.5</td>
</tr>
</tbody>
</table>

Determine the next timing of occurrence of the transient after Crack growth analysis with the first transient load.

<table>
<thead>
<tr>
<th>Transient</th>
<th>Time</th>
<th>Timing of occurrence</th>
</tr>
</thead>
<tbody>
<tr>
<td>C</td>
<td></td>
<td>0.1</td>
</tr>
<tr>
<td>F</td>
<td></td>
<td>0.2</td>
</tr>
<tr>
<td>D</td>
<td></td>
<td>0.3</td>
</tr>
<tr>
<td>B</td>
<td></td>
<td>0.4</td>
</tr>
<tr>
<td>A</td>
<td></td>
<td>0.45</td>
</tr>
<tr>
<td>E</td>
<td></td>
<td>0.5</td>
</tr>
</tbody>
</table>

Rearrange according to the next occurrence determined.

(3) and (4) are repeated beyond.

Fig. 2.4-5 Approach of loading order of transients
The above calculation is repeated, and when the accumulated time reaches the plant operation life for calculation, the crack growth calculation ends. Here, if the sample for calculation shows break or leak in the middle, the calculation is aborted. It should be noted that if the frequency of occurring transient events is high and it is designated to collectively calculate several times of crack growth by the input data, the number of years, given by multiplying a scale factor of the integrated number of times by the interval to the next occurrence year in the process after 3), may be used as the next year of occurrence.

2.4.1.5 Equations applied for crack stability assessment

A linear fracture mechanics, a limit load, an elastic-plastic fracture mechanics and a two-parameter method can be applied for crack stability evaluation. The criteria for each method are shown below.

a. Linear fracture mechanics

Fracture criteria based on the linear fracture mechanics evaluation method shall be as follows:

\[ K_a \text{ or } K_c \geq K_c \]

Unstable fracture (2.4-9)

Where \( K_a \) is a stress intensity factor in crack depth direction, and \( K_c \) is a stress intensity factor in crack length direction.

b. Limit load (elastic)

Fracture criteria based on the limit load shall be as follows:

\[ M_{app} \geq M_{cr} \]

Unstable fracture (2.4-10)

Where \( M_{app} \) is an acting moment, and \( M_{cr} \) is an unstable fracture limit moment based on plastic catastrophe criteria.

c. Limit load (elastic-plastic)

Fracture criteria based on the limit load of an elastic-plastic field shall be as follows:

\[ \sigma_a \geq \sigma_f \]

Unstable fracture (2.4-11)

Where \( \sigma_a \) is a net sectional stress, and \( \sigma_f \) is a flow stress.

d. Elastic-plastic fracture mechanics

Fracture criteria based on the elastic-plastic fracture mechanics shall be as follows:

\[ M_{app} \geq \frac{M_{cr}}{Z} \]

Unstable fracture (2.4-12)

Where \( Z \) is a Z-factor.
e. Two-parameter method

When using the two-parameter method, fracture is determined as follows:

\[
\begin{align*}
L_r & \geq L_r^{\text{MAX}} \\
K_r & \geq K_r^{\text{FAC}}
\end{align*}
\]

Unstable fracture \hspace{1cm} (2.4-13)

Where \( L_r \) is a parameter of plastic collapse criteria for primary load, \( K_r \) is a parameter of brittle fracture criteria for all applied load, and \( L_r^{\text{MAX}} \) and \( K_r^{\text{FAC}} \) are limit values, respectively.

**2.4.1.6 Consideration of non-destructive testing** In PFM, PSI and ISI can be considered as non-destructive testing. In order to consider these non-destructive tests, presence of PSI in input data, the number of times and timing of performance of ISI are defined.

While in realistic non-destructive inspection, a flaw detected by the non-destructive inspection is repaired and deleted, if this concept is brought directly into PFM, the samples designated initially will reduce every time the non-destructive inspection is performed, arising a question of impossibility of properly simulating the set probability density function. In order to avoid such a problem, in the PFM code, the effect of the non-destructive inspection is considered by reducing the existing probability of the samples for calculation (cracks with an initial size). Specifically, the evaluation is performed as in the following manner.

The non-detection probability due to PSI or ISI is defined as \( P_{\text{ND}} \). This non-detection probability is generally defined by either a crack depth or a crack area. If not considering PSI or ISI, \( P_{\text{ND}} = 1 \). Therefore, if one sample shows a break in the middle of the calculated lifetime, it is counted assuming that \( 1 \times P_{\text{ND}} \times P_{\text{ND}} = 1 \times 1 = 1 \) samples are broken.

Here, imagine a case considering PSI or ISI. When considering PSI, a probability of non-detection \( P_{\text{ND}0} \) for an initial size \( a_0 \) is applied, and \( P_{\text{ND}0} \) samples are assumed to be broken. Furthermore, when considering ISI, a probability of non-detection \( P_{\text{ND}1} \) of ISI for the size \( a_1 \) at that time is applied, and \( P_{\text{ND}0} \times P_{\text{ND}1} \) samples are assumed to be broken. When considering the next ISI, moreover, a probability of non-detection \( P_{\text{ND}2} \) of ISI for the size \( a_2 \) at that time is applied, and \( P_{\text{ND}0} \times P_{\text{ND}1} \times P_{\text{ND}2} \) samples assumed to be broken, and a probability of break is aggregated. Thus, the number of cracks counted as break when performing \( n \) times of ISI is \( P_{\text{ND}0} \times P_{\text{ND}1} \times P_{\text{ND}2} \cdots \times P_{\text{ND}n} \) samples assumed to be broken. Since \( P_{\text{ND}0} \), \( P_{\text{ND}1} \), \( P_{\text{ND}2} \), \cdots and \( P_{\text{ND}n} \) are all below 1.0, the probability of break gets smaller each time PSI or ISI is performed than a value when not considering them (see Fig. 2.4-6).
Fig. 2.4-6 Changes in sample existing probability by non-destructive testing
2.4.2 PEPPER-M

2.4.2.1 Features of PEPPER

PEPPER described in the previous section 2.4.1 is a PFM analysis code directed to determine a failure probability targeting a small number of initial defects such as manufacturing defects. Since the manufacturing defects do not largely impact the break probability even in consideration of coalescence in progress because of their low frequency of occurrence, generally a break probability for a single defect is evaluated, and by multiplying an existing probability of the defect, the break probability at an evaluated portion is calculated. However, according to the observation results of SCC cracks occurred in PLR piping of boiling water reactors (BWRs), there were many cases that several cracks occurred in the same welding line. In addition, since its occurrence timing is largely dispersed, an evaluation with a model considering them is necessary in order to properly evaluate the break probability of actual piping where multiple defects such as SCC may occur.

PEPPER-M (Probabilistic Evaluation Program for Pipes aiming Economical and Reliable design for Multiple cracks) is a PFM analysis code developed for the purpose of use in reliability evaluation of recirculation piping of BWR where SCC cracks may occur. PEPPER-M evaluates the occurrence, growth, coalescence and failure of cracks as a sequence and further can evaluate the reliability of piping by systematic evaluation including management of defects due to in-service inspection considered during operation.

2.4.2.2 Functions of PEPPER-M

PEPPER-M has the following functions.

1) Capable to address occurrence, growth and coalescence of multiple circumferential defects of piping.

2) Capable to consider fatigue and SCC as a crack growth mode.

3) Capable to evaluate integrity assessment during an evaluation period with the Rules on fitness-for-service, based on the results of ISI.

4) Can evaluate minimum break strength of a pipe cross-section having plural circumferential cracks.

5) Capable to consider fracture of repaired weld lines.

6) Performs the stratified Monte Carlo simulation to SCC crack growth rate in order to improve the efficiency of the calculation.

7) Avoids a recurring random number issue when the number of samples is huge enough, by using Mersenne Twister for random numbers.

2.4.2.3 Failure evaluation model

Not limited to SCC, in the reliability evaluation for cracks that may initiate in service, there is no guarantee that the crack is single and it is necessary to consider the initiation or coalescence of multiple cracks. In addition, the initiation timing of a crack is not fixed. Therefore, in PEPPER-M, assuming one pipe cross-section as a sample, multiple “crack initiations” having information of “position” and “initiation timing” are given to its cross-section to perform evaluation for the number of
years defined by crack initiation, growth (considering coalescence) and break in the time history. Calculation flow is as shown in Fig. 2.4-7 and the specific procedure is described below.

Fig. 2.4-7  Flowchart of PEPPER-M.

a. Crack initiation process evaluation
Multiple “crack initiations” are given to the piping cross-section (one sample) to make a crack initiated depending on its initiation time.
1) The number of cracks occurred on each piping cross-section is sampled in accordance with its distribution (input conditions).
2) The crack occurrence timing is sampled in accordance with its distribution (input conditions).
3) The depth among the geometry of the crack occurred is fixed and the length distribution is sampled in accordance with its distribution (input conditions).
4) Circumferential position of the crack is sampled by assuming uniform distribution.

5) The distance from the crack to welding fusion boundary is sampled in accordance with its distribution (input conditions).

6) Added length to a crack reaching a welding fusion boundary is determined by entering invariables of the distribution and generating a random number.

For 4), only uniform distribution is considered while for 1), 2), 3), 5) and 6), a constant value, normal distribution, log-normal distribution and exponential distribution can be used. It should be noted that 5) and 6) are inputs needed for SCC crack growth evaluation with different crack growth rate between a base material and weld metal and if the crack growth rate is not different for the base material and the weld metal, those values may be fixed.

b. Crack growth process evaluation

In PEPPER-M, each depth and length growth is evaluated considering SCC and fatigue. The following Paris’ law are used for crack growth evaluation.

\[
\frac{da}{dN} = C \cdot \Delta K^m \tag{2.4-14}
\]

\[
\frac{da}{dt} = C \cdot K^m \tag{2.4-15}
\]

Where \(K\) is a stress intensity factor, and \(\Delta K\) is a stress intensity factor range. Coefficient \(C\) is determined by entering distribution geometry and variables of the distribution and generating a random number. Exponent \(m\) shall be a constant value.

In SCC crack growth, it is possible to consider a lower limit or upper limit of SCC crack growth rate as shown in Fig. 2.4-8. In this region, the crack growth rate is constant regardless of the magnitude of stress intensity factor. A stress intensity factor at the deepest point is used for growth evaluation of crack depth, while a stress intensity factor at the surface point is used for growth evaluation of crack length.

For SCC depth growth, a crack growth rate is different from that of a base material when the crack enters a weld. For this reason, the crack growth rate is switched from the base material to the weld metal. However, growth in the length direction of a crack is kept as the crack growth rate of a base material portion. Since crack growth due to SCC is time-dependent as shown in Eq. (2.4-15), a computation time step is specified. Standard time step is 0.01 year, which can be changed.

Existing standard PFM analysis assuming manufacturing cracks has achieved improvement of analysis accuracy and rationalization of computation time by using the stratified Monte Carlo simulation or a weighted sampling method for an initial geometry (depth and length, or depth and aspect ratio). However, when considering SCC that may initiate after service, a failure probability is largely dependent on the SCC crack growth rate. Therefore, in PEPPER-M, the stratified Monte Carlo simulation is employed for SCC growth rate to achieve rationalization of analysis time and improvement of analysis accuracy.
Specifically, the distribution of coefficient $C$ of Eq. (2.4-15) is divided into multiple cells ($C$ is logarithmically equally divided because of log-normal distribution), and a uniform random number is generated in the cell to prescribe a growth rate. Failure probability is calculated by multiplying the weight of the cells to the calculation result of each cell.

Growth of crack depth and length due to fatigue is calculated by the following equation for austenitic stainless steel in BWR environment.

Note: This figure indicates a case when the ISI interval is five years, there is no failure in the evaluation after the first ISI, and the failure is detected in the evaluation after the second ISI.

Fig. 2.4-8  Evaluation method of repair, leakage and break probabilities with ISI interval of five years.
\[ \frac{da}{dN} = C_F t^{0.5} (\Delta K)^{3.0} (1 - R)^{2.12} \]  \hspace{1cm} (2.4-16)

Here, since fatigue crack growth is very small compared to SCC, the coefficient \(C_F\) is fixed. \(t_r\) is loading time of a transient, and \(R\) is a stress ratio. This evaluation is performed for the number of transient conditions entered.

In the crack growth evaluation, when the crack length exceeds the inner circumference of the pipe, an evaluation is performed by replacing the crack geometry to the fully circumferential crack. In the crack growth evaluation, when the depth of any one of the cracks reaches a limit value (specified in the input), it is regarded as "leakage" and evaluation of this sample is stopped. There are two types of piping failure form: leakage and break. In the code, detection of break is prioritized. Accordingly, the above determination of leakage is performed after determining break described in d. later.

c. Crack coalescence evaluation

If multiple cracks initiate and if the adjacent cracks are grown and each end comes in contact with each other, these cracks are coalesced to make one crack. The depth of the coalesced crack is aligned to the maximum of the coalesced crack and the length is the total value. On the code, the crack with a larger length is remained and the other crack is extinguished. In order to perform such processing, among the distance from the welding boundary to the crack and the depth for the crack entering the weld metal, a value of the remaining longer crack is taken over. Since cracks occur from time to time, some of them may initiate in the previously initiated crack. In such a case, it is regarded as coalescence at the same time as the initiation.

In the code, the time interval for evaluating coalescence can be designated. Before break evaluation in d. described later, coalescence should be performed.

d. Stability evaluation

Permissible bending stress of an evaluation cross-section having cracks is evaluated using the following expression.

\[ S_c = \frac{P_b^{'}}{P_c} - P_m \left(1 - \frac{1}{Z} \right) \]  \hspace{1cm} (2.4-17)

Where \(P_b^{'}\) is a bending stress of plastic collapse, \(P_c\) is a thermal expansion stress, \(P_m\) is a primary general membrane stress, and \(Z\) is a Z-factor (extra coefficient). \(P_b^{'}\) is calculated, considering all the cracks in the cross-section, by determining the neutral axis of bending from a flow stress \(\sigma_f\) of an ingredient.

In the Rules of fitness-for-service, if there are several cracks in the evaluation section and if they do not satisfy the coalescence criteria of fracture evaluation, the fracture evaluation is performed as a single crack. However, for piping, cracks on the same cross-section evaluated may affect the fracture strength. Therefore, in PEPPER-M, a piping plastic collapse strength having multiple circumferential cracks is calculated by the following approach.
[Assumptions]
1) Cylinder shall be a thin cylinder of its average diameter, and a change in cylindrical average diameter due to a crack is not considered.
2) All cracks shall be fan-shaped cracks.

[Method of calculation]
1) Calculate the remaining thickness at an angular position over the entire circumference of a pipe from the position and geometry data of a crack.
2) First, assume an evaluation axis. By the following expression, determine a bending neutral axis with respect to this evaluation axis.

\[ A_f \sigma_f + A_T \sigma_m = A_T \sigma_f \]  \hspace{1cm} (2.4-18)

Where \( A_f \) is an area on the tension side with respect to the neutral axis (except for crackive portion), \( A_T \) is a total cross sectional area, \( A_T \) is an area on the compression side with respect to the neutral axis (including the defective portion), \( \sigma_f \) is a flow stress, and \( \sigma_m \) is a primary general membrane stress.
3) In the code, the neutral axis is varied and iterative computations are performed using the residual thickness to determine a neutral axis angle satisfying an Eq. (2.4-18).
4) Next, a plastic collapse load for this neutral axis is calculated. The plastic collapse load is the sum of the primary moment of the residual thickness at each point on the tension side and the primary moment with respect to the total thickness at each point on the compression side.
5) Assumed angle (\( \phi \)) of the evaluation axis is varied and processes of 1) to 4) above are performed to calculate the plastic collapse load. These steps are performed for the entire periphery, a position at which the plastic collapse load is minimized is set, and the plastic collapse load at the relevant position is used for the plastic collapse load of multiple cracks.

Since these processes take a lot of computation time, for one crack, the neutral axis position is determined by the following equation.

If \((\theta + \beta) \leq \pi\),

\[ \beta = \frac{1}{2} \left( \pi - \frac{a}{t} - \pi \frac{P_m}{\sigma_f} \right) \]  \hspace{1cm} (2.4-19)
\[ P_b' = \frac{2\sigma_f}{\pi} \left( 2 \sin \beta - \frac{a}{t} \sin \theta \right) \]  

(2.4-20)

If \((\theta + \beta) > \pi\),

\[ \beta = \frac{\pi}{2 - (a/t)} \left( 1 - \frac{a}{t} - \frac{P_m}{\sigma_f} \right) \]  

(2.4-21)

\[ P_b' = \frac{2\sigma_f}{\pi} \left( 2 - \frac{a}{t} \right) \sin \beta \]  

(2.4-22)

Where \(\theta\) is a crack angle (half width), \(\beta\) is a moving angle (half width) of neutral axis, \(a\) is a crack depth, \(t\) is a plate thickness.

Flow stress used for break evaluation is determined by performing the Monte Carlo simulation to each evaluation cross-section based on the probability density function to be entered. When the acting primary bending stress exceeds the permissible bending stress of equation (2.4-17), the sample is decided to be "broken". This evaluation is performed for all the load conditions entered. When evaluating the probability of break with respect to a load with a small probability of occurrence such as a large earthquake, a value given by multiplying the probability of an event occurrence to a sample that may be damaged to the load is counted as a probability of break. In this case, the existing probability of the sample is reduced by the probability of break and stability evaluation is continued.

e. Handling of in-service inspection

In PEPPER-M, in-service inspection (ISI) is performed at regular intervals and crack detection and integrity assessment during evaluation period are performed. In the code, when it reaches the timing of ISI performance, an inspection is performed to decide presence of a crack. If there are multiple cracks, one out of two approaches can be selected: a case of calculating a maximum value of a detection probability \(P_D\) of cracks calculated in accordance with a crack detection probability equation designated in input, and a case of calculating with the following equation.

\[ POD = 1 - PND_1 \times PND_2 \cdots PND_n \]  

(2.4-23)

Where \(PND_i\) is a non-detection probability for each crack. In the reliability evaluation, there are "detected crack sizes" detected in ISI and "true crack sizes". In addition, there are "crack sizes for evaluation" evolved during evaluation period in accordance with the Rules on fitness-for-service and used selectively for each evaluation. In the evaluation according to the Rules on fitness-for-service, the "crack sizes on evaluation" are defined on the basis of the "detected crack size" and the growth, coalescence, leakage and break of cracks are evaluated according to the prescription of the Rules on fitness-for-service. Such crack growth evaluation is different in terms of the following from the "true crack sizes".

1) The crack depth for evaluation is decided on the basis of a probability density function based on a true crack depth considering a crack measurement error.

2) A prescribed value of the Rules on fitness-for-service is used as the crack growth rate.
3) In the stability evaluation, a safety factor (permissible states A, B: 2.77, permissible states C, D: 1.39) in accordance with the Rules on fitness-for-service is applied.

4) If the crack depth reaches a limit crack depth on the Rules on fitness-for-service, it is judged as “leakage”.

5) Break evaluation is performed assuming that a load entered depending on the permissible state acts (not considering the frequency of occurrence of a load as in a realistic fracture evaluation).

If it is evaluated that piping failure (leakage or break) may occur during an evaluation period in accordance with the Rules on fitness-for-service, that sample is repaired. In this case, the repair probability of this sample is counted as a crack detection probability $P_D$. For a sample permitted with continued use in the evaluation of the Rules on fitness-for-service or with no crack detected, it is returned to evaluation for an actual crack (evaluation flow on the left in Fig. 2.4-7). In the subsequent evaluation, it is considered that the number of cross-sections evaluated may be decreased by percentage fraction of repair. This process is repeated for each ISI performance.

f. Evaluation of failure probability

Evaluation from a. to e. above is performed for the number of evaluating cross-sections designated in input, and a leakage probability, break probability and repair probability are calculated for every operating year, and by adding these values for the entire cross-section and dividing by the number of cross-sections, a cumulative failure probability $P_f$ is calculated for every operation year. Here, the leakage probability and the break probability are totaled and referred to as the failure probability. Fig. 2.4-8 shows a method of calculating failure probability of each cross-section. Various conditions may be assumed in actual calculation. In the figure, ISI is performed every five years, indicating an example where no failure generated in the evaluation after the first ISI but a failure occurred in the evaluation after the second ISI. Probability in each year is calculated by integrating the leakage probability, break probability and repair probability due to each factor for each year, and finally, these values are integrated over the entire evaluation years to calculate a cumulative probability.

In order to determine the comprehensive probability, update due to repair shall be considered. If repair is performed, it should be assumed that a corresponding new cross-section be added since that year. Fig. 2.4-9 shows this concept. For every year of performing ISI, as an update portion, a value given by multiplying a base probability (failure probability of each year before considering repair) to a repair probability of that year is provided and added with shifting the year. Since re-repair may be considered, as the number of ISI increases, so does the update portion.
2.4.2.3 Calculation results  PEPPER-M can output the following calculation results.

a. Probability by year
   1) Repairing probability (each year)
   2) Leakage probability (each year)
   3) Break probability (each year)
   4) Cumulative repairing probability
   5) Cumulative leakage probability
   6) Cumulative break probability

b. Probability by SCC cell
   1) Cumulative repairing probability
   2) Cumulative leakage probability
   3) Cumulative break probability
   4) Failure (leakage + break)

Fig. 2.4-9  Failure probability evaluation method considering repair.
2.5 SPEC

2.5.1 Summary

Two guideposts of risk-informed approach for nuclear power generation systems are: evaluation of failure frequency of SSC (Systems, Structures and Components) as an initial event; and consequence evaluation in case of SSC failure. Probabilistic risk assessment (PRA) is used in the latter evaluation. Focusing on piping systems, PRA standards and failure frequency of SSC in operation plants are mainly being developed. On the other hand, statistical data and deductive methods are employed in the former evaluation. For active components (e.g. pumps, valves, etc.) in nuclear power plants, the failure frequency data is developed as mentioned above while for static components, no applicable data is not obtained yet because of poorness of statistical data. Since a suitable number of samples are required in order to obtain statistical data for failure of static components, preparation of applicable statistical data is difficult. Therefore, it may be realistic to predict the failure frequency of piping by using the deductive method. When using the deductive method, it is necessary to express the variation of data used in analyses (e.g. piping dimensions, material strength, crack growth behavior, etc.) with a probabilistic model.

Simplified Probabilistic Evaluation System for Cracked Pipes (SPEC) was developed to provide an environment in which reliability can be simply evaluated if the data, such as pipe sizes, material strength, damage progress behavior as mentioned above, is prepared, and to achieve motivation of input to databases and penetration of reliability evaluation. SPEC can perform failure probability evaluation of piping for fatigue and stress corrosion cracking (SCC).

SPEC assumes an initial crack (or detected crack) on piping inner surface and analyzes a failure probability (leakage or break) arising from growth of a crack due to SCC and fatigue by the Monte Carlo simulation. In order to efficient probabilistic calculation, a stratified Monte Carlo sampling method is adopted in which the depth and length of the initial crack are divided into two-dimensional cells and Monte Carlo sampling calculation is performed for each cell.

2.5.2 Specifications of SPEC

2.5.2.1 Basic functions SPEC assumes a single initial crack on a portion to be evaluated (in general, a welding line), samples the geometry, growth rate, fracture strength, etc., averages the evaluation results for a number of samples (percentage of failure sample), and determines a failure probability during the evaluation period.

2.5.2.2 Operation environment SPEC runs on a Microsoft Excel macro (Visual Basic). As for the operation environment, it is operated on Windows 2000 and later version and Microsoft Office 2000 and later version, as an operation system.
2.5.2.3 Analysis logic

PFM analysis in SPEC is following the evaluation items and procedures shown in Fig. 2.5-1.

(1) Input of initial crack size

In SPEC, the geometry of an initial crack can be entered with a depth and an aspect ratio or a depth and a length. These values are given by a probability density function (normal distribution, log-normal distribution or exponential distribution). Using these inputs, the stratified sampling space of crack depth and aspect is defined to perform failure probability calculation by the stratified Monte Carlo method.

(2) Input of crack growth rate

In SPEC, the fatigue and SCC crack growth rate can be entered. These growth rates are represented by Paris’ law shown in Eqs. (2.5-1) and (2.5-2), giving coefficients by a probability density function. The crack growth rates of the Rules on Fitness-for-service for Nuclear Power Plant Components [1] (JSME S NAI-2012, hereinafter, the FFS Codes) and the Rules on Design for Piping Breakage Protection [2] (JSME S ND1-2002, hereinafter, the LBB Codes) of the Japan Society of Mechanical Engineers may be automatically entered. As for fatigue, a multiple ($\alpha_f$) of the crack growth rate of these Codes can be inputted.

\[
\frac{da}{dN} = \alpha_f C_f \Delta K^{m_f} \tag{2.5-1}
\]

\[
\frac{da}{dt} = C_S \Delta K^{m_S} \tag{2.5-2}
\]

where $da/dN$ is a fatigue crack growth rate, $da/dt$ is an SCC crack growth rate, $C_f$ and $C_S$ are coefficients for crack growth rate defined in the Codes, and $m_f$ and $m_S$ are exponents for crack growth law. For SCC, the crack growth rates are different for a heat-affected zone (HAZ) and for weld metal, and accordingly two types of constants ($C_S$) can be inputted.

(3) Input of strength for fracture evaluation

In SPEC, flow stress is inputted as a material strength for fracture evaluation. Flow stress is given in a probability density function.

(4) Input of pre-service inspection and in-service inspection

In SPEC, crack detection probabilities for pre-service inspection and in-service inspection can be considered. A number of proposals have been offered for crack detection probability models. SPEC is equipped with a typical crack detection probability model as shown below [3, 4].

a. Fatigue crack in ferritic steel

\[
\text{PNL-Poor}: \quad POD = \phi \left[ 0.432 + 0.163 \ln \left( \frac{a}{t} \right) \right] \tag{2.5-3}
\]
PNL-Good: \[ POD = \phi \left[ 1.75 + 0.583 \ln \left( \frac{a}{t} \right) \right] \] 
(2.5-4)

PNL-Advanced: \[ POD = \phi \left[ 3.63 + 1.106 \ln \left( \frac{a}{t} \right) \right] \] 
(2.5-5)

LLNL: \[ POD = \text{erf} \left[ 1.33 \ln \left( \frac{A}{A^*} \right) \right] \] 
(2.5-6)
\[ A = \frac{\pi}{2} ac \quad (2c \leq 25.4 \text{ mm}) \]
\[ A = \frac{\pi}{4} 25.4a \quad (2c > 25.4 \text{ mm}) \]
\[ A^* = \frac{\pi}{4} 6.35 \times 25.4 \]

b. Fatigue crack in stainless steel

PNL-Poor: \[ POD = \phi \left[ 0.24 + 1.485 \ln \left( \frac{a}{t} \right) \right] \] 
(2.5-7)

PNL-Good: \[ POD = \phi \left[ 1.526 + 0.533 \ln \left( \frac{a}{t} \right) \right] \] 
(2.5-8)

PNL-Advanced: \[ POD = \phi \left[ 3.63 + 1.106 \ln \left( \frac{a}{t} \right) \right] \] 
(2.5-9)

c. SCC crack in stainless steel [5]
\[ POD = 1 - \exp \left[ - \left( a - 0.3434 \right) \right] \] 
(2.5-10)

In the above equation, \( POD \) is a detection probability, \( \text{erf} \) is an error function, and \( \phi \) is an integral function of standard normal distribution.
\[ \text{erf}(x) = \frac{2}{\sqrt{\pi}} \int_0^x \exp \left( \frac{t^2}{2} \right) dt \] 
(2.5-11)
\[ \phi(x) = \frac{1}{\sqrt{2\pi}} \int_0^x \exp \left( \frac{t^2}{2} \right) dt \] 
(2.5-12)

In the equations of PNL, a detection probability, when \( a/t \) is 0.0, is 0.0, and when \( a/t \) is between 0.0 and 0.05, the detection probability is linearly interpolated. Figs. 2.5-2 and 2.5-3 show detection probabilities of each case. Considering detection error (\( \varepsilon \)) in \( POD \) functions, a crack non-detection probability (\( PND \)) is given by the following equation.
\[ PND = (1 - \varepsilon)(1 - POD) + \varepsilon \] 
(2.5-13)

(5) Crack growth evaluation

Crack growth evaluation is possible according to fatigue and SCC crack growth rates specified in (2) above.
a. Load conditions

For crack growth calculation, the following load conditions are entered.

1) Internal pressure

2) Primary membrane stress: Membrane stress due to external load is inputted.

3) Primary pipe bending stress: Pipe bending stress due to its own weight and earthquake. This stress is different in the circumferential position of a pipe. Conservatively, it is assumed that the maximum bending stress acts on a crack.

4) Secondary pipe bending stress: Pipe bending stress due to thermal expansion. This stress is different in the circumferential position of a pipe as same as primary pipe bending stress. Conservatively, it is also assumed that the maximum bending stress acts on a crack as same as primary pipe bending stress.

5) Residual stress: Residual stress caused by welding is inputted as a function of a distance from the inner surface. Sixth-order polynomial coefficients are calculated in the program. In addition, it is able to directly input sixth-order polynomial coefficients.

6) Transient stress: For fatigue crack growth evaluation, stresses at two points constituting $\Delta K$ are inputted. Input items should be internal pressure, primary pipe bending stress, secondary pipe bending stress, bending stress in thickness.

b. Stress intensity factor

Stress intensity factor of surface crack is calculated by the following equations.

$$K = \left[ A_p G_0 + \sum_{i=0}^{6} A_i G_i \right] \sqrt{\frac{\pi a}{Q}}$$  \hspace{1cm} (2.5-14)

$$Q = 1 + 4.593 \left( \frac{2a}{c} \right)^{1.65}$$  \hspace{1cm} (2.5-15)

$$\sigma = \sum_{i=0}^{6} A_i \left( \frac{x}{a} \right)^i$$  \hspace{1cm} (2.5-16)

where $a$ is a crack depth, $c$ is a crack half length, $A_i$ is coefficient to express a stress distribution given by Eq. (2.5-16), $A_p$ is an internal pressure, $G_i$ is a non-dimensional stress intensity factor (according to the FFS Codes). Solutions of a plate and a cylinder can be applied to stress intensity factors. When the length of surface semi-elliptical crack is greater than the inner circumference of a pipe, it is replaced by a 360˚ circumferential crack to calculate the stress intensity factor.
c. Cut-over of crack growth rate

SCC cracks initiate in HAZ and grow into weld metal. Since SCC crack growth is different in between HAZ and weld metal, the crack growth rate in the depth direction is switched from that of HAZ to that of weld metal when the crack tip reaches the weld metal.

(6) Fracture evaluation

The limit load and the elastic-plastic fracture mechanics can be applied as fracture assessment methods. These shall be dealt with in the same manner as the FFS Codes [1]. Bending stress at the time of plastic collapse is evaluated by the following equations.

\[
\begin{align*}
P'_b &= \frac{2\sigma_f}{\pi Z} \left( 2\sin \beta - \frac{a}{t} \sin \theta \right) \\
\beta &= \frac{1}{2} \left( \pi - \frac{a}{t} \theta - \frac{P_m}{\sigma_f} \pi \right) \\
\end{align*}
\]

(2.5-17)

\[
\begin{align*}
\beta &= \frac{\pi}{2 - \frac{a}{t}} \left( \pi - \frac{a}{t} \frac{P_m}{\sigma_f} \right) \\
\end{align*}
\]

(2.5-18)

For \( \beta > \pi - \theta \)

where \( \theta \) is a crack angle, \( \beta \) is an angle of neutral axis, \( t \) is a thickness, \( \sigma_f \) is a flow stress, \( Z \) is a Z-factor, and \( P'_b \) is a bending stress at the time of plastic collapse. The crack depth used for evaluation is a value determined considering a sizing error (expressed by normal distribution). For cracks detected in ISI, the growth of them is calculated for a specified evaluation period, and stability is assessed at the end of the period. For a sample of which stability cannot be secured, calculation is aborted and the existing probability of the sample is accumulated as a repair probability. Flow stress is calculated by inputted probability density function. In addition, Z-factor can be selected from the following equations in accordance with the material and a crack angle [1].

1) GTAW (TIG) and SMAW of austenitic stainless steel pipe

\[
2\theta \leq 60^\circ : Z = 0.2921 \log \left( \frac{OD}{25} \right) + 0.986
\]

\[
2\theta > 60^\circ : Z = 0.3061 \log \left( \frac{OD}{25} \right) + 1.032
\]

(2.5-19)

2) SAW of austenitic stainless steel pipe and cast austenitic stainless steel pipe of ferrite content less than 20%
If the applied bending stress is larger than the collapse bending stress given by the following expressions, it is judged as break and the calculation of that sample shall be aborted. Existing probability at that time is counted as a probability of break. The loads used for fracture assessment based on the FFS Codes and for fracture assessment of actual cracks are different, and each must be inputted.

1) Case when probability of break is calculated

\[ P_b = P_{b1} + P_{b2} \]  \hspace{1cm} (2.5-22)

2) Case when integrity assessment is performed based on the FFS Codes

\[ P_b = SF \left( P_m + P_{b1} \cdot \frac{P_{b2}}{SF} \right) \]  \hspace{1cm} (2.5-23)

where \( P_b \) is an applied bending stress, \( P_m \) is a membrane stress, \( P_{b1} \) is a primary bending stress, \( P_{b2} \) is a secondary bending stress, and \( SF \) is the following safety factor.

Service level A, B: 2.77
Service level C, D: 1.39

(7) Leak evaluation
A crack depth for judging leakage (for example, a value of 80% of thickness) may be designated. In SPEC, the number of samples exceeding the designated value is summed to calculate a leakage probability.

(8) Integrity assessment
In SPEC, integrity assessment for a detected crack in ISI can be performed based on the FFS Codes.

(9) Residual stress distribution
For welding residual stress distribution that is important for SCC crack growth evaluation, a value for a typical pipe can be selected.

**2.5.2.4 Input items** Example input items for SPEC are shown in Fig. 2.5-4. Since input data using values defined in the FFS Codes and the LBB Codes is supported by using comment function of EXCEL, most of data can be inputted without the need to refer to the documentation manuals.
1) Piping specifications
   Internal diameter, Thickness

2) Calculation conditions
   Evaluation period, Number of SCC evaluations, Number of fatigue evaluations, ISI frequency, Number of cell divisions, Stress intensity factor calculation method, Fatigue crack growth calculation method, SCC growth coefficient, PSI detection probability equation, PSI crack non-detection probability, ISI detection probability equation, ISI crack non-detection probability

3) Load conditions
   Internal pressure, Primary membrane stress, Primary tube bending stress, Secondary tube bending stress

4) Transient conditions
   Transient condition name, Load cycle (times/year), Temperature, Load raising and lowering time [s], Internal pressure stress, Primary tube bending stress, Secondary tube bending stress, In-plate bending stress

5) Load conditions for break evaluation
   Primary membrane stress, Primary tube bending stress, Secondary tube bending stress

6) Load conditions for integrity assessment
   Primary membrane stress, Primary tube bending stress, Thermal expansion stress, In-service condition

7) Residual stress
   Coefficient of six-order function defined by relative plate thickness ratio (x/t)

8) Initial crack distribution
   Initial crack depth/plate thickness, Initial crack half length/half perimeter

9) SCC growth rate, Fatigue crack growth rate

10) Flow stress
    Tensile strength, Yield stress

11) Transient conditions
    Transient type, Frequency of occurrence, Stress conditions

2.5.2.5 Output items
   SPEC outputs conditional leakage probability, conditional break probability and conditional repairing probability; and graph display is possible in a linear or logarithmic scale. Example results of SPEC analysis are shown below.

   1) Failure (leakage + break) probability per sample for each cell of initial crack depth and length, Leakage probability, Break probability.
      ・ Example display of failure probability distribution in cell (Table 2.5-1)
      ・ Example display of failure probability of each cell (Fig. 2.5-5)

   2) Failure (leakage + break) probability, leakage probability, break probability of each calculation year for all samples, and their cumulative values.
· Example of time history of failure probability (Table 2.5-2)
· Time history of failure probability (Fig. 2.5-6)

REFERENCES


Table 2.5-1 Example of failure probability distribution in cell

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Table 2.5-2 Example of time history of failure probability
Year

Leak+Break
0 9.29E-03
1 5.92E-04
2 1.08E-03
3 4.82E-04
4 4.90E-04
5 8.27E-04
6 8.54E-04
7 6.92E-04
8 7.04E-04
9 3.37E-04
10 7.50E-04
11 6.02E-04
12 1.69E-04
13 3.77E-04
14 4.45E-04
15 6.92E-04
16 6.14E-04
17 7.97E-04
18 6.11E-04
19 9.44E-04
20 5.62E-04
21 1.12E-03
22 7.61E-04
23 6.43E-04
24 5.28E-04
25 8.52E-04
26 9.63E-04
27 7.29E-04
28 5.16E-04
29 6.83E-04
30 8.56E-04
31 6.21E-04
32 7.57E-04
33 6.05E-04
34 1.02E-03
35 1.12E-03
36 9.99E-04
37 1.61E-04
38 3.21E-04
39 6.22E-04
40 4.05E-04

Probability of each year
Leak
Break
9.28E-03
8.69E-06
5.91E-04
6.48E-07
1.08E-03
1.35E-06
4.81E-04
5.08E-07
4.89E-04
1.47E-06
8.26E-04
1.15E-06
8.54E-04
3.20E-07
6.92E-04
3.20E-07
7.04E-04
3.20E-07
3.36E-04
3.20E-07
7.50E-04
5.45E-07
6.02E-04
0.00E+00
1.67E-04
1.63E-06
3.77E-04
0.00E+00
4.44E-04
1.09E-06
6.92E-04
0.00E+00
6.14E-04
0.00E+00
7.97E-04
0.00E+00
6.11E-04
0.00E+00
9.44E-04
5.45E-07
5.62E-04
0.00E+00
1.12E-03
9.28E-07
7.60E-04
9.28E-07
6.43E-04
0.00E+00
5.28E-04
0.00E+00
8.52E-04
0.00E+00
9.63E-04
0.00E+00
7.29E-04
0.00E+00
5.16E-04
0.00E+00
6.83E-04
0.00E+00
8.56E-04
0.00E+00
6.21E-04
0.00E+00
7.57E-04
0.00E+00
6.05E-04
0.00E+00
1.02E-03
0.00E+00
1.12E-03
0.00E+00
9.97E-04
1.86E-06
1.61E-04
0.00E+00
3.20E-04
9.28E-07
6.21E-04
9.28E-07
4.05E-04
0.00E+00

Repair Leak+Break
0.00E+00 9.29E-03
0.00E+00 9.88E-03
0.00E+00 1.10E-02
0.00E+00 1.14E-02
0.00E+00 1.19E-02
0.00E+00 1.28E-02
0.00E+00 1.36E-02
0.00E+00 1.43E-02
0.00E+00 1.50E-02
0.00E+00 1.53E-02
0.00E+00 1.61E-02
0.00E+00 1.67E-02
0.00E+00 1.69E-02
0.00E+00 1.72E-02
0.00E+00 1.77E-02
0.00E+00 1.84E-02
0.00E+00 1.90E-02
0.00E+00 1.98E-02
0.00E+00 2.04E-02
0.00E+00 2.14E-02
0.00E+00 2.19E-02
0.00E+00 2.30E-02
0.00E+00 2.38E-02
0.00E+00 2.44E-02
0.00E+00 2.50E-02
0.00E+00 2.58E-02
0.00E+00 2.68E-02
0.00E+00 2.75E-02
0.00E+00 2.80E-02
0.00E+00 2.87E-02
0.00E+00 2.96E-02
0.00E+00 3.02E-02
0.00E+00 3.09E-02
0.00E+00 3.15E-02
0.00E+00 3.26E-02
0.00E+00 3.37E-02
0.00E+00 3.47E-02
0.00E+00 3.48E-02
0.00E+00 3.52E-02
0.00E+00 3.58E-02
0.00E+00 3.62E-02

- 75 -

Cumulative probability
Leak
Break
9.28E-03
8.69E-06
9.87E-03
9.34E-06
1.10E-02
1.07E-05
1.14E-02
1.12E-05
1.19E-02
1.27E-05
1.27E-02
1.38E-05
1.36E-02
1.41E-05
1.43E-02
1.44E-05
1.50E-02
1.48E-05
1.53E-02
1.51E-05
1.61E-02
1.56E-05
1.67E-02
1.56E-05
1.69E-02
1.73E-05
1.72E-02
1.73E-05
1.77E-02
1.84E-05
1.84E-02
1.84E-05
1.90E-02
1.84E-05
1.98E-02
1.84E-05
2.04E-02
1.84E-05
2.13E-02
1.89E-05
2.19E-02
1.89E-05
2.30E-02
1.98E-05
2.38E-02
2.08E-05
2.44E-02
2.08E-05
2.49E-02
2.08E-05
2.58E-02
2.08E-05
2.68E-02
2.08E-05
2.75E-02
2.08E-05
2.80E-02
2.08E-05
2.87E-02
2.08E-05
2.95E-02
2.08E-05
3.02E-02
2.08E-05
3.09E-02
2.08E-05
3.15E-02
2.08E-05
3.25E-02
2.08E-05
3.37E-02
2.08E-05
3.47E-02
2.26E-05
3.48E-02
2.26E-05
3.51E-02
2.35E-05
3.58E-02
2.45E-05
3.62E-02
2.45E-05

Repair
0
0
0
0
0
0
0
0
0
0
0
0
0
0
0
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Note) In the integrity evaluation according to the FFS Codes in the dotted lines, unlike in realistic evaluation of cracks in the left flow, virtual crack growth is evaluated during the evaluation period and whether or not to satisfy an allowable value according to the FFS Codes is evaluated.

Fig. 2.5-1 Flow of calculation
Fig. 2.5-2 Probability of crack detection (ferritic steel)

Fig. 2.5-3 Probability of crack detection (stainless steel)
### Probabilistic Fracture Mechanics for Risk-Informed Activities - Fundamentals and Applications - ver. 1.2e ©JWES, 2020

#### (1) Piping specifications

<table>
<thead>
<tr>
<th>Item</th>
<th>Value</th>
</tr>
</thead>
<tbody>
<tr>
<td>Outer diameter [mm]</td>
<td>[mm]</td>
</tr>
<tr>
<td>Plate thickness [mm]</td>
<td>[mm]</td>
</tr>
</tbody>
</table>

#### (2) Calculation conditions

<table>
<thead>
<tr>
<th>Original random number</th>
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</tr>
</thead>
<tbody>
<tr>
<td>Number of SCC evaluations [/year]</td>
<td>0</td>
</tr>
<tr>
<td>Number of fatigue evaluations [/year]</td>
<td>0</td>
</tr>
<tr>
<td>ISI frequency [year]</td>
<td>0</td>
</tr>
<tr>
<td>Integrity evaluation period during ISI [year]</td>
<td>0</td>
</tr>
<tr>
<td>Initial crack depth/plate thickness</td>
<td>Number of cells</td>
</tr>
<tr>
<td>Initial crack depth/crack half length</td>
<td>Number of cells</td>
</tr>
<tr>
<td>Number of samples per cell</td>
<td></td>
</tr>
<tr>
<td>Stress intensity factor calculation method</td>
<td></td>
</tr>
<tr>
<td>Base material SCC growth coefficient</td>
<td></td>
</tr>
<tr>
<td>Molten metal SCC growth coefficient</td>
<td></td>
</tr>
<tr>
<td>Base material SCC growth coefficient (for evaluating ISI integrity)</td>
<td></td>
</tr>
<tr>
<td>Molten metal SCC growth coefficient (for evaluating ISI integrity)</td>
<td></td>
</tr>
<tr>
<td>Fatigue crack growth calculation expression</td>
<td></td>
</tr>
<tr>
<td>Detection probability expression at ISI</td>
<td></td>
</tr>
<tr>
<td>Crack non-detection probability at ISI</td>
<td></td>
</tr>
<tr>
<td>Standard deviation of ISI crack depth decision error [mm]</td>
<td></td>
</tr>
<tr>
<td>Leakage limit crack depth ratio to t</td>
<td></td>
</tr>
<tr>
<td>Permissible crack depth ratio at ISI integrity evaluation (t/t)</td>
<td></td>
</tr>
<tr>
<td>Z-factor for evaluating elastic-plastic fracture mechanics</td>
<td></td>
</tr>
</tbody>
</table>

<table>
<thead>
<tr>
<th>End calculation</th>
<th>Distribution form</th>
<th>Mean value</th>
<th>S.D.</th>
</tr>
</thead>
<tbody>
<tr>
<td>Initial crack depth/plate thickness</td>
<td></td>
<td></td>
<td></td>
</tr>
<tr>
<td>Initial crack depth/crack half length</td>
<td></td>
<td></td>
<td></td>
</tr>
<tr>
<td>Stress intensity factor calculation method</td>
<td></td>
<td></td>
<td></td>
</tr>
<tr>
<td>Base material SCC growth coefficient</td>
<td></td>
<td></td>
<td></td>
</tr>
<tr>
<td>Molten metal SCC growth coefficient</td>
<td></td>
<td></td>
<td></td>
</tr>
<tr>
<td>Base material SCC growth rate maximum value</td>
<td>Constant</td>
<td></td>
<td></td>
</tr>
<tr>
<td>Molten metal SCC growth rate maximum value</td>
<td>Constant</td>
<td></td>
<td></td>
</tr>
<tr>
<td>Base material SCC growth rate minimum value</td>
<td>Constant</td>
<td></td>
<td></td>
</tr>
<tr>
<td>Molten metal SCC growth rate minimum value</td>
<td>Constant</td>
<td></td>
<td></td>
</tr>
<tr>
<td>Crack/molten metal distance [mm]</td>
<td></td>
<td></td>
<td></td>
</tr>
<tr>
<td>Molten metal arrival added distance [mm]</td>
<td></td>
<td></td>
<td></td>
</tr>
<tr>
<td>Detection crack growth multiple</td>
<td></td>
<td></td>
<td></td>
</tr>
<tr>
<td>Fatigue stress [N/mm²]</td>
<td></td>
<td></td>
<td></td>
</tr>
</tbody>
</table>

#### (3) Normal operation state load conditions

<table>
<thead>
<tr>
<th>Item</th>
<th>Value</th>
</tr>
</thead>
<tbody>
<tr>
<td>Internal pressure [N/mm²]</td>
<td>[N/mm²]</td>
</tr>
<tr>
<td>Primary membrane stress [N/mm²]</td>
<td>[N/mm²]</td>
</tr>
<tr>
<td>Primary tube bending stress [N/mm²]</td>
<td>[N/mm²]</td>
</tr>
<tr>
<td>Secondary tube bending stress [N/mm²]</td>
<td>[N/mm²]</td>
</tr>
</tbody>
</table>

#### (4) Transient conditions

<table>
<thead>
<tr>
<th>Number of transient conditions</th>
<th>3</th>
</tr>
</thead>
<tbody>
<tr>
<td>Load cycle [year]</td>
<td>4</td>
</tr>
<tr>
<td>Load raising/lowering time [s]</td>
<td></td>
</tr>
<tr>
<td>Temperature [°C]</td>
<td>Timing 1</td>
</tr>
<tr>
<td>Internal pressure [N/mm²]</td>
<td></td>
</tr>
<tr>
<td>Primary membrane stress [N/mm²]</td>
<td></td>
</tr>
<tr>
<td>Primary tube bending stress [N/mm²]</td>
<td></td>
</tr>
<tr>
<td>Secondary tube bending stress [N/mm²]</td>
<td></td>
</tr>
<tr>
<td>In-plane bending stress [N/mm²]</td>
<td></td>
</tr>
</tbody>
</table>

#### (5) Load conditions for evaluating break

<table>
<thead>
<tr>
<th>Item</th>
<th>Value</th>
</tr>
</thead>
<tbody>
<tr>
<td>Pressure proof test</td>
<td>Earthquake loading</td>
</tr>
<tr>
<td>Primary membrane stress [N/mm²]</td>
<td>[N/mm²]</td>
</tr>
<tr>
<td>Primary tube bending stress [N/mm²]</td>
<td>[N/mm²]</td>
</tr>
<tr>
<td>Secondary tube bending stress [N/mm²]</td>
<td>[N/mm²]</td>
</tr>
</tbody>
</table>

#### (6) Load conditions for evaluating integrity after ISI

| Number of load conditions | | | |

---

Fig. 2.5-4 Example input items

Molten metal reaching defect depth = Distance from weld metal boundary + Added distance
Fig. 2.5-5 Example of failure probability of each cell

Fig. 2.5-6 Time history of failure probability
2.6 PREFACE

It is well known that cast austenitic stainless steel (CASS) has low permeability of ultrasonic wave and low detectability of flaws. As a result detectable flaw size of CASS becomes larger than that of forged stainless steel. USNRC has regarded the low detectability of CASS as important at license renewal, and industry in the US has been promoting preparation of the target flaw size for the performance demonstration (PD) system for accreditation of non-destructive inspectors by organizing EPRI project. From this background, Code Case N-838 was approved by the ASME Codes and Standards Committee of Section XI to provide the target flaw size for PD and the acceptable flaw size for license renewal. To obtain the flaw tables in the code case, the probabilistic fracture mechanics (PFM) procedure was applied to consider the scatter of material properties and reasonable evaluation margin.

In order to follow the ASME’s activity, a probabilistic fracture mechanics code PREFACE was developed for CASS pipe. This code has a basic function of fatigue crack growth (FCG) and fracture analysis. As for the fracture analysis method, PREFACE can evaluate not only plastic collapse and the ductile crack initiation, but also ductile instability by the two parameters method and J-T criterion. Also PREFACE can consider thermal aging behavior of CASS by incorporating a Japanese degradation model of stress-strain curve and J-resistance curve depending on the ferrite content into the code.

For establishing the flaw table for PD, the reliability of the code has to be confirmed. A benchmark analysis was proposed to verify the function of PREFACE and other PFM codes, and totally six organizations participated [1]. The participants calculated the failure probability of a pipe with a circumferentially part-through flaw by fatigue crack growth and plastic collapse or ductile fracture. In the benchmark analysis, several analysis cases were set. For FCG analysis, the leak probability of PREFACE completely agreed with those of the other PFM codes. For fracture analysis only PREFACE and one participant could calculate the fracture probability of ductile initiation and instability by the two parameters method and J-T criterion. The fracture probabilities of two codes (E and F) agreed quite well as shown in Table 2.6-1.

<table>
<thead>
<tr>
<th>Evaluation Method</th>
<th>Participants</th>
</tr>
</thead>
<tbody>
<tr>
<td>Evaluation Method</td>
<td></td>
</tr>
<tr>
<td>Limit load</td>
<td>A</td>
</tr>
<tr>
<td>3.79×10⁻⁷</td>
<td>3.76×10⁻⁷</td>
</tr>
<tr>
<td>Two parameters</td>
<td>Initiation</td>
</tr>
<tr>
<td>3.12×10⁻³</td>
<td>3.10×10⁻³</td>
</tr>
<tr>
<td>Instability</td>
<td>-</td>
</tr>
<tr>
<td>J-T</td>
<td>-</td>
</tr>
</tbody>
</table>
Further, to verify the CASS specific function, additional analysis was performed. By using PREFACE, a similar flaw table of Code Case N-838 was obtained [2]. EPRI report [3] is a technical basis for Code Case N-838. The ferrite content and chemical composition were referred to this report. Although the degradation model and J-estimation equation are different from those of PREFACE, general tendency of target flaw size from PREFACE is similar to that of the Code Case as shown in Fig. 2.6-1.

![Target flaw size from PREFACE and CC-N-838](image)

**Fig. 2.6-1** Target flaw size from PREFACE and CC-N-838

From those results, the function of PREFACE has been verified and it will be used for calculation of fracture probability of CASS pipe.
REFERENCES


2.7 DR. MAINTE

2.7.1 Background of the development

A number of PRA (Probabilistic Risk Assessment) studies have been applied to the optimization of maintenance activities in nuclear power plants from a viewpoint of safety focusing on the risk of core meltdown. However, even a small-scale incident of component, which never causes the core meltdown, resulted in reactor shutdown and economic losses. Accordingly, in addition to the safety analysis focusing on the risk of core meltdown, it is very useful to develop a simulator based on PFM (Probabilistic Fracture Mechanics) analyses that can establish maintenance strategies in terms of availability and economic efficiency of nuclear power plants.

With the background above mentioned, the authors first studied risk and economic models of maintenance activities of SG (Steam Generator) tubes of PWRs (Pressurized Water Reactors) [1,2]. After that, we developed Dr. Mainte, an integrated simulator, for the maintenance optimization of LWRs (Light Water Reactors) [3]. The concept of the simulator is to provide a decision-making system to optimize maintenance activities for typical components and piping systems comprehensively and quantitatively in terms of safety, availability and economic rationality, environmental impact and social acceptance under various maintenance strategies including altering inspection frequency and inspection accuracy, conducting sampling inspection, repairs and/or replacements, introducing various maintenance rules, long-term fuel cycles, etc.

Besides, a function of visualization of the simulated results by an interactive divided multi-dimensional visualization method was also developed in order to support a decision-making process to optimize the maintenance activities.

For the further improvement of the safety and availability of nuclear power plants, the effect of human error and its reduction on the optimization of maintenance activities have been studied[4].

In addition, an approach of reducing human error is proposed using the divided multi-dimensional visualization method and AI (Artificial Intelligence) to analyze questionnaire for personnel of maintenance activities. More recently it has been applied to the maintenance optimization of social infrastructures such as expressway facilities including various types of anchor bolts and concrete structures.

2.7.2 Design of the system

2.7.2.1 Features of the system

Dr. Mainte has its own special features as shown below.

(1) Integrated simulator

Dr. Mainte, an integrated simulator for maintenance optimization of LWRs is based on PFM analyses (Fig. 2.7-1). The concept of the simulator is to provide a decision-making system to optimize maintenance activities for typical components and piping systems in
nuclear power plants totally and quantitatively in terms of safety, availability, economic rationality both from cost and profit, environmental impact and social acceptance.

(2) Multi-purpose optimization
The simulator realizes the multi-purpose optimization of maintenance activities considering objective functions, constraint conditions and design variables as shown in Fig. 2.7-2.

(3) Interactive divided multi-dimensional visualization for decision making
The simulator supports decision making through divided multi-dimensional visualization of the relationship among objective functions, constraints conditions and design parameters as shown in Fig. 2.7-3. This process can be conducted interactively with the simulator.

(4) Effective utilization of previous database
Dr. Mainte uses the previous database including field data, test data for material strength, analysis codes, research studies concerning social acceptance effectively as shown in Fig. 2.7-4.
Fig. 2.7-1 A model of Dr. Mainte for maintenance optimization of LWRs

MAINTENANCE OPTIMIZATION

Visualization of simulated results by divided multi-dimensional visualization method

OBJECTIVE FUNCTIONS
Ex. CDF (Core Damage Frequency), Probability of leakage and break of components, Maintenance cost, Profit

CONSTRAINT CONDITIONS
Ex. CDF (Core Damage Frequency), Probability of leakage and break of components, Maintenance cost, Profit

DESIGN VARIABLES ( = MAINTENANCE STRATEGIES)
Ex. Operation years, Inspection interval, Sampling inspection, Crack detectability, Crack sizing accuracy, Repair or Replace, Timing of replacement, Various maintenance rules

Fig. 2.7-2 Decision making process in the integrated simulator
Fig. 2.7-3  Concept of divided multi-dimensional visualization method
2.7.2.2 Analysis flow  Fig. 2.7-5 shows simulation windows and the simulation flow of Dr. Mainte. The flow consists of “Start-up window”, “Main analysis window”, “Analysis manager window” and “Interactive divided multi-dimensional visualization tool window.”

<table>
<thead>
<tr>
<th>Start-up window</th>
<th>Main analysis window</th>
<th>Analysis manager window</th>
<th>Interactive divided multi-dimensional visualization tool window</th>
</tr>
</thead>
<tbody>
<tr>
<td>Selection of components</td>
<td>Planning of analysis, Data input</td>
<td>Running simulations</td>
<td>Optimization process</td>
</tr>
</tbody>
</table>

Fig. 2.7-5 Simulation windows and flow
2.7.3 Main technologies

2.7.3.1 PFM analysis Examples of PFM analysis flow and analysis parameters are shown in Fig. 2.7-6. Typical components and piping systems are selected to be analyzed by the model, and the degradation mechanisms and the maintenance activities for the targets are investigated both by literature and field surveys (Table 2.7-1).

When conducting stress analysis of components and piping systems as well as calculating stress intensity factors of cracks, the simulator can access ADVENTURE (ADVanced Engineering analysis Tool for Ultra large REal world) which is a computational mechanics system for large scale analysis and design. Fig. 2.7-7 indicates an example of the stress analysis conducted for the welding region of nozzles in PWR reactor vessel head.

Fig. 2.7-6 Example of PFM analysis flow and analysis parameters
Table 2.7-1  Target components and piping systems, degradation mechanisms and functions of the simulator

<table>
<thead>
<tr>
<th>Components &amp; Piping systems</th>
<th>Degradation Mechanism</th>
<th>Reliability Analysis</th>
<th>Safety Analysis &amp; Social Acceptance</th>
<th>Economic &amp; Environmental Analyses</th>
</tr>
</thead>
<tbody>
<tr>
<td>PWR*1</td>
<td>SG tubes</td>
<td>PWSCC*2</td>
<td>• PFM engines</td>
<td>• Cost Analysis (Just cost)</td>
</tr>
<tr>
<td></td>
<td>RV head nozzles</td>
<td>SCC*4</td>
<td>(Modified pc-PRAISE)</td>
<td>(NPV)</td>
</tr>
<tr>
<td></td>
<td>PL piping</td>
<td>Fatigue</td>
<td>• Safety Analysis</td>
<td>(RO)</td>
</tr>
<tr>
<td></td>
<td></td>
<td></td>
<td>• Apply PFM analysis results to PSA</td>
<td>(CO2 release)</td>
</tr>
<tr>
<td></td>
<td></td>
<td></td>
<td>• Social Acceptance</td>
<td>Analysis with alternative</td>
</tr>
<tr>
<td></td>
<td></td>
<td></td>
<td>• Supply a guideline for social</td>
<td>power generation</td>
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<tr>
<td></td>
<td></td>
<td></td>
<td>acceptance of risk-based maintenance</td>
<td></td>
</tr>
<tr>
<td>BWR*5</td>
<td>PLR piping</td>
<td>SCC</td>
<td>• Cost Analysis (Just cost)</td>
<td></td>
</tr>
<tr>
<td></td>
<td></td>
<td></td>
<td>(Modified pc-PRAISE)</td>
<td></td>
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<td>PASCAL [JAEA]</td>
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<td>Stress analysis engine</td>
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<td></td>
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</tr>
<tr>
<td></td>
<td></td>
<td></td>
<td>(Applicable to complex geometries)</td>
<td></td>
</tr>
<tr>
<td>PWR &amp; BWR</td>
<td>RV*3</td>
<td>PTS*6</td>
<td>• Safety Analysis</td>
<td></td>
</tr>
<tr>
<td></td>
<td>Carbon Steel Pipes</td>
<td>Erosion &amp; Corrosion</td>
<td>• Apply PFM analysis results to PSA</td>
<td></td>
</tr>
<tr>
<td></td>
<td>Dynamic components</td>
<td>Wear, Erosion, etc.</td>
<td>• Social Acceptance</td>
<td></td>
</tr>
<tr>
<td></td>
<td></td>
<td></td>
<td>• Supply a guideline for social</td>
<td></td>
</tr>
<tr>
<td></td>
<td></td>
<td></td>
<td>acceptance of risk-based maintenance</td>
<td></td>
</tr>
</tbody>
</table>

*1 Pressurized Water Reactor  *2 Primary Water Stress Corrosion Cracking  *3 Reactor Vessel  *4 Stress Corrosion Cracking  *5 Boiling Water Reactor  *6 Pressurized Thermal Shock
2.7.3.2 Economic analysis Dr. Mainte can provide economic analysis as shown below.

(1) Cost analysis :
Considering only costs.

(2) Profitability analysis *(financial method using NPV) :
Considering both costs and profits.
*Note : Risk-based or cost-based analyses cannot tell whether or not electricity generation business will pay. Thus, profitability must be analyzed.

(3) Real Option analysis *(up-to-date financial method) :
Considering economic values of the flexibility of strategies quantitatively.
ex. Decision making based on upcoming information

2.7.3.3 Interactive multi-dimensional visualization The simulator supports decision making through the divided multi-dimensional visualization of the relationship among objective functions, constraints conditions and design parameters as previously shown in Fig. 2.7-8.

2.7.3.4 AI *(Neural Network) After PFM analysis, the relationship among design parameters and objective functions can be learned by AI (Neural Network) as shown in Fig. 2.7-9. Then the interactive multi-dimensional visualization can show the expanded data under various maintenance strategies through the learned neural network. Under the several constraint conditions selected by the users, the optimization of maintenance can be interactively conducted by the users.

Fig. 2.7-8 Maintenance optimization through Dr. Mainte
Fig. 2.7-9  Neural network learning of PFM analysis results

PFM data points expansion from 1800 to 10800 through neural network

Objective SPACE 01  Accumulative rupture $\leq 10^{-4}$
Objective SPACE 02  Accumulative NPV $\geq$ 100 Billion yen

Pink colored

★ Setting the necessary objective functions in Objective SPACE and their constraints values, options of maintenance strategies can be visually shown.

Fig. 2.7-10  Neural network learning of PFM analysis data and data expansion
2.7.4 Example of analysis
2.7.4.1 RV head nozzle of PWR An example of PFM analysis of RV (Reactor Vessel) head nozzle of PWR is shown below. Fig. 2.7-11 indicates the crack intensity factor as a function of crack depth using the displacement extrapolation method. Also, the stress intensity factors are summarized in Table 2.7-2 with and without considering residual stress. The detection probability of non-destructive testing as a function of crack depth is assumed as shown in Fig. 2.7-12. Fig. 2.7-13 shows the cumulative probability of leakage rate at the welding region of RV head nozzle with and without ISI (In-Service Inspection) for every 5 or 10 year as a function of operation period.

![Crack intensity factor as a function of crack depth using the displacement extrapolation method](image)

**Table 2.7-2 Stress intensity factors with and without considering residual stress**

(a) with considering residual stress

<table>
<thead>
<tr>
<th>Crack shape (length×depth) [mm]</th>
<th>Crack tip [MPa√m]</th>
<th>Crack opening (0°direction) [MPa√m]</th>
<th>Crack opening (180°direction) [MPa√m]</th>
</tr>
</thead>
<tbody>
<tr>
<td>5×10</td>
<td>22.2</td>
<td>56.1</td>
<td>60.4</td>
</tr>
<tr>
<td>10×20</td>
<td>38.2</td>
<td>122.0</td>
<td>125.6</td>
</tr>
<tr>
<td>18×36</td>
<td>58.1</td>
<td>159.8</td>
<td>172.3</td>
</tr>
</tbody>
</table>

(b) without considering residual stress

<table>
<thead>
<tr>
<th>Crack shape (length×depth) [mm]</th>
<th>Crack tip [MPa√m]</th>
<th>Crack opening (0°direction) [MPa√m]</th>
<th>Crack opening (180°direction) [MPa√m]</th>
</tr>
</thead>
<tbody>
<tr>
<td>10×20</td>
<td>1.7</td>
<td>16.2</td>
<td>16.0</td>
</tr>
<tr>
<td>18×36</td>
<td>2.2</td>
<td>19.4</td>
<td>20.0</td>
</tr>
</tbody>
</table>
Fig. 2.7-12 The detection probability of non-destructive testing as a function of crack depth

Fig. 2.7-13 Cumulative probability of leakage rate at the welding region of RV head nozzle with and without ISI for every 5 or 10 year as a function of operation period
REFERENCES


Chapter 3 Applications of PFM
3.1 Failure Probability Assessment of PWR Reactor Vessels under PTS Event

A probabilistic fracture mechanics (PFM) analysis method for pressure boundary components is useful to evaluate the structural integrity in a quantitative way. This is because the uncertainties related to influence parameters can be rationally incorporated in PFM analysis. From this viewpoint, the probabilistic approach evaluating through-wall cracking frequencies (TWCFs) of reactor pressure vessels (RPVs) has already been adopted as the regulation on fracture toughness requirements against PTS events in the U.S. As a study of applying PFM analysis to the integrity assessment of domestic RPVs, JAEA has been preparing input data and analysis models to calculate TWCFs using PFM analysis code PASCAL3. In this section, activities have been introduced such as preparing input data and models for domestic RPVs, verification of PASCAL3, and formulating guideline on general procedures of PFM analysis for the purpose of utilizing PASCAL3.

3.1.1 Introduction

To maintain the integrity of the major components of nuclear power plants during the service life, it is important to consider the aging degradation of the material and the initiation and growth of defects. As one of the most important components in aging light water reactors (LWRs), the structural integrity of reactor pressure vessels (RPVs) is assessed for a non-ductile fracture. Pressurized thermal shock (PTS) is one of the most severe events to assess the structural integrity of the RPVs. Japanese code prescribes rules based on deterministic fracture mechanics (DFM) to prevent the RPVs from non-ductile fracture under PTS events [1]. On the other hand, the United States Nuclear Regulatory Commission (USNRC) has established a technical basis to support a risk-informed revision to PTS regulations using the results of probabilistic fracture mechanics (PFM) analysis code FAVOR [2,3]. The revised regulations provide alternative reference-temperature (RT)-based screening criteria, which is codified in 10CFR50.61 (a) [4].

In Japan, PFM analysis code PASCAL3 has been developed by JAEA in order to calculate conditional probabilities of through-wall cracking or through-wall cracking frequencies (TWCFs) of RPVs under PTS events [5-11]. In PASCAL3, some functions or models based on Japanese code or domestic data have been implemented such as irradiation embrittlement prediction of JEAC4201-2007 [12] and Weibull type $K_c$ curve [13]. PASCAL3 has also been used in the international round robin analyses [14,15].

As a study of applying PFM analysis to the integrity assessment of domestic RPVs, study on input data and models for domestic RPVs, verification of PASCAL3, and guideline on general procedures of PFM analysis were conducted.
3.1.2 Guideline on general procedures of PFM analysis

3.1.2.1 Objective  In Japan, PFM is now being recognized as a useful tool to evaluate structural integrity quantitatively even though the evaluation method prescribed in JEAC is based on deterministic approach. For the application of PFM analysis to the domestic RPVs, a guideline should be prepared which provides the detailed procedures to evaluate structural integrity of components using PFM analysis. Assuming that the guideline is referenced in regulation or codes/standards in the future, the preparation of a guideline on general procedures of PFM analysis, inputs and models for the analysis of domestic RPVs, and a methodology of verification of PASCAL3 is discussed in this study. Acceptance values of TWCFs are beyond the scope of this study.

3.1.2.2 Framework of guideline  The framework of guideline on general procedures of PFM analysis is shown in Fig. 3.1-1. Requirements or recommendations for PFM analysis are provided in the article, the specific methods and case studies are provided in the interpretation. It consists of scope, general requirements for inputs, models, and methodology, and so on. The outline of the guideline was prepared in 2013. In 2014, the detailed requirements to calculate TWCFs, examples of inputs and the technical basis for domestic RPVs are added to the guideline so that it enables analysts to prepare all the inputs and conditions to calculate TWCFs. As a part of the guideline, some prescriptions on the scope and typical items are described below.

Fig. 3.1-1 Outline of guideline
(1) General
(1.1) Scope

This guideline describes the general procedures to estimate through-wall cracking frequencies (TWCFs) due to brittle fracture under transients such as pressurized thermal shock (PTS) for a beltline region of a reactor pressure vessel of a pressurized water reactor (PWR) using probabilistic fracture mechanics (PFM).

(2) Estimation of Stress Intensity Factor
(2.1) Selection of transients

Transients should be selected appropriate to estimate TWCF due to brittle fracture for the RPV. Those frequencies of occurrence should be also taken into account.

(3) Estimation of Fracture Toughness
(3.1) Neutron Irradiation

Neutron fluence should be selected appropriate to the RPV. Attenuation of fluence through the RPV or difference of fluence on the inner surface of the RPV can be taken into account.

(4) Crack Initiation and Failure
(4.1) Crack Initiation

Brittle fracture occurs when the $K_I$ of the crack tip is equal or greater than $K_{Ic}$, except when the $K_I$ is decreasing. The warm pre-stress effect can be taken into account.

(4.2) Crack Arrest

Brittle fracture arrests when the $K_I$ of the crack tip becomes lower than $K_{Ia}$, after brittle fracture occurred.

(5) Analysis Methods
(5.1) Calculating Conditional Probabilities of Through-wall Cracking and Through-wall Cracking Frequencies

Conditional probabilities of crack initiation and conditional probabilities of through-wall cracking can be calculated from the number of samples of through-wall cracking and total samples using statistical method such as Monte Carlo simulation method. If a PFM analysis code is used, it should be verified enough to calculate TWCFs with high confidence.

3.1.2.3 Input data and models of PASCAL3

Inputs, models and algorithms appropriate for an evaluation of domestic RPVs should be selected and those technical bases should be clarified to estimate TWCF of a certain RPV using PFM analysis code.

Typical items are listed below. As a general rule, the domestic data or methods prescribed in Japanese code are selected. If necessary, data in US or more conservative assumptions are applied.
(1) RPV and Initial Crack
Assuming an RPV of a typical 3-loop pressurized water reactor (PWR) plant in Japan, inner diameter, thickness of plate, thickness of cladding are 2000mm, 200mm, and 5mm, respectively [16].

One of the most critical parameters to TWCF is an initial flaw distribution. So far, the detailed flaw data of domestic RPVs for PFM analysis are not available. The flaw data analyzed by PNNL (Pacific Northwest National Laboratory) [17] can be used. Usually, 1000 data sets of initial flaw distribution are generated by VFLAW code [17]. A sample data of flaw densities of Oconee shown in the report is multiplied by the size of the RPV described above to obtain the total number of flaws in the RPV. Both surface-breaking flaw and embedded flaw are taken into account. The direction of surface-breaking flaws is assumed to be identical to that of cladding. For typical RPVs in Japan, the number of cladding layer on a beltlne is 1 and the direction of cladding is circumferential. Therefore, a circumferential surface-breaking flaw is postulated. 50% of both axially and circumferentially oriented embedded cracks are assigned. Stress intensity factors are calculated for postulated crack with a complementary function implemented in PASCAL3 considering discontinuous stress distributions due to the existing of stainless steel overlay cladding.

(2) Fracture Toughness and Crack Arrest Toughness
Both fracture toughness distribution and crack arrest toughness distribution based on Weibull and lognormal type distributions, respectively shown in Fig. 3.1-2, produced by Katsuyama, et al [18] can be used by PASCAL3.

![Fig. 3.1-2 Ki curves of Weibull type and Kia curves of lognormal type](image-url)
(3) Neutron Irradiation Embrittlement and Neutron Fluence
To estimate a mean shift of $RT_{NDT}$, irradiation embrittlement prediction of JEAC4201-2007 (Sup. 2013) is applied. The shifted value is provided in tables and equations based on Cu and Ni contents, neutron fluence, flux, irradiation temperature. It is modeled as a normal distribution in PASCAL3. The total standard deviation of $RT_{NDT}$ is defined as a root of sum of squares of both standard deviations for initial and shifted values.

Neutron fluence is also modeled as a normal distribution. The standard deviation is 13.1% of mean value. In FAVOR, the inner surface fluence is sampled from two steps of normal distributions, one is from the global uncertainty at the sub region of RPV, and the other is from the local uncertainty at the crack location. The recommended values of the standard deviation are 11.8% and 5.6%, respectively. If these two steps of normal distributions are assumed as one normal distribution, the standard deviation is converted to 13.1%.

(4) Initial $RT_{NDT}$ and Chemical Composition
Initial $RT_{NDT}$ and chemical compositions are modeled as a normal distribution. The mean values and standard deviations are listed in Table 3.1-1. The mean values are based on those of a typical domestic RPV.

<table>
<thead>
<tr>
<th>Item</th>
<th>Mean Value (Base)</th>
<th>Mean Value (Weld)</th>
<th>Standard Deviation</th>
</tr>
</thead>
<tbody>
<tr>
<td>Initial $RT_{NDT}$</td>
<td>-5 °C</td>
<td>-50 °C</td>
<td>9.4 °C</td>
</tr>
<tr>
<td>Cu</td>
<td>0.16 wt%</td>
<td>0.14 wt%</td>
<td>0.01 wt%</td>
</tr>
<tr>
<td>Ni</td>
<td>0.61 wt%</td>
<td>0.80 wt%</td>
<td>0.02 wt%</td>
</tr>
</tbody>
</table>

(5) PTS Transients
If any type of PTS transients is not specified, large- and small-break loss of coolant accident (LBLOCA, SBLOCA) and main steam line break (MSLB) are used to calculate $K_I$ in the current Japanese code [1]. These three transients are selected with the objective of the severity to the structural integrity, i.e. the possibility to give larger $K_I$. In the PTS reevaluation project [19], probabilistic risk assessment (PRA) has been conducted to determine important sequences and its frequencies. PTS transients including stuck open valve (SO) could be selected from PRA, the frequency of which is relatively higher than the three transients prescribed in JEAC4206-2007 [1] even though it gives a smaller $K_I$. Yearly frequencies of occurrence of PTS transients are also selected from PRA for Beaver Valley plant in PTS reevaluation project [19]. In order to obtain time histories of temperature and stress distribution for LBLOCA, SBLOCA and MSLB, Japanese PTS transient data are used. Data from Beaver Valley plant are used for SO.
(6) Through-wall Cracking
Through-wall cracking is assumed to occur when a crack depth reaches 80% of total thickness of the RPV wall. The failure criterion of plastic collapse is also taken into account. Conditional probability of through-wall cracking of a whole RPV with multiple cracks is calculated from conditional probabilities of non-through-wall cracking of each crack. To obtain TWCF, yearly frequencies of transients are multiplied by the conditional probability of through-wall cracking.

3.1.2.4 Verification of PASCAL3
In general, analysis codes in which Monte Carlo method is implemented are used to estimate failure probabilities because a number of random variables are generated and used in a PFM analysis. Therefore, analysis codes used should be verified enough to calculate TWCFs with high confidence. Most of the functions implemented in a general PFM analysis code are classified into two categories. One is the same function as that of a deterministic fracture mechanics, and the other is a function of random variable sampling, which is unique to PFM analysis codes. The classification of the functions of PASCAL3 into those of random sampling and algorithms of a general fracture mechanics analysis are shown in Table 3.1-2. This classification is almost the same as that of FAVOR V&V [20].

It is confirmed that each random variable obtained by PASCAL3 was in good agreement with theoretical value, and also each function implemented in PASCAL3 works normally.

<table>
<thead>
<tr>
<th>Table 3.1-2 Lists of typical functions in two categories</th>
</tr>
</thead>
</table>
| **Random variables** | RT
| Chemical composition | NDT |
| Neutron fluence | |
| Fracture toughness | |
| Crack arrest toughness | |
| **Algorithms** | Irradiation embrittlement prediction |
| Attenuation of neutron fluence | |
| Crack type and stress intensity factor | |
| Crack initiation and arrest | |
| Warm pre-stress | |
| Conditional probability of through-wall cracking / Through-wall cracking frequency | |

3.1.3 Concluding remarks
As a study of applying PFM analysis to the domestic RPV integrity assessment, a guideline on general procedures of PFM analysis is in preparation, assuming that the guideline is referred in regulation or codes/standards in near future. In addition, inputs, models and algorithms appropriate for an evaluation of domestic RPVs are selected based on the
technical basis. As a general rule, the domestic data or methods prescribed in Japanese codes are selected. In the case of those are not enough, some data in U.S. or more conservative assumptions are applied.

In order to calculate TWCFs with high confidence, the functions implemented in PASCAL3 are classified into two categories, and verification of each function are performed.

**ACKNOWLEDGMENTS**

This study was performed under the contract research entrusted from Nuclear Regulation Authority of Japan.

**REFERENCES**


3.2 Probabilistic Assessment of Erosion Behavior of LWR Piping

In piping of a light-water nuclear power plant, wall thinning in carbon steel is one of the typical degradation mechanisms such as stress corrosion cracking (SCC), thermal fatigue, etc. As for the piping failure accident due to thinning, fracture of the water supply line of Surry Unit-2 on December 9, 1986 [1] and the feed water line of Mihama Unit-3 on August 9, 2004 [2] are well known. For carbon steel piping of a nuclear power plant, remarkable thinning might occur in a certain temperature and wetness conditions. Though the thinning portions are limited to bend pipes, downstream portion of valves and orifices, and tees, measurements are not so easy because of an amount of points to be measured. Therefore, each utility has prepared a management guideline on the thinning pipe uniquely, and has performed ultrasonic measurement of thickness to prevent the rupture of thinning pipe [3].

As for the accident of the Mihama Unit-3, it occurred, because the rupture portion had been left off the list of inspection. In order to prove the safety of managed piping, management guidelines on the thinning pipe and observed data of wall thickness were published by KEPCO [2]. Although the predicted wear rate enveloped the observed data in general, some observed data exceed predictions, and the differences between predicted and measured values were comparatively large in some cases. Thus, since the variation of the wear rate is large and thinning area is not clear, the probabilistic approach may be suitable for evaluation of the reliability against the thinning piping.

The variation between the prediction and measured wear rates was evaluated using the published data, and the probability density function was created in this study. Using this probability density function, the failure probability of the thinning pipe was evaluated using Monte Carlo simulation.

3.2.1 Nomenclature

\[
\begin{align*}
D & \quad \text{outer diameter of pipe} \\
L_t & \quad \text{circumferential thinning length} \\
L_a & \quad \text{axial thinning length} \\
P & \quad \text{internal pressure} \\
P_b & \quad \text{bending stress} \\
P_m & \quad \text{membrane stress} \\
P_{Ri} & \quad \text{percentage of repaired portions in the inspected portions} \\
P_{NRi} & \quad \text{percentage of un-repaired sample in the ith inspection} \\
P_{RR0} & \quad \text{initial existence probability of the sample (}=1.0) \\
P_{RRn} & \quad \text{existence probability of the sample after n times of inspection} \\
R_i & \quad \text{inner radius} \\
R_o & \quad \text{outer radius} \\
R_{Ri} & \quad \text{percentage of inspection portions in the ith inspection} \\
S & \quad \text{allowable tensile stress}
\end{align*}
\]
The thinning in piping is observed at the portion where the turbulence of flow is produced such like bends, tees and down stream of orifices or valves. According to the management guidelines on the thinning pipe, the wear rate is given according to environmental conditions of the fluid. The predicted wear rate in the management guidelines is shown in Table 3.2-1 [2]. As shown in the table, the wear rate depends on the flow rate, wetness of fluid and operating temperature, and it is remarkable at the down stream of a flow control valve or a check valve especially. Though the maximum wear rate is evaluated as $5.75 \times 10^{-4}$ mm/h (=5×1.15×10⁻⁴ mm/h) from Table 3.2-1, there is no portion whose wear rate exceeds $1.75 \times 10^{-4}$ mm/h in the published data by KEPCO. Consequently, the parameters of wear rate are set lower than $2 \times 10^{-4}$ mm/h in this study.

Based on the observed thickness of thinning pipe published by KEPCO, the ratio of observed to predicted wear rate shown in Table 3.2-1 was calculated. The results were arranged using normal, log-normal and exponential distribution, and log-normal distributions fitted them best. Using the log-normal distribution, the probability density function was arranged as shown in Fig. 3.2-1. The horizontal axis of this figure is the ratio of observed to predicted wear rate. The ratio of observed to predicted wear rate is expressed by lognormal distribution. The mean value of actual wear rate is 0.297 times of predicted value, and the standard deviation of log-normal distribution is 0.690.

The thickness of piping is managed to satisfy the minimum thickness ($t_{min}$) given by the following equation [4].

$$t_{min} = \frac{PD}{2S\eta + 0.8P}$$

(3.2-1)

where $\eta$ is assumed as $\eta = 0.6$ at the condition for one side welding without a backing metal. Secondary coolant system of PWR is evaluated in this study, and the following conditions are assumed.

a. Material of pipe: SB42 (JIS standard)

b. Maximum operation pressure: 1.275 MPa

c. Maximum operation temperature: 468 K
From these conditions, the minimum thickness is calculated as shown in Table 3.2-2. According to the management guidelines, the thinning piping is renewed when the observed thickness is less than the minimum thickness including 2 years margin.

### 3.2.3 Analytical Conditions

According to the thinning patterns, the allowable bending and membrane stresses are calculated by the following equations (See. Fig. 3.2-2).

**Local circumferential thinning** [5, 6]

\[
\sigma_{m_{cr}} = S_u \left( 1 - \frac{t_{\text{nom}} - t_p}{t_{\text{nom}}} \left( \frac{L_i}{2R_i \pi} - \frac{2\varphi}{\pi} \right) \right)
\]  

(3.2-2)

\[
\varphi = \arcsin \left[ 0.5 \left( \frac{t_{\text{nom}} - t_p}{t_{\text{nom}}} \right) \sin \left( \frac{L_i}{2R_i} \right) \right]
\]

For \( L_i / (2R_i) + \beta \leq \pi \)

\[
\sigma_{b_{cr}} = \frac{2S_u}{\pi} \left[ 2 \sin \beta - \frac{t_{\text{nom}} - t_p}{t_{\text{nom}}} \sin \left( \frac{L_i}{2R_i} \right) \right]
\]  

(3.2-3)

\[
\beta = \frac{1}{2} \left( \pi - \frac{t_{\text{nom}} - t_p}{t_{\text{nom}}} \left( \frac{L_i}{2R_i} \right) - \pi \frac{\sigma_{m_{cr}}}{S_u} \right)
\]

For \( L_i / (2R_i) + \beta > \pi \)

\[
\sigma_{b_{cr}} = \frac{2S_u}{\pi} \left[ 2 \left( 1 - \frac{t_{\text{nom}} - t_p}{t_{\text{nom}}} \right) \sin \beta - \frac{t_{\text{nom}} - t_p}{t_{\text{nom}}} \sin \left( \frac{L_i}{2R_i} \right) \right]
\]  

(3.2-4)

\[
\beta = \pi + \frac{1}{1 - \frac{t_{\text{nom}} - t_p}{t_{\text{nom}}}} \left( \frac{t_{\text{nom}} - t_p}{2R_i} - \pi \frac{\sigma_{m_{cr}}}{S_u} \right)
\]

**Local axial thinning** [7]

\[
\sigma_{m_{ax}} = S_u \left[ 1 - \frac{t_{\text{nom}} - t_p}{t_{\text{nom}}} \left( 1 - \exp \left( -0.157 \frac{L_i}{\sqrt{R_i t_p}} \right) \right) \right]
\]  

(3.2-5)

**Local 360° thinning**

\( \sigma_{b_{ax}} \) and \( \beta \) are given by Eq. (3.2-4)
In the stability assessment of the thinning pipe, membrane stress \( (P_m) \) and bending stress \( (P_b) \) are applied. The applied stress is summarized into Table 3.2-3. These stresses are assumed from the allowable stress to class 2 piping.

Unfortunately, the shape of thinning portion was not published by KEPCO, so it was assumed as follows in this study. The shapes of the local circumferential and the local axial thinning are assumed to be 120° referring the management guideline [2] so that the ratio of the major axis to a minor axis might be set to 2:1. For example, the shape of local circumferential thinning has 120° circumferential length \( (2\pi R_i / 3) \) and axial length is half of it \( (\pi R_i / 3) \), and the shape of local axial thinning has 120° circumferential length \( (2\pi R_i / 3) \) and axial length is twice of it \( (4\pi R_i / 3) \). Here, the aspect ratio \( (L_r / L_a \text{ or } L_a / L_r) \) of thinning is fixed into 0.5, and standard deviation of angle of thinning area is supposed 30°.

Since the number of inspection portions is very large in the actual plant, it is impossible to inspect all portions in one periodic inspection. Therefore, the inspection divided into several times will be performed in the plant. In this analysis, the inspection will be performed every year, and the failure probability of the sample is calculated by the following formula considering the actual inspection interval.

\[
PRB_n = PRB_0 \times PNR_1 \times PNR_2 \times \cdots \times PNR_n
\tag{3.2-6}
\]

\[
PNR_i = 1.0 - R_i P_i
\]

\[
PRB_n = PRB_0 - \sum_{i=1}^{n} R_i P_i
\tag{3.2-7}
\]

In order to grasp the characteristic of Eq. (3.2-6) and Eq. (3.2-7), the existence probability of the sample was calculated for the following two cases.

Case-1 Percentage of inspection: 10% per year

Percentage of repaired portions: 50%

Operation time: 20 years

Case-2 Percentage of inspection: 10% per year

Percentage of repaired portions: 100%

Operation time: 10 years

Existence probability of the sample is summarized into Table 3.2-4. When taking percentage of repair into consideration, Eq. (3.2-7) does not give an appropriate result because it may give small existence probability of the sample with the increasing number of inspection. Therefore, Eq. (3.2-6) will be recommended for the case that the detection probability or percentage of repair is important. Case-2 is the scenario that all samples will be repaired when the thinning depth exceeds some threshold. In the usual inspection, all inspected portions are divided into some groups in consideration of the term of an ISI. All the samples that the thinning depth exceeds the threshold are repaired. Therefore, existence
probability of the sample decreases with the increasing the number of the inspection as shown in Eq. (3.2-7).

The parameters taken into consideration are as follows.

a. Thinning direction: local 360°, circumferential and axial
b. Wear rate: 0.3, 0.5, 1.0, 1.5 and 2.0 \(\times 10^{-4}\) mm/h

c. Applied stress: 0.25, 0.5, 0.75 and 1.0 times of Table 3.2-3
d. Inspection interval: 1, 2, 3, 5 and 10 years

3.2.4 Influence of each parameter

3.2.4.1 Probability of failure in case of no Inspection

The probability of failure in the case of no inspection is shown in Fig. 3.2-3 to Fig. 3.2-5. These figures show the results of the case that smallest wear rate (0.3\(\times 10^4\) mm/h) and applied stress (0.25 times of Table 3.2-3). The principal failure mode in local 360° and local axial thinning pipe is break, and that in local circumferential thinning is leak, and the sum of break and leak probabilities is almost the same in each pattern. When no inspection, probability of failure increases with time even if the wear rate and the applied stress are small. Therefore, suitable management such as inspection, repair, and replace are required for the thinning pipe.

3.2.4.2 Effect of the evaluation way for inspection

As mentioned above, the way of evaluation for the influence of inspection causes the difference in existence probability of a sample and failure probability. The probability of failure is shown in Fig. 3.2-6 in the case of local 360° thinning, an inspection interval of 5 years, 2.0\(\times 10^{-4}\) mm/h of predicted wear rate and applied stress shown in Table 3.2-3. Although the increase in failure probability is not seen after 5 years operation—the inspection for all portions is performed—in case of Eq. (3.2-7), the probability of failure increases gradually in case of Eq. (3.2-6). This is because the existence probability of the sample with the wear rate far exceeding prediction will be almost 0, when the inspection for all portions is completed. In this study, since reuse of failed portions is not considered, the threshold of failure probability appears. The relation between the probability of failure and the inspection interval is shown in Fig. 3.2-7 in the case of 26-inch pipe—the difference between Eq. (3.2-6) and Eq. (3.2-7) is large. The tendency of this case is also the same as that of the above case.

3.2.4.3 Sensitivity to the thinning pattern

The probability of failure for each thinning pattern (360°, local circumferential, local axial) is shown in Fig. 3.2-8. This figure shows the result of the case for an inspection interval of 10 years, 2.0\(\times 10^{-4}\) mm/h of predicted wear rate and 0.25 times of the applied stress shown in Table 3.2-3. When the wear rate and the applied stress are the same in each size of pipe, the probability of failure is larger in the small bore pipe which has small thickness. In cases of the local 360° and the local circumferential thinning, probability of break is extremely larger than that of leak, whereas the probability of leak is larger than the probability of break in case of the local
circumferential thinning. It is shown from this result that the influence of the accident is severe in case of the local 360° or the local axial thinning.

### 3.2.4.4 Sensitivity to wear rate

The sensitivity to the wear rate on probability of failure in the case of local 360° thinning is shown in Fig. 3.2-9. This figure shows the results of the case that an inspection interval is 10 years, and applied stress is 0.25 times of Table 3.2-3. This figure shows that probability of failure is proportional to wear rate in general.

### 3.2.4.5 Sensitivity to applied stress

The sensitivity to the applied stress on the probability of failure in the case of local 360° thinning is shown in Fig. 3.2-10. This figure shows the result of the case for an inspection interval of 10 years and 2.0×10⁻⁴ mm/h of predicted wear rate. It is shown from this figure that the probability of break increases and the probability of leak decreases with increase of the applied stress. However, the sum of them is not much dependent on the applied stress. Especially, for the large bore pipe, this tendency is remarkable. Since the probability of failure by thinning is not dependent on the applied stress, the probability of failure of the thinning pipe is greatly dependent on the difference between the actual and the predicted wear rates.

### 3.2.4.6 Sensitivity to the inspection interval

The sensitivity to the inspection interval on the probability of failure in the case of local 360° thinning is shown in Fig. 3.2-11. This figure shows the results of the case for 2.0×10⁻⁴ mm/h of the predicted wear rate and the applied stress shown in Table 3.2-3. The tendency such that the probability of failure decreases greatly when the inspection interval is small, and it has suggested that fixing the suitable inspection interval is important on the integrity of piping.

### 3.2.5 Discussions

From the viewpoint of plant safety such as ‘core damage frequency’, the frequency of failure is not affected by the secondary coolant system of PWR. Therefore, the failure of pipe is prevented from the viewpoint of cost. The frequencies of break and leak of piping due to thinning are assumed to be limited less than the frequency of Level-C service condition (2.5×10⁻² - 1×10⁻⁴ per reactor year) in this study. The number of inspected portions in Japanese PWR plants (23 units) reaches 81765 and the average is 3555 portions per plant [2]. In order for the frequency of failure due to thinning to be less than the frequency of Level-C service condition, the frequency of failure of one portion has to be below the following value.

\[
\frac{2.5 \times 10^{-2}}{3555} \approx 7 \times 10^{-6} \text{ [yr}^{-1}] \tag{3.2-8}
\]

Since there is no difference between break and leak of piping from the viewpoint of plant safety, the sum of them is limited less than the above-mentioned value. The classification of the failure probability for each size of pipe is shown in Tables 3.2-5 – 3.2-7. In a table, ‘−’ implies that the probability of failure is smaller than 7×10⁻⁶ per reactor year, and ‘×’
implies that the probability of failure exceeds $7 \times 10^{-6}$ per reactor year. This result shows that probability of failure is greatly dependent on the wear rate. Although it will be necessary to perform the inspection frequently when the wear rate is high, it is desirable that the frequency of inspection is longer than 3 years because the pipe—the margin for thinning is below 2 years—should be replaced according to the management guidelines. Neither a small bore pipe nor the portion of which the wear rate is very high has to satisfy this condition.

Usually, in the portion where the wear rate is high, a margin is taken in wall thickness beforehand. Then, the initial thickness of pipe to ensure the inspection interval is 3 years or more is evaluated. The assessment case is that local 360° thinning—the most severe thinning pattern, predicted that wear rate is $2.0 \times 10^{-4}$ mm/h and applied stress is 0.25 times of Table 3.2-3. The probability of failure is evaluated for the cases that inspection intervals are 3 and 5 years, and schedules of pipe are 80, 120, 160. The probability of failure is summarized in Table 3.2-8. In the case of 26-inch pipe, if the schedule of pipe is 80 or more, 3 years of inspection interval is securable, and if the schedule of pipe is 120 or more, 5 years of inspection interval is securable. Therefore, increasing thickness is effective in reduction of failure probability for the large bore pipe. On the other hand, in the case of 4-inch pipe, it is difficult to secure 3 years inspection interval even if the pipe is thickened to schedule 160, because the failure probability much depends on the absolute thickness of pipe. This result suggests that the small diameter of pipe—the absolute thickness is small—is not applicable to the environment in which the wear rate is very high.

The large variation of prediction formula shown in Fig.3.2-1 causes the large failure probability of the thinning pipe. As shown in the figure, the variation in the prediction formula of wall thinning is expressed by a lognormal distribution, and the wear rate exceeding predicted value arises in few cases. Since the published data used in order to create the probability density function shown in Fig.3.2-1 is only 54 points, enlargement of data is indispensable to improve the accuracy of prediction. The probability of failure is shown in Table 3.2-9 for the case that the standard deviation is assumed half of Fig. 3.2-1 ($\sigma = 0.345$) and the inspection interval is 3 years. In this case, if the schedule of pipe is 80 or more, more than 3 years of inspection interval is securable even if in 4-inch pipe. Thus, the improvement in the accuracy of prediction is very important for the management of the thinning pipe by accumulation of the plant data.

### 3.2.6 Conclusions

Based on the thinning data published by KEPCO, probabilistic failure evaluation of thinning pipe was performed. When the failure probability of thinning pipe is controlled below the frequency of Level-C service condition, the failure probability of large diameter pipe is possible to achieve by expecting the margin for the thinning into the initial thickness of pipe. On the other hand, enlargement of thickness is not effective to reduce the failure
probability of small bore pipe and the improvement in the accuracy of prediction is very important by accumulation of the plant data.

REFERENCES

Table 3.2-1  Wear rate specified in the PWR management guideline[2].

| Conditions of fluid | Temperature [K] | <373 | 373~423 | 423~473 | 473~523 | 523≤ |
|---------------------|----------------|
|                     |                |      |         |         |         |      |
| Two-phase flow       |                |      |         |         |         |      |
| Wetness: ≥15% Flow   | <30           | -    | 0.35    | -       | -       | -    |
| rate [m/s]          | 30~50         | -    | -       | 1.15    | -       | -    |
|                     | 50≤           | -    | -       | -       | -       | -    |
| Two-phase flow       |                |      |         |         |         |      |
| Wetness: 5~15% Flow  | <30           | -    | -       | 0.35    | -       | -    |
| rate [m/s]          | 30~50         | -    | -       | 1.15    | -       | -    |
|                     | 50≤           | -    | -       | -       | -       | -    |
| Two-phase flow       |                |      |         |         |         |      |
| Wetness: <5% Flow    | <30           | -    | -       | 0.35    | -       | -    |
| rate [m/s]          | 30~50         | -    | -       | -       | 1.15    | -    |
|                     | 50≤           | -    | -       | -       | -       | -    |
| Single-phase flow    |                |      |         |         |         |      |
| (Water)             | <30           | -    | -       | 0.45    | 0.30    | -    |
| rate [m/s]          | 30~50         | -    | -       | -       | 0.30    | -    |
|                     | 50≤           | -    | -       | -       | -       | -    |

1) In a control valve down-stream part, wear rate shall be 5 times the values shown in the table.
2) In a glove type check valve down-stream part, wear rate shall be twice the values shown in the table.

Table 3.2-2  Dimension of pipe and the minimum thickness.

<table>
<thead>
<tr>
<th>D [mm]</th>
<th>t [mm]</th>
<th>Pmax [MPa]</th>
<th>S [MPa]</th>
<th>tmin [mm]</th>
</tr>
</thead>
<tbody>
<tr>
<td>660.4</td>
<td>18.9</td>
<td>1.275</td>
<td>103</td>
<td>5.50</td>
</tr>
<tr>
<td>318.5</td>
<td>10.3</td>
<td>1.275</td>
<td>103</td>
<td>2.65</td>
</tr>
<tr>
<td>114.3</td>
<td>6.0</td>
<td>1.275</td>
<td>103</td>
<td>0.95</td>
</tr>
</tbody>
</table>

Table 3.2-3  Stress for failure assessment.

<table>
<thead>
<tr>
<th>Thinning patterns</th>
<th>Pm [MPa]</th>
<th>Pb [MPa]</th>
</tr>
</thead>
<tbody>
<tr>
<td>Local 360°</td>
<td>0.8S</td>
<td>0.8S</td>
</tr>
<tr>
<td>Local circumferential</td>
<td>0.4S</td>
<td>0.8S</td>
</tr>
<tr>
<td>Local axial</td>
<td>0.8S</td>
<td>—</td>
</tr>
</tbody>
</table>

Note) S: Allowable tensile stress
Table 3.2-4  Sample's existence probability.

<table>
<thead>
<tr>
<th>Evaluation equation</th>
<th>Case-1</th>
<th>Case-2</th>
</tr>
</thead>
<tbody>
<tr>
<td>Eq. (3.2-6)</td>
<td>0.358</td>
<td>0.122</td>
</tr>
<tr>
<td>Eq. (3.2-7)</td>
<td>0</td>
<td>0</td>
</tr>
</tbody>
</table>

Table 3.2-5  Classification of the failure probability for 4-inch pipe.

<table>
<thead>
<tr>
<th>Wear rate [mm/h]</th>
<th>Inspection interval [year]</th>
<th>360-deg.</th>
<th>Local circum.</th>
<th>Local axial</th>
</tr>
</thead>
<tbody>
<tr>
<td></td>
<td>S1</td>
<td>S2</td>
<td>S3</td>
<td>S4</td>
</tr>
<tr>
<td>0.3×10⁻⁴</td>
<td>1</td>
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<td></td>
<td>2</td>
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<td></td>
<td>10</td>
<td>x</td>
<td>x</td>
<td>x</td>
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<tr>
<td>0.5×10⁻⁴</td>
<td>1</td>
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Note)  
S1: Applied stress is 0.25 times of the value in Table 3.2-3.  
S2: Applied stress is 0.5 times of the value in Table 3.2-3.  
S3: Applied stress is 0.75 times of the value in Table 3.2-3.  
S4: Applied stress is the value in Table 3.2-3.  
-: Failure probability ≤ 7×10⁻⁶ [ry⁻¹]  
×: Failure probability > 7×10⁻⁶ [ry⁻¹]
<table>
<thead>
<tr>
<th>Wear rate [mm/h]</th>
<th>Inspection interval [year]</th>
<th>360-deg.</th>
<th>Local circum.</th>
<th>Local axial</th>
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<tr>
<td>0.3×10⁻⁴</td>
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</table>

Note) S₁, S₂, S₃, S₄, ‘−’ and ‘×’ are the same as Table 3.2-5.
Table 3.2-7 Classification of the failure probability for 26-inch pipe.

<table>
<thead>
<tr>
<th>Wear rate [mm/h]</th>
<th>Inspection interval [year]</th>
<th>Local 360° S1 S2 S3 S4</th>
<th>Local circum. S1 S2 S3 S4</th>
<th>Local axial S1 S2 S3 S4</th>
</tr>
</thead>
<tbody>
<tr>
<td>0.3×10⁻⁴</td>
<td>1</td>
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</tbody>
</table>

Note) S1, S2, S3, S4, ‘-’ and ‘x’ are the same as Table 3.2-5.

Table 3.2-8 Relation between thickness of pipe and failure probability.
(wear rate: 2×10⁻⁴ mm/h, applied stress: Table 3.2-3)

<table>
<thead>
<tr>
<th>Schedule of pipe</th>
<th>Inspection interval [year]</th>
<th>Failure probability [r⁻¹]</th>
</tr>
</thead>
<tbody>
<tr>
<td></td>
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</tr>
<tr>
<td>80</td>
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<td>3.12×10⁻³</td>
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<td>7.60×10⁻³</td>
</tr>
<tr>
<td>120</td>
<td>3</td>
<td>1.18×10⁻³</td>
</tr>
<tr>
<td></td>
<td>5</td>
<td>3.54×10⁻³</td>
</tr>
<tr>
<td>160</td>
<td>3</td>
<td>5.20×10⁻⁴</td>
</tr>
<tr>
<td></td>
<td>5</td>
<td>1.82×10⁻³</td>
</tr>
</tbody>
</table>

Failure probability exceeds 7×10⁻⁶ [r⁻¹] in hatched area.
Table 3.2-9  Sensitivity to standard deviation on failure probability (wear rate: $2 \times 10^{-4}$ mm/h, applied stress: Table 3.2-3).

<table>
<thead>
<tr>
<th>Schedule of pipe</th>
<th>Inspection interval [year]</th>
<th>Failure probability [r·y⁻¹]</th>
<th>4B</th>
<th>12B</th>
<th>26B</th>
</tr>
</thead>
<tbody>
<tr>
<td>80</td>
<td>3</td>
<td>$4.30 \times 10^{-6}$</td>
<td>≈0</td>
<td>≈0</td>
<td>≈0</td>
</tr>
<tr>
<td></td>
<td>5</td>
<td>$2.88 \times 10^{-4}$</td>
<td>$1.60 \times 10^{-7}$</td>
<td>≈0</td>
<td>≈0</td>
</tr>
<tr>
<td>120</td>
<td>3</td>
<td>$5.67 \times 10^{-7}$</td>
<td>≈0</td>
<td>≈0</td>
<td>≈0</td>
</tr>
<tr>
<td></td>
<td>5</td>
<td>$2.70 \times 10^{-5}$</td>
<td>$2.00 \times 10^{-8}$</td>
<td>≈0</td>
<td>≈0</td>
</tr>
<tr>
<td>160</td>
<td>3</td>
<td>$1.00 \times 10^{-7}$</td>
<td>≈0</td>
<td>≈0</td>
<td>≈0</td>
</tr>
<tr>
<td></td>
<td>5</td>
<td>$3.22 \times 10^{-6}$</td>
<td>≈0</td>
<td>≈0</td>
<td>≈0</td>
</tr>
</tbody>
</table>

Standard deviation of wear rate is 0.345.
Failure probability exceeds $7 \times 10^{-6}$ [r·y⁻¹] in hatched area.

Fig. 3.2-1  Probability density function of wear rate.
Fig. 3.2-2 Wall thinning patterns.

Fig. 3.2-3 Cumulative failure probability (4 inch pipe, no inspection, wear rate: $0.3 \times 10^{-4}$ mm/h, applied stress: 0.25 times of Table 3.2-3).
Fig. 3.2-4  Cumulative failure probability (12 inch pipe, no inspection, wear rate: $0.3 \times 10^{-4} \text{ mm/h}$, applied stress: 0.25 times of Table 3.2-3).

Fig. 3.2-5  Cumulative failure probability (26 inch pipe, no inspection, wear rate: $0.3 \times 10^{-4} \text{ mm/h}$, applied stress: 0.25 times of Table 3.2-3).
Fig. 3.2-6 Effect of the evaluation equation of inspection on failure probability (local 360° thinning, an inspection interval of 5 years, wear rate: $2.0 \times 10^{-4}$ mm/h, applied stress: Table 3.2-3).

Fig. 3.2-7 Effect of the evaluation equation of inspection on failure probability (26 inch pipe, local 360° thinning, wear rate: $2.0 \times 10^{-4}$ mm/h, applied stress: Table 3.2-3).
Fig. 3.2-8  Effect of inspection interval on failure probability (local 360° thinning, applied stress: Table 3.2-3).

Fig. 3.2-9  Effect of the thinning pattern on failure mode (inspection interval: 10 years, wear rate: 2.0×10⁻⁴ mm/h, applied stress: 0.25 times of Table 3.2-3).
Fig. 3.2-10  Effect of wear rate on failure probability (local 360° thinning, an inspection interval: 10 years, applied stress: 0.25 times of Table 3.2-3).

Fig. 3.2-11  Effect of applied stress on failure probability (local 360° thinning, inspection interval: 10 years).
3.3 Probabilistic Assessment of Leak-Before-Break Behavior in LWR Pipes

3.3.1 Introduction

Existing design and integrity assessments of nuclear reactor structural components are generally performed in conjunction with deterministic resolution of all related factors. Uncertainties originating in divergence of actual loading history from predicted or design values, variations in strength, and other factors are all included in determination of the safety factor. In this deterministic assessment, estimation of every factor is conservative and the final conclusion derives from cumulative combination of these estimates, resulting in excessively conservative assessments that are rationally problematic.

Key problems that have recently grown in importance in regard to the integrity of structural components in nuclear power plants include predicting the probability of failure under pressurized thermal shock (PTS) and the life of structural components in aging plants and their life extension, and relative to these the frequency of risk-based inspection and the problems associated with severe accidents and earthquakes that involve highly complex phenomena and are difficult to solve. This has gradually led to a growing recognition of the importance of probabilistic assessment. Its application to actual problems has been approved in the United States since 1987, when the relevant regulatory reform by the U.S. Nuclear Regulatory Commission became effective in 10 CFR Part 50 Appendix A “Modification of General Design Criterion 4”, Requirements for Protection Against Dynamic Effects of Postulated Pipe Ruptures.[1]

Given these circumstances, probabilistic methods and in particular probabilistic fracture mechanics (PFM), which incorporate probability theory into fracture mechanics, will undoubtedly take on increasing importance in Japan. Compared to in the United States and Europe, however, in Japan, PFM methodology is seldom applied to actual problems and sufficient consensus has not been achieved on interpretation of its results, its application to structural integrity and remaining life assessment, or decisions based thereon. In this context, establishment of the criteria for selection of input data and models for PFM analysis and the method of their application are essential.

In the United States in particular, pipe assessment using PFM analysis has been applied in many cases, including break accidents [3]-[5], and as noted above, the results have been incorporated in regulatory guidelines. In Japan, however, systematic research on Japanese nuclear reactors that includes consideration of design criteria has not been performed. The authors have reported [2] round-robin PFM analysis of nuclear reactor pressure vessels, and
investigation and research on PFM methodology and applicability has subsequently continued by the Japan Society of Mechanical Engineers RC111 research committee Research on Applicability of Probabilistic Fracture Mechanics Analysis Methods, on assignment from the Japan Atomic Energy Research Institute. Here we discuss the PFM analysis model, analytical conditions, and other aspects deemed most appropriate as standard, particularly in relation to leak before break (LBB) in light-water reactor (LWR) pipes, based on the progress of that committee.

3.3.2 Failure scenario and PFM analysis procedure

3.3.2.1 Failure scenario

The deterministic method based on the LBB concept for assessment of the structural integrity of LWR pipes is largely established. With reference to the Japanese design standard [6], the failure scenario that should be considered in PFM analysis is summarized in Fig. 3.3-1. In this scenario, assessment of leakage quantity related to leakage detection is omitted and in this regard differs from the design standard. In another departure, it includes in-service inspection (ISI; enclosed by dashed lines in the figure), which is not mandated by the design standard but is treated here as an object of sensitivity analysis as later described.

(1) Assumed location of initial defects
The presence of defects in the pipe material itself is negligibly small in comparison with those in the welds. In the present analysis, we assume the presence of an initial defect in a circumferentially oriented weld in the pipe.

(2) Number of initial defects
In the investigation by Harris [3] on the number of initial defects in pipe welds to that time, it was concluded that the defect density as the number per unit volume was $2.4 \times 10^6$/cm$^3$. The number of defects is generally small, and cases of multiple defect growth and merger of adjacent defects are provisionally excluded. In piping system reliability assessments, where necessary, the probability of failure per defect is multiplied by the number of
weld lines and the number of defects per weld line. Even where multiple defects are present, however, the stress intensity factors of adjacent defective tips interact [7] and may thus result in their merger during defect growth. PFM analysis of these mechanisms has been performed in a previous study [8].

(3) Defect configuration

We treat the defect as a semi-elliptical crack on the inner surface of the weld line, as shown in Fig. 3.3-2(a). A weld defect generally occupies a three-dimensional region within the weld, but can be substituted with a semi-elliptical surface crack circumscribing a projection of the defect on the principal stress plane and thereby forming a planar defect. Throughout this paper, the semi-elliptical surface defect obtained by this substitution is referred to as a crack.

In relation to this substitution of the defect by a semi-elliptical crack, it should be noted that leakage probability is largely determined by crack depth distribution and that any pipe weld defects that are visible on the surface are generally eliminated in pre-service inspection (PSI) and the remaining defects are actually embedded, and thus the substitution in the PFM analysis is very conservative.

(4) Expression of initial defect probability

The depth, half-length, and aspect ratio of the assumed semi-elliptical crack in the inner surface of the pipe are denoted by \(a\), \(b\), and \(\beta = \frac{b}{a}\), respectively. For the initial crack, two of these are clearly specified as random variables. Any two may be regarded as intrinsically probabilistically dependent, but at present we can only presume some probabilistic independence to assume distributions based on the results of various flaw detection tests. Generally, the independence of \(a\) and \(\beta\) is presumed rather than that of \(a\) and \(b\), which is more difficult to affirm [3].

(5) Crack penetration

In this analysis, the term penetration refers to attainment of a crack depth \(a\) that is approximately 80% of the plate thickness \(t\), in recognition of the
decline in reliability of the expression for stress intensity factor assessment for a semi-elliptical surface crack near attainment of complete ("crack-through") penetration and crack development from the rear surface can no longer be neglected. The circumferential length \(b\) of the post-penetration crack is as shown in Fig. 3.3-2(b).

(6) Stress cycle
The stress cycle comprises thermal and other transient stresses that occur in startup, shutdown, and operation. The stress cycles that actually occur can be found in annual operating management reports [9], but those actually exerted on reactor pipe systems are extremely small in magnitude and number. In PFM analysis, as in others, the uncertainties involved in transients are ignored and in their place the conservative stress cycles of design transients, though never reached in actual situation, are used [10].

(7) Crack growth analysis
Analysis of the cracking under the stress cycle is performed using an appropriate stress intensity factor and Paris’ law [11].

(8) Penetration and leakage
Crack growth analysis sometimes results in penetration and coolant leakage occurs as a subsequent event, and the leakage amount and rate (quantity/per unit time) are dependent on the circumferential length of the crack, the pressure difference across the pipe wall, and other factors; it is therefore desirable to treat penetration and leakage separately. Leak assessment then requires modeling of leakage quantity or rate, however, and is therefore performed in a simple yet conservative manner by taking penetration itself as leakage.

(9) Leak detection
Consideration of leak detection is essential for assessment of LBB applicability. Generally, if a leak occurs, then it is detected visually or by detectors and plant operation is shut down. Extremely severe cases in which a pipe break occurs before leakage are not excluded from the analysis, but such events occur only if the initial crack is extremely long circumferentially and their contribution to overall break probability is accordingly small, as noted in part (2) of this section. If a leak goes undetected and operation continues, then the possibility that it might lead to a pipe break must be considered, particularly because a penetrating crack grows circumferentially faster than a non-penetrating crack.

The rate of leak detection is dependent on leak detector capability, leak location, and other factors, and its modelling is not necessarily simple. The opposite extremes of safety and non-safety are immediate leak detection followed by plant shutdown and continued reactor operation without
detection of existing leakage throughout the reactor life. Given these extremes, one approach is to analyze the probability of each with the assumption that the actual break probability lies between the two. Another is to set a threshold value that must not be exceeded for crack length or crack opening, which are determining factors in leakage quantity and leakage rate. A third is to impose a limit on the period to leak detection following crack penetration (and thus on the period of non-discovery).

(10) Pipe break
In general, break criteria which might be applied to pipe breaks include (1) the \( K_c \) brittle fracture criterion based on small-scale yield; (2) the \( J/T_c \) criterion (\( J: J \) integral; \( T: \) tearing modulus), assuming large-scale yield; and (3) the plastic collapse criterion, assuming full-section yield of the pipe with the supposition that the material is an elasto perfectly plastic solid [11]. The first cannot be used alone for LWR pipes because of the strength properties of the pipe material. The second provides generality in regard to materials and dimensions for assessment of load-resistant capacity at ductile crack initiation and thereafter, and has been applied in PFM analysis [12][13], but presents problems in regard to the lack of a simple established method of \( J \)-integral assessment and the dependence of \( J_c \) and other strength properties on the test-piece shape and dimensions. The third, in the “G-factor method”, has been complemented for correspondence with the second for medium- and large-bore pipes composed of Japanese carbon steel [10] and has been verified in extensive experimental testing. Related methods include the R6 method [11][14][15] of the U.K. CEGB (now Nuclear Electric), which combines the first and third criteria to assess unstable fracture from a given arbitrary yield scale, and the G-factor method, which is approximately equivalent to Category 2, Option 2 of the R6 method.

Applied load in use as a criterion of break is independent of the load used in assessing crack growth, and is not considered for use in regard to crack growth.

(11) Pre-service inspection (PSI)
In PSI, the weld lines in reactor components are inspected before service by ultrasonic testing (UT), X-ray inspection, and other volumetric methods. In PFM analysis, all defects detected in PSI are regarded as having been completely removed and only undetected defects are considered, in terms of non-detection probability \( P_{ND} \).
(12) In-service inspection (ISI)
The effects of ISI can be quantitatively assessed in PFM analysis in terms of the probability $P_{ND}$ of defect non-detection by ISI. It must be noted in this regard that in ISI, unlike PSI, it is difficult to gain close proximity to relevant regions and, for this and other reasons, ISI may instead consist entirely of visual surface observations or similar such means [16].

3.3.2.2 LBB applicability index in PFM analysis
In general, to say that LBB is applicable could simply point to break non-occurrence without reference to occurrence or non-occurrence of a leakage accident. In this interpretation, the probability of LBB inapplicability, $P_{-\text{LBB}}$, may be expressed in terms of the probabilities of break conditional on leakage, $P_{B|L}$, and break conditional on non-leakage, $P_{B|\sim L}$, as

$$P_{-\text{LBB}} = P_{L}P_{B|L} + P_{\sim L}P_{B|\sim L}$$

(3.3-1)

The terms $P_{B|L}$ and $P_{B|\sim L}$ on the right-hand side are given by

$$P_{B|L} = \frac{P_{L\land B}}{P_{L}}$$

(3.3-2)

$$P_{B|\sim L} = \frac{P_{(\sim L)\land B}}{P_{\sim L}}$$

(3.3-3)

where $P_{L}$, $P_{\sim L}$, $P_{L\land B}$, and $P_{(\sim L)\land B}$ are respectively leakage, non-leakage, leakage and break, and non-leakage and break. $P_{(\sim L)\land B}$ and $P_{B|\sim L}$ both apply only to cases of breaks in exceptionally long circumferential non-penetrating cracks and $P_{L\land B}$ as later shown (see Sec. 3.3-4(1)) is negligibly small compared with $P_{B|L}$ under the analytical conditions. Probabilistic assessment of LBB behavior can therefore be limited to assessment of break accident occurrence behavior due to circumferential crack growth literally in the presence of leak accident occurrence. In this context, we accordingly represent the facility of LBB applicability by the inverse of Eq. (3.3-2), which is the main constituent of Eq. (3.3-1), and newly propose and apply the LBB applicability index (in simple terms, the “LBB index”):
\[ \lambda_{LBB} = \frac{I}{P_{B|L}} = \frac{P_L}{P_{L\&B}} \]  

(3.3-4)

Since \( P_{L\&B} \approx 0 \) and \( P_B = P_{L\&B} + P_{L\&B} \approx P_{L\&B} \), we then have

\[ \lambda_{LBB} \approx \frac{P_L}{P_B} \]  

(3.3-5)

On a semilog graph, this is equivalent to \( \log P_L - \log P_B \). It is an expression of the concept that break occurrence is difficult under a condition in which leak occurrence increases as its value increases. With reversal on the logarithmic axis with respect to \( 10^0 \), the break probability under the leak condition can be immediately obtained, and it may therefore be regarded as a natural definition.

3.3.3 Analytical model and input data

This section describes the analysis model based on the failure scenario in Sec. 3.3.2(1).

3.3.3.1 Focus of analysis  

The analysis is focused on the weld regions of piping LWRs, more specifically on the weld regions of LWR carbon-steel 4B, 16B, and 26B pipes, as it has been shown by deterministic assessment of pipe integrity that LBB applicability may vary with pipe bore [10]. A temperature of 300°C is assumed and the material properties, which are shown in Table 3.3-1, are taken from Ministry of International Trade and Industry Notification 501 [17] and Reference [18].
3.3.3.2 Probability distribution of initial crack size  

As the distribution of the initial crack depth, we use the Marshall distribution based on investigation relating to pressure-vessel defects: [19][20]

\[ f(x) = \frac{\lambda e^{-\lambda x}}{1-e^{-\lambda}} , \quad \lambda = 0.16 \ (1/\text{mm}) \]  \hspace{1cm} (3.3-6)

In this distribution, the mean value of the initial crack depth is approximately 6.25 mm (1/0.16), and the probability of initial crack penetration (the probability that \( a/t \) exceeds the crack penetration threshold of 0.8) before PSI is 0.09 for 4B piping. This is extremely conservative. It is taken from the maximum shown in past publications, and as the standard in this analysis. In performing the sensitivity analysis, we use the logarithmic normal distribution by Bruckner [21] in his investigation of LWR piping:

\[ f(x) = \frac{D}{\sqrt{2\pi} S} \exp \left\{ -\frac{1}{2} \left( \frac{\log x}{\sigma} \right)^2 \right\} , \quad \mu = 0.294, \quad S = 1.61 \]  \hspace{1cm} (3.3-7)

where \( D \) is a normalization factor representing the probability density in \( f(x) \) (see Fig. 3.3-3).

Available information on aspect ratio distribution is extremely limited, and we have found only the following logarithmic normal distribution used by LLNL [4]:

<table>
<thead>
<tr>
<th>Table 3.3-1 Pipe specifications and material constants</th>
</tr>
</thead>
<tbody>
<tr>
<td>Size</td>
</tr>
<tr>
<td>Schedule</td>
</tr>
<tr>
<td>Outer diameter (mm)</td>
</tr>
<tr>
<td>Thickness (mm)</td>
</tr>
</tbody>
</table>

Material | STS 410
Operation temperature | 300°C
Design stress intensity \( S_m^{[17]} \) | 122.6 MPa
Design yield strength \( S_y^{[17]} \) | 183.4 MPa
Design ultimate strength \( S_u^{[17]} \) | 404.0 MPa
Flow stress \( \sigma_f \left( = \frac{1}{2} (S_y + S_u) \right) \) | 293.7 MPa
Young’s modulus \( E^{[17]} \) | 178.5 GPa
Poisson’s ratio \( \nu^{[17]} \) | 0.3
Fracture toughness \( J_{IC}^{[17]} \) | 209.9 kN/m
Fracture toughness \( K_{IC} \) | 156.2 MPa√m

Notes: \( K_{IC} \) is taken as following mean \( K_{IC}^{*} \) and standard deviation \( \sigma = 0.1 K_{IC}^{*} \) normal distribution. The mean value is calculated from the relation

\[ J_{IC} = (1-\nu^2)\left( K_{IC}^{*} \right)^2/E. \]

Its value is \( K_{IC}^{*} - 2.3\sigma \) with 99% lower-limit reliability. This is in accord with the Notification 501 method for determination of material-property values.
where normalization factor $D'$ is the $g(x)$ probability density. This distribution is assumed for all cases covered in this paper.

\[ g(y) = \frac{D'}{\sqrt{2\pi y}} \exp\left\{ -\frac{1}{2S_\beta^2} \left( \frac{\log y}{\mu_\beta} \right)^2 \right\}, \quad y \geq 1 \quad (3.3-8) \]

\[ \mu_\beta = 1.366, \quad S_\beta = 0.538 \quad (3.3-9) \]

**3.3.3.3 Stress cycle** The stress cycle used in the analyses is based on the primary and secondary stresses shown in Table 3.3-2 from the BWR main steam system stress cycle (with the applied stress-cycle number conservatively taken as twice the design number) [10], and in the analysis assume a life of 80 years and thus twice the design life.

**Table 3.3-2 Load conditions [10] in the crack growth analysis**

<table>
<thead>
<tr>
<th>No.</th>
<th>Tension</th>
<th>Bending</th>
<th>Cycles/yr</th>
</tr>
</thead>
<tbody>
<tr>
<td></td>
<td>max</td>
<td>min</td>
<td>max</td>
</tr>
<tr>
<td>1</td>
<td>1.0</td>
<td>0.0</td>
<td>0.0</td>
</tr>
<tr>
<td>2</td>
<td>1.5</td>
<td>0.4</td>
<td>0.0</td>
</tr>
<tr>
<td>3</td>
<td>1.0</td>
<td>0.75</td>
<td>0.0</td>
</tr>
<tr>
<td>4</td>
<td>0.0</td>
<td>0.0</td>
<td>1.0</td>
</tr>
<tr>
<td>5</td>
<td>0.0</td>
<td>0.0</td>
<td>0.5</td>
</tr>
<tr>
<td>6</td>
<td>0.0</td>
<td>0.0</td>
<td>0.1</td>
</tr>
</tbody>
</table>
### 3.3.3.4 Stress intensity factor and crack growth rule

As the stress intensity factor for non-penetrating cracks, we apply the Newman–Raju equation [22] for semi-elliptical cracks in plates under membrane and bending stresses. For penetrating cracks, we apply the Tada–Paris equation [23]:

\[
F(\theta) = 1 + 7.5 \left( \frac{\theta}{\pi} \right)^{1.5} - 15 \left( \frac{\theta}{\pi} \right)^{2.5} + 23 \left( \frac{\theta}{\pi} \right)^{3.5}
\]

(3.3-11)

where \( R_o \) and \( \theta \) are the mean pipe radius and the crack half-angle, respectively.

For crack growth, we apply Paris’ law[11]:

\[
\frac{da}{dN} = C(\Delta K_a)^n
\]

\[
\frac{db}{dN} = C(\Delta K_b)^n
\]

(3.3-12)

where \( K_a \) and \( K_b \) are the \( K \) values at the crack depth and the deepest point lengthwise, respectively. \( \Delta K \) is the change in \( K \) with change in stress

\[
\Delta K = K_{\text{max}} - K_{\text{min}}
\]

(3.3-13)

We use the \( C \) and \( n \) material-property values shown in Table 3.3-3, which are from the upper-limit curve of the reference crack growth law in water in ASME Section 11 Appendix A [24].

<table>
<thead>
<tr>
<th>( \Delta K \leq 13.2 \text{ (MPa} \cdot \text{m}^{1/2}) )</th>
<th>( \Delta K &gt; 13.2 \text{ (MPa} \cdot \text{m}^{1/2}) )</th>
</tr>
</thead>
<tbody>
<tr>
<td>( C )</td>
<td>( 1.75 \times 10^{-10} )</td>
</tr>
<tr>
<td>( n )</td>
<td>5.95</td>
</tr>
</tbody>
</table>

### 3.3.3.5 Crack penetration and leakage

A crack of depth \( a \) which is 80% or more of the plate thickness \( t \) is defined as a penetrating crack, and this penetration is assumed to immediately result in leakage.
3.3.3.6 Pipe break criterion The G-factor method[10] is applied as the standard for the pipe break criterion, and the R6 Category 1, Option 1 [14] method is applied for sensitivity analysis. R6 Category 1 is ordinarily inappropriate for pipe material STS410 because of its high ductility and the resultant excessively conservative results, but worldwide only R6 Appendix 10 [25] provides a detailed assessment procedure for PFM analysis and Category 1, Option 1 was chosen for the present analysis as the simplest and least arbitrary of the R6 recommended methods, and the trends were ascertained on that basis.

(1) G-factor method
Involvement of secondary stress in breaks is regarded as negligible. As primary stress, we use the limit on membrane stress $\sigma_m$ and bending stress $\sigma_b$ given in ASME Section III [26] as

$$\sigma_m + \sigma_b \leq 1.5S_m$$  \hspace{1cm} (3.3-14)

The acting membrane stress and bending stress are determined by this upper limit. We take $\sigma_m$ as constantly applied internal pressure and self-weight, and, in accordance with deterministic analysis [10][27], assume it to be 0.5 $S_m$, giving $\sigma_b = 1.0S_m$.

$$\begin{align*}
\sigma_m &= 0.5S_m \\
\sigma_b &= 1.0S_m
\end{align*}$$  \hspace{1cm} (3.3-15)

The occurrence of break is determined by

$$\sigma_b \geq \sigma_L$$  \hspace{1cm} (3.3-16)

where $\sigma_L$ is the limit bending stress, which is defined as follows in accordance with the neutral bend angle $\beta$ of the pipe section based on the membrane stress with crack presence [11][15], defined as

$$\beta = \frac{1}{2}\left(\pi - \alpha \theta - \pi \frac{\sigma_m}{\sigma_f}\right)$$  \hspace{1cm} (3.3-17)

If $\beta \leq \pi - \theta$, then
\[
\sigma_L = \frac{1}{G} \cdot \frac{2}{\pi} \sigma_f(2 \sin \beta - \alpha \sin \theta) \tag{3.3-18}
\]

whereas if \( \beta > \pi - \theta \), then

\[
\beta = \frac{\pi}{2 - \alpha} \left(1 - \frac{\sigma_m}{\sigma_f}\right) \tag{3.3-19}
\]

and

\[
\sigma_L = \frac{1}{G} \cdot \frac{2}{\pi} \sigma_f(2 - \alpha) \sin \beta \tag{3.3-20}
\]

In the above equations, \( t \), \( \alpha \), \( \theta \), and \( \sigma_f \) are respectively plate thickness, relative crack depth (\( a/t \)), crack half-angle (\( b/R \); \( R \) : pipe internal radius), and flow stress. We take \( \sigma_f \) as the mean of the yield stress \( \sigma_y \) and the tensile strength \( \sigma_u \) (see Table 3.3-1). \( G \) is the G factor, defined by[10]

\[
G = \begin{cases} 
1 & (2 \leq D < 6) \\
\max \left(1, \left(0.692 - 0.0115 \ D\right) + \left(0.188 + 0.0104 \ D\right) \log \left(\frac{L_0}{a}\right)\right) & (D \geq 6)
\end{cases} \tag{3.3-21}
\]

where \( D \) is the pipe’s nominal diameter.

(2) R6 method

In addition to Eq. (3.3-15), another stress size used to determine break occurrence is the primary and secondary stress limit in ASME Section III, [26] expressed as

\[
\sigma_m + \sigma_b + Q \leq 3S_m \tag{3.3-22}
\]

With Eq. (3.3-15) applied to the upper limit of this inequality, the secondary stress \( Q \) is

\[
Q = 1.5S_m \tag{3.3-23}
\]

Break occurrence is determined from \( L_r \), \( K_r \), and FAC (the failure assessment curve) [14] by

The side of FAC occupied by Point \((L_r, K_r)\) \tag{3.3-24}
The dimensionless stress intensity factor $K_r$ is defined by

$$K_r = \frac{K_{ip} + K_{is}}{K_{ic}} + \rho \quad (3.3-25)$$

where $K_{ip}$ and $K_{is}$ are the stress intensity factors of primary and secondary stresses, respectively, and as in crack growth analysis the current crack size and stress size are found from Eqs. (3.3-15) and (3.3-23). Secondary stress $Q$ is taken as being solely the membrane stress. $K_{ic}$ is the fracture toughness. The stress intensity factor reference position is taken as the surface point in the direction of unstable crack growth. Preliminary analysis has shown that taking this as the deepest part of the crack has no appreciable effect on the analysis results. Parameter $\rho$ is a corrective value for interaction between primary and secondary stresses, defined as

$$\rho = \begin{cases} 
\rho_i \left( K_{is}/K_{ip} \cdot L_r \right) & (L_r < 0.8) \\
4 \rho_i \left( K_{is}/K_{ip} \cdot L_r \right) \cdot (1.05 - L_r) & (0.8 \leq L_r < 1.05) \\
0 & (L_r \geq 1.05)
\end{cases} \quad (3.3-26)$$

$$\rho_i(x) = \begin{cases} 
0 & (x < 0) \\
0.1x^{0.714} - 0.007x^2 + 0.00003x^5 & (0 \leq x < 5.2) \\
0.25 & (x \geq 5.2)
\end{cases} \quad (3.3-27)$$

Parameter $L_r$ is the ratio of all acting loads that generate primary stress to the limit load for crack material yield, and thus represents the degree of yield. It is defined in the analysis by

$$L_r = \frac{\sigma_b}{\sigma_{L_r}} \quad (3.3-28)$$

where $\sigma_b$ is the limit value of primary stress in accordance with Eq. (3.3-15) and $\sigma_{L_r}$ is equivalent to $\sigma_L$ with a $G$ factor of 1 in Eqs. (3.3-17) to (3.3-20) and flow stress $\sigma_f$ replaced by yield stress $\sigma_y$.

The FAC (see Fig. 3.3-4) in accordance with Option 1 is obtained from
\[
K = \begin{cases} 
(1 - 0.14 L) \left( 0.3 + 0.7 \exp \left(-0.65 L^6 \right) \right) & \text{for } L \leq L_{\text{max}} \\
0 & \text{for } L > L_{\text{max}} 
\end{cases}
\] 
(3.3-29)

\[
L_{\text{max}} = \frac{\sigma_f}{\sigma_y}.
\] 
(3.3-30)

3.3.3.7 PSI For the probability \( P_{\text{ND}} \) of defect oversights in the PSI, the equation used by LLNL for ferritic steel pipes LLNL[4] is applied:

\[
P_{\text{ND}} = \left(1 - \varepsilon \right) \left(1 - \frac{2}{\sqrt{\pi}} \int_0^{\log A A^*} e^{-t^2} dt \right) + \varepsilon
\]
(3.3-31)

where

\[
A = \begin{cases} 
\frac{\pi}{2} ab & \text{for } 2b \leq D_B \\
\frac{\pi}{4} a D_B & \text{for } 2b > D_B 
\end{cases}
\]
(3.3-32)

\[
A^* = \frac{\pi}{4} a^* D_B
\]
(3.3-33)

and \( D_B \) is the ultrasonic beam diameter of 25.4 mm, \( a^* \) is the defect depth of 6.35 mm having a 50% probability of going undetected, and parameters \( \varepsilon \) and \( \nu \) are 0.005 and 1.33, respectively.

3.3.3.8 ISI As noted in (l) above, ISI may forego volumetric inspection and thus tend to leave inner surface cracks undetected. For this reason, we take as the standard of reference the case in which no ISI is assumed.
3.3.3.9 Numerical calculation  Rather than attempting to shorten the run time for PFM analysis by avoiding the use of Monte Carlo methods [28], let us apply stratified sampling [2][3] by first dividing the sampling space by the crack depth ratio $a/h$ and the aspect ratio $a/b$ ($1/\beta \leq 1$) into cells that are sample groups and then applying a relatively simple Monte Carlo method to each cell. The number of cells is $200 \times 200$ and the number of samples per cell is 250, based on a preliminary analysis performed on condition of convergence.

3.3.4 Sensitivity analysis

Sensitivity analysis is performed for pipe bore, leak detection capability, defect depth probability distribution, break condition, crack growth rate, loading history, and ISI. The focus is mainly on the change over time in probability of failure per defect and the LBB index as defined by Eq. (3.3-5). Table 3.3-4 shows a summary of the sensitivity analysis conditions (as well as the case name designations).

<table>
<thead>
<tr>
<th>Case</th>
<th>Leak monitor</th>
<th>Crack depth distribution</th>
<th>Fracture criteria</th>
<th>Crack growth rate</th>
<th>Applied stress</th>
<th>ISI</th>
</tr>
</thead>
<tbody>
<tr>
<td>REF</td>
<td>N</td>
<td>Marshall</td>
<td>G-factor</td>
<td>100%</td>
<td>100%</td>
<td>N</td>
</tr>
<tr>
<td>CLM</td>
<td>Y</td>
<td></td>
<td></td>
<td></td>
<td></td>
<td></td>
</tr>
<tr>
<td>(immediate)</td>
<td></td>
<td></td>
<td></td>
<td></td>
<td></td>
<td></td>
</tr>
<tr>
<td>1YR</td>
<td>Y</td>
<td></td>
<td></td>
<td></td>
<td></td>
<td></td>
</tr>
<tr>
<td>(after 1 year)</td>
<td></td>
<td></td>
<td></td>
<td></td>
<td></td>
<td></td>
</tr>
<tr>
<td>DP</td>
<td>Y</td>
<td>Bruckner</td>
<td>R6</td>
<td>50%</td>
<td></td>
<td></td>
</tr>
<tr>
<td>R6</td>
<td></td>
<td></td>
<td></td>
<td></td>
<td></td>
<td></td>
</tr>
<tr>
<td>CK1</td>
<td></td>
<td></td>
<td></td>
<td>50%</td>
<td></td>
<td></td>
</tr>
<tr>
<td>CK2</td>
<td></td>
<td></td>
<td></td>
<td>200%</td>
<td></td>
<td></td>
</tr>
<tr>
<td>LS1</td>
<td></td>
<td></td>
<td></td>
<td></td>
<td>75%</td>
<td></td>
</tr>
<tr>
<td>LS2</td>
<td></td>
<td></td>
<td></td>
<td></td>
<td>125%</td>
<td></td>
</tr>
<tr>
<td>IS</td>
<td></td>
<td></td>
<td></td>
<td></td>
<td></td>
<td>Y</td>
</tr>
</tbody>
</table>

Notes: The case names CLM, 1YR, DP, and IS are respectively taken from complete leak monitor, 1 year, crack depth, and ISI. CK and LS are respectively complete for K and load stress. Suffix 1 or 2 may be attached to indicate the order of analysis, and the pipe bore may be attached to the case names as in CLM-4B, REF-16B, and 1YR-26B.

3.3.4.1 Effect of leak detection capability  Fig. 3.3-5 shows changes over time in leak and break probabilities and the LBB index in the standard (REF), CLM, and 1YR cases for each bore. The failure probability appears extremely high for the 4B bore but this is an effect of the conservative Marshall distribution relating to surface cracks. A more rational assessment will be possible if embedded defect modelling and probability distribution of the defect position and size can be appropriately applied and a simple, highly reliable
technique for assessment of the embedded defect stress intensity factor can be established.

In the standard case (Case REF), the leak and break probabilities increase and the LBB index decreases with decreasing pipe bore. The LBB index obtained in the PFM analysis is in clear agreement with the deterministic analysis showing a trend of increasing LBB with increasing bore size [10]. If no leak detection is performed at all, break probability rises greatly in correspondence with circumferential crack growth in the years following crack penetration of the plate thickness and results in a decreasing LBB index over time, with the rate of decrease slowing in the period 40 to 80 years.

In Case CLM, in which leaks are detected immediately after penetration and the plant is then shut down, the break probability in effect is the probability

![Fig. 3.3-5 Failure probability and LBB index over time for Cases REF, CLM, and 1YR](image-url)
of break without leak. In Fig. 3.3-5, except for the first few years, the Case CLM break probability \( P_{\text{CLM}} \) is on an order that is negligible compared with that of Case REF \( P_B \) and, as described in Sec. 3.3.2(2), in effect \( P_B \sim P_{\text{L} \land \text{B}} \).

Actual leak detection lies somewhere between non-performance in Case REF and successful detection immediately following initiation in Case CLM. In Case 1YR, the period of one year from initiation to detection may be considered quite conservative, but as shown in Fig. 3.3-5, the probability of break remains low for all three bores and close to Case CLM rather than Case REF. The LBB indices of Case 1YR in 80 reactor-years for 4B, 16B, and 26B are respectively improved by factors of 50, 500, and 1,500 over those of Case REF (the probabilities of undetected LBB are respectively 1/50, 1/500, and 1/1,500), thus showing the very strong effect of leak detection capability on LBB applicability.

Both Case CLM and Case 1YR show very little increase in break probability after reactor-year 10. The LBB index therefore rises over the years, and LBB applicability accordingly rises. This is attributable to a rate of crack growth in the ligament that is higher toward the plate than circumferential, and the rise in rate of leak accident occurrence per reactor year is thus an effect of break accident reduction by leak detection. The possibility of probabilistically confirming this, as well as the effectiveness of the LBB index concept, in PFM analysis with the LBB index is important.

As shown by the relation between the pipe bore and the change in LBB index in 80 reactor-years for Cases REF, CLM, and 1YR in Fig. 3.3-6, the LBB index tends to increase with pipe bore and this tendency becomes more prominent with increasing leak detection capability.
3.3.4.2 Crack-length probability distribution under leakage  In the present analysis, penetration and leakage are viewed conservatively as one and the same category. By its basic nature, however, leakage should be assessed in connection with bore area [27], internal pressure, and other related factors, and leakage events and probabilities considered on that basis. In PFM analysis, it is possible to determine bore-area probability distribution and other aspects and apply the findings to aid in investigation of LBB applicability. As one example, in the present analysis, we found the probability that the crack half-length \( b \) exceeded a certain value under the Case REF conditions with 4B and 26B bore sizes, as shown in Fig. 3.3-7. The crack length in small- and large-bore pipes under leakage clearly differs, becoming substantially larger in large-bore pipes. The bore-area distribution thus broadens as a function of increasing crack length, and may accordingly serve as a means of leak detection.

![Fig. 3.3-7 Distribution of crack half-length probability under leakage for Case REF](image)

3.3.4.3 Effect of initial crack-depth probability distribution  We calculated the changes in leak probability over time and LBB index with bore size in Cases REF and DP, and found that regardless of the bore size, the leak and break probabilities were both lower in Case DP than in Case REF, by approximately one order of magnitude; for that reason, there was almost no difference between the two cases in LBB index.
3.3.4.4 Effect of break criterion  Fig.s 3.3-8 and 3.3-9 show the change over time in leak and break probabilities and LBB index, and the relation between LBB index and bore size, respectively, for Cases REF and R6. As noted earlier, the R6 Category 1, Option 1 method is appropriate for simple assessment but is also excessively conservative. This is clearly apparent from Fig. 3.3-8, in which the curve of break probability over time obtained by the R6 method is very close to that of leak probability and thus quite unlike that obtained by the G-factor method in Case REF. As shown in Fig. 3.3-9, in Case R6, the dependence of the LBB index on bore size is also relatively small. This is presumably attributable
to the remarkably strong effect of crack length on the FAC $K_c$ axis when using Category 1.

**3.3.4.5 Effect of fatigue crack growth rate** We analyzed the change over time in break probability and the correspondence with the relation between change in LBB index and bore size, for Cases REF, CK1, and CK2. As shown by the change over time in break probability in Fig. 3.3-10, the contribution of differences in crack growth rate to break probability increases markedly with bore size. On the other hand, the slope of the LBB index with respect to the bore size changes little with differences in crack growth rate, and the action on LBB applicability is approximately linear.

**3.3.4.6 Effect of loading history** Calculation of the changes in LBB index with bore size for Cases REF, LS1, and LS2 showed that under these conditions as well, the influence of load size is approximately the same as in the previous section, with the load size acting approximately linearly on LBB applicability.

**3.3.4.7 Effect of ISI** The effect of ISI is easier to comprehend in terms of its reduction of the rate of occurrence of leak or break per reactor year (the derivative of leak or break probability with respect to time), rather than its reduction of leak or break probability. Fig. 3.3-11 shows the change in leak probability, break probability, leak rate, and break rate at given times in Case IS-16B. The leak...
probability and break probability both become flat following the first ISI at 20 reactor-years, but neither shows any apparent effect of ISI at 40 or 60 reactor-years. In contrast, the leak rate and the break rate are both substantially reduced at each ISI implementation. It must be noted, however, that as mentioned above, these results are obtained using the same non-detection probability as that of defect non-detection in PSI, whereas in actuality the leak rate and break rate between ISI implementations presumably exceed the over-year rates and thus cannot be reduced as shown in Fig. 3.3-11.

3.3.5 Summary

We narrowed the focus to quantification of the behavior of LBB in LWR pipes, formulated a failure scenario based on currently available findings from Japan and other countries, and constructed an analysis model. We then performed sensitivity analysis encompassing uncertainties in input data selection, design conditions, and knowledge and test results. In this analysis, we introduced the LBB applicability index for a criterion for assessment of the facility of LBB applicability.

The results of the sensitivity analysis indicated that LBB applicability increases with pipe bore size, that leak detector capability strongly affects break probability, that LBB applicability rather paradoxically increases over time even with the highly conservative assumption of leak detection one year after leak initiation, and that selection of crack configuration distribution strongly affects failure probability but has little or no effect on LBB applicability. Sensitivity analysis was performed using Category 1, Option 1, the simplest of the R6 methods of break assessment. The results showed this method to be highly conservative, with no difference in LBB applicability index for differing bores sizes. The results of the investigation also showed that, under the conditions of the applied analysis, both load size and crack growth rate have approximately linear effects on the LBB applicability, and that ISI should be expected to reduce the failure rate per reactor-year rather than the failure probability.
REFERENCES


3.4 Seismic Performance Assessment of LWR Piping (1)

Design and structural integrity evaluation of components in nuclear power plants are usually performed using a deterministic method. Here, the results obtained usually involve excessive margin, because a certain safety margin is taken into account in every evaluation process. Such a situation inevitably causes an increase in plant construction cost. A probabilistic method is one of the candidates to reduce such excessive margin. In a probabilistic structural integrity evaluation, the failure probability is calculated using mathematical models, which include the dominant factors concerning failure behaviors. Here structural integrity is assessed by the failure probability. As the safety margin is considered only once in the final stage of failure probability, the probabilistic evaluation is regarded to give a rational estimation compared to the conventional deterministic evaluation. Fracture mechanics considering probabilistic issues is called probabilistic fracture mechanics (PFM). The study on PFM started in the middle of the 1970s to assess structural reliability of an aircraft and a pressurized vessel of a nuclear power plant [1-4]. Nowadays, PFM is an important part in safety design of nuclear power plants, reliability assessments of aircraft and so on. In PFM analyses, crack size, material strength, crack growth rate and other variables are expressed using probabilistic models, and then leak and break probabilities are calculated.

Generally, the load contributions to pipe break are mainly internal pressure, dead weight, thermal expansion and seismic load. The loads except the seismic load are always applied to the pipe, and can be evaluated considering the conditions of a piping system and its support structures. On the other hand, for the seismic load, the stress is large, but its frequency is very small, and it has complicated load transfer paths from a seismic source to the components to be considered. Therefore, variation of the stress on a pipe under a seismic event tends to be very large. Consequently, in the PFM analysis considering seismic loading, it is important to take into account the variation of the seismic loading.

3.4.1 Conventional PFM analysis with seismic loading

In conventional PFM analysis codes, for instance ‘PRAISE’ developed by Lawrence Livermore National Laboratory (LLNL) [4], seismic loading is treated as a deterministic value. Fig. 3.4-1 shows the conventional procedure to deal with the seismic loading in PFM analyses. Although it is basically possible to take uncertainty and variation into consideration in each step of Fig. 3.4-1, the conventional PFM codes estimate them as follows.

Probabilistic treatment is not employed in steps 2 and 3 because this is not included in the PFM analysis codes. The stress calculated by a deterministic seismic analysis is used in the PFM analysis. Here stress occurring in piping is assumed to be proportional to ground acceleration. Although the evaluation of Step 1 is not included in the PFM code, the relationship between the amplitude of the ground acceleration and frequency of earthquake
is taken into consideration using a seismic hazard curve. The load due to a certain amount of ground acceleration is calculated using the relationship between the load calculated in Step 3 and the ground acceleration postulated in Step 1. Step 4 is taken into account in the PFM analysis even when the seismic load is not considered. Steps 5 and 6 are particularly taken into consideration with respect to the seismic load.

When considering an earthquake, it is assumed to occur for the first time during an evaluation period. In the PRAISE code, the time of an earthquake is specified by the user. Fig. 3.4-2 shows a typical crack growth trajectory in a non-dimensional aspect ratio vs. crack depth map of the PRAISE code [4]. When an earthquake occurs at time $t_i$, the crack grows from $a_i$ to $a_i^{'}$, and its stability is assessed using this crack size ($a_i^{'}$). After this assessment, the crack size is returned to $a_i$, and the crack growth due to transient loads is calculated subsequently. When an earthquake occurs for the crack of size $a_2$, the crack tentatively grows from $a_2$ to $a_2^{'}$, and its stability is assessed using this crack size. After this, the crack size is returned to $a_2$. Such a procedure is repeated until the crack penetrates the pipe wall or the evaluation period reaches the pre-determined plant life. This analysis procedure does not consider two or more earthquakes. The case that multiple earthquakes occur at different times is evaluated on the same basis as crack growth analysis by multiple thermal transients.

### 3.4.2 Proposed method with variation of seismic loading

#### 3.4.2.1 Two kinds of variation of seismic loading

Variation of seismic loading for a pipe can be roughly classified into the following two parts (see Fig. 3.4-3). One is the variation of the ground acceleration and frequency of the earthquake acting on the nuclear power plant building. This variation is estimated using a seismic hazard curve. The other is the variation of piping response under a seismic event, which is caused by the variation of a seismic analysis model, material properties, etc. of the building and piping. The way of dealing with these two kinds of variation in a PFM analysis is investigated in the present study.

#### 3.4.2.2 Model of ground acceleration

The ground acceleration and frequency of an earthquake are quantitatively expressed with a seismic hazard curve as shown in Fig. 3.4-4. Using the hazard curve, Step 1 of Fig. 3.4-1 can be evaluated. Compared with thermal transients, which occur several times during a plant life, the earthquake has the following features: very low in frequency and very large in loading. In usual PFM analyses not considering the variation of seismic loading, the stratified Monte Carlo simulation is applied taking the initial depth and aspect ratio of a crack as random variables, both of which have significant effects on failure probability. The variation of the initial crack size has a much larger influence on failure probability than that in crack growth rate or material strength. In stratified Monte Carlo simulation, it is possible to calculate efficiently by distinguishing the sound region and failure (break and leak) region and giving a large
number of samples to the boundary of the both regions. Therefore, for the stratified Monte Carlo simulation, separating a failure region from a non-failure region and employing a suitable number of samples in each cell, is effective in performing efficient calculations. However, if an earthquake causes large stress in a pipe, failure might occur even when a crack is very small. In such a case, the failure probability depends not only on the crack size but also on the variation of the seismic loading. When a usual Monte Carlo simulation is applied to the case of large variation of loading, the failure probability is very sensitive to the probability of existence of a failed sample. When increasing the number of samples to avoid this problem, calculation time also increases because the frequency of a large earthquake is very low. In order to perform efficient calculations, importance sampling is employed in the proposed method. An earthquake is assumed to occur once a year, and the break probability of the sample \( P_f \) failed by the earthquake is calculated as the existence probability of the failed sample \( P_{fP} \) multiplied by the occurrence frequency of an earthquake \( P_e \): \( P_f = P_{fP} \times P_e \) (3.4-1)

Fig. 3.4-5 shows the comparison between the break probability calculated by the proposed method and that of the conventional method. A thin line denotes the result using the conventional method, while a thick line that of the proposed method. The number of cells and the number of samples are the same in both calculations. In the conventional method, failure probability oscillates significantly because of the large difference of existence probability of the broken samples. On the other hand, the proposed method gives a smooth result, which is in the middle of the oscillatory result of the conventional method. Thus, it is concluded that the proposed method can evaluate pertinent the break probability without increasing the number of samples, even if considering an earthquake with large variation of the ground acceleration and frequency.

### 3.4.2.3 Model of variation of seismic stress

The stress occurring in a pipe due to a building shaking is calculated by the procedure of steps 2 and 3 shown in Fig. 3.4-1. In a deterministic evaluation method, the responses of the building, components or piping are evaluated with a large margin, because a safety margin is taken into account for every process of the evaluation. Although the objective of the evaluation is different in a building and in components as shown in steps 2 and 3, the cause of variation is common in both the steps. The variation arises from the analysis model, material properties used for the analysis, the difference between a design and an actual construction, damping factor and so on. In Step 2, the interaction between the ground and the building is also taken into account. It is necessary to calculate the stress of the piping considering variation of all these factors. However, it is difficult to deal with all of them as random variables in a PFM analysis, because the stochastic process between the ground acceleration and the stress occurring in piping is very complicated. In the field of the seismic probabilistic risk assessment (PRA),
SMACS—precise probabilistic seismic response analysis code, which estimates directly the response of components from the ground motion—was developed in the seismic safety margins research program (SSMRP)[5], which was carried out by LLNL. However, since it required much expense and time for calculation, the code has not been used since its development. In the seismic PRA, a simplified method is generally used instead of such detailed analysis codes. In the simplified method, seismic response and variation of components are estimated based on the published information such as design documents. This technique is called ‘response factor method’. In the present study, the stress occurring in a pipe is calculated by referring to the realistic response of piping, which is assessed by the response factor method. Ebisawa et al. [6] reported that the probability density function for the stress in piping caused by the earthquake ($S_E$) can be expressed by the following lognormal distribution.

$$f(S_E) = \frac{1}{\sigma S_E \sqrt{2\pi}} \exp \left\{ -\frac{1}{2} \left[ \frac{\ln(S_E / \mu)}{\sigma} \right]^2 \right\}$$

where $S_D$ is the stress calculated from a deterministic technique, $RF$ is the response factor that Ebisawa et al. proposed for piping, and $\mu$ is the mean value. Eq. (3.4-2) shows that stress is expressed by a log-normal distribution with a mean value of $S_D / 3.92$. When using the seismic hazard curve, it is necessary to define the deterministic stress ($S_D$) and median value ($\mu$) according to the ground acceleration. The ground acceleration of the S2 earthquake and the piping stress caused by this acceleration are denoted $\alpha_{S2}$ and $S_{DS2}$, respectively. Since the ground acceleration and the stress in the piping are in a proportional relation, the stress in the piping for ground acceleration ($\alpha$) can be calculated from the following equation.

$$S_D(\alpha) = \frac{\alpha}{\alpha_{S2}} S_{S2}$$  

### 3.4.2.4 Probability of failure by earthquake

In usual PFM analyses, the cumulative failure probability is calculated. If a plant is in operation below an emergency condition, continuous operation is carried out. Therefore, it is appropriate that the probability of failure is calculated as a cumulative value. On the other hand, it is only required to secure the coolant boundary when a large earthquake, such as an S2 earthquake (categorized in emergency condition), occurs. In this case, the seismic stress may be at less ultimate strength, then large plastic deformation of the components are allowable. After the earthquake occurs, continuous operation of the plant would not be required. So that it does
not require a fatigue evaluation in service condition D, if the S2 earthquake occurs, the subsequent plant operation does not be considered in the plant design conditions. According to such a plant operation, the earthquake exceeding service condition C (exceeding S1 earthquake) is assumed that the earthquake happens for the first time at the time of evaluation in the PEPPER code. Calculation method of the failure probability due to a large earthquake (not using the cumulative probability) in PEPPER code is similar to the conventional PFM analysis codes. Based on this assumption, the failure probability at nth year, \( P_f(n) \), is expressed by the following equation.

\[
P_f(n) = \sum_{i=0}^{n} P_{f_x}(i) + P_{f_e}(n)
\]

where \( P_{f_x}(i) \) is the failure probability at ith year due to the loads exepting for earthquake, and \( P_{f_e}(n) \) is the failure probability at nth year due to earthquake.

**3.4.2.5 Upper limit of the seismic loads**  
As shown in Eq. (3.4-2), distribution of stress due to earthquakes is usually expressed in log-normal distribution. When used without upper limitation on this distribution, the stress caused by the earthquake exceeds the collapse stress of crack-free pipes, resulting collapse of pipes regardless to the size of the cracks in such conditions. Failure probability, \( F(S_E) \), in this case is given by the following equation.

\[
F(S_E) = \int_{S_C}^{\infty} \frac{1}{\sigma S_E \sqrt{2\pi}} \exp \left[-\frac{1}{2} \left( \frac{\ln(S_E/\mu)}{\sigma} \right)^2 \right] dS_E
\]

\[
\sigma = 0.95
\]

\[
\mu = \frac{S_D}{RF} = \frac{S_D}{3.92}
\]

where \( S_C \) is collapse stress of crack-free pipes. If this failure probability given by Eq. (3.4-5) exceeds much the failure probability due to loads exeptiong earthquake, the failure probability given by PFM analysis dependents of the failure probability calculated by Eq. (3.4-5)—the probability that the seismic stress exceeds the collapse stress of pipes—regardless to the size of the cracks. The failure probability assessment assuming the seismic loads exceeding the collapse loads of crack-free pipes is the same as flagility assessment in the seismic PRA and it is out of scope of PFM analysis considering the effect of cracks. Thus, the following two conditions were set in the PEPPER code when dealing with earthquakes.

1) Seismic stress does not exceed the collapse loads of crack-free pipes

2) The variation of the seismic stress is the 99% confidence level range
Applying these conditions, it is possible to failure probability analysis in consideration of both the effect of crack and the variations of seismic loads.

### 3.4.3 Comparative evaluation

Break probabilities are compared for the two cases for which the seismic load is treated as either deterministic or probabilistic value.

#### 3.4.3.1 Analysis conditions

The inelastic PFM analysis code named ‘PEPPER’ (Probabilistic Evaluation Program for Pipe aiming Economical and Reliable design [7]) is employed in this comparison study. Main steam pipes of a Japanese BWR are considered. These are made of carbon steel, STS410 in JIS Standard. Dimensions of the pipes are given in Table 3.4-1. Material properties of the pipes at 573K (normal operating condition of BWR) are given in Table 3.4-2. An initial crack is postulated as a circumferential, inner semi-elliptical crack. The depth of the initial crack is expressed by the exponential distribution

\[
f(a) = \frac{\exp\left(-\frac{a}{\mu}\right)}{\mu} \quad [a: \text{mm}] \tag{3.4-6}
\]

where \(a\) is the crack depth in mm. The initial aspect ratio is expressed by the lognormal distribution [8]

\[
f(\beta_R) = \frac{1.419}{\sigma\beta_R \sqrt{2\pi}} \exp\left\{ -\frac{1}{2} \left[ \ln\left(\frac{\beta_R}{\mu_{\beta_R}}\right) \right]^2 \right\} \tag{3.4-7}
\]

\[\beta_R = \frac{c}{a} > 1\]

\[\mu_{\beta_R} = 1.336\]

\[\sigma = 0.5639\]

where \(\beta_R\) is the aspect ratio and \(c\) is the length of the initial crack. The applied loads contributing to crack growth are summarized in Table 3.4-3. These loads are estimated by referring to the design loads of the Japanese BWR plants [9]. They are considered in order to evaluate crack growth, but not to assess crack stability. Thus, a large seismic load, such as an S2 earthquake, is not considered in the loads to evaluate crack growth. The fracture mechanics parameter used in the crack growth analysis is the stress intensity factor range \((\Delta K)\). The formula proposed by Raju and Newman [10] is employed in this calculation. Crack growth rate is calculated based on the following Paris' law [11]:
\[
\frac{da}{dN} [\text{mm/cycle}] = 1.738 \times 10^{-10} \Delta K^{5.95} \quad (\Delta K < 13.2 \text{ MPa} \sqrt{\text{m}})
\]
\[
\frac{da}{dN} [\text{mm/cycle}] = 5.325 \times 10^{-6} \Delta K^{1.95} \quad (\Delta K \geq 13.2 \text{ MPa} \sqrt{\text{m}})
\]

When the crack penetrates the pipe wall, crack is judged as a penetrated crack. For the penetrated crack, crack growth analysis is not performed. If stable, the failure mode of the crack is judged as ‘LEAK’, while if unstable, its failure mode is judged as ‘BREAK’.

Loads used for the crack stability assessment, except the seismic load, to which the piping is always subjected, are shown in Table 3.4-4. Here \( P \) and \( Q \) are primary and secondary stresses, respectively, and subscripts \( m \) and \( bg \) denote membrane and global bending \((= M / Z)\), respectively. Membrane stress is caused by internal pressure, while bending stress is caused by thermal expansion. The stresses used for the crack stability assessment are determined by referring to the design allowable stress. The membrane stress \((\sigma_m)\) due to the internal pressure and the global bending stress \((\sigma_{bg} = M / Z)\) caused by thermal expansion, which is applied perpendicularly to the postulated circumferential crack, are assumed as follows:

\[
\sigma_m = 0.5S_m \quad \text{(3.4-9)}
\]
\[
\sigma_{bg} = \frac{3S_m}{C_2} \quad \text{(3.4-10)}
\]

where \( S_m \) is the allowable design stress intensity value, and \( C_2 \) is the stress index[12].

The seismic load is only applied to perform the crack stability assessment. The primary stress caused by the S2 earthquake (allowable stress condition; IV\( \Lambda \)) is limited to \( 3S_m \) in the case of grade-1 piping as follows [13]:

\[
\frac{B_1P_{in}D_o}{2t} + \frac{B_2M_{ip}}{z_i} \leq 3S_m \quad \text{(3.4-11)}
\]

where \( B_1 \) and \( B_2 \) are the stress indices [12], \( P_{in} \) and \( M_{ip} \) are the internal pressure and the bending moment due to the mechanical load, and \( D_o \), \( t \) and \( z_i \) are the outer diameter of the pipe, pipe thickness, and the secondary section moment, respectively. The first term of the left hand side of Eq. (3.4-9) denotes the stress caused by internal pressure, which is assumed to be \( 0.5S_m \) as mentioned above. Therefore, the stress caused by all the mechanical loads including the seismic stress is limited to \( 2.5S_m \) \((= 3S_m - 0.5S_m)\). Since the moment caused by dead weight usually has only a small influence, the global bending stress of the pipe caused by an earthquake is given by the following equation with the stress index, \( B_2 \):

\[
\sigma_b = \frac{2.5S_m}{B_2} \quad \text{(3.4-12)}
\]
Table 3.4-5 shows the loading conditions for the crack stability assessment when dealing with the seismic load as a deterministic value. In the table, $P_{bg}$ is the seismic stress.

When considering variation, the seismic stress is evaluated using a seismic hazard curve and Eq. (3.4-2). Corresponding to the seismic hazard curve, it is necessary to assume the ground acceleration of an $S_2$ earthquake as a standard point. In this study, the ground acceleration of an $S_2$ earthquake is postulated as 4 m/s$^2$. In Eq. (3.4-3), $\alpha_{S_2}$ becomes 4 m/s$^2$ and $SDS_2$, is the primary bending stress ($P_{bg}$) given in Table 3.4-5. The relation between the ground acceleration and the frequency of the earthquake is expressed with the seismic hazard curve as shown in Fig. 3.4-6. In the case of the probabilistic seismic load, the thermal expansion stress ($Q_{bg}$) is not taken into account because the effect of the secondary stress on failure behavior is negligible compared with the large primary stress due to the earthquake.

The net-section criterion is used for the crack stability assessment. From the past unstable fracture tests of the pipes, the net-section criterion does not always give pessimistic results for the carbon steel pipes, because of tearing instability. The G-factor, which was a modification factor allowing for the effect of tearing instability, was proposed by Asada et al. [9] to give pessimistic results. The critical moment, $M_{cr}$, at the time of using the G-factor is given by

$$M_{cr} = GM_0$$

$$2 \leq D_B < 6$$

$$G = 1$$

$$6 \leq D_B$$

$$G = \max \{1, 0.692 - 0.0115D_B + (0.188 + 0.0104D_B)\log(\Theta)\}$$

where $M_0$ is the critical moment calculated using the net-section criterion [14], $D_B$ is the nominal size of pipe in inch, and $\Theta$ is the crack angle in degree.

Pre-service inspection (PSI) is considered in the analyses. The non-detection probability, $P_{ND}$, is given by [8]

$$P_{ND} = \frac{1}{2}(1 - e)\left[1 - \frac{2}{\sqrt{\pi}} \int_0^{\ln(A/A^*)} e^{-t^2} \, dt \right] + e$$

(3.4-14)

where

$$A = \begin{cases} 
\frac{\pi}{2} ac & (2c \leq D_B) \\
\frac{\pi}{4} aD_B & (2c > D_B) 
\end{cases}$$

$$A^* = \frac{\pi}{4} a^* D_B$$
\[ \varepsilon = 0.005 \]
\[ D_B = 25.4\text{mm} \]
\[ V = 1.33 \]
\[ a^* = 6.35\text{mm} \]

### 3.4.3.2 Break probability when hazard curve is not considered

Figure 3.5-7 shows the break probability of the case without considering the seismic hazard curve (Cases 1 to 6). Here, in the case of considering the earthquake (Cases 4 to 6), only one earthquake represented by Eq. (3.5-2) was considered during the plant life (80 years). In the 26B pipes, the break probability due to loads other than the earthquake is very small, but it is greatly increased by considering the seismic loads. On the other hand, in 4B pipes, the break probability hardly changes even when seismic loads are considered. 16B pipes show an intermediate tendency. Figure 3.5-8 shows the break probability of pipes due to only the seismic loads. Unlike the relative trend shown in Fig. 3.5-7, the break probability due to an earthquake increases as the pipe diameter decreases. However, since the break probability of 4B pipe due to seismic loads is two orders smaller than the that due to loads excepting for the earthquake, its influence is relatively small.

Figures 3.5-9 and 10 show the fracture map of 26B and 4B pipes, respectively. The horizontal two axes indicate relative depth \((a/t)\) and aspect ratio \((a/c)\) of the initial cracks, and the vertical axis shows the failure rate of the samples in the calculation cells. Here, the weight of each cell (probability of each cell) is not taken into account. The break probability of pipes due to the seismic load shown in the figure is the value when an earthquake occurs in 80 years. Many break samples are concentrated in the cells of small aspect ratio \((a/c)\) (ie long initial crack cells). Comparing the failure map of 26B and 4B pipes, it can be seen that the region of the cells leading to break or leak of 4B pipes is wider than the 26B pipes. The reason why the rupture area of the 4B pipes is expanded is that the ratio of radius to thickness \((R/t)\) is larger than that of the 26B pipes, and the ratio of crack length to circumference of the pipe is large. The reason that the leak area expands is because the thickness of the 4B pipes is smaller than that of the 26B pipes, and the influence of crack propagation appears strongly. Because the probability of samples decreases due to PSI, the break and leak rate decreases in the cells with deep initial cracks. The probability of detection used in this evaluation depends on the absolute value of the size of the cracks as shown in Eq. (3.4-14). For this reason, this tendency appears greatly in the 26B pipes where the size of the cracks becomes relatively large. Deep initial cracks penetrate early in plant operation, so the proportion of samples that break due to an earthquake in the deep initial crack cells is small. In the case of 4B pipes, the samples broken by the earthquake are concentrated around the boundary of the leak and the leak cells and the distribution is continuous. This tendency shows that propagated cracks with reduced fracture resistance are fractured by seismic load and the break probability depends on both the crack size and the seismic load. On the other hand, in the case of the 26B pipes,
the break samples are concentrated in shallower cells compared with the 4B pipes. In addition, the break cells are discontinuous as compared with the case of 4B pipes. The tendency of the break cells to be discontinuous suggests that the fracture behavior does not depend largely on the crack size and the fracture behavior depends more on the seismic stress.

3.4.3.3 Effect of the magnitude of the seismic hazard curve In this chapter, the effect of seismic hazard curve are examined. Seismic hazard curves used in seismic PRAs depend on location conditions of the nuclear power plants and are unique to each plant. In this examination, the four seismic hazard curves shown in Fig. 3.4-6 were assumed with reference to the past study [14], and the effect of these curves on the failure probability was evaluated. The analysis results of Case 7 to Case 10 in Table 3.4-6 are summarized in Fig. 3.4-11. Under the plant site conditions where small seismic hazard curves like Hazard-C and Hazard-D are assumed, the break probability is hardly affected by the earthquake. In these cases crack size and loads excepting for the seismic load will dominate the fracture behavior. On the other hand, seismic load has a large effect on the fracture probability under the location condition of a plant where a large seismic hazard curve is assumed like Hazard-A. Like this, similar to the seismic PRA, PFM analysis shows that the setting of the seismic hazard curve affects the reliability of the plants.

3.4.3.4 Effective range of seismic hazard curve Since seismic PRA evaluates failure of crack-free equipments, the wide range of seismic hazard curves are applied to the assessments. However, in PFM analysis, the potential failure probability due to loads other than earthquakes is relatively large in order to take cracks into account. For this reason, it is expected that seismic loads with low occurrence frequency even if the load is large do not affect the failure probability as a result. Therefore, parametric analysis of Case 11 to Case 14 shown in Table 3.4-6 was carried out and the effective area of the seismic hazard curve was examined. The break probability of these cases is shown in Fig. 3.4-12. From these results, it can be seen that the break probability is hardly affected by considering earthquakes with occurrence frequency less than $10^{-5}$/year. Therefore, in the PFM analysis that takes cracks into consideration, the region where the occurrence frequency of the seismic hazard curve is high (region with small ground acceleration) is important for evaluation.

3.4.3.5 Effect of crack depth considered penetration In the LBB evaluation, when the crack depth reached 80% of the thickness, it is judged that the crack penetrated. For this reason, PFM analysis is often evaluated using this criterion. However, the break probability due to the seismic loads tends to increase with the passage of time as shown in Fig. 3.4-8. This implies that fracture strength decreases as the crack grows, and it is suggested that the fracture probability increases as the crack depth considered as penetration increases. In order to confirm the influence of penetrating crack depth on break probability, Cases 15 and Case 16. Theses results are shown in Fig. 3.4-13. The break probability is about four times
larger than the case where penetrating crack depth is 80% of the thickness. From this, in order to evaluate the conservative break probability, the penetration crack depth should be set to $a/t = 1.0$.

3.4.3.6 Effect of crack growth rate

The crack growth loads used in this study were set to obtain the equivalent crack growth behavior to that evaluated based on design thermal transient conditions of the nuclear power plants [7]. However, because the thermal transient conditions of actual nuclear power plants are different from the design conditions. As shown in Fig. 3.4-8, the break probability due to the seismic loads tends to increase with the passage of time. Therefore, assuming a case where the crack growth rate becomes 10 times, the effect of the earthquake on the failure probability was evaluated in Cases 17 and Case 18. The analysis results are shown in Fig. 3.4-14 and Fig. 3.4-15. The leak probability increases as the crack growth rate increases, but the break probability (including the influence of the earthquake) is hardly influenced by the crack growth rate. Leak probability increase as the crack growth rate increases, because it is simply judged whether the crack depth exceeds the limit value or not. On the other hand, the break probability greatly depends on the aspect ratio as well as the crack depth, and it is hardly affected by the crack growth rate.

3.4.3.7 Effect of stress ratio

Generally, the crack grows longer as the percentage of bending stress increases. This is because the stress intensity factor at surface point of the crack becomes larger compared with the deepest point. Stresses generated by thermal fluctuations, which are reported as the one of the causes of piping failure accident, are mainly bending stress, and it is important for safety assessment of nuclear power plants to grasp the failure probability when such a load occurs. Therefore, analyses of Case 19 and Case 20 were carried out, assuming that a bending stress of ±50 (MPa) as a crack growth load was repeated at a rate of once per hour. The results are shown in Fig. 3.4-16 and Fig. 3.4-17. In the case of 4B pipes, the crack becomes longer and the area reduction of the ligament becomes conspicuous, so the break probability becomes extremely larger than that when the design load conditions are applied. However, even in this case, the effect of the seismic load is negligibly small as in the case of using the aforementioned design loading conditions. In 26B pipes, leak probability increases greatly unlike 4B pipes, and the break probability when considering the seismic load is about two orders larger than when the seismic load is not considered. From these results, it was found that the break probability of the large diameter pipes is larger by the seismic load also under the bending stress condition where the crack grows long.

3.4.4 Conclusions

A PEPPER code capable of taking into consideration the variation and frequency of seismic loads was developed and the effect of the earthquake on the failure probability of cracked pipes were evaluated using this code. In order to consider the large seismic load, the
PEPPER code introduced the importance of the sampling method for the sampling of the seismic load to improve the accuracy and efficiency of calculation.

Since the frequency of large earthquakes that contribute to pipe fracture is small, the effect of the earthquake greatly appears in PFM analysis in case of large diameter pipes with small break probability due to the design loading conditions. In the case of small diameter pipes, it is clear that the break probability by the design loading conditions is high, but the increase in break probability due to the seismic loads is negligibly small. When using seismic hazard curves in PFM analysis, the accuracy of the region where the occurrence probability is high and the acceleration is small is important in the seismic hazard curve. This is different from seismic PRA using a wide range of seismic hazard curves. The larger the crack is, the easier the break probability is affected by the seismic loads. For this reason, it is conservative to assume that the crack depth considered as penetration is enlarged and that the ratio of bending stress is large, so that the crack grows long as the analysis condition. On the other hand, the crack growth rate has little influence on the break probability due to the seismic loads.

ACKNOWLEDGEMENT

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REFERENCES


### Table 3.4-1 Analyzed pipe dimensions

<table>
<thead>
<tr>
<th>Pipe</th>
<th>4B</th>
<th>16B</th>
<th>26B</th>
</tr>
</thead>
<tbody>
<tr>
<td>Diameter [mm]</td>
<td>114.3</td>
<td>406.4</td>
<td>660.4</td>
</tr>
<tr>
<td>Thickness [mm]</td>
<td>11.1</td>
<td>26.2</td>
<td>33.3</td>
</tr>
<tr>
<td>Radius/Thickness [-]</td>
<td>5.1</td>
<td>7.8</td>
<td>9.9</td>
</tr>
</tbody>
</table>

### Table 3.4-2 Material constants of a pipe

<table>
<thead>
<tr>
<th>Items</th>
<th>Unit</th>
<th>Values</th>
</tr>
</thead>
<tbody>
<tr>
<td>Design stress intensity</td>
<td>[MPa]</td>
<td>122.6</td>
</tr>
<tr>
<td>Design yield strength</td>
<td>[MPa]</td>
<td>183.4</td>
</tr>
<tr>
<td>Design ultimate strength</td>
<td>[MPa]</td>
<td>404</td>
</tr>
<tr>
<td>Flow stress</td>
<td>[MPa]</td>
<td>293.7</td>
</tr>
<tr>
<td>Young's modulus</td>
<td>[GPa]</td>
<td>178.5</td>
</tr>
<tr>
<td>Poisson's ratio</td>
<td>-</td>
<td>0.3</td>
</tr>
</tbody>
</table>

### Table 3.4-3 Loading conditions for crack growth analysis

<table>
<thead>
<tr>
<th>Load No.</th>
<th>Frequency [cycles/year]</th>
<th>( \sigma_m ) [MPa]</th>
<th>( \sigma_b ) [MPa]</th>
</tr>
</thead>
<tbody>
<tr>
<td>1</td>
<td>7</td>
<td>0</td>
<td>0</td>
</tr>
<tr>
<td>2</td>
<td>18</td>
<td>49</td>
<td>0</td>
</tr>
<tr>
<td>3</td>
<td>320</td>
<td>92</td>
<td>0</td>
</tr>
<tr>
<td>4</td>
<td>8</td>
<td>0</td>
<td>-122.6</td>
</tr>
<tr>
<td>5</td>
<td>16</td>
<td>0</td>
<td>-61.3</td>
</tr>
<tr>
<td>6</td>
<td>330</td>
<td>0</td>
<td>-12.3</td>
</tr>
</tbody>
</table>

\( \sigma_m \) : Membrane Stress
\( \sigma_b \) : Bending Stress
Table 3.4-4  Loads applied for crack stability assessment except seismic loads

<table>
<thead>
<tr>
<th>Stress</th>
<th>4B</th>
<th>16B</th>
<th>26B</th>
</tr>
</thead>
<tbody>
<tr>
<td>Membrane Stress</td>
<td>61.3</td>
<td>61.3</td>
<td>61.3</td>
</tr>
<tr>
<td>Bending Stress (M/Z)</td>
<td>150.8</td>
<td>109.4</td>
<td>91.1</td>
</tr>
</tbody>
</table>

Table 3.4-5  Seismic loads and frequency evaluated by the deterministic technique

<table>
<thead>
<tr>
<th>Pipe Size</th>
<th>4B</th>
<th>16B</th>
<th>26B</th>
</tr>
</thead>
<tbody>
<tr>
<td>Deterministic seismic stress [MPa]</td>
<td>125.7</td>
<td>91.2</td>
<td>75.9</td>
</tr>
<tr>
<td>Median of seismic stress [MPa]</td>
<td>32.1</td>
<td>23.3</td>
<td>19.4</td>
</tr>
</tbody>
</table>
Table 3.4-6 Analytical parameters

<table>
<thead>
<tr>
<th>case</th>
<th>pipe size</th>
<th>frequency of earthquake</th>
<th>scope of frequency of earthquake</th>
<th>a/t judged as penetration</th>
<th>crack growth rate</th>
<th>crack growth load</th>
</tr>
</thead>
<tbody>
<tr>
<td>1</td>
<td>4B</td>
<td>None</td>
<td>-</td>
<td>0.8</td>
<td>Eq.(3.4-8)</td>
<td>Table 3.4-3</td>
</tr>
<tr>
<td>2</td>
<td>16B</td>
<td>None</td>
<td>-</td>
<td>0.8</td>
<td>Eq.(3.4-8)</td>
<td>Table 3.4-3</td>
</tr>
<tr>
<td>3</td>
<td>26B</td>
<td>None</td>
<td>-</td>
<td>0.8</td>
<td>Eq.(3.4-8)</td>
<td>Table 3.4-3</td>
</tr>
<tr>
<td>4</td>
<td>4B</td>
<td>400gal, 1/80 year</td>
<td>-</td>
<td>0.8</td>
<td>Eq.(3.4-8)</td>
<td>Table 3.4-3</td>
</tr>
<tr>
<td>5</td>
<td>16B</td>
<td>400gal, 1/80 year</td>
<td>-</td>
<td>0.8</td>
<td>Eq.(3.4-8)</td>
<td>Table 3.4-3</td>
</tr>
<tr>
<td>6</td>
<td>26B</td>
<td>400gal, 1/80 year</td>
<td>-</td>
<td>0.8</td>
<td>Eq.(3.4-8)</td>
<td>Table 3.4-3</td>
</tr>
<tr>
<td>7</td>
<td>26B</td>
<td>Hazard-A</td>
<td>10^{-10}/year</td>
<td>0.8</td>
<td>Eq.(3.4-8)</td>
<td>Table 3.4-3</td>
</tr>
<tr>
<td>8</td>
<td>26B</td>
<td>Hazard-B</td>
<td>10^{-10}/year</td>
<td>0.8</td>
<td>Eq.(3.4-8)</td>
<td>Table 3.4-3</td>
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<tr>
<td>9</td>
<td>26B</td>
<td>Hazard-C</td>
<td>10^{-10}/year</td>
<td>0.8</td>
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<tr>
<td>10</td>
<td>26B</td>
<td>Hazard-D</td>
<td>10^{-10}/year</td>
<td>0.8</td>
<td>Eq.(3.4-8)</td>
<td>Table 3.4-3</td>
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<td>11</td>
<td>26B</td>
<td>Hazard-B</td>
<td>10^{-1}/year</td>
<td>0.8</td>
<td>Eq.(3.4-8)</td>
<td>Table 3.4-3</td>
</tr>
<tr>
<td>12</td>
<td>26B</td>
<td>Hazard-B</td>
<td>10^{-3}/year</td>
<td>0.8</td>
<td>Eq.(3.4-8)</td>
<td>Table 3.4-3</td>
</tr>
<tr>
<td>13</td>
<td>26B</td>
<td>Hazard-B</td>
<td>10^{-5}/year</td>
<td>0.8</td>
<td>Eq.(3.4-8)</td>
<td>Table 3.4-3</td>
</tr>
<tr>
<td>14</td>
<td>26B</td>
<td>Hazard-B</td>
<td>10^{-7}/year</td>
<td>0.8</td>
<td>Eq.(3.4-8)</td>
<td>Table 3.4-3</td>
</tr>
<tr>
<td>15</td>
<td>4B</td>
<td>Hazard-B</td>
<td>10^{-10}/year</td>
<td>1.0</td>
<td>Eq.(3.4-8)</td>
<td>Table 3.4-3</td>
</tr>
<tr>
<td>16</td>
<td>26B</td>
<td>Hazard-B</td>
<td>10^{-10}/year</td>
<td>1.0</td>
<td>Eq.(3.4-8)</td>
<td>Table 3.4-3</td>
</tr>
<tr>
<td>17</td>
<td>4B</td>
<td>Hazard-B</td>
<td>10^{-10}/year</td>
<td>0.8</td>
<td>10 times of Eq.(3.4-8)</td>
<td>Table 3.4-3</td>
</tr>
<tr>
<td>18</td>
<td>26B</td>
<td>Hazard-B</td>
<td>10^{-10}/year</td>
<td>0.8</td>
<td>10 times of Eq.(3.4-8)</td>
<td>Table 3.4-3</td>
</tr>
<tr>
<td>19</td>
<td>4B</td>
<td>Hazard-B</td>
<td>10^{-10}/year</td>
<td>0.8</td>
<td>Eq.(3.4-8)</td>
<td>σ_b=±50MPa/hour</td>
</tr>
<tr>
<td>20</td>
<td>26B</td>
<td>Hazard-B</td>
<td>10^{-10}/year</td>
<td>0.8</td>
<td>Eq.(3.4-8)</td>
<td>σ_b=±50MPa/hour</td>
</tr>
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</table>
Fig. 3.4-1  Procedure to deal with seismic loads in conventional PFM analysis

1. The size of the ground motion in the base of a plant building is assumed.
2. Analyze an oscillating response of the plant building by the ground motion, and calculate the response acceleration in the floor level in which piping is installed.
3. Using the floor response, analyze the oscillating response of piping and calculate the seismic load at the evaluated portion. Then, the number of cycles of seismic load by one earthquake is evaluated.
4. Supposing an initial crack, analyze the crack growth by the load cycle of plant transients, and compute the amount of crack growth until an earthquake occurs.
5. Analyze the crack growth using the crack size calculated in Step 4 due to the load cycles of one earthquake.
6. Assess the crack stability against the seismic load after crack growth, and judge whether pipe is stable or not.

Fig. 3.4-2  Schematic representation of crack growth trajectories including influence of earthquake
Dispersion in Seismic Stress

Ground Acc.: Seismic Hazard Curve
- Amplitude
- Frequency

Excess Probability

Dispersion in Seismic Load

Fig. 3.4-3 Concept of variation of ground acceleration and seismic stress of pipes
Fig. 3.4-4 An example of a seismic hazard curve.

Fig. 3.4-5 Effect of importance sampling of seismic stress
Fig. 3.4-6 Seismic hazard curve used in PFM analyses

Fig. 3.4-7 Effect of seismic load on cumulative break probabilities
Fig. 3.4-8 Cumulative break probability due to $S_2$ earthquake in each 80 years
Cumulative Leak Probability

Cumulative Break Probability

Break Probability Due to Seismic Load

Fig. 3.4-9 Fracture map after 80 years operation (26-inches pipe)
Fig. 3.4-10 Fracture map after 80 years operation (4-inches pipe)
Fig. 3.4-11  Effect of seismic hazard curves on the break probability

Fig. 3.4-12  Effective range of seismic hazard curves on the break probability
Note) $a/t_p$ is relative crack depth judged as penetration

Fig. 3.4-13 Effects of penetrating crack depth on the break probability

Fig. 3.4-14 Effects of crack growth rate on the break probability (4-inches pipe)
Fig. 3.4-15 Effects of crack growth rate on the break probability (26-inches pipe)

Fig. 3.4-16 Effects of stress ratio on the break probability (4-inches pipe)
Fig. 3.4-17  Effects of stress ratio on the break probability (26-inches pipe)
3.5 Seismic Performance Assessment of LWR Piping (2)

3.5.1 Introduction

About 45 percent of the nuclear power plants in Japan have been operating for more than 30 years and cracks resulting from long-term operation have been detected in piping systems during in-service inspections. Furthermore, in recent years, some nuclear power plants have experienced several severe earthquakes beyond the previous design basis ground motions. These earthquakes include the Niigata-ken Chuetsu-Oki Earthquake in 2007 and the Tohoku District - off the Pacific Ocean Earthquake in 2011. Therefore, fragility evaluation for piping systems taking both age-related degradations and seismic loads into consideration has become increasingly important for the structural integrity evaluation and the seismic probabilistic risk assessment (PRA).

In seismic PRA, the prediction of core damage frequency is carried out based on the fragility curve and seismic hazard curve [1]. Therefore, fragility curves for piping systems considering the effects of age-related degradations are useful in the structural integrity evaluation and risk assessment of nuclear power plants which experience long-term operation.

Probabilistic fracture mechanics (PFM) is recognized as a rational methodology for failure probability analysis and fragility evaluation of aged piping, because it can take the scatters and uncertainties of influence parameters into account. In JAEA, a PFM analysis code PASCAL-SP (PFM Analysis of Structural Components in Aging Light water reactor - SCC at Welded Joints of Piping) was developed to calculate the failure probability for aged piping considering age-related degradations, including stress corrosion cracking (SCC) and fatigue [2-4]. Based on the PASCAL-SP, some example analysis results of failure probabilities, fragility curves and a preliminary investigation on seismic safety margin are described in this section.

3.5.2 Fragility evaluation functions in PASCAL-SP

3.5.2.1 Probabilistic model for seismic stress

In the fragility evaluation of current PRA, the seismic stress is represented as a probabilistic variable following the log-normal distribution [1]:

\[
f_R(\sigma_s) = \frac{1}{\sqrt{2\pi}\beta_u \sigma_s} \exp \left[ -\frac{1}{2} \left( \frac{\ln(\sigma_s/\mu)}{\beta_u} \right)^2 \right]
\]  

(3.5-1)

where, \(\sigma_s\) is the seismic stress, \(\mu\) is the median value and \(\beta_u\) is the standard deviation of \(\ln(\sigma_s)\).

In the PASCAL-SP, a function that treats the seismic stress as a probabilistic variable based on Eq. (3.5-1) was introduced to be consistent with seismic PRA. The seismic stress is used to both crack growth calculation and failure evaluation. It has to be noted that the failure probability considering seismic stress is a probability postulating that the earthquake occurs at every evaluating time. When a postulating earthquake occurs, the
crack growth is calculated and failure is judged considering seismic stress. After this evaluation for the postulating earthquake is completed, the crack size is returned to the values before the earthquake.

3.5.2.2 Uncertainty analysis In order to treat both the epistemic and the aleatory uncertainties for probabilistic variables, a two-loop approach utilized in previous paper [5] was adopted in PASCAL-SP, as shown in Fig. 3.5-1. The epistemic uncertainties are sampled at the outer loop on the basis of the Latin Hypercube Sampling (LHS) scheme, while aleatory uncertainties are sampled at the inner loop. Using the probabilistic variables sampled at the outer loop by considering the epistemic uncertainties, many time history analysis results of failure probability are calculated using Monte Carlo method at the inner loop. Through processing these analysis results, mean and percentile values for failure probability can be obtained.

Fig. 3.5-1 Analysis flow of uncertainty analysis in PASCAL-SP

3.5.3 Failure probability analysis and fragility evaluation

To calculate the failure probability and obtain the fragility curve considering both age-related degradations and seismic loads, some example probabilistic analyses were performed. Only uncertainty of the seismic stress was considered to be the epistemic
uncertainty, as a preliminary investigation on percentile of failure probability and seismic safety margin.

As a typical example, the primary loop recirculation (PLR) piping system in Japanese boiling water reactor (BWR) was chosen as the target. The crack initiation by SCC was evaluated on the basis of a probabilistic crack initiation model proposed by previous paper [6]. Initiated crack was modeled as a circumferential semi-elliptical crack at the inner surface of the pipe in accordance with the JSME FFS Code [7]. The crack growth rates due to SCC and fatigue were calculated on the basis of probabilistic models of crack growth rate developed from data provided in the JSME FFS Code [4, 7]. Stresses due to earthquake, normal operating loads, and operational transients were considered, as well as a weld residual stress. For loading conditions, the seismic stress was treated as the probabilistic variable in order to evaluate effects of its uncertainty on the failure probability. Details of seismic stress will be described in separated paper [8]. To obtain fragility curve through the probabilistic analyses, different seismic stresses corresponding to different seismic ground motions were considered. The response seismic stress of pipe was represented as equivalent stress cycles in accordance with the Rules on Protection Design against Postulated Pipe Rupture for Nuclear Power Plants [9].

3.5.3.1 Fragility curves The improved PASCAL-SP was used to evaluate seismic fragility from the beginning of plant operation. The mean failure probabilities of aged pipes for different seismic ground motions are shown in Fig. 3.5-2. The failure probability increases as increasing plant operation year, due to the progress of aged-related degradations. The failure probability also increases with the increase of the seismic ground motions. When the seismic ground motion is relatively small, age-related degradations are a dominant influence factor on the failure probability. When the seismic ground motion is relatively large, seismic stress becomes the dominant influence factor on the failure probability.

The fragility curves were obtained from the failure probabilities with several seismic ground motions as shown in Fig. 3.5-2. The fragility curves for different plant operation years are shown in Fig. 3.5-3. The fragility curve increases with the plant operation year. This is because crack initiation and propagation caused by SCC and fatigue increase with plant operation.
3.5.3.2 Preliminary investigation on seismic safety margin  In the PASCAL-SP, the two-loop approach which can treat both the epistemic and aleatory uncertainties was adopted. The percentile of fragility curve can be obtained for aged piping by using the two-loop approach to conduct uncertainty analysis. In seismic PRA for unaged components, high confidence low probability of failure (HCLPF) corresponding to seismic ground motion at 5% failure probability of 95th percentile is an important index. HCLPF is used to preliminarily investigate the seismic safety margin for aged piping. The 95th percentiles for different plant operation years are shown in Fig. 3.5-4. Based on 5% failure probability
shown in the figure, the HCLPF values can be obtained. The variation of HCLPF values with plant operation years is shown in Fig. 3.5-5. Until 8th year, the HCLPF value is almost constant with a value 2.7. This is because just after the beginning of plant operation, cracks due to SCC almost do not initiate. The cracks gradually initiates around 8th year from the beginning of plant operation, so the HCLPF begins to decrease at about 8 years due to the initiation and propagation of cracks.

From these results, it is confirmed that the PASCAL-SP is useful to quantitatively evaluate the fragility curve which is important in seismic PRA and to investigate the change of seismic safety margin due to aging mechanisms.

Fig. 3.5-4 95th percentiles of fragility curves of different plant operation years [8].

Fig. 3.5-5 HCLPF value considering age-related degradation [8].
3.5.4 Concluding remarks

The PFM analysis code PASCAL-SP has been developed in JAEA to evaluate fragility curve by treating seismic stress as probabilistic variable and taking both epistemic and aleatory uncertainties into account. As an example analysis, the failure probabilities and fragility curves of an aged PLR pipe in BWR plant were calculated considering SCC and fatigue which are the typical age-related degradations in BWR environment. Moreover, as a preliminary investigation, the influence of age-related degradations on the seismic safety margin of piping was also examined. It was clarified that PASCAL-SP is useful in the seismic PRA since it can evaluate the fragility curve and the seismic safety margin.

ACKNOWLEDGMENTS

This study was performed under a contract between Secretariat of Nuclear Regulation Authority and JAEA.

REFERENCES


3.6 Failure Probability Assessment of SG Tube

3.6.1 Introduction

Some degradations, especially stress corrosion cracking, in steam generator (SG) tubes made of Inconel 600 mill anneal (MA) material have been affecting the availability of pressurized water reactors (PWRs). In fact, some old steam generators have been replaced because of severe damage of SG tubes and high maintenance cost accompanied. It is therefore preferable to establish a reliable evaluation method to forecast the integrity of SG tubes in order to keep the safe operation of nuclear power plants as well as to avoid unnecessary maintenance.

In Japan, as maintenance activities of steam generators, utilities inspect all of SG tubes using eddy current testing (ECT) at every outage and repair any SG tubes which cause abnormal signals without confirming existence of defects. Furthermore, most of the steam generators with Inconel 600 MA SG tubes have been replaced by new steam generators with Inconel 690 thermal treatment (TT) SG tubes of higher corrosion resistance. Although some researches have been performed to optimize SG tube maintenance based on probabilistic methodologies focusing on the risks [1-5], there seems to be no research focusing on both risks and costs, and even profitability for SG tube maintenance.

This paper describes sensitivity analysis of several significant parameters that affect Inconel 600 MA SG tube leakage and rupture using probabilistic fracture mechanics (PFM). Based on the results obtained by the PFM analysis, an attempt was made to perform risk-benefit analyses as an application of PFM analysis in order to construct risk-based and cost-based maintenance strategies for SG tubes. In addition, the risk-benefit analysis was also applied to the Inconel 690 TT SG tube maintenance as well as to evaluate the effect of introducing maintenance criteria for detected cracks in the SG tubes during outage.

3.6.2 Methods and input data

3.6.2.1 Risk analysis

**Input data** The probabilities of SG tube leakage and rupture are defined as risks in the present study. A model was made modifying pc-PRAISE (Piping Reliability Analysis Including Seismic Events) to evaluate primary-secondary leakage and SG tube rupture during 60-year operations due to stress corrosion cracking (SCC) located in roll expansion zones of the tubes at the top of the tubesheet. Here parameters such as inspection accuracy, inspection interval, sampling inspection and crack propagation law were taken into account as significant parameters to influence the probabilities of the leakage and rupture.

The model assumes generation of initial semi-elliptical circumferential surface cracks with a fixed crack depth and log-normally distributed crack lengths at the inner surface of SG tubes defined in Table 3.6-1. Crack initiation occurs at log-normally distributed crack incubation period \( t \) defined as follows.
\[ f(t) = \frac{1}{\sqrt{2\pi \times 0.6t}} \exp\left\{ -\frac{(\log t - 2.5)^2}{0.72} \right\} \]  
(3.6-1)

Then it assumes propagation velocity of the cracks, \( \dot{a} \), in accordance with selected crack propagation laws given in Eq.(3.6-2),

\[ \log \dot{a} = -12 + CK \]  
(3.6-2)

\( C = 0.033 \) (case 1)  
\( C = 0.074 \) (case 2)  
\( C = 0.100 \) (case 3)

where \( K \) is the stress intensity factor.

The rupture was assumed to take place in accordance with net-section failure criteria given as follows,

\[ \sigma_L A_p \geq \sigma_{\text{flow}} (A_p - A_{\text{crack}}) \]  
(3.6-3)

In this expression \( A_p \) is the cross-sectioned area of the tube, \( A_{\text{crack}} \) is the area of the crack, \( \sigma_L \) is the load-controlled component of the axial stress, and \( \sigma_{\text{flow}} \) is the flow stress of the material.

Input data applied in the analysis are summarized in Table 3.6-1.

Because original pc-PRAISE code enables to calculate the probabilities of leakage and rupture for a single pipe, the following calculation process was used in order to apply it to whole SG tubes in a 4-loop unit.

Supposing that the probability of leakage, \( p \), can be applied to all of the SG tubes in spite of the location of each SG tube, the probability of leakage of \( i \) tubes out of \( n \) tubes is expressed as follows:

\[ p_i = p^i (1 - p)^{n-i} \]  
(3.6-4)

Thus, the probability of leaking at least one SG tube in the unit, \( p_f \), is described as follows:

\[ p_f = 1 - \sum_i p^i (1 - p)^{n-i} \]  
(3.6-5)
Because it is assumed that $p_i$ follows Poisson distribution in this study, the $p_i$ can be given as Eq.(3.6-6).

$$p_i = 1 - e^{-np} \quad \text{(3.6-6)}$$

The same calculation process was also conducted to obtain the probability of at least one SG tube rupture in a 4-loop unit.

**Analysed parameters** The probabilities of SG tube leakage and rupture are defined as risks in the present study. A model was made modifying pc-PRAISE (Piping Reliability Analysis).

1. **Crack propagation law** Regarding SCC crack propagation laws of Inconel 600 in primary water under PWR operating conditions, the experimental data were reported by Scott et. al.[6] as shown in Fig.3.6-1. Based on the reported data, sensitivity analysis was performed for three crack propagation laws denoted cases 1, 2 and 3 in Fig. 3.6-1. In this study, the crack propagation laws were assumed to be linear for the simplicity when comparing the results of the analysis.

2. **In-service inspection interval** Considering the situation when long-time cycle operation is adopted, calculations were performed for the inspection intervals of 12, 18 and 24 months.

3. **Inspection accuracy** Three detection probability curves were selected in this study, expressed as,

$$P_D = 1 - \left( 1 - \varepsilon \left( 1 - \frac{2}{\sqrt{\pi}} \int_0^{\log A/A^*} e^{-\frac{\gamma}{2}} \, dt \right) + \varepsilon \right) \quad \text{(3.6-7)}$$

where,

$$A^* = \frac{\pi}{4} a^* \times 9.8 \quad \text{(3.6-8)}$$

where $P_D$ is the probability of detecting a crack of area $A$(mm$^2$). The parameters, $\gamma$, $\varepsilon$, and $a^*$ are defined for each detection curves as follows.

- 40% through wall (TW) defects are detectable with probability of 0.5 (case D40 in Fig. 3.6-2)
  $$\varepsilon = 0.005, a^* = 0.017, \gamma = 3.6$$
- 20% TW defects are detectable with probability of 0.5 (case D20 in Fig. 3.6-2)
  $$\varepsilon = 0.005, a^* = 0.010, \gamma = 4.0$$
- 10% TW defects are detectable with probability of 0.5 (case D10 in Fig. 3.6-2)
  $$\varepsilon = 0.005, a^* = 0.005, \gamma = 4.0$$
The case D40 almost simulates a conventional eddy current testing (ECT) probe using bobbin type coils. The inspection accuracy of the case D20 and case D10 would be expected through development of the SG tube inspection probe. It should be noted here that cracks are repaired whenever they are detected by inspection during outage.

(4) Sampling inspection

Considering the adoption of sampling inspection of SG tubes from the viewpoint of maintenance efficiency, sensitivity analysis was also conducted for sampling inspections. Two types of sampling inspection were evaluated as follows.

- Dividing all SG tubes into two parts and inspecting each part every other year (1/2 sampling inspection)
- Dividing all SG tubes into three parts and inspecting each part every three years (1/3 sampling inspection)

In addition to the analysis of sampling inspection, a 100 percent inspection was also analyzed to compare the sampling inspection cases.

In the analysis of the 1/3 sampling inspection, the probability of at least one tube leakage in a 4-loop unit, $P_{1/3s}$, was calculated as the following process.

The three regions for the 1/3 sampling inspection are named A1, A2 and A3 and each region will be inspected every three years, namely, the region A1 will be inspected in the first year, A2 in the second year, and A3 in the third year. Assuming that the tube leakage probabilities for a single SG tube located in the three regions are $P_{A1}$, $P_{A2}$ and $P_{A3}$, the probability of no SG tube leakage in a 4-loop unit, $P_0$, is expressed by the following equation,

$$P_0 = (1 - P_{A1})^{n/3} (1 - P_{A2})^{n/3} (1 - P_{A3})^{n/3}$$

(3.6-9)

where $n$ is the total number of SG tubes in the unit. Thus, the probability of at least one SG tube leakage in the unit through 1/3 sampling inspection, $P_{1/3s}$, can be obtained by Eq.(3.6-10).

$$P_{1/3s} = 1 - \left\{ (1 - P_{A1})^{n/3} (1 - P_{A2})^{n/3} (1 - P_{A3})^{n/3} \right\}$$

(3.6-10)

It should be noted that the probabilities of the leakage of the tubes are assumed to be independent on the location of the tubes.
3.6.2.2 Risk-Benefit Analysis  As risk-benefit analysis, the expected costs for 60-year operations were calculated first and then the expected profitability was also assessed considering both costs and revenues for 60-year operation.

Cost analysis  The quantification of the risks for the leakage and rupture enables to evaluate maintenance strategies from the viewpoint of cost. In the risk-benefit analysis, the evaluation was performed in terms of two maintenance strategies, that is:

- Investment to improve inspection accuracy
- Sampling inspection

The expected costs of tube leakage and rupture in the \( t \) th year per one plant were calculated as follows,

\[
\text{(Expected cost of leakage)} = C_{\text{leak}} \times p_{\text{leak}}(t) \\
\text{(Expected cost of rupture)} = C_{\text{rupture}} \times p_{\text{rupture}}(t)
\] (3.6-11)

where \( C_{\text{leak}} \) and \( C_{\text{rupture}} \) denote the expected losses from the leakage and rupture, and \( p_{\text{leak}}(t) \), \( p_{\text{rupture}}(t) \) represent the probabilities of leakage and rupture at least one SG tube for the \( t \) th year per one plant, respectively.

In the same way, the cost of repairing SG tubes in the \( t \) th year per one plant was also defined as follows,

\[
\text{(Cost of repairing SG tubes in a 4 – loop unit)} = N_{\text{tubes}} \times C_{\text{repair}} \times p_{\text{repair}}(t)
\] (3.6-13)

where \( N_{\text{tubes}} \), \( C_{\text{repair}} \) and \( p_{\text{repair}}(t) \) denote the number of SG tubes in a 4-loop unit, the cost of repair and the probability of repairing one SG tube in the \( t \) th year, respectively.

The other cost items considered in this analysis are summarized in Table 3.6-2. It should be noted that the values adopted in Table 3.6-2 are possible tentative values for the present study.

Profitability analysis  On decision-making for long-term investment, it is required to consider the time value of money [7]. In such a case, discounted cash flow (DCF) method is often used to evaluate the long-term investment. Here, net present value (NPV) was calculated as an index of the investment. The NPV is one of the most fundamental financial indices for decision-making based on DCF. At the time of \( T \), if \( \text{NPV}(T) > 0 \), it is justified to be worthwhile investing by the time of \( T \), namely, keeping operation of the plant with a specific maintenance strategy in the case of this study.
The NPV($T$) here was defined as:

\[
NPV(T) = \sum_{n=1}^{T} \left[ S(t) - C_{\text{other}}(t) - C_{\text{R&D}}(t) - C_{\text{acc}}(t) - N_{\text{tube}} C_{\text{repair}} P_{\text{repair}}(t) - C_{\text{leak}} P_{\text{leak}}(t) - C_{\text{rupture}} P_{\text{rupture}}(t) \right] \times (1 - r_{\text{tax}})
\]

(3.6-14)

The meanings of the symbols indicated in Eq.(3.6-14) are summarized in Table 3.6-2.

3.6.2.3 Application to Inconel 690 TT SG Tubes

As mentioned in the introductory part, some old steam generators using Inconel 600 MA SG tubes have been replaced by new steam generators using Inconel 690 TT SG tubes of excellent corrosion resistance. Although no SCC has ever been detected in the new SG tubes in the field as well as laboratory tests simulating PWR water chemistry, similar risk-benefit analysis was applied to Inconel 690 TT SG tubes assuming its crack initiation probability and crack propagation law as illustrated in Figs. 3.6-3 and 3.6-4. In Fig. 3.6-3, the accumulated crack initiation probability of Inconel 690 TT SG tube is normalized by that of Inconel 600 MA SG tube at the operation time of 60 years. In Fig. 3.6-4, the slope of crack propagation law of Inconel 690 TT SG tube is normalized by the slope of Inconel 600 MA SG tube.

3.6.2.4 Effect of Introducing Maintenance Criteria

The risk-benefit analysis was also applied to investigate of the effect of introducing maintenance criteria for detected cracks in the SG tubes during outage on the profitability. Examples are shown for cases when using the case D20 inspection.

3.6.3 Results & discussion

3.6.3.1 Risk analysis

Crack propagation law

Fig. 3.6-5 shows the sensitivity of the crack propagation laws to the cumulative leak probability for one SG tube. In this result, the probability of the leakage based on “case 3” crack propagation law is at least two orders of magnitude higher than that based on "case 1". Especially, the difference of the probabilities is significant during 10 years after operation. It is therefore found that the leak probability has a strong correlation with the crack propagation law. Although very limited numbers of reports are available in literature concerning crack propagation law of SCC in SG tubes under primary water chemistry, it is suggested from above result that obtaining an accurate SCC crack propagation law especially at the early stage of crack propagation is quite essential to know the integrity of SG tubes.

In the following evaluations, the "case 3" propagation law is adopted as the base case.
**In-service inspection interval** The effect of in-service inspection interval on the probability of the leakage is illustrated in Fig. 3.6-6. There is a little difference in the probability among the intervals. Accordingly, the inspection intervals between 12 and 24 months seem to have a little sensitivity to the probability of leakage.

**Inspection accuracy** Fig. 3.6-7 indicates the result of the sensitivity analysis for inspection accuracy. The probability of leakage in the case D20 inspection is two orders of magnitude lower than that in the case D40. The case D10 inspection significantly reduces the probability of leakage to approximately five orders of magnitude over 60-year operation period compared to the case D40 inspection.

Accordingly, inspection accuracy has high sensitivity to the probability of leakage, suggesting that improvement of inspection accuracy for SG tubes can greatly contribute to the integrity of SG operation.

**Sampling inspection** Fig. 3.6-8 indicates the probabilities of leakage and rupture of a 4-loop unit per reactor-year with two types of sampling inspections. With respect to the leakage, the probability is roughly unity after 12 years. It should be noted that this result was obtained for the material of Inconel 600 and that in the case of Inconel 690 TT which has excellent corrosion resistance, much lower probabilities would be expected.

### 3.6.3.2 Risk-Benefit Analysis

**Cost analysis** Figs. 3.6-9 and 3.6-10 illustrate the items of annual costs when investment to improve inspection accuracy and sampling inspection are carried out, respectively.

The improvement of the inspection accuracy reduces the cost of leakage after about 10-year operations (Fig. 3.6-9) due to detecting and repairing cracks at the early stage. Therefore, the investment to improve the inspection accuracy seems to be essential for minimizing the maintenance cost.

The two types of sampling inspection increase the cost of rupture compared to 100% inspection as shown in Fig. 3.6-10. Because SG tubes made of Inconel 600 suffer from corrosion damage severely, the annual cost for the sampling inspection cases increases compared to 100% inspection after approximately 10-year operations.

The costs of leakage, rupture and repair are expected costs because the definition of these costs includes probabilities as shown in Eqs. (3.6-11), (3.6-12) and (3.6-13). Therefore in order to evaluate the effect of the investment to improve the inspection accuracy on the expected costs, Fig. 3.6-11 is illustrated. From Fig. 3.6-11, it is suggested that the investment for improving the inspection accuracy would pay quite well in this case.

**Profitability analysis**

(1) Inspection accuracy

Fig. 3.6-12 shows the effect of inspection accuracy on NPV, suggesting that there is almost no difference in NPV among the three inspection accuracy until 10 years. However, with "case D40" and "case D20" inspections, it is no longer profitable to keep the operation after 10 and 30 years, respectively.
(2) Sampling inspection

Fig. 3.6-13 shows the NPV value when two types of sampling inspection are performed. Both NPV values calculated for the two types of sampling inspection are less than that calculated for 100% inspection. This result seems to indicate that it is not worth while to perform sampling inspection for plants using Inconel 600 MA SG tubes which have poor resistance to SCC from a viewpoint of long-term profitability.

3.6.3.3 Application to Inconel 690 TT SG Tubes

In Figs. 3.6-14(a) and (b), x and y-axes show normalized crack initiation probability and crack propagation velocity by those of Inconel 600 MA SG tube, respectively. The z-axes in Figs. 3.6-14(a) and (b) show the cumulative leak probability and NPV at the end of 60-year operation, respectively. Although no SCC has ever been experienced in Inconel 690 TT SG tubes in the field, one can understand the cumulative leak probability and NPV, or profitability, when assuming characteristics of Inconel 690 TT over SCC in the x-y plane illustrated in Fig. 3.6-14.

Besides, using Fig. 3.6-14(b), one can know various useful information such as how much investment could be possible to study SCC characteristics of Inconel 690 TT SG tubes and to develop another new SG tube material, if necessary.

3.6.3.4 Effect of Introducing Maintenance Criteria

Figs. 3.6-15 and 3.6-16 are typical examples of applying risk-benefit analyses to introducing maintenance criteria. Fig. 3.6-15 shows the results obtained on the assumption that the crack initiation and propagation are the base cases, namely, the cases of Inconel 600 MA SG tube in this study as appeared in Figs. 3.6-1 and 3.6-3 and on the assumption that the inspection accuracy is the case D20 as appeared in Fig. 3.6-2. Fig. 3.6-16 shows the results obtained on the assumption that the crack initiation is the half base case, that propagation velocity is the 1/10 base case and that the inspection accuracy is the case D20.

Difference in NPV from “no criteria” shown in Fig. 3.6-15(a) suggests that in the case of Fig. 3.6-15, 40%TW criteria which mean permission of continuous operation with a crack less than 40% TW, is preferable until approximately 16th year after operation. However, in the case of Fig. 3.6-16, since the crack initiation and propagation are slow compared to the case of Fig. 3.6-15, the higher value of TW criteria is preferable. The NPV with 50%TW criteria increase by 6 hundred-million yen at 30-year operation compared to “no criteria”.

The effect of introducing maintenance criteria is also shown in Figs. 3.6-15(b) and 3.6-16(b) in terms of cumulative repaired SG tubes during 60-year operations, suggesting that introducing the maintenance criteria reduces the number of repaired tubes. Although the number of cumulative repaired tubes when executing 40%TW criteria in the case of Fig. 3.6-15 was the lowest, the NPV value was the worst. This should be due to the fact that the higher cost of leakage compared to other criteria decreased the NPV.
3.6.4 Conclusions

An attempt was made to perform risk-benefit analysis for the purpose of optimizing the maintenance activities of SG tubes based on PFM approach.

In the risk analysis, probabilities of SG tube leakage and rupture are defined as risks, and the probabilities of these risks are found to be influenced significantly by crack propagation law, accuracy of inspection and sampling inspection.

In the risk-benefit analysis, it is suggested that investment to improve inspection accuracy would reduce the total costs during 60-year operations and increase the profitability.

The application of the risk-benefit analysis in this study to various SG tube maintenance strategies including replacing Inconel 600 MA SG tube by Inconel 690 TT SG tube and introducing maintenance criteria for detected cracks during an outage period would be useful for decision making from the viewpoints of both safety and profitability.

REFERENCES


Table 3.6-1  Summary of input data.

<table>
<thead>
<tr>
<th>Parameters</th>
<th>Value</th>
</tr>
</thead>
<tbody>
<tr>
<td>Plant type</td>
<td>4-loop SG</td>
</tr>
<tr>
<td>Material</td>
<td>Inconel 600</td>
</tr>
<tr>
<td>Wall thickness</td>
<td>1.27 mm (0.05 inch)</td>
</tr>
<tr>
<td>Inside radius</td>
<td>22.22 mm (0.875 inch)</td>
</tr>
<tr>
<td>Plant operation time</td>
<td>60 years</td>
</tr>
<tr>
<td>Steady-state temp.</td>
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</tr>
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<td>Normal operating pressure</td>
<td>95.5 atm</td>
</tr>
<tr>
<td>Flow stress</td>
<td>Normally distributed</td>
</tr>
<tr>
<td></td>
<td>Average 43 kg/mm²</td>
</tr>
<tr>
<td></td>
<td>Std. Dev. 4.3 kg/mm²</td>
</tr>
<tr>
<td>Residual stress</td>
<td>Normally distributed</td>
</tr>
<tr>
<td></td>
<td>Average 20 kg/mm² (in base case)</td>
</tr>
<tr>
<td></td>
<td>Std. Dev. 1 kg/mm²</td>
</tr>
<tr>
<td>Threshold for detectable leak rates</td>
<td>0 gpm   (Every leakage is detectable and repaired when it is found)</td>
</tr>
<tr>
<td>Crack initiation</td>
<td>Log-normally distributed</td>
</tr>
<tr>
<td></td>
<td>Average 14.6 years, Std. Dev. 9.6</td>
</tr>
<tr>
<td>Crack shape at a initiation</td>
<td>Depth : 0.0254mm/ 0.001inch (deterministic)</td>
</tr>
<tr>
<td></td>
<td>Length : Log-normally distributed</td>
</tr>
<tr>
<td></td>
<td>Median 3.18mm (1/8inch)</td>
</tr>
<tr>
<td></td>
<td>Std. Dev. 21.6mm (0.85inch)</td>
</tr>
<tr>
<td>Crack propagation law</td>
<td>(See Fig. 3.6-1)</td>
</tr>
<tr>
<td>Inspection accuracy</td>
<td>(See Fig. 3.6-2)</td>
</tr>
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Table 3.6-2  Meanings of the symbols indicated in the NPV equation.

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<tr>
<th>Items</th>
<th>Value in this analysis</th>
<th>Comments</th>
</tr>
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<tbody>
<tr>
<td>$NPV(T)$</td>
<td></td>
<td>Net present value at the time of T (million yen)</td>
</tr>
<tr>
<td>$t$</td>
<td></td>
<td>Operation time (year)</td>
</tr>
<tr>
<td>$N_{tube}$</td>
<td>$3,382 \times 4SG$</td>
<td>Total SG tubes in a 4-loop unit</td>
</tr>
<tr>
<td>$S(t)$</td>
<td>$80,000$</td>
<td>Revenue for power generation (million yen)</td>
</tr>
<tr>
<td>$C_{other}(t)$</td>
<td>$70,000$</td>
<td>Cost for power generation (except SG maintenance cost) (million yen)</td>
</tr>
<tr>
<td>$C_{R&amp;D}(t)$</td>
<td>$0$ (Normal)</td>
<td>Cost for improvement of inspection accuracy of SG tubes (million yen, per one reactor)</td>
</tr>
<tr>
<td></td>
<td>$400$ (Better)</td>
<td></td>
</tr>
<tr>
<td></td>
<td>$1000$ (Best)</td>
<td></td>
</tr>
<tr>
<td>$C_{inspect}(t)$</td>
<td>$400$ (Base)</td>
<td>Cost for inspection of SG during outage (million yen)</td>
</tr>
<tr>
<td></td>
<td>$200$ (1/2 Sampling)</td>
<td></td>
</tr>
<tr>
<td></td>
<td>$133$ (1/3 Sampling)</td>
<td></td>
</tr>
<tr>
<td>$C_{repair}$</td>
<td>$5$ (/1tube)</td>
<td>Cost for repairing one SG tube (million yen)</td>
</tr>
<tr>
<td>$C_{leak}$</td>
<td>$10,000$</td>
<td>Expected loss from leakage (million yen)</td>
</tr>
<tr>
<td>$C_{rupture}$</td>
<td>$100,000$</td>
<td>Expected loss from rupture (million yen)</td>
</tr>
<tr>
<td>$r_0$</td>
<td>$1$</td>
<td>Discount rate (%)</td>
</tr>
<tr>
<td>$r_{tax}$</td>
<td>$50$</td>
<td>Effective tax rate (%)</td>
</tr>
</tbody>
</table>
Fig. 3.6-1 Crack propagation laws for analysis.  

Fig. 3.6-2 Defective probability curves for this analysis.

Fig. 3.6-3 Cumulative crack initiation probability of Inconel 690 TT SG tube.

Fig. 3.6-4 Crack propagation laws for analysis of Inconel 690 TT SG tube.
Fig. 3.6-5  Leak probabilities for various crack propagation laws.

Fig. 3.6-6  Probabilities for various in-service inspection intervals.

Fig. 3.6-7  Cumulative probabilities of leakage and rupture for various inspection accuracies.

Fig. 3.6-8  Probabilities of leak and rupture with sampling inspections for \( t \) th year per one plant.
Fig. 3.6-9  Evaluated costs for inspection accuracy.
(a) case D_{40}, (b) case D_{20}, (c) case D_{10}

Fig.3.6-10  Evaluated costs with sampling inspections.
(a) 100%, (b) 1/2 sampling, (c) 1/3 sampling
Fig. 3.6-11  R&D cost vs. cumulative “expected cost”.

Fig. 3.6-12  Net present values for various inspection accuracy.

Fig. 3.6-13  Net present value with sampling inspections.
Fig. 3.6-14 Risk-benefit analysis for Inconel 690TT SG tube at the end of 60-year operation.
(a): Cumulative leak probability,  (b): NPV
Fig. 3.6-15 Risk-benefit analysis for maintenance criteria.
Crack initiation & propagation: Base case.
Inspection accuracy: case D20.
(a) Difference in NPV from No criteria
(b) Cumulative repaired SG tubes

Fig. 3.6-16 Risk-benefit analysis for maintenance criteria.
Crack initiation & propagation: 1/10 Base case.
Inspection accuracy: case D20.
(a) Difference in NPV from No criteria
(b) Cumulative repaired SG tubes
3.7 Depth Distribution Assessment of Thermal Fatigue Cracks

The application of risk-informed technologies not only to in-service inspections but also to the design of components and systems, encompassing a plant life-cycle, is the way to be pursued for the improvement of design of new reactors such as fast breeder reactors. When doing so, it is necessary to develop an analytical method that is capable of estimating failure probabilities without a failure database that can only be established on the long-time accumulation of operational experiences. The prediction method should estimate failure probabilities based on actual mechanisms that cause failure. For this purpose, this study developed a structural reliability evaluation method using probabilistic prediction of crack depth distributions for thermal fatigue, which is one of representative failure modes to be prevented in components of nuclear plants. This method is an extension of probabilistic fracture mechanics approach but is capable of modeling crack initiation, crack propagation, and crack depth density distribution at a given cycle. To verify the methodology, crack depth distribution observed in thermal fatigue test specimens was evaluated, and it was shown that the method could reproduce the observed crack depth distributions fairly well. This is considered to explore the possibility that probabilistic fracture mechanics approach can be verified by experiments, which was deemed impossible so far. Further improvement such as explicit implementation of interaction mechanisms between adjacent cracks will allow this methodology to be applied to the procedure of optimization of in-service inspection planning, as well as to the optimization of safety factors in component design of nuclear plants.

3.7.1 Introduction

For the improvement of in-service inspection planning of nuclear plants, risk-informed technologies have been implemented to a number of light water reactors, and significant benefits have been recognized. Recently, activities are going on to introduce the technologies not only to in-service inspection but also to design of components and systems, encompassing a plant life-cycle [1-10]. This approach is particularly promising for new reactors such as fast reactors, to maximize potential capabilities and fully extract attractive concepts of those reactors. However, when introducing this approach to new reactors that do not have enough experience to establish their own failure database, it is necessary to develop a method that predicts failure probabilities analytically.

From this perspective, mechanisms that cause failure should be captured precisely, and the methods should be built on the understanding of the mechanisms. In the case of fatigue crack propagation, which is a representative failure mode to be prevented in component design of nuclear plants, it has been common to postulate a single initial crack within a welded joint and to analyze the propagation of this single crack. However, when a structure is subjected to thermal fatigue, normally multiple cracks initiate and propagate, and there should be interactions between neighboring cracks, such as coupling of cracks.
and/or stress relaxation at the crack tip, that may increase and/or decrease crack initiation life and/or propagation rate. As a result, a distribution of crack depth appears. Evaluation of these processes with a single postulated crack in a deterministic way may be beneficial in terms of saving efforts necessary for evaluation, but it is not adequate for improving the accuracy of evaluation. Therefore, this study develops a basis of probabilistic structural reliability evaluation procedure for thermal fatigue. This procedure maintains the simplicity of a single crack model and yet is capable of modeling crack initiation, crack propagation, and crack depth density distribution. The capability of reproducing crack distribution will be of great benefit for structural reliability analysis. One of such benefits is the improvement of in-service inspection planning of components of fast reactors that are subject to thermal fatigue. Furthermore, one can consider that if a crack depth distribution is appropriately reproduced, failure probability predicted by the same methodology can be judged to be appropriate as well. In other words, this approach explores the possibility of verifying a probabilistic structural reliability evaluation by existing experimental results, which has been considered to be practically impossible because predicted failure probability is usually too low to compare with information that can be obtained from operational experience or laboratory tests.

### 3.7.2 Thermal fatigue tests for evaluation

In this section, thermal fatigue tests evaluated in this paper are summarized [11].

#### 3.7.2.1 Specimens

The geometry of specimens was basically thick pipe, and the inside of the pipe was subjected to thermal fatigue cycles induced by hot and cold sodium that flow alternatively. Two kinds of specimens were evaluated. The first one is “structural discontinuity model,” which is a thick pipe and the second one is “taper model,” which is a pipe whose diameter varies in longitudinal direction with the thickest point at the longitudinal center. Both of them were made from SUS304 stainless steel, of which chemical compositions and mechanical properties are shown in Table 3.7-1. The outer and inner diameters of the discontinuity model were 120.5 mm and 40.5 mm, respectively. For the taper model, those were 60.5–113.5 mm and 53.5 mm, respectively. In both cases, the inside surface of the pipe is smoothly finished, and there was no initial defects or notches.

The temperatures of hot and cold sodium were 600°C and 300°C for both specimens. The structural discontinuity model was tested up to 3053 cycles. With regard to the taper model, three identical specimens were tested up to 700, 1300, and 2000 cycles under the same conditions. This allows us to study the evolution of thermal fatigue damage and cracks according to the increase in the number of cycles. The outline of the tests is described in Table 3.7-2. After the tests, the specimens were cut in a number of locations, and cross-sections were carefully observed to detect and measure thermal fatigue cracks that were initiated and propagated during the tests. The information obtained by the observation is stored in a database. There we observed a number of typical thermal fatigue
cracks both in longitudinal and circumferential directions (in a network shape), as described in detail later.

Table 3.7-1 Chemical composition and mechanical properties of tested material. Material: SUS304

(i) Chemical composition (wt. %)

<table>
<thead>
<tr>
<th>Model</th>
<th>C</th>
<th>Si</th>
<th>Mn</th>
<th>P</th>
<th>S</th>
<th>Ni</th>
<th>Cr</th>
</tr>
</thead>
<tbody>
<tr>
<td>Structural discontinuity</td>
<td>0.08</td>
<td>0.78</td>
<td>1.49</td>
<td>0.19</td>
<td>0.008</td>
<td>10.4</td>
<td>19.64</td>
</tr>
<tr>
<td>model</td>
<td></td>
<td></td>
<td></td>
<td></td>
<td></td>
<td></td>
<td></td>
</tr>
<tr>
<td>Taper model</td>
<td>0.04</td>
<td>0.48</td>
<td>1.53</td>
<td>0.025</td>
<td>0.002</td>
<td>9.47</td>
<td>18.5</td>
</tr>
</tbody>
</table>

(ii) Mechanical Properties

<table>
<thead>
<tr>
<th>Model</th>
<th>Temperature (C)</th>
<th>Yield strength (MPa)</th>
<th>Tensile strength (MPa)</th>
<th>Elongation (%)</th>
</tr>
</thead>
<tbody>
<tr>
<td>Structural discontinuity</td>
<td>RT 550</td>
<td>270 135</td>
<td>568 396</td>
<td>64.3 41.6</td>
</tr>
<tr>
<td>model</td>
<td></td>
<td></td>
<td></td>
<td></td>
</tr>
<tr>
<td>Taper model</td>
<td>RT 550</td>
<td>270 135</td>
<td>568 396</td>
<td>64.3 41.6</td>
</tr>
</tbody>
</table>
Table 3.7-2 Outline of thermal fatigue tests

<table>
<thead>
<tr>
<th>(i) Structural discontinuity model</th>
</tr>
</thead>
<tbody>
<tr>
<td>Material</td>
</tr>
<tr>
<td>Thermal transients</td>
</tr>
<tr>
<td></td>
</tr>
<tr>
<td>Sodium temperature</td>
</tr>
<tr>
<td></td>
</tr>
<tr>
<td>Configuration</td>
</tr>
<tr>
<td></td>
</tr>
<tr>
<td></td>
</tr>
<tr>
<td>Number of cycles</td>
</tr>
<tr>
<td>Maximum fatigue damage</td>
</tr>
</tbody>
</table>

<table>
<thead>
<tr>
<th>(ii) Taper model</th>
</tr>
</thead>
<tbody>
<tr>
<td>Material</td>
</tr>
<tr>
<td>Thermal transients</td>
</tr>
<tr>
<td></td>
</tr>
<tr>
<td>Sodium temperature</td>
</tr>
<tr>
<td></td>
</tr>
<tr>
<td>Configuration</td>
</tr>
<tr>
<td></td>
</tr>
<tr>
<td></td>
</tr>
<tr>
<td>Number of cycles</td>
</tr>
<tr>
<td>Maximum fatigue damage</td>
</tr>
</tbody>
</table>

3.7.2.2 Stress and Fatigue Damage Thermal stresses were elastically calculated by the finite element method (FEM). The models used for analysis and boundary conditions are shown in Fig. 3.7-1. An isoparametric quadrilateral axisymmetric element was employed. The numbers of elements were 202 and 740 for the structural discontinuity and taper models, respectively. The degrees of freedom were 1311 and 4737 for the structural discontinuity and taper models, respectively. Stresses at representative locations in the specimen are summarized in Table 3.7-3. In this table, von Mises type equivalent strain range as well as stress components of membrane stress, bending stress, and peak stress are shown. Stress distribution in the circumferential cross-section was basically dominated by bending and peak stresses in both types of specimens. In the longitudinal direction, the maximum stress was observed at the center of the pipe. Stress gradually decreased as the distance from the center increased. The maximum von Mises equivalent stress ranges at the
inner surface of the center of the pipe were 1722 MPa for the structural discontinuity model and 1737 MPa for the taper model. The stress ranges of longitudinal and circumferential components were 1670 MPa and 1867 MPa for the structural discontinuity model, and 1693 MPa and 1814 MPa for the taper model. The stress component in the circumferential direction was somewhat superior to that in the longitudinal direction in both specimens.

Fatigue damage distributions were calculated based on the FEM analysis. Strain range was estimated from the elastically calculated stress and cyclic stress-strain curve [12] of the material with an elastic follow-up factor of 2, whose validity was shown by an inelastic analysis. (The elastic follow-up factor represents the increase in strain due to plastic deformations with respect to elastically calculated strain). Fatigue damage was calculated based on the strain range and the average trend of fatigue life of SUS304 [13] using Miner’s rule. Fatigue damage showed the same tendency as that of stress distribution. The maximum damage fractions were observed at the center of the inner surface of the pipe, and the values were 24.6 for the structural discontinuity model and 16.3 for the taper model (at 2000 cycles), both of which were well beyond the crack initiation criterion of unity.

3.7.2.3 Observation of Crack Initiation and Propagation  Fig. 3.7-2 shows the inside surface of the specimens after the thermal fatigue tests. Both the structural discontinuity model and the taper model showed multiple cracks both in longitudinal and circumferential directions in a network shape. Cracks in the longitudinal direction were somewhat more evident compared to those in the circumferential direction. This observation corresponded to the fact that stress range in the circumferential direction was estimated somewhat greater than that in the longitudinal distribution.

The locations and depth of cracks observed in cross-sections of the specimens were measured after the tests and were stored in a database. The crack depth measured by observing cross-sections is generally smaller than real depth in most cases, except for circumferential cracks whose depth is uniform irrespective of the location of measurement. In the case of semi-elliptical cracks, an expected value of the measured depth of a crack whose real depth is 1 is \( \pi/4 \). Therefore, in this paper, measured depth times \( 4/\pi \) were used for further evaluation. The maximum value of measured crack depth was 17.4 mm, which corresponded to 44% of the wall thickness of the structural discontinuity model. Crack depth increased as fatigue damage increased in both the structural discontinuity model and the taper model. In the case of the taper model, it was obvious that more cracks were observed as the number of fatigue cycles increased.

3.7.2.4 Statistical Analysis of Crack Depth and Crack Space  Fig. 3.7-3 shows observed histograms of crack depth distribution. In these histograms, cracks observed at various locations with different fatigue damage levels are plotted together. The shape of the histogram is similar to a log-normal distribution. The mean value and the standard deviation were 6.3 mm and 5.9 mm for the structural discontinuity model and 5.0 mm and 4.2 mm for the taper model (at 2000 cycles), respectively. In both cases, the maximum
number was observed at about 10 mm. For cracks whose depth is less than 1 mm, the count could have been affected by the accuracy of measurement. For the structural discontinuity model, it should be noted that a local maximum was observed at the depth of about 20 mm. The reason for this local maximum will be discussed later in the section of probabilistic prediction of crack depth distribution.

Furthermore, spaces between two adjacent cracks were measured, and the histograms of the spaces are shown in Fig. 3.7-4. It is clear in the figure that the space between cracks strongly depend on crack depth. The deeper the cracks, the wider the spaces between cracks; The mean and the standard deviation for the structural discontinuity model were 6.9 mm and 5.0 mm for cracks whose depths were more than 1 mm, and 16.6 mm and 13.2 mm for cracks whose depths were more than 5 mm, respectively. The mean and the standard deviation for the taper model were 6.2 mm and 3.7 mm at the 700th cycle, 4.0 mm and 2.0 mm at the 1300th cycle, and 3.6 mm and 1.9 mm at the 2000th cycle, respectively.

Fig. 3.7-5 shows the relationships between crack depth and spaces between cracks. As stated above, the deeper the cracks, the wider the spaces. Although there were considerable scatters, applying a bivariate normal distribution, an average trend was calculated; crack space was 3.2 times of crack depth for the structural discontinuity model and was 1.8 for the taper model with coefficients of correlation of 0.54 and 0.83, respectively. The reasons for the difference in the proportional coefficient and the coefficient of correlation are not known.

The relationship between fatigue damage and crack space of the structural discontinuity specimen and the taper model at the 2000th cycle showed that despite the fact that fatigue damage fairly varied from 1.8 to 24.6, there was no apparent difference in crack space. This implies that once fatigue damage reached a certain level (value), even if more thermal transients are imposed, new crack initiation rarely occurs and degradation proceeds in the form of propagation of cracks that had already been initiated.
Fig. 3.7-1 Models used for finite element analysis

Table 3.7-3 Stresses at representative locations
(i) Structural discontinuity model (MPa)

<table>
<thead>
<tr>
<th>Location in Fig.3.7-1(i)</th>
<th>Distance from center (mm)</th>
<th>Stress range (von Mises)</th>
<th>Tensile stress component</th>
<th>Compressive stress component</th>
</tr>
</thead>
<tbody>
<tr>
<td></td>
<td></td>
<td></td>
<td>Membrane</td>
<td>Bending</td>
</tr>
<tr>
<td>1</td>
<td>0</td>
<td>1773</td>
<td>67</td>
<td>406</td>
</tr>
<tr>
<td>2</td>
<td>30</td>
<td>1675</td>
<td>52</td>
<td>330</td>
</tr>
<tr>
<td>3</td>
<td>60</td>
<td>1175</td>
<td>20</td>
<td>123</td>
</tr>
</tbody>
</table>

(ii) Taper model (MPa)

<table>
<thead>
<tr>
<th>Location in Fig.3.7-1(ii)</th>
<th>Distance from center (mm)</th>
<th>Stress range (von Mises)</th>
<th>Tensile stress component</th>
<th>Compressive stress component</th>
</tr>
</thead>
<tbody>
<tr>
<td></td>
<td></td>
<td></td>
<td>Membrane</td>
<td>Bending</td>
</tr>
<tr>
<td>A</td>
<td>0</td>
<td>1728</td>
<td>52</td>
<td>422</td>
</tr>
<tr>
<td>B</td>
<td>60</td>
<td>1597</td>
<td>48</td>
<td>464</td>
</tr>
<tr>
<td>C</td>
<td>120</td>
<td>1271</td>
<td>31</td>
<td>401</td>
</tr>
</tbody>
</table>
Fig. 3.7-2 Crack initiated and propagated in a network shape
Fig. 3.7-3 Histogram of crack depth
3.7.3 Probabilistic evaluation of crack depth distribution

In this section, the crack initiation and propagation behavior in the two structural tests whose details were described in the previous section were evaluated deterministically and probabilistically.

3.7.3.1 Deterministic evaluation  Neither the structural discontinuity model nor the taper model had initial defects or notches, which means all the cracks observed after the tests were initiated during the thermal transients. Therefore, both crack initiation and propagation were evaluated. Numbers of cycles to crack initiation were evaluated based on the von Mises equivalent strain range at the surface of a specimen. It was assumed based on replica
observations of standard test specimens made of 316FR subjected to fatigue at 600°C reported in Ref. [14] that a semi-elliptical crack with a depth of 0.25 mm and an aspect ratio of 1.0 was initiated at the half of average fatigue life. 316FR is a stainless steel that has equivalent fatigue life to SUS304 [15], and the relationship between crack initiation cycle and fatigue life can be considered to be identical for SUS304 and 316FR. The assumption can be expressed by

\[ N_i = \frac{1}{2} N_f \]  

(3.7-1)

where \( N_i \) is the number of cycles to crack initiation and \( N_f \) is the number of cycles to failure obtained from material tests. For crack propagation, although cracks were observed both in longitudinal and circumferential directions, for simplicity, only circumferential cracks were evaluated in this study. Crack propagation was estimated based on a simplified method [16], which estimates a J integral range from a stress intensity factor range using a plasticity correction factor, which is a function of elastic follow-up factor and reference stress. The formulation for semi-elliptical surface crack on the inside surface of cylinder developed by Shiratori [17] was used to obtain the stress intensity factor range. An elastic follow-up factor of 2.5 was used for propagation. The crack propagation rate was determined based on an experimentally obtained relationship between J-integral range and crack propagation distance per cycle, using Paris’ law as follows:

\[ \frac{da}{dn} = C_f \Delta J^{m_f} \]  

(3.7-2)

where J is the J-integral range. The coefficient and the power of Paris’ law are given in Ref. 18. The conditions used for the analysis are summarized in Table 3.7-4. Based on Eqs. (3.7-1) and (3.7-2), crack depth at the Nth cycle \( a(N) \) can be calculated as follows:

\[ a(N) = a_0 + \int_{0}^{N-N_i} C_f \Delta J^{m_f} dn \]  

(3.7-3)

where \( a_0 \) is the initial crack depth (=0.25 mm).

The calculated relationships between the number of fatigue cycles versus crack depth and stress intensity factor range for the structural discontinuity model and the taper model are shown in Fig. 3.7-6. Three values of the coefficient of Paris’ law that correspond to the mean, the upper limit (+2 sigma), and the lower limit (-2 sigma) of the scatter of the experimentally obtained crack propagation rate were used for evaluation. For the structural discontinuity model, the stress intensity factor range monotonically increased when the crack propagation rate was at the lower limit. When the crack propagation rate was mean, a local maximum of stress intensity factor range was observed approximately at the 2250th cycle, which corresponded to the crack depth of 16 mm, or about 40% of wall thickness. Beyond this cycle, the stress intensity factor range decreased. When the crack propagation rate was at the upper limit, the trend was the same as above. However, a local maximum
was observed at an earlier cycle. It is to be noted that at a point where the shape of the crack changed from semi-elliptical to circumferential, an abrupt increase in stress intensity factor range was observed. In the case of the structural discontinuity model, predicted crack depths at the end of the experiment corresponding to mean, upper limit, and lower limit crack propagation rates were 15 mm, 1 mm, and 23 mm, respectively. The same tendency was observed for the taper model, although the stress intensity factor range and the amount of crack propagation are less than those for the structural discontinuity model.

Fig. 3.7-5 Correlation between crack depth and space
Table 3.7-4 Conditions for evaluation

<table>
<thead>
<tr>
<th>Crack initiation</th>
<th>Number of cycles to crack initiation, Ni</th>
<th>Half of average fatigue life of SUS304</th>
</tr>
</thead>
<tbody>
<tr>
<td></td>
<td>Stress-strain relationship</td>
<td>Logarithmic standard deviation: 0.4</td>
</tr>
<tr>
<td></td>
<td>Elastic follow-up factor</td>
<td>Cyclic stress-strain curve</td>
</tr>
<tr>
<td></td>
<td></td>
<td>2.0</td>
</tr>
<tr>
<td>Crack propagation</td>
<td>Configuration of crack</td>
<td>Semi-elliptical and circumferential</td>
</tr>
<tr>
<td></td>
<td>Elastic follow-up factor</td>
<td>2.5</td>
</tr>
<tr>
<td></td>
<td>Crack propagation law: Paris’ law</td>
<td>Coefficient: 1.0x10^{-4}</td>
</tr>
<tr>
<td></td>
<td></td>
<td>Power: 1.37</td>
</tr>
<tr>
<td></td>
<td></td>
<td>Logarithmic standard deviation of</td>
</tr>
<tr>
<td></td>
<td></td>
<td>coefficient: 0.4</td>
</tr>
</tbody>
</table>
Fig. 3.7-6 Deterministic crack propagation evaluation
3.7.3.2 Probabilistic evaluation  Aiming to reproduce the crack depth distribution described in the previous section, a probabilistic evaluation was carried out. The basic methodology was the same as the one used for the deterministic evaluation. In this section, two random variables were employed. One is crack initiation life, the number of cycles to initiation of a crack with the depth of 0.25 mm, and the other is crack propagation rate.
represented by the coefficient of Paris’ law. The probabilistic density function of crack initiation life was modeled as a log-normal distribution with the logarithmic standard variation of 0.4. For the crack propagation rate, a log-normal distribution was applied as well, and the standard deviation was determined to be 0.4 based on experimental data [18]. The probabilistic density functions were modified to neglect distributions beyond ±2 sigma where no experimental data were observed; that is, probabilistic density beyond ±2 sigma was set to zero, keeping the integral between ±2 sigma unity.

It is very natural to consider that the scatter of crack depth observed in the tests was caused not only by a scatter of crack initiation life and propagation rate but also by the interaction of adjacent cracks that may accelerate or slow down. Mechanisms of interaction may involve coupling of neighboring cracks that increases stress intensity factor and the relaxation of stresses at a crack tip that cause the reduction of stress intensity factor. However, for simplicity, the mechanism of the interaction was not explicitly modeled in the evaluation in this section, and it is discussed separately.

The probabilistic structural reliability assessment code MSSREAL-P [19], which was developed by Asayama et al., was employed for the calculation of probabilistic density function of crack depth and failure probability. This code calculates the probabilistic density function for crack depth at the Nth cycle \( f_n(a) \) by a procedure described below.

The bivariate probabilistic density function of the number of cycles to crack initiation and the coefficient of Paris’ law is represented by Eq. (3.7-4). Here, \( N_i \) and \( C_f \) are assumed to be independent,

\[
g(N_i, C_f) = \frac{1}{2\pi\sigma_{N_i}\sigma_{C_f}} e^{-\frac{1}{2} \left( \frac{[\log(N_i-\mu_{N_i})]^2}{\sigma_{N_i}^2} + \frac{[\log(C_f-\mu_{C_f})]^2}{\sigma_{C_f}^2} \right)} \quad (3.7-4)
\]

If we fix the number of cycles imposed \( N \) for a while, set \( A_\alpha \) can be defined as a set of combinations of \( N_i \) and \( C_f \) to make crack depth at the Nth cycle \( a \). In this particular case, set \( A \) consists of lines on the \( N_i-C_f \) plane, each of which corresponds to a specific crack depth,

\[
A_\alpha = \left\{ (N_i, C_f) \mid \int_{a_\alpha}^{N=N_i} C_f \Delta J^{(\alpha)} \, dn = a \right\} \quad (3.7-5)
\]

The integration of the bivariate probabilistic density function represented by Eq. (3.7-4) over set \( A \) that corresponds to a specific crack depth \( a \) gives the frequency of the cracks whose depth is \( a \),

\[
f_n(a) = \int_{(N_i, C_f) \in A_\alpha} g(N_i, C_f) \, dA_\alpha \quad (3.7-6)
\]

The above formulations hold for an arbitrary cycle \( N \); thus we can obtain a probabilistic function of crack depth for an arbitrary number of cycles.

The result of crack depth evaluation is shown in Fig. 3.7-7 along with the
experimental observations. In the evaluation, a crack depth distribution was calculated for three cross-sections with different stress levels, and those were combined to be compared with the experimental results. Both for the structural discontinuity model and the taper model, the trend of the observed crack depth distribution was reproduced by the evaluation fairly well. In the case of the structural discontinuity model, a local maximum was predicted at the depth of about 20 mm, which corresponds to the local maximum of 16 mm that was observed in the experiment. The local maximum of crack depth density is considered to be related to the fact that stress intensity factor range shows a local maximum at the crack depth of about 25 mm. The mean value and the standard deviation obtained from experimental results were 6.3 mm and 5.9 mm for the structural discontinuity model and 5.0 mm and 4.2 mm for the taper model at 2000 cycles, respectively, as described before. The evaluated values of mean and the standard deviation were 7.1 mm and 7.1 mm for the structural discontinuity model and 3.8 mm and 5.4 mm for the taper model, respectively. Prediction was possible within an accuracy of a factor of 1.5 for mean and standard deviation.

Based on the evaluation described above, failure probability can be calculated. It is to be noted that as the crack depth distribution, which is the basis of failure probability calculation, is precisely reproduced, failure probability calculated from the distribution should also be accurate. This methodology explores the possibility of validating probabilistic integrity assessment methods by experiments, which was deemed impossible so far.

In practice, the value of failure probability depends on the definition of failure. In the present study, failure was determined at the point when crack depth reached 3/4 of wall thickness using the following equation:
Fig. 3.7-8 Semi-elliptical model to calculate interaction of cracks

\[ P_f(N) = \int_{a_{cr}}^{\infty} f_N(a) \, da \]  

(3.7-7)

where \( P_f(N) \) is the failure probability at the Nth cycle, \( a_{cr} \) is the critical crack depth that corresponds to failure, which is equal to 3/4 in this study, \( a \) is the crack depth, and \( f_N(a) \) is the crack depth density function at the Nth cycle. The calculated failure probabilities for the structural discontinuity model and the taper model (at the 2000th cycle) were \( 2.3 \times 10^{-1} \) and \( 2.0 \times 10^{-2} \).

### 3.7.4 Discussions

The evaluation described above showed that the crack depth distribution could be reproduced by a probabilistic approach fairly well. In the present study, crack initiation life and crack propagation rate (the coefficient of Paris’ law) were set to be random variables,
and they were simply determined by material test data obtained from multiple heats of materials. In this section, the relationship between the distribution thus determined and the mechanisms that are considered to be actually operating, such as the acceleration of crack propagation due to coupling of neighboring cracks or slowing down of crack propagation due to relaxation of stresses at crack tips, is discussed.

The scatter assumed in the present study for crack propagation rate can be converted to the variation of stress intensity factor range, using Paris’ law. The scatter of \( \pm 2 \sigma \) in the former corresponds to the variation of stress intensity factor range from 0.77 to 1.3 with a 1.0 average. There is a possibility that this variation was caused by the interactions among neighboring cracks that can accelerate or slow down crack propagation.

Firstly, the reduction of crack propagation rate that can be caused by relaxation of stresses at the crack tips is examined. The extent of the reduction in the stress intensity factor range that is actually induced by neighboring multiple cracks can depend on the geometry of specimen, loading conditions, crack depth, the density of cracks, etc., and the relationships between the reduction the aspect ratio varied from 1/10 to 1/2 of wall thickness and from 1 to 4, respectively. Using these models, the ratio of stress intensity factor of one of multiple cracks to a single crack was calculated using the finite element method. The isoparametric quadrilateral axisymmetric element was used. An example of the semi-elliptical model is shown in Fig. 3.7-8. Calculated results are shown in Fig. 3.7-9. The loading conditions used for the calculation of Fig. 3.7-9 are the same as those of the structural discontinuity model. When cracks are located very closely, the reduction ratio can be as small as 0.3. When we assume that the mean space of cracks is about 1.8–3.2 times of mean depth as discussed in the previous section, the reduction ratio is estimated to be 0.4–0.8. The extent of reduction was smaller for semi-elliptical cracks than for circumferential cracks, which gave lower limits of the reduction factor and those parameters may be very complex. In this study, for the estimation of the reduction, a simplified model in which circumferential or semi-elliptical cracks are periodically located inside a thick pipe was employed. The ratio of crack space to crack depth varied from 0.5 to 10 for the circumferential crack mode, and from 0.5 to 4 for the semi-elliptical crack model. In the case of the semi-elliptical crack model, cracks were aligned so that their center is on a single longitudinal line, and the depth and the aspect ratio varied from 1/10 to 1/2 of wall thickness and from 1 to 4, respectively. Using these models, the ratio of stress intensity factor of one of multiple cracks to a single crack was calculated using the finite element method. The isoparametric quadrilateral axisymmetric element was used. An example of the semi-elliptical model is shown in Fig. 3.7-8. Calculated results are shown in Fig. 3.7-9. The loading conditions used for the calculation of Fig. 3.7-9 are the same as those of the structural discontinuity model. When cracks are located very closely, the reduction ratio can be as small as 0.3. When we assume that the mean space of cracks is about 1.8–3.2 times of mean depth as discussed in the previous section, the reduction ratio is estimated to be 0.4–0.8. The extent of reduction was smaller for semi-elliptical cracks than for circumferential cracks.
cracks, which gave lower limits of the reduction factor.

Secondly, the effects of the increase in the stress intensity factor range due to coupling of neighboring cracks were examined. It was assumed that when two semi-elliptical cracks couple, the shape of a new crack is one that encompasses the two coupling cracks. Under this assumption, if two identical cracks couple, the depth of a new crack becomes 1.41 times the original depth. This corresponds to the increase in stress intensity factor by 1.18 times the original value.

From the above discussions, we can estimate the variation of stress intensity factor range that can be attributed to the interactions between neighboring cracks as approximately 0.5–1.2 times the stress intensity factor for a single crack. Comparing this value with the variation of stress intensity factor range estimated from the scatter of crack propagation rate (±2 sigma) which was 0.77–1.3 as described above, it is understood that both are approximately equivalent. Therefore, the scatter of the crack propagation rate that was simply modeled based on the scatter of material tests can be interpreted as the result of the interactions that occur between neighboring cracks. The implementation of the interaction mechanisms is expected to contribute to the generalization of the procedure and the improvement of the accuracy of evaluation, and this will lead to further validation of the method and to the application of this procedure to integrity assessment of nuclear plants including the optimization of in-service inspection planning.

This procedure has just been developed and has not been applied to existing plants; a possible way of application to optimization of in-service inspection planning is as follows: As a first step, failure probabilities of various portions of plant systems or components that are subject to thermal fatigue will be calculated for screening. Based on the result, portions that indicate relatively higher values of calculated failure probability are selected to be candidates to be inspected. Then, the procedure proposed in this paper is applied; one can further select portions to be inspected with priority from the candidates. This will be done based on the calculated probabilistic density of deeper cracks. Portions with higher possibility of yielding deeper cracks should be inspected first among those indicated identical levels of failure probability. This procedure will enhance the reliability of components and, at the same time, will minimize inspection cost.
3.7.5 Conclusions

By analyzing structural test specimens that were subjected to thermal fatigue induced by hot and cold sodium, it was demonstrated that a distribution of depth of cracks that were initiated and propagated during thermal fatigue cycles can be accurately reproduced by a probabilistic approach.

The relationship between crack depth and crack spaces was measured, and it was...
clarified that there was a strong correlation between them. This observation indicates that there exist interactions between adjacent cracks such as relaxation of stresses at crack tips that reduce stress intensity factors and coupling of neighboring cracks that increases them.

In the present study, crack initiation life and crack propagation rate (the coefficient of Paris’ law) were set to be random variables, and their probabilistic density functions were determined from experimental data with multiple heats of materials. It was shown by preliminary estimation that the scatter of crack propagation rate thus determined can be interpreted as the scatter caused by the variation of stress intensity factors stemming from the interactions between multiple cracks.

Evaluation of crack depth density distribution enables the verification of the probabilistic fracture mechanics approach by experiments. That is, failure probability predicted by a method that can evaluate crack depth density distribution accurately can be considered fairly reliable.

Explicitly incorporating the mechanisms that can occur with neighboring multiple cracks to the present approach will enable this approach to contribute to the optimization of in-service inspections and design of components of power plants that are subjected to thermal fatigue.

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Probabilistic Fracture Mechanics for Risk-Informed Activities - Fundamentals and Applications -
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3.8 Probabilistic Evaluation Regarding the Rules of Fitness-for-Service

3.8.1 Effect of crack dimension error on fatigue crack

3.8.1.1 Introduction Since October 2003, the Rules on Fitness-for-service for Nuclear Power Plants [1] (hereinafter, the FFS Codes) has been enforced. Applying the FFS Codes allowed continuous use without repair or replacement when a crack is detected on components by an inspection if there is no safety problem provided that the behavior of the crack is evaluated. Since there were no prescriptions for evaluating integrity of a detected crack, if some cracks were detected in components of a nuclear power plant, they were all repaired or replaced and returned to non-crack condition, and were evaluated for their integrity according to the design Codes [2]. Therefore, in the PFM analysis so far, a failure probability was calculated on the basis of a scenario assuming that when cracks were detected, they should be all repaired. In this calculation, the existing probability of a sample was controlled on the basis of a crack detection probability (probability whether crack can be detected) in an inspection, and a failure probability was calculated. However, when the FFS Codes are introduced, not only detectability of a crack but also sizing error of the crack should be considered. Because if a crack is detected but it is determined to be the size that has no safety problems, continuous operation is allowed and the sizes of the detected crack may affect the determination criteria. In this section, assuming plant operations with the FFS Codes introduced, examination results on the effects of the crack sizing error on a piping failure probability are described.

3.8.1.2 Method of evaluating crack sizing error Since cracks due to Stress Corrosion Cracking (SCC) on shrouds or Primary Loop Recirculation system (PLR) piping of a Boiling Water Reactor (BWR) were detected in around 2002, a lot of studies have been carried out for crack sizing and it has been found that crack sizing may be largely varied depending on an inspection method, crack size, position, etc. [3]. Figs. 3.8-1 to 3.8-3 are examples of study on crack sizing errors. In these examples, crack sizing errors are indicated in a normal distribution considering averaged errors regardless of the size of cracks. However, these data seem to indicate that as the crack gets larger, it may be detected smaller and the error may be larger. Thus in this study, it was assumed that the averaged errors and dispersion (standard deviation of normal distribution) have correlation with the size of a crack and a crack sizing error was assumed by a linear function of crack dimensions.

Basic idea of incorporating the impact of crack sizing into a Probabilistic Fracture Mechanics (PFM) analysis code is shown in Fig. 3.8-4. In normal PFM analysis, initial crack sizes defined by a probability density function or sizes after growth of the crack are used for failure evaluation or evaluation of crack detection probability. This is evaluation based on "true crack sizes". On the other hand, determination whether or not continuous operation is possible by applying the FFS Codes or repair or replacement should be done is evaluated by using "estimated crack sizes" including the crack sizing error measured in
non-destructive inspection. The difference of these two crack sizes occurred by the crack sizing error makes a difference in fracture probability for a case where operations are performed with the FFS Codes introduced and cracks permitted and for a case of conventional practice with all the detected cracks repaired. In the FFS Codes, since a margin of safety is considered in crack stability evaluation, if the crack sizing error is covered by a safety factor, there will not be a significant difference in these failure probabilities and it is determined that the crack sizing error is not significance in safety.

When applying the FFS Codes, whether performing repair or replacement should be determined on the basis of crack depth and crack stability evaluation results. For the crack depth, the following restrictions are provided.

\[ a_d < 0.75t \]  \hspace{1cm} (3.8-1)

where \( a_d \) is an estimated crack depth, and \( t \) is a thickness. In this study, as shown in Eq. (3.8-1), if the estimated crack depth at the end of evaluation period is 75% or less of the thickness, continuous operation is possible. In the crack stability evaluation, a primary bending stress \( P_{bg} \) is restricted as below.

\[ P_{bg} < S_C \]  \hspace{1cm} (3.8-2)

\[ S_C = \frac{1}{SF} \left( \frac{P_{bg}'}{Z} - P_e \right) - P_m \left( 1 - \frac{1}{Z \cdot SF} \right) \]

For \( \beta \leq \pi - \theta_d \)

\[ P_{bg}' = \frac{2\sigma_f}{\pi} \left( 2 \sin \beta - \frac{a_d}{t} \sin \theta_d \right) \]

\[ \beta = \frac{1}{2} \left( \pi - \frac{a_d}{t} \theta_d - \frac{P_m}{\sigma_f} \pi \right) \]

For \( \beta > \pi - \theta_d \)

\[ P_{bg}' = \frac{2\sigma_f}{\pi} \left( 2 - \frac{a_d}{t} \right) \sin \beta \]

\[ \beta = \frac{\pi - 1}{2 - \frac{a_d}{t}} \left( \pi - \frac{a_d}{t} - \frac{P_m}{\sigma_f} \right) \]

where \( S_C \) is a permissible bending stress, \( P_{bg}' \) is a bending stress at the time of plastic collapse, \( P_e \) is a thermal expansion stress, \( P_m \) is a primary membrane stress, \( \sigma_f \) is a fluxion stress, \( \theta_d \) is a crack half angle (in radian), \( \beta \) is an angle of neutral axis, and \( SF \) is a safety factor. \( Z \) is a Z-factor and is given for ferritic steel by the following equation [1].

\[ Z = 0.2885 \log \left( \frac{OD}{25} \right) + 0.9573 \]  \hspace{1cm} (3.8-3)
Here, $OD$ is a nominal diameter (A-size in JIS). When applying the FFS Codes, if the primary bending stress is below $S_C$ at the end of the evaluation period or the crack depth is 75% of the plate thickness, continuous operation is possible.

### 3.8.1.3 Impact evaluation of crack sizing error

In the FFS Codes, a safety factor: 3.0 (2.77 for bending stress) for Service Level-A and B in stability evaluation of a cracked pipe; or 1.5 (1.39 for bending stress) for Service Level-C and D is considered for primary stress. On the other hand, since the safety factor is not considered for secondary stress (including thermal expansion stress), in this study, for a case of acting only primary stress and for a case of mainly acting secondary stress, the impact of the crack sizing error on the failure probability of piping was examined.

#### 3.8.1.3.1 Evaluation conditions

Assuming crack growth due to fatigue, the impact of the crack sizing error on the failure probability was evaluated. A main steam pipe of BWR was assumed for analysis and three types (small, middle and large bore pipes) as in Table 3.8-1 were assumed.

As an initial crack, a circumferential surface crack in the pipe is assumed. As a depth of the initial crack, the following exponential distribution is used [4].

$$f(a) = \frac{1}{\mu} \exp \left( \frac{-a}{\mu} \right) \quad [a: \text{mm}]$$

where $a$ is a true crack depth. As aspect ratio distribution, the following log-normal distribution [5] is used.

$$f(\beta_R) = \frac{1}{\sigma \beta_R \sqrt{2\pi}} \exp \left\{ -\frac{1}{2} \left[ \frac{\ln(\beta_R / \mu_{\beta})}{\sigma} \right]^2 \right\}$$

where $\beta_R$ is an aspect ratio, and $c$ is a true crack half length.

Piping material is STS410. Material properties shown in Table 3.8-2 defined in the Design Codes [2] are used in the analyses.

As loads for evaluating crack growth, loads used for evaluating crack growth in LBB assessment of main steam system piping of BWR are used as shown in Table 3.8-3 [6]. This load corresponds to a load condition for evaluating crack growth in LBB evaluation criteria defined so as to envelop the crack growth behavior according to the design thermal transient conditions.
A stress intensity factor [7] for a semi-elliptical surface crack in a plate is used as a fracture mechanics parameter. A crack growth rate is, based on the FFS Codes, determined using the following equations for the stress intensity factor range ($\Delta K$) [1].

$$\frac{da}{dN} [\text{mm/cycle}] = 1.738 \times 10^{-13} \Delta K^{5.95} \quad (\Delta K < 13.2 \text{ MPa}\sqrt{\text{m}}) \quad (3.8-6)$$

$$\frac{da}{dN} [\text{mm/cycle}] = 5.325 \times 10^{-9} \Delta K^{1.95} \quad (\Delta K \geq 13.2 \text{ MPa}\sqrt{\text{m}}) \quad (3.8-7)$$

In this evaluation, crack growth analysis is performed till a crack penetrates the plate thickness. Crack growth is not calculated after the crack penetration, while calculation for a sample resulting in penetration is stopped at that moment. Determination of unstable fracture is performed prior to determination of penetration and calculation for a sample having suffered unstable fracture is stopped at that moment. In addition, failure after repair or replacement is not considered. For unstable fracture evaluation for a true crack, assuming that a safety factor $SF$ in the Eq. (3.8-2) is 1.0, the evaluation is performed using a true crack depth, $a$, and a true crack half angle, $\theta$.

A pre-service inspection (PSI) and an in-service inspection (ISI) are performed as crack inspections. ISI is performed 100% every ten years referring to an inspection rate for Class 1 piping in the FFS Codes. A probability of non-detection $PND$ is evaluated by the following equations using true crack dimensions [5].

$$PND = \frac{1}{2} \left(1 - e^{-\frac{1}{2} \sqrt{\pi} \int_{0}^{\ln(A'/A)} e^{-t^2} dt} \right) + \varepsilon \quad (3.8-8)$$

$$A = \begin{cases} \frac{\pi}{2} ac & (2c \leq D_B) \\ \frac{\pi}{4} a \alpha D_B & (2c > D_B) \end{cases}$$

$$A' = \frac{\pi}{4} a^* D_B$$

$$\varepsilon = 0.005$$

$$D_B = 25.4 \text{ [mm]}$$

$$V = 1.33$$

$$a^* = 6.35 \text{ [mm]}$$

Referring to the variation of crack depth shown in Figs. 3.8-1 to 3.8-3, in this evaluation, a parameter indicated in Table 3.8-4 was set as variation of crack sizing error. In the examples shown in Figs. 3.8-1 to 3.8-3, the average error is fixed regardless of crack sizes, a case of larger averaged error according to the crack sizes was also assumed. Here, the averaged error and the standard deviation are expressed by the following equation.
assuming a linear function with respect to crack sizes and parameter survey was performed.

**Crack depth**

\[ \delta(a) = A_0 + A_1 a \]  
(3.8-9)

\[ \sigma(a) = B_0 + B_1 a \]  
(3.8-10)

**Crack length**

\[ \delta(c) = C_0 + C_1 c \]  
(3.8-11)

\[ \sigma(c) = D_0 + D_1 c \]  
(3.8-12)

where \( \delta(a) \) and \( \delta(c) \) are average errors, and \( \sigma(a) \) and \( \sigma(c) \) are standard deviations, as in Fig. 3.8-5. Cases 1 to Case 3 assume averaged error=0 and use only standard deviation as a parameter. Cases 4 to 7 assume fixed standard deviations and use averaged error as a parameter. Cases 8 to 10 assume fixed standard deviations and use averaged error as a parameter of a linear function of crack sizes. In addition, Cases 11 to 13 assume fixed averaged error and use standard deviation as a parameter of a linear function of crack sizes. As examples, Figs. 3.8-6 to 3.8-8 show sizing errors for Cases 3, 5 and 10.

For fracture assessment loads, two cases are assumed: a case of acting only primary stress and a case of acting mainly secondary stress represented by thermal expansion stress. In the case of piping, if elastic follow-up is large at an evaluated portion, thermal expansion stress may behave like primary stress, causing plastic collapse. Therefore, for conservative fracture assessment, the thermal expansion stress may be considered as primary stress. In LBB evaluation, such conservative assumption is also adopted [6]. If only primary stress is applied, a fracture assessment loads shall be as follows referring to allowable primary stress in the Design Codes [2] for Service Level-A and B.

\[ P_m = 0.5 S_m = 61.5 \ \text{[MPa]} \]

\[ P_{bg} = S_m = 123 \ \text{[MPa]} \]

where \( P_m \) is primary membrane stress, and \( P_{bg} \) is primary bending stress.

Stress conditions when secondary stress acts mainly were assumed as shown in Table 3.8-5. Here, primary membrane stress \( (P_m) \) is stress due to internal pressure and calculated from maximum allowable operation pressure of BWR assuming 9 MPa. Primary bending stress \( (P_{bg}) \) was assumed to be 5 MPa from the stress due to dead weight in actual piping systems. Secondary bending stress \( (Q_{bg}) \) is assumed from allowable secondary stress \( (3S_m) \) using stress index \( (C_2) \) for an elbow.

\[ Q_{bg} = \frac{3S_m}{C_2} \]  
(3.8-13)
\[ C_z = \frac{1.95}{h^{2/3}} \]

\[ h = \frac{tR}{r} \]

where \( R \) is an elbow bend radius, assuming a long elbow here. \( r \) is an average radius of piping. In Eqs. (3.8.1-2), the safety factor is considered only for primary stress and not for thermal expansion stress. Therefore, when considering stress conditions similar to actual piping system with large thermal expansion stress and small primary stress as in Table 3.8-5, it is expected that effect of crack sizing error may be larger.

**3.8.1.3.2 Evaluation results**  
Fig. 3.8-9 shows a time-dependent change of failure probability when repairing or replacing all the detected cracks considering only primary stress as a load for evaluating destruction. In this case, since PSI and every ten-year ISI are performed, an increase in failure probability during operation is not prominent. Table 3.8-6 summarizes conditional cumulative failure probabilities after 40 years (cumulative failure probability per crack). The failure probability is \( 1.4 \times 10^{-3} [/\text{crack}] \) for 4B piping, \( 5.6 \times 10^{-6} [/\text{crack}] \) for 16B piping, and \( 4.19 \times 10^{-10} [/\text{crack}] \) for 26B piping.

Figs. 3.8-10 to 3.8-12 show comparison between break probability (\( P_{B-NFS} \)) when all the detected cracks are repaired or replaced and break probability (\( P_{B-FS} \)) when operation is performed with the FFS Codes introduced and cracks determined to be sound in the evaluation permitted. Here, the safety factor (\( SF \)) in Eq. (3.8-2) was taken as a parameter in the range of 1.0 to 3.0. When introducing the FFS Codes, in a case of smaller safety factor as detection error increases, an increase of break probability due to crack sizing error is observed. Cases 7 to 10 exhibit such a relatively large effect. If the safety factor becomes higher than 2.0, however, break probabilities become roughly equivalent between a case of repairing or replacing all the cracks detected and a case of continuous operation with cracks. Comparing a small diameter pipe and a large diameter pipe indicates that the small diameter pipe needs a larger safety factor in general. Allowable stress as shown in Eq. (3.8-2) is a function of a crack angle (\( \theta \)) and relative crack depth (\( a/t \)). In addition, when introducing the FFS Codes, sizing error with respect to the remaining percentage of a ligament is more influenced for the small diameter piping with small thickness or piping perimeter, requiring a higher safety factor. Sizing error identified by the Nuclear and Industrial Safety Agency for SCC cracks of BWR’s PLR piping (austenitic stainless steel piping) which has different steel type is \( 2\sigma = 4.4 \text{ mm} \) [8]. In ferritic steel, if the same or higher level of crack sizing error is assumed, sizing error may be bounded by Case 3 (\( \sigma = 3 \text{ mm} \)). In this case, break probabilities become equivalent between a case of repairing or replacing all the cracks detected and a case of introducing the FFS Codes and permitting the crack determined to be safe in the evaluation, when the safety factor is 2.0 or higher for 4B piping, and 1.5 or higher for 16B and 26B. Thus, crack sizing error is covered with a safety factor 3.0 for in Service
Level-A and B and it can be seen that the crack sizing error does not affect adversely fatigue cracks in view of safety.

In the FFS Codes, allowable stress is defined considering a safety factor according to the Service Conditions as shown in Eq. (3.8-2). On the other hand, although a crack depth is limited to 75% of thickness for leakage, no safety factor is considered for the limit. It gives less effect on the safety of a plant as compared with break, leakage may cause an immeasurable economic loss once it occurs. Figs. 3.8-10 to 3.8-15 show comparison between leakage probability ($P_{L-NFS}$) when all the detected cracks are repaired without introducing the FFS Codes and leakage probability ($P_{L-FS}$) when the FFS Codes is introduced and cracks determined to be safe in the evaluation is permitted for operation. The figures indicate that the effect of crack sizing error on leakage probability is larger than the effect on the break probability. Assuming that the crack sizing error is an absolute value of crack sizes, smaller diameter piping with relatively smaller ligament for crack size in the evaluation similarly as breakage is more affected by the crack sizing error. However, considering that the crack sizing error can be bounded by Case 3 similarly as break, leakage probabilities become equivalent between a case of repairing or replacing all the cracks detected and a case of introducing the FFS Codes and permitting the crack determined to be safe in the evaluation, when the safety factor is 2.0 or higher for 4B piping, and 1.75 or higher for 16B and 26B. Thus, for leakage, if a safety factor for Service Level-A and B is 2.77, it can be seen that the crack sizing error is covered with the safety factor.

Next, Fig. 3.8-16 shows, in conditions in which secondary stress mainly acts as a load for evaluating fracture, a time-dependent change of failure probability when repairing or replacing all the detected cracks. In the case where secondary stress acts mainly, Fig. 3.8-17 to Fig. 3.8-22 show comparison between break probability when all the detectable cracks are repaired or replaced ($P_{B-NFS}$) and break probability when operation is performed with the FFS Codes introduced and crack determined to be sound in the evaluation period ($P_{B-FS}$). Table 3.8-7 summarizes conditional cumulative break probabilities after 40 years. Crack growth load, is the same as a case when only primary stress acts as a load for evaluating fracture shown in Fig. 3.8-9. Since the secondary stress acts mainly as a load for evaluating fracture, the break probability is smaller by one digit (26B piping) to semi-digit (4B piping) or around compared with the case when only the primary stress acts. Under the influence of lower break probability, leakage probability contrarily increases slightly as compared to the case when only the primary stress acts. Effect of crack sizing error is significantly manifested as piping diameter is smaller. If thermal expansion stress not considering a safety factor mainly acts, a failure probability considering crack sizing error increases and does not decrease even when the safety factor is set larger. These results suggest that an action of suppressing crack sizing error smaller for maintaining higher reliability when secondary stress is an actor or of taking into account sizing error when evaluating crack using the FFS Codes is required. In a piping
system, since excessive thermal expansion displacement is limited by supporting structures, failure is not generated alone, and thus it should be noted that handling by standards and actual failure behavior may be different.

Finally, the effect of pre-expecting sizing error when evaluating crack is considered. Assuming sizing error $2\sigma = 4.4\text{mm}$ for SCC crack of austenitic stainless steel identified by the Nuclear and Industrial Safety Agency, a failure probability was calculated with a margin of $4.4\text{ mm}$ for observed crack sizes. Table 3.8-8 shows comparison between break and leakage probabilities when all the detected cracks are repaired without introducing the FFS Codes and, those when the FFS Codes is introduced and cracks determined to be safe in the evaluation is permitted for operation. As shown in table, by considering crack sizing error in the evaluation, break probability is the same level as a case when all the detected cracks are repaired. For leakage, the probability slightly increases in larger diameter piping. This is because a margin of $4.4\text{ mm}$ is $2\sigma$ points and cracks exceeding these may be affecting. However, a leakage probability of large diameter piping is about two digits smaller than that of small diameter piping, and thus no substantial effect is considered on reliability of piping. These results indicate that when the FFS Codes is applied, the same level of reliability as in the conventional operation in which all the detected cracks are repaired can be assured by identifying crack sizing error and performing evaluation taking the error into account.

3.8.1.4 Summary Crack sizing error for failure probability when operation is performed while permitting cracks in piping of a nuclear power plant by introducing the FFS Codes, and safety factor considered in the FFS Codes were examined. In the examination, error ($2\sigma = 4.4\text{mm}$) for SCC crack in PLR piping of BWR was targeted as crack sizing error.

If any stress acting on piping is assumed to be the primary stress, the crack sizing error is covered by safety factor 2.77 for Service Level-A and B held in the FFS Codes, and break and leakage probabilities are the same level of those as in a case when all the detected cracks are all repaired.

As a condition near to a load in an actual plant, if it is assumed that the secondary stress mainly acts, since in the FFS Codes, break and leakage probabilities increase than in a case when all the detected cracks are repaired. This result suggests that a safety factor in the FFS Codes does not cover crack sizing error depending on the load conditions. In a piping system, since excessive thermal expansion displacement is limited by supporting structures, failure does not occur alone, and thus it can be said by analogy that handling by standards and actual failure behavior may be different. In such a case, if sizing error is considered in advance in crack evaluation, break and leakage probabilities are at the same level as in a case when all the detected cracks are repaired.

This study identified that by anticipating in advance sizing error in crack evaluation, even in the case of applying the FFS Codes and performing operations with cracks...
permitted, the same level of reliability could be assured as in the case when all the detected cracks were repaired in conventional plant operations.

REFERENCES


### Table 3.8-1 Analysis target piping sizes

<table>
<thead>
<tr>
<th>Pipe</th>
<th>4B</th>
<th>16B</th>
<th>26B</th>
</tr>
</thead>
<tbody>
<tr>
<td>Diameter [mm]</td>
<td>114.3</td>
<td>406.4</td>
<td>660.4</td>
</tr>
<tr>
<td>Thickness [mm]</td>
<td>11.1</td>
<td>26.2</td>
<td>33.3</td>
</tr>
</tbody>
</table>

### Table 3.8-2 Physical properties of piping for analysis

<table>
<thead>
<tr>
<th>Items</th>
<th>Values</th>
</tr>
</thead>
<tbody>
<tr>
<td>Design yield strength</td>
<td>$S_y = 183\text{[MPa]}$</td>
</tr>
<tr>
<td>Design ultimate strength</td>
<td>$S_u = 404\text{[MPa]}$</td>
</tr>
<tr>
<td>Flow stress</td>
<td>$S_f = \frac{S_y + S_u}{2} = 293\text{[MPa]}$</td>
</tr>
<tr>
<td>Design stress intensity</td>
<td>$S_m = 123\text{[MPa]}$</td>
</tr>
<tr>
<td>Modulus of elasticity</td>
<td>$E = 185\text{[GPa]}$</td>
</tr>
</tbody>
</table>

### Table 3.8-3 Load conditions of crack growth analysis

<table>
<thead>
<tr>
<th></th>
<th></th>
<th></th>
<th></th>
</tr>
</thead>
<tbody>
<tr>
<td></td>
<td></td>
<td>Min.</td>
<td>Max.</td>
</tr>
<tr>
<td>1</td>
<td>7</td>
<td>0</td>
<td>123</td>
</tr>
<tr>
<td>2</td>
<td>18</td>
<td>49.2</td>
<td>184.5</td>
</tr>
<tr>
<td>3</td>
<td>320</td>
<td>92.2</td>
<td>123</td>
</tr>
<tr>
<td>4</td>
<td>8</td>
<td>0</td>
<td>0</td>
</tr>
<tr>
<td>5</td>
<td>16</td>
<td>0</td>
<td>0</td>
</tr>
<tr>
<td>6</td>
<td>330</td>
<td>0</td>
<td>0</td>
</tr>
</tbody>
</table>
Table 3.8-4 Parameters of crack sizing error

<table>
<thead>
<tr>
<th>Case</th>
<th>0</th>
<th>1</th>
<th>2</th>
<th>3</th>
<th>4</th>
<th>5</th>
<th>6</th>
</tr>
</thead>
<tbody>
<tr>
<td>A0</td>
<td>0.0</td>
<td>0.0</td>
<td>0.0</td>
<td>0.0</td>
<td>-1.0</td>
<td>-2.0</td>
<td></td>
</tr>
<tr>
<td>A1</td>
<td>1.0</td>
<td>1.0</td>
<td>1.0</td>
<td>1.0</td>
<td>1.0</td>
<td>1.0</td>
<td>1.0</td>
</tr>
<tr>
<td>B0</td>
<td>1.0</td>
<td>2.0</td>
<td>3.0</td>
<td>1.5</td>
<td>1.5</td>
<td>1.5</td>
<td>1.5</td>
</tr>
<tr>
<td>B1</td>
<td>0.0</td>
<td>0.0</td>
<td>0.0</td>
<td>0.0</td>
<td>0.0</td>
<td>0.0</td>
<td>0.0</td>
</tr>
<tr>
<td>C0</td>
<td>0.0</td>
<td>0.0</td>
<td>0.0</td>
<td>0.0</td>
<td>-1.0</td>
<td>-2.0</td>
<td></td>
</tr>
<tr>
<td>C1</td>
<td>1.0</td>
<td>1.0</td>
<td>1.0</td>
<td>1.0</td>
<td>1.0</td>
<td>1.0</td>
<td>1.0</td>
</tr>
<tr>
<td>D0</td>
<td>1.0</td>
<td>2.0</td>
<td>3.0</td>
<td>1.5</td>
<td>1.5</td>
<td>1.5</td>
<td>1.5</td>
</tr>
<tr>
<td>D1</td>
<td>0.0</td>
<td>0.0</td>
<td>0.0</td>
<td>0.0</td>
<td>0.0</td>
<td>0.0</td>
<td>0.0</td>
</tr>
</tbody>
</table>

Table 3.8-5 Load conditions when mainly assuming secondary stress

<table>
<thead>
<tr>
<th>Stress</th>
<th>Stress [MPa]</th>
</tr>
</thead>
<tbody>
<tr>
<td></td>
<td>4B</td>
</tr>
<tr>
<td>$P_m$</td>
<td>23.2</td>
</tr>
<tr>
<td>$P_{bg}$</td>
<td>5.0</td>
</tr>
<tr>
<td>$Q_{bg}$</td>
<td>139.4</td>
</tr>
</tbody>
</table>

*: Fitness for Service
Table 3.8-6 Cumulative failure probability after 40 years when considering only primary stress

<table>
<thead>
<tr>
<th>Pipe</th>
<th>4B</th>
<th>16B</th>
<th>26B</th>
</tr>
</thead>
<tbody>
<tr>
<td>Leak probability</td>
<td>8.73×10^{-3}</td>
<td>1.33×10^{-4}</td>
<td>3.8.1×10^{-5}</td>
</tr>
<tr>
<td>Break probability</td>
<td>1.37×10^{-3}</td>
<td>5.59×10^{-6}</td>
<td>4.19×10^{-7}</td>
</tr>
</tbody>
</table>

Table 3.8-7 Cumulative failure probability after 40 years when mainly considering secondary stress

<table>
<thead>
<tr>
<th>Pipe</th>
<th>4B</th>
<th>16B</th>
<th>26B</th>
</tr>
</thead>
<tbody>
<tr>
<td>Leak probability</td>
<td>9.17×10^{-3}</td>
<td>1.37×10^{-4}</td>
<td>3.95×10^{-5}</td>
</tr>
<tr>
<td>Break probability</td>
<td>7.33×10^{-4}</td>
<td>1.11×10^{-6}</td>
<td>4.22×10^{-8}</td>
</tr>
</tbody>
</table>

Table 3.8-8 Cumulative failure probability after 40 years operation in consideration of crack sizing error

<table>
<thead>
<tr>
<th>Pipe</th>
<th>Repair all cracks</th>
<th>SF=1.5</th>
<th>SF=3.0</th>
</tr>
</thead>
<tbody>
<tr>
<td></td>
<td>Leak</td>
<td>Break</td>
<td>Leak</td>
</tr>
<tr>
<td>4B</td>
<td>9.17×10^{-5}</td>
<td>7.33×10^{-4}</td>
<td>9.20×10^{-3}</td>
</tr>
<tr>
<td>16B</td>
<td>1.37×10^{-4}</td>
<td>1.11×10^{-6}</td>
<td>1.42×10^{-4}</td>
</tr>
<tr>
<td>26B</td>
<td>3.95×10^{-5}</td>
<td>4.22×10^{-8}</td>
<td>4.33×10^{-5}</td>
</tr>
</tbody>
</table>
(Carbon steel pipe, 150A×10t, 600A×50t, fatigue crack)

Fig. 3.8-1 Sizing error of carbon steel piping by tip echo technique of UTS project [3, 4]

Average error $X : -0.20$
Standard deviation $\sigma : \pm 0.86$
RMS error: 0.88
Correlation coefficient: 0.81

(Stainless steel pipe, 150A×10t, 600A×35t, fatigue crack)

Fig. 3.8-2 Sizing error of stainless steel piping by tip echo technique of UTS project [3, 4]
### Fig. 3.8-3 Sizing error of stainless steel piping by tip echo technique of UTS project [3, 4]

<table>
<thead>
<tr>
<th>Crack depth [mm]</th>
<th>Crack indication depth [mm]</th>
</tr>
</thead>
<tbody>
<tr>
<td>0</td>
<td>0</td>
</tr>
<tr>
<td>1</td>
<td>1.65</td>
</tr>
<tr>
<td>2</td>
<td>3.3</td>
</tr>
<tr>
<td>3</td>
<td>4.95</td>
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<tr>
<td>4</td>
<td>6.6</td>
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<tr>
<td>5</td>
<td>8.25</td>
</tr>
<tr>
<td>6</td>
<td>10</td>
</tr>
<tr>
<td>7</td>
<td>11.75</td>
</tr>
<tr>
<td>8</td>
<td>13.5</td>
</tr>
<tr>
<td>9</td>
<td>15.25</td>
</tr>
<tr>
<td>10</td>
<td>17</td>
</tr>
</tbody>
</table>

Average error: -0.85
Standard deviation: ±1.50
RMS error: 1.72
Correlation coefficient: 0.56
Regression constant: 0.43

### Fig. 3.8-4 PFM analysis flowchart considering structural integrity evaluation according to FFS Codes

1. **Failure assessment**
   - Break? **YES**
   - **NO**
2. **Probability of failure** $P_{f}$
3. **Probability of detection** $P_{OD}$
4. **Recognized size** $a_{R}$
5. **Applied Load** $P_{applied}$
6. **Limit Load** $P_{lim}$
7. **Crack growth** $\Delta a$
8. **Repair**

Renewal of crack existing probability $P_{r} = P_{l} \times (1 - P_{OD})$
Fig. 3.8-5 Method of providing crack sizing error

\[ B_1 = (B - B_0) / x \]

\[ B: \text{standard deviation at crack size } x \]

Fig. 3.8-6 Example of crack sizing error (Case 3)

\[ \text{Observed crack size} \quad \text{Actual crack size} \]

\[ \text{Mean} \quad \text{Standard deviation} \]
Fig. 3.8-7 Example of crack sizing error (Case 5)

Fig. 3.8-8 Example of crack sizing error (Case 10)
Fig. 3.8-9 Cumulative failure probability where only primary stress is applied
(\(\text{\textendash}\): Brea, \(\text{\textendash}\): Leakage, \(\text{\textbullet}\): 4B, \(\square\): 16B, \(\triangle\): 26B)

Fig. 3.8-10 Relation between break probability and safety factor of the FFS Codes (4B piping, when only primary stress is applied)
Fig. 3.8-11 Relation between break probability and safety factor of the FFS Codes (16B piping, when only primary stress is applied)

Fig. 3.8-12 Relation between break probability and safety factor of the FFS Codes (26B piping, when only primary stress is applied)
Fig. 3.8-13 Relation between leakage probability and safety factor of the FFS Codes (4B piping, when only primary stress is applied)

Fig. 3.8-14 Relation between leakage probability and safety factor of the FFS Codes (16B piping, when only primary stress is applied)
Fig. 3.8-15 Relation between leakage probability and safety factor of the FFS Codes (26B piping, when only primary stress is applied)

Fig. 3.8-16 Cumulative failure probability where mainly secondary stress is applied

(—: Breakage, —: Leakage, ○: 4B, □: 16B, △: 26B)
Fig. 3.8-17 Relation between break probability and safety factor of the FFS Codes (4B piping, when mainly secondary stress is applied)

Fig. 3.8-18 Relation between break probability and safety factor of the FFS Codes (16B piping, when mainly secondary stress is applied)
Fig. 3.8-19 Relation between break probability and safety factor of the FFS Codes (26B piping, when mainly secondary stress is applied)

Fig. 3.8-20 Relation between leakage probability and safety factor of the FFS Codes (4B piping, when mainly secondary stress is applied)
Fig. 3.8-21 Relation between leakage probability and safety factor of the FFS Codes (16B piping, when mainly secondary stress is applied)

Fig. 3.8-22 Relation between leakage probability and safety factor of the FFS Codes (26B piping, when mainly secondary stress is applied)
3.8.2 Effect of crack detection performance and sizing accuracy on reliability of piping with stress corrosion cracks

Many stress corrosion cracks have been observed in wild joints of PLR piping of Japanese BWR plants from around 2000. Stress corrosion cracks initiate in heat affected zone (HAZ) and grow into weld metal. Since HAZ and weld metal have different metallurgical microstructure, detection of stress corrosion cracks is difficult because of high noise from weld boundary in the echoes of ultrasonic test (UT), and some oversight experiences of comparatively large stress corrosion cracks were reported. Performance of crack detection is greatly dependent on the inspection tools and an inspector's skill, accessibility, limited inspection time in a radioactive area, etc. Machida [1] evaluated the effect of crack detection performance on a single stress corrosion crack, and reported that oversight of the crack is the most important from a viewpoint of reliability of piping. However, the number of stress corrosion cracks observed in a weld joint is not unity in many cases, and it is needed to take into consideration initiation and growth of multiple circumferential cracks in the realistic reliability assessments of PLR piping. Machida et al. [2] developed a PFM analysis code which can analyze initiation, growth of stress corrosion cracks and failure probability of pipes with multiple circumferential cracks. In this study [3], the influence of NDT performance on failure probability of PLR piping is evaluated using the developed PFM code.
3.8.2.1 **Nomenclature**

- \( a \): Crack depth
- \( a_d \): Detectable crack depth
- \( c \): Crack half length
- \( C_{SCC} \): Coefficient of SCC growth rate
- \( d_c \): Distance from inner surface to the position where stress corrosion cracks grow into weld metal
- \( K_{\text{min}} \): Minimum stress intensity factor
- \( K_{\text{max}} \): Maximum stress intensity factor
- \( L \): Distance from weld joint to a crack
- \( m_{SCC} \): Exponent of SCC growth rate
- \( R \): Stress ratio \( = \frac{K_{\text{min}}}{K_{\text{max}}} \)
- \( POD \): Probability of detection of a crack
- \( S_U \): Design ultimate tensile strength
- \( T \): Temperature
- \( t_r \): Loading time
- \( \alpha \): Parameter of \( d_c \)
- \( \Delta K \): Stress intensity factor range
- \( \mu_{C_{\text{HAZ}}} \): Mean of coefficient of SCC growth law in HAZ
- \( \mu_{C_{\text{WM}}} \): Mean of coefficient of SCC growth law in weld metal
- \( \mu_L \): Mean of distance from weld metal to stress corrosion cracks
- \( \mu_{\sigma_f} \): Mean of flow stress
- \( \mu_{\sigma_U} \): Mean of ultimate tensile strength
- \( \mu_{\sigma_Y} \): Mean of yield stress
- \( \sigma_{C_{\text{HAZ}}} \): Standard deviation of coefficient of SCC growth rate in HAZ
- \( \sigma_{C_{\text{WM}}} \): Standard deviation of coefficient of SCC growth rate in weld metal
- \( \sigma_L \): Standard deviation of distance from weld metal to stress corrosion cracks
- \( \sigma_{\text{Sizing}} \): Standard deviation of crack sizing error in NDT
- \( \sigma_{\sigma_f} \): Standard deviation of flow stress
- \( \sigma_{\sigma_U} \): Standard deviation of ultimate tensile strength
- \( \sigma_{\sigma_Y} \): Standard deviation of yield stress
3.8.2.2 PFM Code  To evaluate reliability of pipes with stress corrosion cracks, initiation, growth and coalescence of multiple cracks should be considered. The conventional PFM analysis technique—many samples of time historical analyses for a single crack are performed and failure probability of each sample is accumulated—is not suitable for the multiple stress corrosion cracks. Then, a PFM analysis code named “PEPPER-M, Probabilistic Evaluation Program for Pipe aiming Economical and Reliable design for Multiple cracks” has been developed [2]. PEPPER-M can evaluate initiation of cracks and growth behavior considering coalescence of them, and can evaluate failure behavior for pipes with multiple circumferential cracks. In the developed PFM code, the following parameters are taken into account as the probability density functions:

- The number of initiation cracks
- Initiation time of a crack
- Initial crack length
- Angular position of a crack
- Distance from a weld joint to a crack
- Crack depth at reaching weld metal
- Crack growth rate
- Flaw stress

Initial crack depth assumes a constant value in this study. Failure probability of piping has very high sensitivity to the crack growth rate when the growth of crack is governed by stress corrosion cracking (SCC), because the crack growth rate due to SCC is very high and it has large variations. Therefore, the stratified Monte Carlo simulation method is applied to the sampling of crack growth rate due to SCC for analyzing failure probability efficiently.

3.8.2.3 Crack Initiation Model  In order to perform PFM analysis for the pipe with stress corrosion cracks, the appropriate evaluation of the crack initiation process due to SCC is indispensable. The following probabilistic data are required for the crack initiation model:

- The number of initiated cracks in a weld joint
- Initiation time of a crack
- Initial crack length / aspect ratio

In this study, the above conditions are determined based on the observed data from Japanese BWR plants. Sufficient numbers of statistical data are obtained as follows:

- The number of inspected plants: 23
- The number of inspected weld joints: 1277
- Total of initiated cracks: 166

There are some very long cracks in the observed data, and these cracks are estimated that some short cracks coalesced before detection. Therefore, long cracks over 100mm are divided below 100mm in length, and each crack is counted up the number of initiated cracks (above-mentioned c). Distribution of the number of initiated cracks per a weld joint is expressed by the lognormal distribution as shown in Fig. 3.8-23. The number of cracks
per a weld joint is unity in many cases, and the maximum is fifteen. Parameters of the lognormal distribution are as follows:

Mean: 2.19
Standard deviation: 0.873

Stress corrosion cracks are not detected in almost all weld joints and the weld joints in which cracks initiate are few in percentage. The percentage of cracked weld joints per a plant in 12, 16 and 24 in. pipes (riser, header and main pipes) are assumed to be 0.8%, 1.7% and 4.1%, respectively [5].

The crack size observed by in-service inspection (ISI) is not the crack size just after initiation, and the initial crack size can be counted backward using the observed data. Here, the initial crack depth is assumed to be 0.5mm, and distribution of initial crack length is evaluated. When the observed crack length is smaller than 100mm, its origin is assumed a single crack. Since the long cracks over 100mm are supposed that contiguous cracks coalesced during growth before detected, they are divided into some cracks shorter than 100mm. There are also many short cracks in the observed data. When the aspect ratio \((a/c)\) of observed cracks are smaller than 2.0, the initiated crack length \((2c)\) is assumed 1.0mm (initiated aspect ratio of a crack is \(a/c = 1.0\)), because the initial crack length counted backward simply might be extremely short length, and it is unexpected. The initial crack length distribution is estimated as shown in Fig. 3.8-24. This distribution is fitted by the exponential distribution with mean of 14mm.

The crack initiation time can be found by subtracting the crack growth time from the plant operation time when crack is detected. Distribution of the crack initiation time is expressed by the lognormal distribution as shown in Fig. 3.8-25, and mean of the crack initiation time is estimated 9.21 year.

![Fig. 3.8-23 The number of cracks initiated per a weld joint.](image)
3.8.2.4 Crack Growth Model

The crack growth rates due to fatigue and SCC are given by the following formulae [4]:

**<Fatigue>**

\[
\frac{da}{dN} = 8.17 \times 10^{-9} \cdot t_r^{0.5} \cdot \frac{\Delta K^{3.0}}{(1-R)^{1.56}}
\]

\[
\Delta K = K_{\text{max}} - K_{\text{min}} \quad (R \geq 0)
\]

\[
\Delta K = K_{\text{max}} \quad (R < 0)
\]

\[
t_r = 1000 \quad \text{(When the strain rate cannot be defined.)}
\]

**<SCC>**

\[
\frac{da}{dt} = C_{\text{SCC}} \cdot K^{m_{\text{SCC}}}
\]

The variation in fatigue crack growth rate can be ignored, because SCC will dominate crack growth behavior in PLR piping. The exponent \(m_{\text{SCC}}\) is assumed to be constant value, and the variation in the coefficient \(C_{\text{SCC}}\) is taken into consideration in both HAZ and weld metal. These variations are expressed by the lognormal distribution, and parameters of them are as follows:
\[<\text{HAZ}>[1]\]

Mean: \( \mu_{C_{\text{HAZ}}} = 9.018 \times 10^{-14} \)

Standard deviation: \( \sigma_{C_{\text{HAZ}}} = 0.1537 \)

\[<\text{Weld metal}>[1]\]

Mean: \( \mu_{C_{\text{WMM}}} = 1.017 \times 10^{-14} \)

Standard deviation: \( \sigma_{C_{\text{WMM}}} = 0.3952 \)

Since crack growth rate in weld metal differs from that in HAZ greatly, a distance from a weld joint to a crack and crack depth reaching weld metal are very important for estimation of the crack growth behavior. These distributions are estimated based on the observed data in Japanese BWR plants [5]. Distribution of the distance from a weld joint to a crack is expressed with the lognormal distribution as shown in Fig. 3.8-26, and mean and standard deviation are as follows:

Mean: \( \mu_L = 1.15 \text{[mm]} \)

Standard deviation: \( \sigma_L = 1.39 \)

Generally, stress corrosion cracks initiate in HAZ, and grow into weld metal (see Fig. 3.8-27). The crack depth reaching weld metal is expressed by the following simple formula:

\[
d_c = L + \alpha
\]  

\((3.8-16)\)

where \( \alpha \) is expressed by the normal distribution, and its parameters are expressed as follows:

Mean: \( \mu_\alpha = 2.99 \text{[mm]} \)

Standard deviation: \( \sigma_\alpha = 1.31 \text{[mm]} \)

Fig. 3.8-26  Distance from a weld joint to stress corrosion cracks.

Fig. 3.8-27  Schematic of crack growth due to SCC.
3.8.2.5 Crack Stability Assessment Model  

Limit load analysis is applied to crack stability assessment (failure criterion), because the material of PLR piping (SUS316LC in JIS Standard) has high ductility and toughness. Flow stress is calculated as the average of ultimate tensile strength and yield stress. Mean of the tensile strength ($\mu_{\sigma_u}$) is given by the following formulae [6]:

$$\mu_{\sigma_u} = A_0 + A_1 \cdot T + A_2 \cdot T^2 + A_3 \cdot T^3 + A_4 \cdot T^4 + A_5 \cdot T^5 \quad (3.8-17)$$

- $A_0 = 5.646$  
- $A_1 = -12.11$  
- $A_2 = 4.285 \times 10^{-2}$  
- $A_3 = -0.6583 \times 10^{-5}$  
- $A_4 = 0.4505 \times 10^{-8}$  
- $A_5 = -0.2108 \times 10^{-11}$

Using Eq. (3.8-17), mean of the ultimate tensile stress at maximum operation temperature (302°C) becomes as follows:

$$\mu_{\sigma_u} = 441 \text{ [MPa]}$$

Assuming the design ultimate tensile strength [7] is 95% lower confidence level, the standard deviation of the ultimate tensile strength can be found from the following relations:

$$\sigma_{\sigma_u} = \frac{1}{1.96} \left( \mu_{\sigma_u} - S_U \right) = \frac{441 - 427}{1.96} = 14 \text{ [MPa]} \quad (3.8-18)$$

$$S_U = 427 \text{ [MPa]}$$

Mean of the yield stress is given by the following formulae [8]:

$$\mu_{\sigma_y} = \sigma_{Y,RT} \bar{\gamma}_Y \quad (3.8-19)$$

- $\bar{\gamma}_Y = 1.036 - 1.948 \times 10^{-3} T + 3.104 \times 10^{-6} T^2 - 1.974 \times 10^{-9} T^3$
- $\sigma_{Y,RT} = 254 \text{ [MPa]}$
- $\alpha_Y = 1.29$

Mean of the yield stress at the maximum operation temperature is as follows:

$$\mu_{\sigma_y} = 172 \text{ MPa}$$

Assuming the design yield stress [7] is 95% lower confidence level, standard deviation of the yield stress can be found from the following relations:

$$\sigma_{\sigma_y} = \frac{1 - \frac{1}{1.96} \alpha_Y}{\mu_{\sigma_y}} \mu_{\sigma_y} = \frac{1 - \frac{1}{1.29}}{1.96} \cdot 172 = 20 \text{ MPa} \quad (3.8-20)$$

Mean and standard deviation of the flow stress are also assumed to be the average of yield stress and ultimate tensile strength, and are calculated as follows:
Mean: $\mu_{\sigma_f} = \frac{\mu_{\sigma_{uy}} + \mu_{\tau_y}}{2} = 307 \text{ [MPa]}$

Standard deviation: $\sigma_{\sigma_f} = \frac{\sigma_{\sigma_{uy}} + \sigma_{\tau_y}}{2} = 17 \text{ [MPa]}$

3.8.2.6 Non-Destructive Test Model

When defects are detected in the weld joints by ISI, integrity of the pipes will be assessed according to the FFS Codes considering growth of cracks during a postulated evaluation period. Based on Japanese recent study [9], probability of detection (POD) for stress corrosion cracks by UT is expressed by the following formula [1]:

$$POD(a) = 1 - \exp\left(\frac{-a - 0.3434}{\mu_{\sigma_{uy}} + \mu_{\tau_y}}\right)$$

(3.8-21)

The crack sizing error in UT is expressed by the normal distribution with 1.47mm of standard deviation ($\sigma_{\text{Sizing}}$) when the crack size is measured by the inspectors qualified by performance demonstration (PD) system. This standard deviation is determined that the allowable sizing error (4.4mm) to pass the qualify test in PD system is $3\sigma$ point.

$$\sigma_{\text{Sizing}} = \frac{4.4}{3} = 1.47 \text{ [mm]}$$

(3.8-22)

The standard deviation of crack sizing error performed by nonqualified inspectors is assumed 2.2mm based on the past data relating UT performance for stress corrosion cracks [10].
### 3.8.2.7 PFM Analysis for PLR Piping

#### 3.8.2.7.1 Base Case Analysis

Failure probability of PLR piping is analyzed using the developed PFM code [2]. Analyzed pipes are summarized in Table 3.8-9. The number of calculation samples is set to 106, and failure probabilities during 40 years operation are analyzed. Inservice inspection will be carried out for 100% of weld joints every 5 years (this condition is a requirement by Nuclear and Industrial Safety Agency, NISA). Stresses at normal operating condition and weld residual stress are used in the PFM analysis. Stresses shown in Table 3.8-10 are conditioned so as to envelop the stresses of all the weld joints in typical PLR piping. Weld residual stress shown in Fig. 3.8-28 and normal operating loads shown in Table 3.8-10 are used in the crack growth analysis due to SCC. Startup and shutdown of a plant at yearly interval are taken into consideration in the crack growth analysis due to fatigue, and alternating thermal expansion and internal pressure loads are given in the analysis.

The failure probabilities under the above-mentioned conditions are shown in Fig. 3.8-29. The frequencies of failure for all sizes of pipes are very low as shown in Table 3.8-11; consequently the present ISI program is well enough to maintain the reliability of piping at the suitable safety level. When multiple circumferential cracks are assumed, the percentage of very long cracks or fully circumferential cracks is comparatively high. Consequently, break probabilities of these cases are very high compared with the cases of a single crack [1].

PEPPER-M employs the stratified Monte Carlo simulation for the crack growth rate due to SCC. The correlation between crack growth rate due to SCC and failure probability is shown in Fig. 3.8-30. As shown in the figure, failure occurs when the crack growth rate due to SCC is higher than around $\mu + 3\sigma$, and this crack growth rate exceeds that defined in FFS Codes. This result shows that the successive inspections are essential to confirm validity of the crack growth estimation in the integrity assessments according to FFS Codes. Similarly, it is suggested that expand of data for crack growth rate and increase in accuracy of crack growth estimation should be indispensable.

<table>
<thead>
<tr>
<th>Pipe size [in.]</th>
<th>Outer diameter [mm]</th>
<th>Thickness [mm]</th>
</tr>
</thead>
<tbody>
<tr>
<td>24</td>
<td>625.4</td>
<td>38.5</td>
</tr>
<tr>
<td>16</td>
<td>416.0</td>
<td>26.2</td>
</tr>
<tr>
<td>12</td>
<td>326.5</td>
<td>21.4</td>
</tr>
</tbody>
</table>

<table>
<thead>
<tr>
<th>Items</th>
<th>Loading condition</th>
</tr>
</thead>
<tbody>
<tr>
<td>Internal pressure</td>
<td>9 MPa</td>
</tr>
<tr>
<td>Stress due to dead weight</td>
<td>10 MPa</td>
</tr>
<tr>
<td>Stress due to thermal expansion</td>
<td>40 MPa</td>
</tr>
</tbody>
</table>

Table 3.8-9 Analyzed pipes

Table 3.8-10 Normal operating loads
Fig. 3.8-28  Weld residual stress distribution in a pipe.

Fig. 3.8-29  Failure frequency during plant operation.

Fig. 3.8-30  Distribution of failure probability within the cells to crack growth rate.

Table 3.8-11  Cumulative failure probability per a weld joint.

<table>
<thead>
<tr>
<th>Pipe size [in.]</th>
<th>Failure mode</th>
<th>Failure probability [w.j.⁻¹]</th>
</tr>
</thead>
<tbody>
<tr>
<td>12</td>
<td>Leak</td>
<td>1×10⁻⁶</td>
</tr>
<tr>
<td></td>
<td>Break</td>
<td>2×10⁻⁶</td>
</tr>
<tr>
<td>16</td>
<td>Leak</td>
<td>8×10⁻⁷</td>
</tr>
<tr>
<td></td>
<td>Break</td>
<td>3×10⁻⁷</td>
</tr>
<tr>
<td>24</td>
<td>Leak</td>
<td>1×10⁻⁷</td>
</tr>
<tr>
<td></td>
<td>Break</td>
<td>1×10⁻⁶</td>
</tr>
</tbody>
</table>
3.8.2.7.2 Influence of Sizing Error  PD system is introduced into sizing of stress corrosion cracks in austenitic stainless steel piping, and the qualified inspectors might measure the depth of cracks due to SCC in PLR piping at present. In the structural integrity assessments according to FFS Codes, sizing error is excluded when the measurement of crack depth is performed by the qualified inspectors, but $4.4\, \text{mm} \, (\mu + 2\sigma)$ must be added to the measured depth for the case of unqualified inspectors [5]. Then, the influence of sizing error on the reliability of piping is analyzed. In these analyses, the following three kinds of standard deviation are taken into consideration as sizing error in crack depth.

a) $\sigma_{\text{Sizing}} = 1.47\, \text{mm} : \text{Qualified inspectors}

b) $\sigma_{\text{Sizing}} = 2.2\, \text{mm} : \text{Unqualified inspectors}

c) $\sigma_{\text{Sizing}} = 4.4\, \text{mm} : \text{The worst case (reference)}$

Table 3.8-12 shows the cumulative failure probabilities at 40 years operation. As shown in the table, the failure probability of a 24 in. pipe hardly changes even if the sizing error varies, and the effect of sizing error is negligible in this case, while the effect of sizing error is clearly shown in the cases of 12 and 16 in. pipes. This is because the crack growth rates of these pipes are very high and crack sizing error lead to the error of predicted crack size. The effectiveness of the inspection by qualified inspectors is clear from these results. Therefore, measurement of crack size by qualified inspectors is essential to maintain reliability of the PLR piping at a suitable safety level.

<table>
<thead>
<tr>
<th>Failure prob. [w.j.⁻¹]</th>
<th>Sizing error, $\sigma_{\text{Sizing}}$ [mm]</th>
<th>Pipe size [in.]</th>
</tr>
</thead>
<tbody>
<tr>
<td></td>
<td></td>
<td>12</td>
</tr>
<tr>
<td>Leak</td>
<td>1.47</td>
<td>$1 \times 10^6$</td>
</tr>
<tr>
<td></td>
<td>2.2</td>
<td>$4 \times 10^6$</td>
</tr>
<tr>
<td></td>
<td>4.4</td>
<td>$1 \times 10^5$</td>
</tr>
<tr>
<td>Break</td>
<td>1.47</td>
<td>$2 \times 10^6$</td>
</tr>
<tr>
<td></td>
<td>2.2</td>
<td>$8 \times 10^6$</td>
</tr>
<tr>
<td></td>
<td>4.4</td>
<td>$2 \times 10^5$</td>
</tr>
</tbody>
</table>
3.8.2.7.3 Influence of Oversight in ISI  In addition to the above-mentioned crack sizing error, influence of oversight of cracks cannot be negligible in NDT. The oversight percentage is assumed in the range of 0 to 2%, and its influence on failure probability is analyzed. Table 3.8-13 shows the cumulative failure probabilities considering oversight in ISI at 40 years operation. In PEPPER-M, probability of non-detection (PND) is given by the following formula:

\[
\sum_{i=1}^{n} PND = (1 - POD_1)(1 - POD_2) \cdots (1 - POD_n) \tag{3.8-23}
\]

Therefore, the percentage of overlooked cracks decreases with increasing the number of inspections, in other words, with decreasing crack growth rate. Similar to the effect of crack sizing error, the effect of oversight in a 24 in. pipe is negligibly small, while the effect appears on failure probability in 12 and 16 in. pipes of which the crack growth rate is high and the number of inspections is small. Although the failure probabilities of these pipes increase with increasing oversight percentage, the degrees of them are as comparable as the influences by the sizing error. In order to keep the reliability of pipes with stress corrosion cracks, decreasing the frequency of oversight is very important.

<table>
<thead>
<tr>
<th>Failure mode [w.j.]</th>
<th>Oversight percentage [%]</th>
<th>Pipe size [in.]</th>
</tr>
</thead>
<tbody>
<tr>
<td></td>
<td></td>
<td>12</td>
</tr>
<tr>
<td>Leak</td>
<td></td>
<td>0</td>
</tr>
<tr>
<td></td>
<td>0.5</td>
<td>2×10^{-6}</td>
</tr>
<tr>
<td></td>
<td>1.0</td>
<td>2×10^{-6}</td>
</tr>
<tr>
<td></td>
<td>2.0</td>
<td>3×10^{-6}</td>
</tr>
<tr>
<td>Break</td>
<td></td>
<td>0</td>
</tr>
<tr>
<td></td>
<td>0.5</td>
<td>4×10^{-6}</td>
</tr>
<tr>
<td></td>
<td>1.0</td>
<td>5×10^{-6}</td>
</tr>
<tr>
<td></td>
<td>2.0</td>
<td>7×10^{-6}</td>
</tr>
</tbody>
</table>
3.8.2.7.4 Influence of Minimum Detectable Flaw Depth  In the integrity assessment of flawed pipes according to FSS Codes, NDT performance has high influence on the results. In order to determine the inspection skill or tools used in NDT, the influences of detectable crack depth on the reliability of piping need to be clarified. Then, correlations between the detectable crack depth and failure probability of piping are analyzed. In this analysis, relation between the crack depth and the POD is postulated by the step function shown in Fig. 3.8-31. The influences of oversight and detectable crack depth on failure probability are analyzed for 24 and 12 in. pipes, and failure probabilities are summarized in Table 3.8-14 and Table 3.8-15. As shown in the tables, failure probability is unaffected by the detectable crack depth in UT. The failure probability of a 12 in. pipe increases a little with increasing oversight percentage. The failure in the cases of 0% oversight and 0mm detectable crack depth occurs owing to the errors in crack growth estimation and crack sizing, and they have much influence on the reliability of pipes with stress corrosion cracks. Consequently, for higher reliability of piping, reducing errors in crack growth estimation (including crack sizing errors) and oversight of cracks, and planned successive inspections should be carried out to correct crack growth estimation.

![Fig. 3.8-31 Postulated step function of POD curves.](image-url)
Table 3.8-14 Influence of detectable crack depth and oversight percentage on failure probability in 24 in. pipes.

<table>
<thead>
<tr>
<th>Failure mode</th>
<th>(a_d^* \text{ [mm]})</th>
<th>Oversight probability</th>
</tr>
</thead>
<tbody>
<tr>
<td></td>
<td>0%</td>
<td>0.5%</td>
</tr>
<tr>
<td>Leak [w.j.-1]</td>
<td>0</td>
<td>1x10^-7</td>
</tr>
<tr>
<td></td>
<td>1</td>
<td>1x10^-7</td>
</tr>
<tr>
<td></td>
<td>2</td>
<td>1x10^-7</td>
</tr>
<tr>
<td></td>
<td>3</td>
<td>1x10^-7</td>
</tr>
<tr>
<td></td>
<td>4</td>
<td>1x10^-7</td>
</tr>
<tr>
<td></td>
<td>5</td>
<td>1x10^-7</td>
</tr>
<tr>
<td></td>
<td>6</td>
<td>1x10^-7</td>
</tr>
<tr>
<td>Break [w.j.-1]</td>
<td>0</td>
<td>1x10^-6</td>
</tr>
<tr>
<td></td>
<td>1</td>
<td>1x10^-6</td>
</tr>
<tr>
<td></td>
<td>2</td>
<td>1x10^-6</td>
</tr>
<tr>
<td></td>
<td>3</td>
<td>1x10^-6</td>
</tr>
<tr>
<td></td>
<td>4</td>
<td>1x10^-6</td>
</tr>
<tr>
<td></td>
<td>5</td>
<td>1x10^-6</td>
</tr>
<tr>
<td></td>
<td>6</td>
<td>1x10^-6</td>
</tr>
</tbody>
</table>

*: Detectable crack depth.

Table 3.8-15 Influence of detectable crack depth and oversight percentage on failure probability in 12 in. pipes.

<table>
<thead>
<tr>
<th>Failure mode</th>
<th>(a_d^* \text{ [mm]})</th>
<th>Oversight percentage</th>
</tr>
</thead>
<tbody>
<tr>
<td></td>
<td>0%</td>
<td>0.5%</td>
</tr>
<tr>
<td>Leak [w.j.-1]</td>
<td>0</td>
<td>1x10^-6</td>
</tr>
<tr>
<td></td>
<td>1</td>
<td>1x10^-6</td>
</tr>
<tr>
<td></td>
<td>2</td>
<td>1x10^-6</td>
</tr>
<tr>
<td></td>
<td>3</td>
<td>1x10^-6</td>
</tr>
<tr>
<td></td>
<td>4</td>
<td>1x10^-6</td>
</tr>
<tr>
<td></td>
<td>5</td>
<td>1x10^-6</td>
</tr>
<tr>
<td></td>
<td>6</td>
<td>1x10^-6</td>
</tr>
<tr>
<td>Break [w.j.-1]</td>
<td>0</td>
<td>3x10^-6</td>
</tr>
<tr>
<td></td>
<td>1</td>
<td>3x10^-6</td>
</tr>
<tr>
<td></td>
<td>2</td>
<td>3x10^-6</td>
</tr>
<tr>
<td></td>
<td>3</td>
<td>2x10^-6</td>
</tr>
<tr>
<td></td>
<td>4</td>
<td>2x10^-6</td>
</tr>
<tr>
<td></td>
<td>5</td>
<td>3x10^-6</td>
</tr>
<tr>
<td></td>
<td>6</td>
<td>2x10^-6</td>
</tr>
</tbody>
</table>

*: Detectable crack depth.
3.8.2.8 Conclusions  For the reliability of PLR piping, reducing oversight of cracks and crack sizing error are effective. Detectable crack size has not much influence on reliability of piping. It is more important to reduce oversight of cracks rather than the improvement of the detectable crack size in NDT for the higher reliability. For the sizing of detected cracks, qualified inspectors should measure to get accurate measured results. The errors in crack growth estimation also have much influence on the reliability of piping. The errors in crack sizing and crack growth estimation should be correct using the results of the successive inspections.

ACKNOWLEDGMENTS

The author thanks Ichiro Saruyama for cording a PFM program and Naoki Goto for assisting with preparing analytical conditions.

REFERENCES


3.9 Optimization of Maintenance Strategy Based on Risk, Cost, Benefit

3.9.1 Introduction

A number of PRA (Probabilistic Risk Assessment) studies have been applied to the optimization of maintenance activities in nuclear power plants from a viewpoint of safety focusing on the risk of core meltdown. However, even a small-scale incident of component, which never causes the core meltdown, resulted in reactor shutdown and economic losses. Accordingly, in addition to the safety analysis focusing on the risk of core meltdown, it is very useful to develop a simulator that can establish maintenance strategies in terms of availability and economic efficiency of nuclear power plants.

With the background above mentioned, the authors first studied risk and economic models of maintenance activities of SG (Steam Generator) tubes of PWRs (Pressurized Water Reactors) [1,2]. After that, we developed Dr. Mainte, an integrated simulator, for the maintenance optimization of LWRs [3]. The concept of the simulator is to provide a decision-making system to optimize maintenance activities for typical components and piping systems comprehensively and quantitatively in terms of safety, availability and economic rationality (both from cost and profit), environmental impact and social acceptance under various maintenance strategies including altering inspection frequency and inspection accuracy, conducting sampling inspection, repairs and/or replacements, introducing various maintenance rules, long-term fuel cycles, etc. Besides, a function of visualization of the simulated results by a divided multi-dimensional visualization method was also developed in order to support a decision-making process to optimize the maintenance activities.

Here, for the further improvement of the safety and availability of nuclear power plants, the effect of human error and its reduction on the optimization of maintenance activities have been studied. In addition, an approach of reducing human error is proposed.

3.9.2 Approach

3.9.2.1 Model of Dr. Mainte

Fig. 3.9-1 shows a model for maintenance optimization of LWRs in terms of safety, availability, economic rationality, environmental impact and social acceptance. Typical components and piping systems were selected to be analyzed by the model, and the degradation mechanisms and the maintenance activities for the targets were investigated both by literature and field surveys (Table 3.9-1).
Fig. 3.9-1  A model of Dr. Mainte for maintenance optimization of LWRs

Table 3.9-1  Target components and piping systems, degradation mechanisms and functions of the simulator
3.9.2.2 PFM-analysis FE-analysis is expected to be an indispensable tool for seismic analysis and design of complex building structures, as well as development of new components and devices of seismic control and design. In terms of availability analyses, the PFM-based model can evaluate the leakage and rupture probabilities of piping under various maintenance strategies including altering inspection frequency and inspection accuracy, conducting sampling inspection, repairs and/or replacements, introducing various maintenance rules, long-term fuel cycles, etc.

When conducting stress analysis of components and piping systems as well as calculating stress intensity factors of cracks, the simulator can access ADVENTURE (ADVanced ENgineering analysis Tool for Ultra large REal world) which is a computational mechanics system for large scale analysis and design [4]. Fig. 3.9-2 indicates an example of the stress analysis for the welding region of nozzles in PWR reactor vessel head.

![Example of stress analysis using ADVENTURE (13 million DOF meshes of PWR RV head)](image)

3.9.2.3 Safety Analysis In terms of safety, the results of PFM analysis are used to evaluate CDF (Core Damage Frequency).

3.9.2.4 Economic Analysis The economic evaluation takes account of the following factors; (1) Direct costs of maintenance activities, (2) Losses expected to be due to the leakage and rupture. In addition to the cost analysis, to justify whether or not it is worth while implementing the selected maintenance strategies from an economic point of view, NPV (Net Present Value) can be estimated as an index which is one of the most fundamental financial indices for decision makings [5]. Also, real option approaches can be assessed in the model.
3.9.2.5 **Social Acceptance Analysis**  Surveys of interview and questionnaire were carried out to solve the social acceptance of decision makings on risk-based maintenance activities. The results of the surveys have been feed backed to the functions of the simulator.

3.9.2.6 **Environmental Impact Analysis**  Compared with other power generation systems, CO₂ emission and its cost can be calculated.

3.9.2.7 **Decision-Making Support System**  A function of visualization of the simulated results by a divided multi-dimensional visualization method has been also coded in order to support decision-makings to optimize the maintenance strategies.

3.9.3 **Results**

3.9.3.1 **Effects of Human Error and its Reduction**  Examples of PFM analyses shown here are effects of human error and its reduction on the optimization of maintenance activities of PLR (Primary Loop Recirculation) piping system. Nondetectable probability of ultrasonic inspection, ε, independent of the crack depth was defined as human error here and its effect on the leak probability and economic rationality was evaluated.

Conditions of PFM analyses for PLR piping system are summarized in Table 3.9-2. The nondetectable probability, ε, curve as a function of crack depth is shown in Fig. 3.9-3. The ε value of 0.005 is assumed to be appropriate for both austenitic and ferritic stainless steel piping based on USA studies in 1980s [6-8].

| Table 3.9-2  Conditions of PFM analyses for PLR piping system (400A) of BWR |
|---|---|
| **Analysis code** | Dr. Mainte |
| **Pipe material** | SUS316 NG |
| **Pipe diameter** | 400A : 8 inch (406.4 mm) |
| **Pipe thickness** | 400A : 0.84 inch (21.3 mm) |
| **Operation period** | 60 years |
| **Operation temperature** | 288 ℃ |
| **Operation pressure** | 87.5 atm |
| **Flow stress** | 31.6kg/mm2 (mean) |
| **Residual stress due to welding** | 1.3 kg/mm2 (standard deviation) |
| **Crack initiation Probability** | mean value of $\sigma_{\text{f}} = 573 - 3.369 \log \sigma_{\text{f}}$ |
| | std. dev. of $\sigma_{\text{f}} = 0.08$ |
| | $f_0 = f_{\text{material}}(f_{\text{environment}})(f_{\text{loading}})$ |
| | $f_0 = 1.879$ |
| | $f = O_2^{0.06} \exp \left[ \frac{-1231}{T} \right] \exp \left( 6390 \sigma_{\text{f}}^{0.77} \right)$ |
| | $\sigma_{\text{f}} = \left( 25 \times 10^{-15} \text{m}^2 \right)^{0.5} $ |
| | O2: Oxygen conc., $\gamma$: Conductivity, $\sigma$: Stress, T: Temp. |
| **Crack initiation numbers** | 400A : 23/welding line |
| **Crack propagation rate** | Based on Harris equation |
| | $k = 0.015 \left[ \frac{\text{m}}{\text{yr}} \right]$ |
| | $C_1 = 0.0192$, $C_2 = 0.00821$, $C_3 (\text{mean}) = -0.006$, $C_3 (\text{std.}) = 0.0792$, $C_4 = 1.19$ |
| **IS interval** | 100 % / 3, 5, 7, 10, 13 years |
| **Maintenance options** | Repair or Replace |
Fig. 3.9-3  Nondetectable probability of ultrasonic inspection, $\varepsilon$, independent of the crack depth was defined as human error

Figs. 3.9-4 and 3.9-5 show sensitivity analyses of human error or the nondetectable probability, $\varepsilon$, ranging from 0.0025 to 0.1 over accumulated leakage probability and annual NPV of PLR piping system of BWR for 40 years operation, respectively.

Although the analyses were conducted under limited conditions of the present PFM analyses, the accumulated leakage probability showed no significant difference even the nondetectable probability, $\varepsilon$, became 50% down from 0.005 to 0.0025. Moreover, the accumulated leakage probability at 40 years showed only 2% difference even the nondetectable probability, $\varepsilon$, became 20 times up from 0.005 to 0.1. The annual NPV showed no significant change under the present range of nondetectable probability, $\varepsilon$.

Accordingly, although human error reduction is essential to the further improvement of the reliability of maintenance activities, assessments of the efficacy of human error reduction is required.
3.9.3.2 Maintenance Optimization by Dr. Mainte

Maintenance Optimization by Dr. Mainte functions) was calculated based on PFM analysis. Conditions of PFM analyses for PLR piping system are summarized in Table 3.9-2 and the related maintenance strategical parameters are summarized in Tables 3.9-3 - 3.9-5.

Then the data were shown in a divided multi-dimensional visualization space (Fig. 3.9-6: 1,800 data points). Furthermore, after the limited number of data in Fig. 3.9-6
were learned by neural networks, 10,800 data points were newly generated by the networks as shown in Fig. 3.9-7. In Fig. 3.9-7, the maintenance strategies which can meet the following constraint conditions are shown in pink-colored.
- Accumulative break probability for 60 years < 10^{-4}
- Accumulative NPV for 60 years > 100 billion Yen.
From Fig. 3.9-7, the correlation between design parameters and objective functions relating to the maintenance activities can be understood visually. Using the decision-making support system, the multidisciplinary design optimization can be easily conducted interactively by adjusting the constraint conditions of objective functions.

Table 3.9-3 Maintenance optimization by Dr. Mainte

<table>
<thead>
<tr>
<th>Item</th>
<th>Conditions</th>
</tr>
</thead>
<tbody>
<tr>
<td>Target component</td>
<td>BWR PLR piping system (400A)</td>
</tr>
<tr>
<td>Degradation mode</td>
<td>SCC of welding joints</td>
</tr>
<tr>
<td>Material</td>
<td>SUS316 (Residual stress is considered)</td>
</tr>
<tr>
<td>Numbers of joint</td>
<td>20</td>
</tr>
<tr>
<td>Maintenance strategy</td>
<td>•Inspection interval : 100% / 3, 5, 7 10, 13 years</td>
</tr>
<tr>
<td></td>
<td>•Improving crack detectability by R&amp;D :</td>
</tr>
<tr>
<td></td>
<td>Crack depth of 50% detectable are</td>
</tr>
<tr>
<td></td>
<td>(1) 3.2mm (2) 1.5mm (3) 0.8mm.</td>
</tr>
<tr>
<td></td>
<td>400 and 1000 Myen budgets for (2) and (3), respectively.</td>
</tr>
<tr>
<td></td>
<td>•Maintenance when cracks are detected : repair or replace</td>
</tr>
<tr>
<td>Objective functions</td>
<td>•Probability of pipe rupture</td>
</tr>
<tr>
<td></td>
<td>•Probability of pipe leakage</td>
</tr>
<tr>
<td></td>
<td>•CO2 release volume</td>
</tr>
<tr>
<td></td>
<td>•NPV (Net Present Value), etc</td>
</tr>
<tr>
<td>Constraint conditions</td>
<td>•Accumulative probability of pipe rupture ( \leq 10^{-4} )</td>
</tr>
<tr>
<td></td>
<td>•Accumulative NPV ( \geq 100 ) billion yen</td>
</tr>
</tbody>
</table>

Table 3.9-4 Conditions of economic rationality evaluations

<table>
<thead>
<tr>
<th>Item</th>
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</tr>
</thead>
<tbody>
<tr>
<td>Annual Sales per unit (M yen)</td>
<td>80,000</td>
</tr>
<tr>
<td>Sales cost (M yen)</td>
<td>70,000</td>
</tr>
<tr>
<td>Inspection cost / weld line (M yen)</td>
<td>5</td>
</tr>
<tr>
<td>Repair cost / weld line (M yen)</td>
<td>40</td>
</tr>
<tr>
<td>Replacement cost / weld line (M yen)</td>
<td>70</td>
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<tr>
<td>Loss due to Leakage (M yen)</td>
<td>120,000</td>
</tr>
<tr>
<td>Risk free rate</td>
<td>0.01</td>
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</table>

Table 3.9-5 Conditions of environmental impact evaluations

<table>
<thead>
<tr>
<th>Item</th>
<th>Conditions</th>
</tr>
</thead>
<tbody>
<tr>
<td>Operation conditions</td>
<td>12 month operation</td>
</tr>
<tr>
<td></td>
<td>1 month outage after operation (fossil power generation)</td>
</tr>
<tr>
<td></td>
<td>100% inspection / 5 years</td>
</tr>
<tr>
<td>Output: nuclear power generation</td>
<td>1000 MW</td>
</tr>
<tr>
<td>Output: fossil alternative power generation</td>
<td>1000 MW</td>
</tr>
<tr>
<td>Fossil alternative power generation</td>
<td>During periods of outage and shutdown due to leakage</td>
</tr>
<tr>
<td>Pipe leakage</td>
<td>6 month shutdown</td>
</tr>
<tr>
<td>CO2 release due to nuclear (g / kWh)</td>
<td>20</td>
</tr>
<tr>
<td>CO2 release due to fossil (g / kWh)</td>
<td>760</td>
</tr>
<tr>
<td>CO2 release cost (yen / ton)</td>
<td>12,000</td>
</tr>
</tbody>
</table>
Maintenance strategies 30 cases $\times$ 60 years = 1800 data points through PFM calculations

**Fig. 3.9-6** Visualization of maintenance related design parameters and objective functions

PFM data points expansion from 1800 to 10800 through neural network

**Fig. 3.9-7** Maintenance optimization by Dr. Mainte
3.9.3.3 Reducing Human Error by Dr. Mainte  The probability of human error is well known to be strongly dependent on working environments. Accordingly, Dr. Mainte’s functions, "neural network" and “divided multi-dimensional visualization method,” were used to modify working environments and to predict the modification effects by a questionnaire approach and eventually to reduce human error. Neural network is one type of artificial intelligence based on the modeling of complex neural networks of neurons in human brain. Advanced information processing can be realized by repeated learning.

A questionnaire approach is summarized below.

**Step 1**: Design and conduct a questionnaire (5-grade evaluation) which includes satisfactory items by workers (objective functions) and the working environments which influence the satisfactory items (design parameters).

**Step 2**: Quantify the relationship between objective functions and design parameters by non-linear analysis using "neural network" (Fig. 3.9-8).

**Step 3**: Look down at the relationship between objective functions and design parameters by “divided multi-dimensional visualization method” and optimize working environment interactively (Fig. 3.9-8). Quantify the relationship between objective functions and design parameters by non-linear analysis using neural network.

**Step 4**: Predict the effect of improving the design parameters on the objective functions by the neural network after learning the relationship between objective functions and design parameters.

![Fig. 3.9-8 Modification of working environments by questionnaire approach](image_url)
3.9.4 Conclusions

An integrated simulator for the maintenance optimization of LWRs has been applied to evaluate the effect of human error and its reduction on the optimization of maintenance activities. In addition, an approach of reducing human error is proposed. Because of various functions as a decision-making tool, the model developed in this study is expected to be used to assess the maintenance strategies of utilities as well as the technological and economical foundations of various guidelines and standards that will be adopted in Japan.

REFERENCES

3.10 Cooperation of PFM and PRA

3.10.1 Introduction

Safety evaluation methods for nuclear facilities have deterministic safety evaluation and probabilistic risk assessment (PRA). The deterministic safety evaluation method is used in the present nuclear safety regulation. Here, a few design base events to cover many potential abnormal events for nuclear facilities are assumed, the safety analyses for the events are performed under conservative conditions, and it is judged to ensure the safety of the nuclear facilities by comparing the analysis results with the acceptance criteria prepared under the conservative conditions. The method can show to ensure sufficient safety because of conservative evaluation step by step, but it cannot quantify the degree of safety.

On the other hand, the PRA can quantify the safety by a statistical method focusing on the contribution of the components in the nuclear facilities. This method estimates the frequency and impact of the potential accidents quantitatively and decides the degree of the safety by “risk” which is obtained by multiplication of the frequency and the impact. The method can also provide useful information to enhance the safety of the nuclear facilities.

Recently the PRA is used for the periodical safety assessment to review the status to ensure the safety of the nuclear power stations periodically, and the core damage frequency (CDF) obtained by the PRA approach is used as a typical indicator of risk information in the safety management regulations of the nuclear facilities [1]. The practical use of the PRA has become more popular.

3.10.2 Probabilistic risk assessment (PRA)

In Japan, the development of the PRA methods started in 1990’s [2]. The implementation procedure of the developed methods is as follows: First, the cause events (initiating events) to result in the abnormal states of the nuclear plants are identified and the frequency is estimated. The initiating events include internal initiating events to occur in the plants such as reactor coolant loss and main water supply loss, while external initiating events to occur in the outside of the plants such as earthquake, fire, and aircraft crash.

Next, using the event tree (ET) as shown in Fig. 3.10-1 [3], the combination of the success and the failure of safety function to prevent the development of the initiating event is discussed, In the discussion, using the fault tree (FT) as shown in Fig. 3.10-2 [3], the reliability of safety function and human manipulation is analyzed and CDF of the event probability of the large core damage is obtained. These procedures are called Level 1 PRA.

For the next step, the event progress after core damage is analyzed. The containment failure frequency of the event probability to release large amount of radioactive substances from the containment vessels is estimated, the release and migration of fission products (FP) are calculated under the event progress condition, and the source term of the amount of FP release into the circumferences due to the containment failure is determined. These procedures are called Level 2 PRA.
In addition, taking into account the failure modes of the containment vessels and the amount of FP release, the migration of FP into the circumferences and public exposure through the air diffusion and the food chain are calculated to obtain the health risk of the public. These procedures are called Level 3 PRA.

There are three different classes of the evaluation index shown in the above: Level 1 PRA, which evaluates the frequency to cause the core damage accident, Level 2 PRA, which evaluates the frequency to cause the containment failure and estimates the amount and time to release fission products into the circumferences (source term), Level 3 PRA, which evaluates the magnitude as well as the frequency to cause the impact to the public and the circumferences. Additionally power PRA and shut down PRA are classified by plant operation state.

The following advantages are obtained by the PRA: quantitative estimation of CDF and various risks, the degree of the likelihood of the core damage accident under an assumed scenario, the factor to contribute the event progress largely, and the quantitative amount of the risk change with or without expected functions. However, for the demerit of the PRA, various factors of incompleteness and uncertainty are involved depending on the scope of the PRA i.e., the extent of modelling, the maturity level of applied method, the adequacy of the database to be used for the assessment, and the other factors. It is necessary to understand to these characteristics when evaluating the PRA results.

![Fig. 3.10-1 Typical event tree [3]](image-url)
Fig. 3.10-2 Typical fault tree [3]

Fig. 3.10-3 Classification of PRA [3]
3.10.3 Cooperation of PFM and PRA

(1) Benefit to use the analysis results of PFM in PRA

Probabilistic fracture mechanics analysis can estimate the failure probability of vessels and piping under various conditions deductively though the PRA uses the frequency to cause the events and the failures rate of equipments which are inductively obtained from the performance record of plants. It is natural that the failure frequency values such as CDF calculated by PFM and PRA should not be compared directly. However, the effective application of the PFM analysis result is thought to be as follows:

• Estimation of frequency to cause initiating events in event tree

Table 3.10-1 [4] shows classification and frequency of the initiating events. The change of the CDF for the initiating event to be set to target over the years and the change rate of the CDF depending on the maintenance condition change can be quantitatively evaluated by the PFM analysis, i.e. the frequency to cause the initiating event relating to piping failure. This can lead the rational inspection process to omit the inspections for the piping areas with extremely small CDF.

• Estimation of failure rate for all element events in fault tree

The frequency to cause the top event taking into account the change over the years and the change of the maintenance conditions can be estimated by calculating the failure probability of the element events using the PFM analysis results when the failure of piping is assumed to be equivalent to the breakdown of active components.

For the first application, the calculation of the frequency to cause the initiating events and the risk-informed in-service inspection (RI-ISI) are proposed as a concrete application example. For the second application, the possibility of its application cannot be ignored. However, as shown in the database of equipment failure rate in Table 3.10-2 [5], the breakdown probability of the active components is much larger than the failure probability of piping obtained by the PFM analysis in the PRA fault tree. This predicts the very small influence of the PFM analysis result on the occurrence probability of the top events in the fault tree. Therefore, the advantage obtained by the application may be small.
Table 3.10-1 Classification of initiating events and example of event frequency [4]

<table>
<thead>
<tr>
<th>Initiating event</th>
<th>Event frequency (1/reactor year)</th>
</tr>
</thead>
<tbody>
<tr>
<td>1 Large-break LOCA</td>
<td>1.8×10⁻⁵</td>
</tr>
<tr>
<td>2 Medium-break LOCA</td>
<td>5.6×10⁻⁵</td>
</tr>
<tr>
<td>3 Small-break LOCA</td>
<td>1.8×10⁻⁴</td>
</tr>
<tr>
<td>4 Minimal leak</td>
<td>2.4×10⁻³</td>
</tr>
<tr>
<td>5 Residual heat removal system isolation valve LOCA</td>
<td>8.8×10⁻⁹</td>
</tr>
<tr>
<td>6 Loss of main feed water</td>
<td>1.4×10⁻²</td>
</tr>
<tr>
<td>7 Loss of external power supply</td>
<td>7.1×10⁻³</td>
</tr>
<tr>
<td>8 ATWS</td>
<td>3.2×10⁻⁶</td>
</tr>
<tr>
<td>9 Steam generator tube failure</td>
<td>2.5×10⁻³</td>
</tr>
<tr>
<td>10 Transient event</td>
<td>1.0×10⁻¹</td>
</tr>
<tr>
<td>11 Failure of secondary cooling system</td>
<td>3.6×10⁻⁴</td>
</tr>
<tr>
<td>12 Loss of component cooling water</td>
<td>1.8×10⁻⁴</td>
</tr>
<tr>
<td>13 Failure of one series of DC power supply</td>
<td>1.8×10⁻⁴</td>
</tr>
<tr>
<td>14 Manual shutdown</td>
<td>1.4×10⁻¹</td>
</tr>
</tbody>
</table>
Table 3.10-2 Example of failure rate database for domestic nuclear equipment [5]

<table>
<thead>
<tr>
<th>Equipment</th>
<th>Failure mode</th>
<th>Failure rate</th>
<th></th>
</tr>
</thead>
<tbody>
<tr>
<td></td>
<td></td>
<td>Average</td>
<td>EF</td>
</tr>
<tr>
<td>Motor pump</td>
<td>Failure to start</td>
<td>2.0×10⁻⁵ /d</td>
<td>15</td>
</tr>
<tr>
<td></td>
<td>Failure to run continuously</td>
<td>4.0×10⁻⁷ /h</td>
<td>5</td>
</tr>
<tr>
<td></td>
<td>Failure to start</td>
<td>4.7×10⁻⁴ /d</td>
<td>10</td>
</tr>
<tr>
<td></td>
<td>Failure to run continuously</td>
<td>1.6×10⁻⁶ /h</td>
<td>10</td>
</tr>
<tr>
<td>Turbine driving pump</td>
<td>Failure to start</td>
<td>1.7×10⁻⁵ /d</td>
<td>10</td>
</tr>
<tr>
<td></td>
<td>Failure to run continuously</td>
<td>2.8×10⁻⁶ /d</td>
<td>20</td>
</tr>
<tr>
<td></td>
<td>Failure to open</td>
<td>9.1×10⁻⁶ /d</td>
<td>20</td>
</tr>
<tr>
<td></td>
<td>Failure to close</td>
<td>5.4×10⁻⁵ /d</td>
<td>10</td>
</tr>
<tr>
<td></td>
<td>Mishandling of opening/closing</td>
<td>5.4×10⁻⁶ /d</td>
<td>10</td>
</tr>
<tr>
<td></td>
<td>Blockade</td>
<td>2.9×10⁻⁹ /h</td>
<td>20</td>
</tr>
<tr>
<td></td>
<td>Blockade</td>
<td>2.9×10⁻⁹ /h</td>
<td>20</td>
</tr>
<tr>
<td>Motor valve</td>
<td>Failure to open</td>
<td>5.4×10⁻⁵ /d</td>
<td>10</td>
</tr>
<tr>
<td></td>
<td>Failure to close</td>
<td>5.4×10⁻⁶ /d</td>
<td>30</td>
</tr>
<tr>
<td></td>
<td>Mishandling of opening/closing</td>
<td>2.8×10⁻⁸ /h</td>
<td>15</td>
</tr>
<tr>
<td></td>
<td>Blockade</td>
<td>7.1×10⁻⁹ /h</td>
<td>30</td>
</tr>
<tr>
<td>Air operated valve</td>
<td>Failure to open</td>
<td>5.4×10⁻⁵ /d</td>
<td>10</td>
</tr>
<tr>
<td></td>
<td>Failure to close</td>
<td>5.4×10⁻⁶ /d</td>
<td>30</td>
</tr>
<tr>
<td></td>
<td>Mishandling of opening/closing</td>
<td>2.8×10⁻⁸ /h</td>
<td>15</td>
</tr>
<tr>
<td></td>
<td>Blockade</td>
<td>7.1×10⁻⁹ /h</td>
<td>30</td>
</tr>
<tr>
<td>Hydraulic valve</td>
<td>Failure to open</td>
<td>2.7×10⁻⁶ /d</td>
<td>30</td>
</tr>
<tr>
<td></td>
<td>Failure to close</td>
<td>5.6×10⁻⁶ /d</td>
<td>20</td>
</tr>
<tr>
<td></td>
<td>Mishandling of opening/closing</td>
<td>2.2×10⁻⁹ /h</td>
<td>20</td>
</tr>
<tr>
<td>Checking valve</td>
<td>Failure to open</td>
<td>4.7×10⁻⁴ /d</td>
<td>7</td>
</tr>
<tr>
<td></td>
<td>Failure to close</td>
<td>1.2×10⁻⁴ /h</td>
<td>30</td>
</tr>
<tr>
<td>Emergency diesel generator</td>
<td>Failure to start</td>
<td>4.7×10⁻⁴ /d</td>
<td>7</td>
</tr>
<tr>
<td></td>
<td>Failure to run continuously</td>
<td>1.2×10⁻⁴ /h</td>
<td>30</td>
</tr>
</tbody>
</table>
(2) Evaluation methods of PRA using PFM analysis result

The PFM analysis is performed under maintenance conditions. The influence of the failure probability of piping and the failure rate of equipment on the safety of facilities and plants can be evaluated quantitatively. In the following, the application methods of the PFM analysis results to the failure probability of piping are introduced and the typical example of the cooperation of PFM and PRA is represented.

(a) Application of PFM analysis results to failure probability of piping

Piping is an indispensable component for nuclear power plants and it has various materials and geometries. By connecting many pipes by welding and other methods, reactor coolant system, main steam system and main feed water system, for instance, are composed. The components to be decomposed in these systems by the diameter, material and other items are called “segment” in the PFM analysis. The PFM analysis can estimate the failure probability per one welding line using the volume of the weld part and the existence probability of a defect in it. As shown in Fig. 3.10-4, one segment is assumed to have N lines of weld seam. The failure probability $P_B$ is defined with Eq. (3.10-1) when the failure probability of the $n$-th ($n=1 \sim N$) weld line is $P_{nB}$. Figs. 3.10-5 and 3.10-6 show the failure probability of the segment changes depending on the number of weld line and the wall thickness of pipe. It is found that the failure probability increases more as the number of weld line and the wall thickness of pipe increase more.

\[
P_B = 1 - (1 - P_{1B})(1 - P_{2B})(1 - P_{3B}) \cdots (1 - P_{NB}) \quad (3.10-1)
\]

$P_B$: Failure probability of segment

$P_{nB}$: Failure probability of the $n$-th weld line ($n=1 \sim N$)

Fig. 3.10-4 Weld lines in one pipe
Fig. 3.10-5 Relationship between failure probability of a segment and a number of weld line

Fig. 3.10-6 Relationship between failure probability of a segment and wall thickness of pipe
(b) Application method of PFM analysis results to PRA

In this section, a method to apply the failure probability of pipes and vessels obtained by the PFM analysis and the failure probability of segments calculated by Eq. (3.10-1) to the PRA.

First we show, the application to the estimation of the initiating event frequency. In the general PRA, the frequency of the initiating event is calculated on the basis of the performance data of the domestic and offshore plants. For estimating the initiation frequency from the PFM results, the initiating event frequency caused by the pipe failure events such as the reactor coolant loss accident (LOCA), steam generator tube rupture (SGTR), and the secondary system pipe failure can be directly obtained by calculating the whole failure probability for every system using Eq. (3.10-1). The conditional probability of core damage represents the probability to damage the reactor core when the event probability of the initiating event is 1. The core damage frequency for discrete initiating events obtained through the process denotes the core damage frequency taking into account various maintenance conditions. Table 3.10-3 [6,7] and Table 3.10-4 [7] list the example of the event frequency of the initiating event based on the performance data and the conditional probability of core damage frequency, respectively. The values calculated by the PFM analysis and the values obtained from the performance data are different from each other and they cannot be compared from the viewpoint of event frequency. However, the event frequency calculated by the PFM analysis can change depending on the maintenance conditions and the core damage frequency (CDF), which is affected by the event frequency, can also change depending on the maintenance conditions. Using this relationship, there can be a method to determine the priority of the inspection in the basis of the change amount of the CDF caused by changing the maintenance conditions. That is an idea to apply to risk-informed in-service inspection (RI-ISI) mentioned below.

The RI-ISI is an in-service inspection approach used in the US at present and it is classified into the ASME/WOG method and the EPRI method. Recently in addition to these methods, a case standard to have RI-ISI program to simplify specific risk assessment is published as ASME code case N-716 [8]. The ISI is an inspection to investigate the ageing situation of equipment and piping systematically. Especially its non-destructive inspection is used for weld parts. In the RI-ISI, risk importance is evaluated for each segment defined by dividing all systems in more detail, and the ISI is conducted in the segments with higher risk importance preferentially. It is thought that this approach can keep the equivalent safety as the conventional method decreasing the number of inspection points and the maintenance cost. Additionally the decrease of inspection may lead the reduction of radiation exposure.
### Table 3.10-3 Example of core damage frequency for initiating events in BWR [6,7]

<table>
<thead>
<tr>
<th>Initiating event</th>
<th>Initiating event frequency (/reactor year)*</th>
<th>Conditional core damage probability**</th>
</tr>
</thead>
<tbody>
<tr>
<td>Large LOCA</td>
<td>$2.2 \times 10^{-5}$</td>
<td>$4.5 \times 10^{-7}$</td>
</tr>
<tr>
<td>Medium LOCA</td>
<td>$7.0 \times 10^{-5}$</td>
<td>$5.3 \times 10^{-7}$</td>
</tr>
<tr>
<td>Small LOCA</td>
<td>$2.2 \times 10^{-4}$</td>
<td>$4.5 \times 10^{-7}$</td>
</tr>
<tr>
<td>ISLOCA</td>
<td>$6.4 \times 10^{-9}$</td>
<td>$6.7 \times 10^{-1}$</td>
</tr>
<tr>
<td>PCS available</td>
<td>$2.4 \times 10^{-1}$</td>
<td>$1.4 \times 10^{8}$</td>
</tr>
<tr>
<td>PSC unavailable</td>
<td>$3.4 \times 10^{-2}$</td>
<td>$1.6 \times 10^{8}$</td>
</tr>
<tr>
<td>Station blackout</td>
<td>$3.9 \times 10^{-3}$</td>
<td>$3.3 \times 10^{-7}$</td>
</tr>
<tr>
<td>Manual shutdown</td>
<td>$3.2 \times 10^{-1}$</td>
<td>$3.8 \times 10^{11}$</td>
</tr>
</tbody>
</table>

*: Values in Ref. [6], **: Values calculated from Refs. [6] and [7]

### Table 3.10-4 Example of conditional core damage frequency in PWR [7]

<table>
<thead>
<tr>
<th>Initiating event</th>
<th>Initiating event frequency (/reactor year)*</th>
<th>Conditional core damage probability**</th>
</tr>
</thead>
<tbody>
<tr>
<td>Large LOCA</td>
<td>$2 \times 10^{-4}$</td>
<td>$8.09 \times 10^{-4}$</td>
</tr>
<tr>
<td>Medium LOCA</td>
<td>$6 \times 10^{-4}$</td>
<td>$2.43 \times 10^{-4}$</td>
</tr>
<tr>
<td>Small LOCA</td>
<td>$2 \times 10^{-4}$</td>
<td>$2.43 \times 10^{-4}$</td>
</tr>
<tr>
<td>SGTR</td>
<td>$3.2 \times 10^{-3}$</td>
<td>$3.01 \times 10^{-5}$</td>
</tr>
<tr>
<td>Interface system LOCA</td>
<td>$5.4 \times 10^{-8}$</td>
<td>$2.77 \times 10^{-1}$</td>
</tr>
<tr>
<td>Failure of secondary system</td>
<td>$9 \times 10^{-4}$</td>
<td>$1.32 \times 10^{-4}$</td>
</tr>
<tr>
<td>Station blackout</td>
<td>$4.5 \times 10^{-3}$</td>
<td>$4.76 \times 10^{-6}$</td>
</tr>
<tr>
<td>Loss of PCS function</td>
<td>$2.3 \times 10^{-2}$</td>
<td>$1.21 \times 10^{-7}$</td>
</tr>
<tr>
<td>Other transient events</td>
<td>$1.1 \times 10^{-1}$</td>
<td>$3.58 \times 10^{-10}$</td>
</tr>
<tr>
<td>Manual shutdown</td>
<td>$1.5 \times 10^{-1}$</td>
<td>$3.10 \times 10^{-11}$</td>
</tr>
<tr>
<td>Loss of component cooling</td>
<td>$1.1 \times 10^{-4}$</td>
<td>$8.53 \times 10^{-4}$</td>
</tr>
<tr>
<td>water system function</td>
<td></td>
<td></td>
</tr>
<tr>
<td>ATWS</td>
<td>$2.8 \times 10^{-6}$</td>
<td>$3.98 \times 10^{-3}$</td>
</tr>
</tbody>
</table>

*: Values in Ref. [6], **: Values calculated from Refs. [6] and [7]
(3) Risk-informed in-service inspection (RI-ISI)

RI-ISI is proposed as one of safety regulation approaches to use risk information. Herein, the outlines of the ASME/WOG method and the EPRI method are explained. In the ASME/WOG method, the risk importance is evaluated using the analysis such as PFM and PRA.

(a) ASME/WOG method

The evaluation procedure of the ASME/WOG method is shown in Fig. 3.10-7. First, an evaluation extent is determined and then it is divided into many segments. Next, the possibility and influence of pipe failure are evaluated for all segments. Additionally the risk importance for the segments is estimated from these results. The ISI plan is proposed based on the estimation. After the plan is examined from the viewpoint of the number of inspection points, the influence on risk, the influence on cost and other factors, the ISI is carried out. The ISI results are fed back to the next ISI plan.

![Evaluation procedure of ASME/WOG method](image)

Fig. 3.10-7 Evaluation procedure of ASME/WOG method

In the procedure of the ASME/WOG method, the PFM and PRA analysis are involved in the potential assessment of pipe failure, the impact assessment of pipe failure and the risk importance assessment.

In the potential assessment of pipe failure, the failure probability for all segments is calculated by the PFM analysis for the failure mechanism of fatigue and stress corrosion cracking (SCC). For other failure mechanisms, failure probability is estimated by expert judgment, which is called an expert panel, and the performance evaluation using the past operation data.

In the impact assessment of pipe failure, the CDF for the pipe segment to be concerned is used. It is obtained by both failure probability or failure frequency for the pipe segment calculated by the PFM analysis and the conditional core damage probability or the conditional core damage frequency computed by the PRA analysis. The practical formulas for the impact assessment of pipe failure are shown in (i) to (iii) [9] below. The
summation of the core damage frequency for the respective pipe segment obtained by (i) to (iii) represents all core damage frequency. The calculation procedure for all core damage frequency is shown in (iv) [9].

(i) Core damage frequency when initiating event occurs due to pipe segment failure

When an initiating event occurs due to pipe segment failure, the initiating event surely occurs but there are not any impacts on a mitigation system. Hence, the event frequency of pipe segment failure is equal to the event frequency of the initiating event. The function of all mitigation system for the caused initiator is also equal to that for the internal event. Therefore, when the initiating event occurs due to the pipe segment failure, the core damage frequency of the pipe segment is estimated by multiplying the event frequency of pipe failure with the conditional core damage probability obtained from the internal PRA for the initiating event caused by the pipe failure by the following equations.

$$CDF_{PI} = FR_{PB} \times CCD_{PIE}$$

CDF_{PI} : Core damage frequency due to pipe segment failure (/reactor year)
FR_{PB} : Conditional core damage probability of initiating event by internal PRA
CCD_{PIE} : Pipe failure frequency (/reactor year)

$$CCD_{PIE} = \frac{CDF_{IE}}{FREQ_{IE}}$$

CDF_{IE} : Core damage frequency of initiating event by internal PRA (/reactor year)
FREQ_{IE} : Initiating event frequency by internal PRA (/reactor year)

FR_{PB} = \frac{FP_{PB}}{EOL}

FP_{PB} : Pipe failure probability analyzed by PFM
EOL : Operating license period used in PFM analysis

(ii) Core damage frequency when mitigation systems are affected by pipe segment failure

When the mitigation systems are affected by pipe segment failure, any initiating events do not occur due to the pipe segment failure. Hence, the functional influence of the mitigation systems are assumed to occur by the pipe segment failure when the initiating events related with the internal PRA occur. Therefore, when the mitigation systems are affected by the pipe segment failure, the core damage frequency of the pipe segment is estimated by multiplying the failure probability of the pipe segment with the conditional core damage frequency with the failure probability of the pipe segment of 1.0 as the following equations. Here, the conditional core damage frequency with the failure probability of the pipe segment of 1.0 is estimated by subtracting the core damage frequency calculated by the internal PRA from all core damage frequency with
the failure probability of the pipe segment of 1.0. On the other hand, any mitigation systems with non-important class and small pipe failure probability are often not modelled. In this case, the conditional core damage frequency is calculated assuming the failure probability of the equivalent equipment as the pipe segment from the viewpoint of the contribution (surrogate components) is 1.0.

\[ \text{CDF}_{PB2} = \text{FR}_{PB} \times \text{CCDF}_{PB} \]

- \text{CDF}_{PB2} : Core damage frequency due to pipe segment failure (/reactor year)
- \text{CCDF}_{PB} : Conditional core damage probability with failure probability of pipe segment of 1.0 (/reactor year)

\[ \text{CCDF}_{PB} = \frac{\text{CDF}_{PB=1.0} - \text{CDF}_{BASE}}{\text{CDF}_{PB=1.0}} \]

- \text{CDF}_{PB=1.0} : Core damage frequency of all initiating events when failure probability of surrogate components is assumed to be 1.0, in which initiating event frequency is calculated using internal PRA (/reactor year)
- \text{CDF}_{BASE} : Core damage frequency of all initiating events in internal PRA (/reactor year)

(iii) Core damage frequency when mitigation systems for initiating event caused by pipe segment failure are affected

When an initiating event occurs due to pipe segment failure and it affects the mitigation system, the event frequency of pipe segment failure is the initiating event frequency. Also the following equations are defined because the initiator affects the mitigation system. When the initiating event caused by the pipe segment failure affects the mitigation system, the core damage frequency of the pipe segment is calculated by multiplying the event frequency of the pipe failure with the conditional core damage probability in which the failure probability of the pipe segment in the mitigation system for the initiating event is assumed to be 1.0.

\[ \text{CDF}_{PB3} = \text{FR}_{PB} \times \text{CCDP}_{IE,seg=1.0} \]

- \text{CDF}_{PB3} : Core damage frequency in pipe segment failure (/reactor year)
- \text{CCDP}_{IE,seg=1.0} : Conditional core damage frequency with failure probability of pipe segment of 1.0 (/reactor year)

\[ \text{CCDP}_{IE,seg=1.0} = \frac{\text{CDF}_{IE,seg=1.0}}{\text{FREQ}_{IE}} \]

- \text{CDF}_{IE,seg=1.0} : Core damage frequency of initiating event with failure probability of pipe segment of 1.0 (/reactor year)
- \text{FREQ}_{IE} : initiating event frequency (/reactor year)
(iv) All core damage frequency of pipe segment failure

All core damage frequency of pipe segment failure is obtained from the summation of the core damage frequency of the respective pipe segment calculated by the equations in (i) to (iii).

\[ CDF_{PB} = \sum_{i=1,z} CDF_{PB|i} \]

\( i \) : Number of pipe segment \((i = 1 - z)\)

\( z \) : Total number of pipe segment

The risk importance assessment is a method to define the importance for risk of pipe segment using the ratio of all CDF values obtained from the influent evaluation of the pipe failure to the CDF values assuming the pipe segment failure and the other index. The representative methods of the risk importance evaluation are listed in Table 3.10-5 [2].
### Table 3.10-5 Risk importance evaluation

<table>
<thead>
<tr>
<th>Method</th>
<th>Outline</th>
</tr>
</thead>
</table>
| **Risk Reduction Worth (RRW)**             | **RRW = \( \frac{P(\text{top})}{P(\text{top}/A = 0)} \).**  
  \( P(\text{top}) \): Top event probability.  
  \( P(\text{top}/A = 0) \): Top event probability when loss rate of function of Equipment A (occurrence probability of event A) is zero.  
  The approach is an index how much to reduce risk (top event) when the loss rate of function of the object equipment is zero. When reducing the risk by any improvement, it is used to identify the candidate equipment. |
| **Risk Achievement Worth (RAW)**           | **RAW = \( \frac{P(\text{top})}{P(\text{top}/A = 1)} \).**  
  \( P(\text{top}/A = 1) \): Top event probability when loss rate of function of Equipment A (occurrence probability of event A) is 1.0.  
  The approach is an index how much to increase risk when the object equipment fails. It denotes the risk caused by leaving the failure as it is, and it is useful for planning the inspection and routine tests. |
| **Birnbaum Method**                        | **Birnbaum = \( P(\text{top}/A = 1) - P(\text{top}/A = 0) \).**  
  The approach is an index to represent the change of the occurrence probability of the top event when the loss probability for the function of the object equipment changes. It is equal to the sum of the risk reduction worth with the risk achievement worth. |
| **Fussel-Vesely Method**                   | **Fussel-Vesely = \( \frac{P(\text{top})}{P(\text{top})} = \frac{[P(\text{top}) - P(\text{top}/A = 1)]}{P(\text{top})} \).**  
  \( P(\text{top}) \): Occurrence probability of the top event contributed by failure of Equipment i (Failure of Equipment i is not always a critical condition for the top event).  
  The approach shows the conditional probability involving in contribution of the loss of function of the object equipment when the top event is assumed to occur. |
| **Criticality Importance**                 | **Criticality Importance = [P(\text{top}/A = 1) - P(\text{top}/A = 0)] \times \frac{P(A)}{P(\text{top})}.**  
  \( P(\text{top}/A = 1) \): Loss probability of function of Equipment A (Occurrence probability of event A).  
  The approach is an index to represent the conditional probability in which the loss of function of the object equipment plays a dominant role when the top event is assumed to occur. |
In this way, in the ASME/WOG method which is one of the methods used for the RI-ISI, the influence evaluation of pipe failure is conducted using the failure probability computed by the PFM analysis and the conditional core damage probability obtained by the PRA analysis. Also, the important segment is identified from the viewpoint of safety using the risk importance assessment.

(b) EPRI method

The other approach for the RI-ISI is the EPRI method. Though it does not use the PFM analysis, it is introduced here as reference. The evaluation procedure of the EPRI method is shown in Fig. 3.10-8. The procedure is almost the same as that of the ASME/WOG method. However, it is different from the ASME/WOG method in the potential evaluation of pipe failure, the influence evaluation of pipe failure and risk classification and so on.

![Evaluation procedure of EPRI method](image)

The screening criterion based on the mechanism of the damage and degradation developed by Electric Power Research Institute (EPRI) is used for the potential evaluation of pipe failure, which is produced from the analysis results of failure case data not using the PFM analysis. The criterion is based on the qualitative relationship between the damage degradation mechanism and the failure extent. The typical potential category of the pipe failure is listed in Table 3.10-6.

In the influence evaluation of the pipe failure, the PRA procedure to evaluate the influence of the pipe segment failure is almost the same as that of the ASME/WOG method. However, it is very different in the viewpoint such that the influence is classified by the conditional core damage probability (CCDP) qualitatively. The typical influence category of the pipe failure is shown in Table 3.10-7.

For the risk classification, a risk map to choose inspection locations is produced as shown in Table 3.10-8, which is based on the results in Tables 3.10-6 and 3.10-7. In Table 3.10-8, the risk map is divided in between Category 1 and Category 7 which is choice standard of inspection elements.
By the EPRI method, the qualitative risk can be evaluated but the quantitative one cannot. That means the priority cannot be clarified in case that many inspection locations are in the same category. Also, for the screening criterion based on the mechanism of the damage and degradation, the domestic case data and plant-specific case data should be stored and re-analyzed.

<table>
<thead>
<tr>
<th>Potential category for pipe rupture</th>
<th>Expected leak condition</th>
<th>Rough indication value of failure probability (/reactor year)</th>
<th>Degradation mechanism of sensitive location</th>
</tr>
</thead>
<tbody>
<tr>
<td>HIGH</td>
<td>LARGE</td>
<td>1.0×10⁻⁴</td>
<td>FAC*</td>
</tr>
<tr>
<td>MEDIUM</td>
<td>SMALL</td>
<td>1.0×10⁻⁵</td>
<td>Degradation mechanism excluding FAC</td>
</tr>
<tr>
<td>LOW</td>
<td>NONE</td>
<td>1.0×10⁻⁶</td>
<td>No degradation mechanism</td>
</tr>
</tbody>
</table>

*: Flow-Accelerated Corrosion

<table>
<thead>
<tr>
<th>Influence category for pipe failure</th>
<th>CCDP (/reactor year)</th>
<th>Commentary</th>
</tr>
</thead>
<tbody>
<tr>
<td>HIGH</td>
<td>1.0×10⁻⁴ ≤ CCDP</td>
<td>Pressure boundary failure which causes the events to affect the risk of the plants significantly or fails the mitigating performance of the plants definitely.</td>
</tr>
<tr>
<td>MEDIUM</td>
<td>1.0×10⁻⁶ &lt; CCDP ≤ 1.0×10⁻⁴</td>
<td>Between HIGH and LOW</td>
</tr>
<tr>
<td>LOW</td>
<td>CCDP ≤ 1.0×10⁻⁶</td>
<td>Pressure boundary failure not to affect the predicted operating events or the mitigating performance of the plants significantly.</td>
</tr>
</tbody>
</table>
Table 3.10-8 Typical risk map

<table>
<thead>
<tr>
<th>Potential for pipe rupture</th>
<th>Consequences of pipe rupture</th>
</tr>
</thead>
<tbody>
<tr>
<td>HIGH</td>
<td>LOW, Category 7</td>
</tr>
<tr>
<td>MEDIUM</td>
<td>LOW, Category 7</td>
</tr>
<tr>
<td>LOW</td>
<td>LOW, Category 7</td>
</tr>
</tbody>
</table>

(4) Case study of PFM analysis result, in-service inspection period and its frequency

In nuclear piping, the occurrence of cracking is checked by in-service inspection (ISI). For the potential cracking of piping caused by ageing, the probability of leakage in piping by the cracking development is desirable to be as small as possible to improve the reliability for operating the nuclear power plants. To take the measures, the increasing inspection frequency can be come up with, while the effect on the reliability improvement can be limited for the excess inspection frequency. For the choice of a rational maintenance approach, the preceding mentioned cooperation of PFM analysis and PRA analysis such as RI-ISI is proposed. Here, the case study on how to estimate the rational period and frequency of inspection by the PFM analysis is illustrated.

Five cases of an inspection approach were assumed at the same weld point as shown in Table 3.10-9 and their superiority or inferiority was evaluated by calculating the cumulative rupture probability for forty years from in-service start. The main calculation conditions are listed in Table 3.10-10. By assuming 150 mm in the pipe outer diameter, 18.3 mm in the wall thickness and marginal in the inspection quality, the sensitivity analysis was conducted. The analysis results are shown in Fig. 3.10-9 [10].

The results of Fig. 3.10-9 are summarized as follows;
- The cumulative rupture probability decreased by conducting ISI.
- The cumulative rupture probability decreased more by conducting ISI twice than once.
- The cumulative rupture probability after 40 years from the in-service starting decreased more by ISI after 20 years or 30 years than by ISI after 10 years in case of once ISI. Also, the probability was almost the same between at ISI after 20 years and at ISI after 30 years.
- The cumulative rupture probability after 40 years by conducting ISI twice was reduced by approximately one-third in comparison with the probability without ISI.

This way, the PFM analysis results can be used to evaluate the period and frequency of ISI. Additionally the significance between systems such as priority of the inspection can be compared by reflecting the PFM analysis results to the PRA analysis. The applicability
of the cooperation between PFM and PRA to the rational maintenance activity is discussed through an example as shown in the following paragraph.

Table 3.10-9 Inspection case

<table>
<thead>
<tr>
<th>(i)</th>
<th>No ISI</th>
</tr>
</thead>
<tbody>
<tr>
<td>(ii)</td>
<td>Only once ISI after 10 years</td>
</tr>
<tr>
<td>(iii)</td>
<td>Only once ISI after 20 years</td>
</tr>
<tr>
<td>(iv)</td>
<td>Only once ISI after 30 years</td>
</tr>
<tr>
<td>(v)</td>
<td>Two ISIs after 10 and 30 years</td>
</tr>
</tbody>
</table>

Table 3.10-10 Calculation conditions

<table>
<thead>
<tr>
<th>PFM analysis code</th>
<th>WinPRAISE 4.24 (Windows version of PRAISE code)</th>
</tr>
</thead>
<tbody>
<tr>
<td>Pipe materials</td>
<td>Equivalent material of type 316 stainless steel</td>
</tr>
<tr>
<td>Diameter and wall thickness</td>
<td>Diameter, 6 inches (150 mm); Thickness, 0.72 inches (18.3 mm)</td>
</tr>
<tr>
<td>Existence probability of manufacturing defects</td>
<td>Based on Mood’s theory [11], Hahn’s equation [12], and Harris’s data [13]</td>
</tr>
<tr>
<td>Distribution of size of manufacturing defect</td>
<td>Based on exponential distribution of Marshall [14] and equation of Abramowitz [15]</td>
</tr>
<tr>
<td>PSI and inspection quality [16,17]</td>
<td>PSI, RT* and UT*; UT inspection quality, Marginal [18]</td>
</tr>
<tr>
<td>Transient event</td>
<td>Start-stop</td>
</tr>
<tr>
<td></td>
<td>Coolant temperature, 288°C; Stress, 21 kgf/mm²; Frequency, 2 times/year</td>
</tr>
<tr>
<td></td>
<td>Uniform stress</td>
</tr>
<tr>
<td></td>
<td>Stress, 21 kgf/mm²; Frequency, 10 times/year</td>
</tr>
<tr>
<td></td>
<td>Thermal shock</td>
</tr>
<tr>
<td></td>
<td>Temperature change, 149°C (step-like); Frequency, 10 times/year</td>
</tr>
<tr>
<td>Frequency of ISI</td>
<td>No inspection</td>
</tr>
<tr>
<td>Crack propagation rate</td>
<td>Based on equation of Harris [13,19]</td>
</tr>
<tr>
<td>Leak detection capability</td>
<td>1 gpm (gallon per minute) [13]</td>
</tr>
</tbody>
</table>

Note: *, Radiographic test; **, Ultrasonic test
Fig. 3.10-9 Comparison of cumulative rupture probability depending on inspection case

(5) Example of PRA analysis cooperated with PFM analysis results

In this section, an example of safety evaluation conducted by the PRA analysis cooperated with the PFM analysis results is introduced, and the method how to use the evaluation for drawing up the in-service inspection (ISI) plan is explained.

Here, the following example was chosen; the core damage frequency in the loss of coolant accident (LOCA), which was one of initiating events caused by pipe rupture, was estimated by both the accident event frequency calculated by the PFM analysis results and the conditional core damage probability in the LOCA. Additionally the change of the core damage frequency was investigated for the ISI condition changes in the PFM analysis conditions.

(a) Calculation conditions

As shown in Table 3.10-11, the primary piping in pressurized water reactor (PWR) is classified into three categories of large-diameter pipe (more than 8 inches), medium-diameter pipe (more than 2 inches and under 8 inches), and small-diameter pipe (under 2 inches), and the number of welding line in the pipe is assumed to be one hundred. The PFM analysis is performed under the conditions shown in Table 3.10-12 and the typical values of the pipe. The pipe rupture probability for the three categories and event frequencies of small-break LOCA, medium-break LOCA, and large-break LOCA based
on 100 lines are obtained. Next, the core damage frequency is calculated using the conditional core damage probability for the typical nuclear power stations in Table 3.10-13 [10]. Here, the cumulative rupture probability assuming that the allowable operation period is forty years is used as pipe rupture probability. Moreover, the core damage probability is calculated under the conditions of ISI as shown in Table 3.10-14.

<table>
<thead>
<tr>
<th>Typical pipe</th>
<th>Initiating event</th>
<th>Outer diameter (mm)</th>
<th>Wall thickness (mm)</th>
<th>Number of welding line</th>
</tr>
</thead>
<tbody>
<tr>
<td>Small-diameter (1/2 inches)</td>
<td>Small-break LOCA</td>
<td>21.7</td>
<td>4.7</td>
<td>100</td>
</tr>
<tr>
<td>Medium-diameter (2 inches)</td>
<td>Medium-break LOCA</td>
<td>60.5</td>
<td>8.7</td>
<td>100</td>
</tr>
<tr>
<td>Large-diameter (8 inches)</td>
<td>Large-break LOCA</td>
<td>216.3</td>
<td>23.0</td>
<td>100</td>
</tr>
</tbody>
</table>
Table 3.10-12 PFM analysis conditions

<table>
<thead>
<tr>
<th>PFM analysis code</th>
<th>WinPRAISE 4.24 (Windows version of PRAISE)</th>
</tr>
</thead>
<tbody>
<tr>
<td>Pipe materials</td>
<td>Equivalent Type 316 stainless steel</td>
</tr>
<tr>
<td>Diameter and wall thickness of pipe</td>
<td>See in Table 3.10-10</td>
</tr>
<tr>
<td>Existence probability of manufacturing defects</td>
<td>Based on Mood’s theory [11], Hahn’s equation [12], and Harris’s data [13]</td>
</tr>
<tr>
<td>Distribution of size of manufacturing defect</td>
<td>Based on exponential distribution of Marshall [14] and equation of Abramowitz [15]</td>
</tr>
<tr>
<td>PSI and inspection quality</td>
<td>PSI, RT* and UT*; UT inspection quality, Marginal [18]</td>
</tr>
<tr>
<td></td>
<td>[16,17]</td>
</tr>
<tr>
<td>Transient event</td>
<td></td>
</tr>
<tr>
<td>Start-stop</td>
<td>Coolant temperature, 288°C; Stress, 21 kgf/mm²; Frequency, 2 times/year</td>
</tr>
<tr>
<td>Uniform stress</td>
<td>Stress, 21 kgf/mm²; Frequency, 10 times/year</td>
</tr>
<tr>
<td>Thermal shock</td>
<td>Temperature change, 149°C (step-like); Frequency, 10 times/year</td>
</tr>
<tr>
<td>Frequency of ISI</td>
<td>No inspection</td>
</tr>
<tr>
<td>Crack propagation rate</td>
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</tr>
<tr>
<td>Leak detection capability</td>
<td>1 gpm (gallon per minute) [13]</td>
</tr>
</tbody>
</table>

Note: *, Radiographic test; **, Ultrasonic test

Table 3.10-13 Example of conditional core damage probability for typical nuclear power station [10]

<table>
<thead>
<tr>
<th>Initiating event</th>
<th>Conditional core damage probability</th>
</tr>
</thead>
<tbody>
<tr>
<td>Small-break LOCA</td>
<td>6.8×10⁻⁴</td>
</tr>
<tr>
<td>Medium-break LOCA</td>
<td>7.7×10⁻⁴</td>
</tr>
<tr>
<td>Large-break LOCA</td>
<td>2.1×10⁻³</td>
</tr>
</tbody>
</table>
Table 3.10-14 PFM analysis input conditions (Conditions of ISI frequency)

<table>
<thead>
<tr>
<th>Condition of ISI frequency</th>
<th>Inspection quality</th>
</tr>
</thead>
<tbody>
<tr>
<td>(i) No inspection</td>
<td>−</td>
</tr>
<tr>
<td>(ii) Only once inspection after ten years from operation start-up</td>
<td></td>
</tr>
<tr>
<td>(iii) Only once inspection after twenty years from operation start-up</td>
<td>Marginal</td>
</tr>
<tr>
<td>(iv) Only once inspection after thirty years from operation start-up</td>
<td></td>
</tr>
<tr>
<td>(v) Once inspection per ten years from operation start-up</td>
<td></td>
</tr>
</tbody>
</table>

(b) Computation results

The rupture probability for one welding line, event frequency and core damage frequency calculated under the conditions mentioned above are listed in Table 3.10-15.

Table 3.10-15 Calculation results available to safety evaluation

<table>
<thead>
<tr>
<th>Initiating event</th>
<th>Rupture probability per one welding line</th>
<th>Event frequency (/reactor year)</th>
<th>Core damage frequency (/reactor year)</th>
</tr>
</thead>
<tbody>
<tr>
<td>Small-break LOCA</td>
<td>3.1×10⁻¹⁰</td>
<td>7.7×10⁻¹⁰</td>
<td>5.2×10⁻¹³</td>
</tr>
<tr>
<td>Medium-break LOCA</td>
<td>5.1×10⁻¹³</td>
<td>1.3×10⁻¹²</td>
<td>9.9×10⁻¹⁶</td>
</tr>
<tr>
<td>Large-break LOCA</td>
<td>5.9×10⁻¹⁶</td>
<td>1.5×10⁻¹⁵</td>
<td>3.1×10⁻¹⁸</td>
</tr>
</tbody>
</table>

Fig. 3.10-10 shows the change of the core damage frequency depending on ISI frequency. Fig. 3.10-11 also shows the ratio to the core damage frequency without ISI.
Fig. 3.10-10 Change of CDF depending on ISI frequency

Fig. 3.10-11 Relative value for CDF in total LOCA
(c) Discussions

As shown in Table 3.10-15, the core damage frequency can be estimated from individual LOCA frequency due to the primary pipe break and the conditional core damage probability. For the example mentioned above, the rupture probability per one welding line has a large effect on the core damage frequency directly. This may be why the pipe rupture probability directly affects the core damage frequency because the initiating event is only LOCA.

Fig. 3.10-10 shows the relationship between the core damage frequency and ISI frequency. As an overall tendency, the core damage frequency is lower when ISI is conducted every ten years. When ISI is conducted only once, early ISI is likely to decrease. However, the core damage frequency for the ISI after twenty years is lower than that for after ten years. This is thought to be why the flaws to cause the pipe rupture develop and are removed by the ISI after twenty years. Also, the ISI for the small-bore pipes which have the highest core damage frequency is effective on the purpose of decreasing the whole core damage frequency.

Fig. 3.10-11 shows the ISI decreases the LOCA frequency by approximately 40 to 80%. This example demonstrated the most effective action to improve the safety is the maintenance activity (the determination of the inspection period and the frequency and the classification of systems to be inspected) because it decreases the core damage frequency of small break LOCA in which the absolute value to decrease the core damage frequency is the largest.

In the example mentioned above, the safety evaluation was performed by estimating the relative values to the core damage frequency assuming the absolute values of the core damage frequency and the condition of no ISI action. In the evaluation, the only LOCA was assumed as initiating event, the outer diameter of pipe and the analysis conditions were simplified and the primary nuclear piping was classified to three groups of large one, medium one and small one as segment division condition. Finally it was concluded the evaluation method could be used for the maintenance activity.

3.10.4 Summary

The application method of the PFM analysis result to the PRA evaluation and the application example are demonstrated. At present, the application method of the PFM analysis result to the PRA evaluation has been put into practical use as risk informed in-service inspection (RI-ISI). The RI-ISI can achieve the rational maintenance activity that decreases the number of inspection points and keeps the same safety as the conventional methods because the priority of the inspection points can be determined and the significant points can be checked by ISI preferentially.

Also, the application example that the PFM analysis and the results were used for the PRA evaluation was shown. In the example, the maintenance activity was discussed as for the determination of the inspection period and the frequency and the classification of
systems to be inspected. Finally the applicability of the PFM analysis to the maintenance activity was clearly demonstrated.

REFERENCES


Chapter 4  Verification & Validation of PFM
4.1 PFM Round Robin Analyses of Reactor Pressure Vessels

Tsuruga Unit 1, the oldest commercial nuclear power plant in Japan, marked the 40th anniversary on the 14th, March, 2010 from its start of commercial operation. The life-time extension until 2016 was decided in January 2010[1]. Other old NPPs will need some rational evaluation of plant safety to make judgment of life extension some day in the near future. Probabilistic fracture mechanics (PFM) has been regarded as a promising technique for this purpose.

Japan Atomic Energy Agency (JAEA: formerly called as JAERI) had sponsored research committees on PFM organized by Japan Society of Mechanical Engineers (JSME) and Japan Welding Engineering Society (JWES) for around two decades. This work still continues with almost the same members in JWES. The purpose of the continuous activity is to provide probabilistic approaches in several fields of integrity problems of nuclear power plant. These Japanese research activities on PFM are summarized in Table 4.1-1 [2]. Great efforts have been paid to round robin analyses as shown in the table. The purpose of these round robin analyses was to evaluate the precision of PFM programs and to raise the level of analyzers’ technique in the participating research groups. In order to enhance this activity, the international round robin program in Asian countries was planned to develop international communication and cooperation of PFM technique in this area where the nuclear power development seems to be activated rapidly.

The first phase of International PFM Round Robin (RR) analysis was performed focusing on the structural integrity of reactor pressure vessels (RPVs) during pressurized thermal shock events from 2009 to 2011 [3]. The purposes of this program were to establish reliable procedures to evaluate fracture probability of reactor pressure vessels and to maintain the continuous cooperation among Asian institutes in the probabilistic approach to nuclear safety. The phase 1 of this RR was successfully completed with many participants from Japan, Korea, Taiwan and China.

The phase 2 of RR analysis [4] is to compare the scheme and results related to the assessment of structural integrity of RPV for the events with safety important in the design consideration but relatively low fracture probability. The problems are defined mainly focusing on pressure – temperature limit (P-T curves) in a normal cool-down condition and the low temperature over-pressurization (LTOP) transient for BWR plants [5].

Problems and results of these two International RR programs are summarized in this section.
Table 4.1-1 Progress of PFM researches sponsored by JAEA*(formerly JAERI)
(Reproduced from Ref. [2])

<table>
<thead>
<tr>
<th>Period</th>
<th>Executing Organization</th>
<th>Components</th>
<th>Main Activities</th>
<th>Remarks</th>
</tr>
</thead>
<tbody>
<tr>
<td>1988 ~ 1990</td>
<td>JWES&lt;sup&gt;1)&lt;/sup&gt; PFM WG under LE Sub-Committee</td>
<td>RPV</td>
<td>• Survey of existing PFM codes and numerical and fracture mechanics models for PFM analysis</td>
<td>• PFM Round Robin Analysis of RPV</td>
</tr>
<tr>
<td></td>
<td></td>
<td></td>
<td>• Research on numerical algorithm</td>
<td></td>
</tr>
<tr>
<td>1991</td>
<td>MRI&lt;sup&gt;2)&lt;/sup&gt; PFM Committee</td>
<td>RPV</td>
<td>• Survey of input data</td>
<td>• Survey of analysis model</td>
</tr>
<tr>
<td>1992 ~ 1994</td>
<td>JSME&lt;sup&gt;3)&lt;/sup&gt; RC111 Committee</td>
<td>RPV</td>
<td>• Survey of analysis model and research on numerical algorithm</td>
<td>Proposal of a standard guideline for PFM analysis</td>
</tr>
<tr>
<td></td>
<td></td>
<td>Piping</td>
<td>• Round Robin Analysis of RPV under operating load and PTS</td>
<td></td>
</tr>
<tr>
<td></td>
<td></td>
<td></td>
<td>• Round Robin Analysis of Piping</td>
<td></td>
</tr>
<tr>
<td>1996 ~ 2000</td>
<td>JWES PFM Sub-Committee</td>
<td>RPV</td>
<td>• Refinement of PFM methodology: Input seismic load, SIF database, Treatment of embedded crack</td>
<td>Practical application to structural integrity issues of LWR components</td>
</tr>
<tr>
<td></td>
<td></td>
<td>Piping</td>
<td>• Application to ISI code</td>
<td></td>
</tr>
<tr>
<td></td>
<td></td>
<td></td>
<td>• Application to RII, cost/benefit analysis in inspection strategy</td>
<td></td>
</tr>
<tr>
<td></td>
<td></td>
<td>SG</td>
<td>• Utilization of PASCAL to round robin analyses</td>
<td></td>
</tr>
<tr>
<td></td>
<td></td>
<td></td>
<td>• Survey of application in other fields, need in structural integrity issues in LWR components</td>
<td></td>
</tr>
<tr>
<td>2001 ~ Present</td>
<td>JWES&lt;sup&gt;*&lt;/sup&gt; PFM Sub-Committee</td>
<td>Ditto</td>
<td>Ditto</td>
<td>Ditto</td>
</tr>
</tbody>
</table>

* PFM researches sponsored by JAERI were completed in 2000. PFM Sub-Committee in JWES has succeeded the research activities as a voluntary research.
1) The Japan Welding Engineering Society
2) Mitsubishi Research Institute Incorporation
3) The Japan Society of Mechanical Engineers
4.1.1 International RR program (Phase 1) [3]

4.1.1.1 Participants The RR program was proposed at the 7th ASINCO[6] in 2008 and performed from 2009 with participants of six research groups from Japan, four from Korea and one from Taiwan. After the program started, one group joined from China. The number of all participants becomes 12 from four Asian countries. A variety of software covered ANSYS, ABAQUS, FAVOR and PASCAL2 for calculation of temperature and stress distributions in RPV, and WinPraise, FAVOR and PASCAL2[7-9] for PFM analyses.

4.1.1.2 Description of RR Problem In this program, two sorts of PTS transients are selected for the RR problem; Typical PTS and SGTR transients. These transients are characterized by different cooling rates during the transients with a constant system pressure as shown in Fig. 4.1-1. The other conditions are summarized in detail in Table 4.1-2.

Before performing probabilistic analyses, deterministic analyses for temperature and stress distributions in vessel wall during transients were solved by each participant and results were compared.

For a probabilistic problem, a basic problem using SGTR transient without inspection was defined. Problems 1 is for the PTS transient and the other settings are the same as the basic problem. Problem 2 treats the inspection performance as shown in Fig. 4.1-2. Model A is the best with high inspection performance, Model C is a marginal case with relatively low performance, and Model B is the middle case.

Problem 3 is set for sensitivity analyses for copper contents (0.1, 0.2 and 0.3 wt.%) in RPV steel and the initial RTNDT (-10
Probabilistic Fracture Mechanics for Risk-Informed Activities - Fundamentals and Applications - 
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and -40°C). Since these parameters have influences for the degree of irradiation embrittlement and fracture toughness values of RPV steel, the fracture probabilities by varying these parameters will show dependences. Additionally Problem 4 can be set by each participant for any other sensitivity analyses.

<table>
<thead>
<tr>
<th>Item</th>
<th>Description</th>
<th>Value</th>
</tr>
</thead>
<tbody>
<tr>
<td><strong>Vessel Dimensions</strong></td>
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<td>200 mm</td>
</tr>
<tr>
<td></td>
<td>Inner radius</td>
<td>2000 mm</td>
</tr>
<tr>
<td><strong>PTS transient</strong></td>
<td>Initial water temperature</td>
<td>288 (°C) [=stress free temp.]</td>
</tr>
<tr>
<td></td>
<td>Final water temperature</td>
<td>65.6 (°C)</td>
</tr>
<tr>
<td></td>
<td>Cooling rate (exponential)</td>
<td>0.0025 (1/sec)</td>
</tr>
<tr>
<td></td>
<td>Inner pressure</td>
<td>6.895 (MPa) constant</td>
</tr>
<tr>
<td><strong>SGTR transient</strong></td>
<td>Initial water temperature</td>
<td>288 (°C) [=stress free temp.]</td>
</tr>
<tr>
<td></td>
<td>Final water temperature</td>
<td>93.3 (°C)</td>
</tr>
<tr>
<td></td>
<td>Cooling rate (exponential)</td>
<td>0.000667 (1/sec)</td>
</tr>
<tr>
<td></td>
<td>Inner pressure</td>
<td>6.895 (MPa) constant</td>
</tr>
<tr>
<td><strong>Material properties for</strong></td>
<td>Coefficient of heat transfer</td>
<td>1817 (W/m²/K)</td>
</tr>
<tr>
<td>thermal analysis**</td>
<td>Density</td>
<td>7600 (kg/m³)</td>
</tr>
<tr>
<td></td>
<td>Thermal conductivity</td>
<td>54.60 (W/m²/K) (20°C)</td>
</tr>
<tr>
<td></td>
<td></td>
<td>45.80 (W/m²/K) (300°C)</td>
</tr>
<tr>
<td></td>
<td>Specific heat</td>
<td>488.722 (J/kg/K) (20°C)</td>
</tr>
<tr>
<td></td>
<td></td>
<td>568.520 (J/kg/K) (300°C)</td>
</tr>
<tr>
<td><strong>Material properties for</strong></td>
<td>Young’s modulus</td>
<td>204 GPa (20°C)</td>
</tr>
<tr>
<td>stress analysis**</td>
<td></td>
<td>185 GPa (300°C)</td>
</tr>
<tr>
<td></td>
<td>Poisson’s ratio</td>
<td>0.3</td>
</tr>
<tr>
<td></td>
<td>Thermal expansion coefficient</td>
<td>1.090×10⁻⁵ (1/K) (20°C)</td>
</tr>
<tr>
<td></td>
<td></td>
<td>1.490×10⁻⁵ (1/K) (300°C)</td>
</tr>
<tr>
<td><strong>Initial crack</strong></td>
<td>Crack direction</td>
<td>Axial direction</td>
</tr>
<tr>
<td></td>
<td>Crack geometry</td>
<td>Semi-elliptical surface crack</td>
</tr>
<tr>
<td></td>
<td>Aspect ratio</td>
<td>a/c = 1/3 (c is the half crack length.)</td>
</tr>
<tr>
<td></td>
<td>Crack depth distribution</td>
<td>Exponential (Marshall)</td>
</tr>
<tr>
<td></td>
<td>Prob. of non-detection</td>
<td>*</td>
</tr>
<tr>
<td></td>
<td>Average of Initial RTNDT</td>
<td>*</td>
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Table 4.1-2  Conditions for deterministic and PFM analyses
### Material properties for fracture mechanics analysis

<table>
<thead>
<tr>
<th>Parameter</th>
<th>Value or Details</th>
</tr>
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<tbody>
<tr>
<td>Std dev. of Initial RT&lt;sub&gt;NDT&lt;/sub&gt;</td>
<td>10 (°C)</td>
</tr>
<tr>
<td>Prediction Formula of ΔRT&lt;sub&gt;NDT&lt;/sub&gt;</td>
<td>US NRC R.G.1.99 Rev.2</td>
</tr>
<tr>
<td>Std deviation of ΔRT&lt;sub&gt;NDT&lt;/sub&gt;</td>
<td>0.0</td>
</tr>
<tr>
<td>Average of Cu content</td>
<td>0.3 (wt%) *</td>
</tr>
<tr>
<td>Std deviation of Cu content</td>
<td>10% of average</td>
</tr>
<tr>
<td>Average of Ni content</td>
<td>1.0 (wt%)</td>
</tr>
<tr>
<td>Std deviation of Ni content</td>
<td>0.05 (wt%)</td>
</tr>
<tr>
<td>K&lt;sub&gt;I&lt;/sub&gt; (ORNL average curve)</td>
<td>Standard deviation is 15% of avg.</td>
</tr>
<tr>
<td>K&lt;sub&gt;a&lt;/sub&gt; (ORNL average curve)</td>
<td>Standard deviation is 10% of avg.</td>
</tr>
<tr>
<td>Upper shelf fracture toughness</td>
<td>219.8 (MPa·m&lt;sup&gt;1/2&lt;/sup&gt;)</td>
</tr>
<tr>
<td>Flow stress</td>
<td>551.6 (MPa)</td>
</tr>
<tr>
<td>Yield stress</td>
<td>489 (MPa) (20°C)</td>
</tr>
<tr>
<td></td>
<td>423 (MPa) (300°C)</td>
</tr>
<tr>
<td>Neutron Fluence</td>
<td></td>
</tr>
<tr>
<td>Std deviation of fluence</td>
<td>10% of average</td>
</tr>
<tr>
<td>Attenuation of fluence</td>
<td>0.0094 (mm&lt;sup&gt;-1&lt;/sup&gt;)</td>
</tr>
<tr>
<td>Others</td>
<td></td>
</tr>
<tr>
<td>Warm Pre-stress</td>
<td>not considered</td>
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<tr>
<td>Truncation of distribution</td>
<td>5 times of standard deviation</td>
</tr>
</tbody>
</table>

* Parameters for sensitivity analyses

### 4.1.1.3 Results and Discussions

(a) **Deterministic Analysis**  
According to the RR procedure, each participant submitted the deterministic analysis results. All results from participants are summarized in Figs. 4.1-3 and 4.1-4 for temperature and stress distributions for SGTR transient, and for PTS transient. For comparison, the results at 20 min. and 30 min. after the transient start are selected in these figures. These results almost agree with each other. Although the results of temperature distributions are differed only a few degrees from each other, the stress distributions scattered significantly.
(b) Probabilistic Analyses

(1) Basic, Problems 1, 2 and 3

For probabilistic analyses, each participant submitted the analysis results as shown in Figs. 4.1-5 and 4.1-6. In the figures, CPF stands for the Conditional Probability of Fracture, i.e., the probability of crack propagation through the vessel wall. From Fig. 4.1-5 (a), the effect of transient severity, i.e., cooling rates difference between PTS and SGTR transients is clearly found. The effect of inspection qualities is also seen from Fig. 4.1-5 (b). Similarly, Figs. 4.1-5 (c) and (d) indicate the effects of copper contents and initial RTNDT on probabilities of fracture. Fig. 4.1-6 shows the results of Korean participants which indicate similar tendencies and probabilities.
(2) Problem 4: Sensitivity analyses

There are many problems calculated by participants. The results may appear in other summary papers from each country. Fig. 4.1-7 shows a few examples of them. Fig. 4.1-7(a) shows the effect of upper bound of Cu contents, and Fig. 4.1-7(b) shows the effect of simulation number.

Fig. 4.1-5 Comparison of probabilistic analysis results (Participant F)
Fig. 4.1-6  Comparison of results of probabilistic analyses

Fig. 4.1-7  Results of sensitivity analyses related to Problem 3
4.1.2 Round robin analyses (Phase 2) [4]

4.1.2.1 Outline  The round robin analyses are performed by the following steps;

1. Deterministic evaluation: Temperature and stress in vessel wall during the events
2. Probabilistic analyses: Base, R1, R2, and R3
3. Sensitivity analyses (R4): Optional

4.1.2.2 Analysis condition  Major input data are listed in Table 4.1-3. No cladding is considered in the problems. One of temperature transients employed is a normal cool-down condition, which is prescribed in the design code, such as ASME B&PV code Sec. III. The maximum cooling rate of 55°C/h is used for the analyses. For the system pressure during cool-down, the following equation is applied as a criterion for allowable stress intensity factor;

\[ 2K_{lm} + K_{IT} = K_{IC} \left( MPa \sqrt{m} \right) \]

where \( K_{lm} \) is a stress intensity factor due to internal pressure, and \( K_{IT} \) stress intensity factor due to thermal stress. The \( K_{lm} \) and \( K_{IT} \) are calculated by the following equations [2];

\[ K_{lm} = M_m \left( \frac{pR}{t} \right) \]

\[ M_m = 0.926 \cdot 1.099 \]

\[ K_{IT} = M_T \cdot CR \cdot \left( \frac{t}{0.0254} \right)^{2.5} \]

\[ M_T = 0.953 \cdot 10^{-3} \cdot 1.099 \cdot 9/5, \quad CR = \text{Cool-down Rate (°C/h)} \]

where \( p \) is a system pressure (MPa), \( R \) and \( t \) are the inner radius (m) and thickness (m) of RPV, respectively.

The distribution of Fracture toughness \( K_{IC} \) curve is recently known as a Weibull type based on the weakest link theory. However in this RR problem, the normal distribution is used for simplification in a similar way to the Phase 1. The \( K_{IC} \) curve is also modeled as a normal distribution. Both \( K_{IC} \) and \( K_{IT} \) mean curves are as follows;

\[ K_{IC} = 1.43 \cdot \{36.5 + 3.084exp[0.036 \cdot (T \text{ - } RT_{NDT} + 56)]\} \]

\[ K_{IT} = 1.25 \cdot \{29.5 + 1.344exp[0.026 \cdot (T \text{ - } RT_{NDT} + 89)]\} \]

Another transient is LTOP event for BWR plants, which is described later. The values of neutron fluence simulated are of the end of plant design life (EOL) and hypothetically extended EOL.

As an initial flaw, a postulated surface crack for deterministic analysis or crack depth distributions for probabilistic analysis are applied. For the probabilistic analyses, mean flaw number distribution shown in Fig. 4.1-8 is used, which was copied from the reference [5].
Table 4.1-3 PFM Analysis conditions

<table>
<thead>
<tr>
<th>Vessel Dimensions</th>
<th>Thickness 160 mm</th>
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<td>Inner radius 3200 mm</td>
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<table>
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<th>Cooling-down</th>
<th>Initial water temperature 288 (°C) [=stress free temp.]</th>
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<tr>
<td></td>
<td>Final water temperature 20 (°C)</td>
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<td></td>
<td>Cooling rate 55 (°C/h)</td>
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<td>Inner pressure Allowable pressure*1</td>
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<th>Material properties for thermal analysis</th>
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<tr>
<td></td>
<td>Density 7600 (kg/m³)</td>
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<tr>
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<td>Thermal conductivity 54.60 (W/m²/K) (20°C)</td>
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<tr>
<td></td>
<td>45.80 (W/m²/K) (300°C)</td>
</tr>
<tr>
<td></td>
<td>Specific heat 488.722 (J/kg/K) (20°C)</td>
</tr>
<tr>
<td></td>
<td>568.520 (J/kg/K) (300°C)</td>
</tr>
</tbody>
</table>

<table>
<thead>
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<th>Material properties for stress analysis</th>
<th>Young’s modulus 204 GPa (20°C)</th>
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<tbody>
<tr>
<td></td>
<td>185 GPa (300°C)</td>
</tr>
<tr>
<td></td>
<td>Poisson’s ratio 0.3</td>
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<tr>
<td></td>
<td>Thermal expansion coefficient 1.090 × 10⁻⁵ (1/K) (20°C)</td>
</tr>
<tr>
<td></td>
<td>1.490 × 10⁻⁵ (1/K) (300°C)</td>
</tr>
</tbody>
</table>

<table>
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<tr>
<th>Initial crack</th>
<th>Crack direction Axial direction</th>
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<td></td>
<td>Crack geometry Semi-elliptical for surface crack</td>
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<tr>
<td></td>
<td>Elliptical for embedded crack</td>
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<tr>
<td></td>
<td>Aspect ratio a/c = 1/3 (c is the half crack length.)</td>
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<tr>
<td></td>
<td>Crack depth distribution 40 (mm) for a postulated surface crack</td>
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<td>Fig.4.1-8 for probabilistic analysis</td>
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<table>
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<tr>
<th>Material properties for fracture mechanics analysis</th>
<th>Mean of Initial RT_{NDT} -30 for weld and 0 for base (°C)*2</th>
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<td>Std dev. of Initial RT_{NDT} 10 (°C)</td>
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<td></td>
<td>Prediction Formula of ΔRT_{NDT} JEAC4201 or 10CFR50.61a</td>
</tr>
<tr>
<td></td>
<td>Std deviation of ΔRT_{NDT} 0.0</td>
</tr>
<tr>
<td></td>
<td>Mean of Cu content 0.3 (wt%) *2</td>
</tr>
<tr>
<td></td>
<td>Std deviation of Cu content 10% of mean value</td>
</tr>
<tr>
<td></td>
<td>Mean of Ni content 0.6 for base metal 1.0 for weld (wt%)</td>
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<tr>
<td></td>
<td>Std deviation of Ni content 0.05 (wt%)</td>
</tr>
<tr>
<td></td>
<td>K_{lc} (ORNL mean curve) Stand. Dev. is 15% of mean</td>
</tr>
<tr>
<td></td>
<td>K_{la} (ORNL mean curve) Stand. Dev. is 10% of mean</td>
</tr>
</tbody>
</table>
Upper shelf fracture toughness 219.8 (MPa·m$^{1/2}$)
Flow stress 551.6 (MPa)
Yield stress 489 (MPa) (20°C)
423 (MPa) (300°C)

Neutron fluence
- Std deviation of fluence 10% of mean value
- Attenuation of fluence 0.0094 (mm$^{-1}$)

Others
- Warm Pre-stress not considered
- Truncation of distribution 5 times of standard deviation

*1: Value determined from the following equation; $2K_{Im} + K_{IT} = K_{IC}$
*2: Parameters for sensitivity analyses, noted by * in Table 4.1-3, are given as follows;
- Initial RTNDT: 0°C for weld metal, 30°C for base metal
- Cu contents: 0.1 wt%, 0.2 wt%, 0.3 wt%
- Fluence: 0.1, 1, 10 (10$^{19}$ n/cm$^2$, E>1MeV)

Fig. 4.1-8 Mean Flaw Number Distribution of the RPV Beltline Region against the Percentage of the Vessel Wall Thickness [5]

4.1.2.3 RR problems description

(a) Deterministic analyses

The temperature and stress values within the vessel wall during the cooling-down condition are to be calculated with each participant’s own method, such as Pre-PASCAL, i.e. FEM program as preprocessor for PASCAL3 [10]. Data to be provided are temperature and stress distributions through the vessel wall at several timings during the loading conditions. The
stress intensity factor corresponding to a semi-elliptical surface crack with a dimension of 40 mm depth by 240 mm length is also calculated at the same timings during the conditions. Examples of temperature and stress distributions for the cooling-down condition are shown in Figs. 4.1-9 and 4.1-10.

Fig. 4.1-9 Examples of temperature distribution during cooling-down condition

Fig. 4.1-10 Examples of stress distribution during cooling-down condition

Time histories of $K_I$ at the surface and deepest points of a postulated surface crack for the cooling-down condition are shown in Fig. 4.1-11 as an example. The results in the figure
were obtained from a deterministic analysis. The change in trend of $K_I$ at time = 250 s is caused by the inner pressure, i.e., the inner pressure is constant with the maximum value of 7 MPa when time ≤ 250 s, while it is determined by the criterion for the allowable stress intensity factor when time > 250 s.

![Fig. 4.1-11 Time histories of $K_I$ during the cooling-down condition](image)

(b) BASE Case Probabilistic Problem:

Probabilities of crack initiation and through-wall cracking are to be calculated with a postulated surface crack of 40 mm depth for the cooling-down condition described above. Non-destructive inspection is not considered, and the mean value of initial $RT_{NDT}$ is 0°C for base metal and the mean of Cu content is 0.3 wt%. The other input data are shown in Table 4.1-3.

Fig. 4.1-12 represents the conditional probability of crack initiation (CPI) as a function of fast neutron fluence for the cooling-down condition. The results were obtained from probabilistic analyses. CPI values are compared for the cases that a crack was postulated in base and weld metals. CPI values for each case increased with fast neutron fluence, but the values were different due to differences of initial $RT_{NDT}$ and chemical composition. Besides the CPI results, the conditional probability of through-wall cracking (CPTWC) was also analyzed in this study and the same results as those in Fig. 4.1-12 were obtained. This means that crack initiated during cooling-down would not be arrested because of the relatively small temperature gradient compared with that generated through pressurized thermal shock events in PWRs.

(c) Case 1 of Probabilistic Problem: R1 (LTOP transient)
Probabilities of crack initiation and through-wall cracking are calculated for the LTOP transient condition as shown in Fig. 4.1-13 [5]. The other input data for this problem are the same as BASE case problem.

Fig. 4.1-12 CPI for the cooling-down condition for base case.

Fig. 4.1-13 The Hypothetical Transient of LTOP Event [5]
Fig. 4.1-14 represents CPI as a function of fast neutron fluence for the LTOP event obtained from sensitivity analyses on initial crack size. In this case, the depth distribution of the initial crack is taken into consideration.

![CPI for Case 1](image1)

(d) Case 2 of Probabilistic Problem: R2 (Sensitivity analysis for initial crack size)

Mean value of Cu content is 0.3 wt% and the initial $RT_{NDT}$ is 0°C for base metal. The distribution of initial crack as shown in Fig. 4.1-8 is used. The existence of multiple cracks in a vessel is not considered. Therefore, total probabilities of crack initiation and fracture are calculated by multiplying the probabilities for a single crack by crack density and the volume of vessel.

![CPI for Case 2](image2)
(e) Case 3 of Probabilistic Problem: R3 (Sensitivity analysis for Cu and Ni contents and the initial RT\textsubscript{NDT})

Sensitivity analyses for Cu content (0.1, 0.2 and 0.3 wt.%), Ni content (0.6, 0.8 and 1.0 wt.%) and the initial RT\textsubscript{NDT} of -30 and 0°C for weld metal or 0 and 30°C for base metal were performed. Other parameters are the same as BASE or Case 2.

Fig. 4.1-16 CPI for Case 3 (Cu and Ni contents were varied, 10CFR50.61a).

Fig. 4.1-16 represents CPI as a function of fast neutron fluence for the cooling-down condition obtained from sensitivity analyses on chemical compositions. The prediction formula used in this case is taken from 10CFR50.61a. It is clearly shown that CPI values decrease with Cu and Ni because lower Cu and Ni cause lower ΔRT\textsubscript{NDT} in accordance with the prediction formula. Besides the CPI results, CPTWC were also analyzed, and almost the same results as those in Fig. 4.1-16 were obtained. In addition, the prediction formula provided in JEAC4201 was also investigated for chemical compositions. From the results, similar tendencies in CPI and CPTWC were found but the variations of detailed CPI and CPTWC values compositions were a little larger than those obtained from the prediction formula in 10CFR50.61a. Fig. 4.1-17 shows the effect of initial RT\textsubscript{NDT} and the difference of material, i.e. base metal and weld metal. The results of three groups show similar CPI.
Activities for some attendants are described in this section. Fig. 4.1-18 represents an example of sensitivity analyses on probabilistic evaluation models of fracture toughness $K_{IC}$ and $K_{IC}$. In these analyses, JAEA used the newly developed probabilistic models based on experimental data of Japanese RPV steels. The results in Fig. 4.1-18 show that general tendencies of all results are similar to each other.

Fig. 4.1-18 CPI for Case 4 (comparison of $K_{IC}$ models).
4.1.3 Concluding remarks

The international round robin on PFM analysis was initiated in Asian countries, and two phases of program were carried out. The first one is for PTS of PWR, and the second one is a normal cool-down condition and the low temperature over-pressurization (LTOP) transient for BWR. These problems and the results were presented at a series of ASINCO workshops.

ACKNOWLEDGMENTS

This paper includes a lot of efforts related to the PFM RR analyses by every participant. The authors thank to all of the members participated in this RR activity from Korea, Taiwan, China and Japan.

REFERENCES


4.2 PFM Round Robin Analyses of LWR Piping

Probabilistic methods have been used increasingly within the last two decades as the reliability assessment methods of the structures in nuclear facilities substituting for the deterministic methods. As the reliability assessment methods of the structures in nuclear facilities, probabilistic fracture mechanics (PFM) is applied to the reliability assessment of structures with flaws. As part of Risk-Informed asset management, research on the development and application of PFM analysis codes has been performed in electric utilities, research institutes, and the societies in Japan. In order to spread the application of this technology, it is necessary to conform to the procedures of analysis of the codes and the validity of results of analysis which are developed individually. As risk-informed asset management requires that the reliability assessment result should be an absolute value, the validity of the reliability assessment test is indicated by carrying out the benchmark analyses with the two independently developed codes and comparing the results [1].

The two codes PASCAL-SP [2-4] and PEPPER-M [5] used for benchmark analysis have been independently developed by Japan Atomic Energy Agency (JAEA) and TEPCO systems Corporation (TEPSYS), respectively. After the validity of the procedure of analysis in these codes was conformed, the reliability assessment of PLR piping containing cracks due to stress corrosion cracking (SCC) was carried out according to some regulatory requirements and the rules on fitness-for-service in Japan.

4.2.1 PFM code (PASCAL-SP)

As a part of the probabilistic structural integrity research for LWR components, PFM code PASCAL-SP (PFM Analysis of Structural Components in Aging LWR - Stress Corrosion Cracking at Welded Joints of Piping) has been developed [2-4]. The code evaluates the failure probability of an aged welded joint of piping with regard to mainly SCC based on a Monte Carlo calculation method. The development of the code has been aimed to improve the accuracy and reliability of PFM analysis by introducing new analysis methodologies and algorithms considering the recent development in the fracture mechanics methodologies and computer performance. PASCAL-SP code conforms to approaches of Nuclear and Industrial Safety Agency of Japan (NISA) and Codes for Nuclear Power Generation Facilities - Rules on Fitness-for-Service for Nuclear Power Plants - of the Japan Society of Mechanical Engineers (JSME FFS) [6]. Fig. 4.2-1 shows the outline of the evaluation procedure in PASCAL-SP code. After sampling of random variables for initial analysis condition with regard to the scatter and uncertainties, plant operation situations including some events such as transient, earthquake and inspection are simulated. During the plant operation, flaw initiation and growth due to SCC and fatigue flaw growth caused by seismic stress and transient events are consider. The equation of SIF for circumferential inner surface crack in JSME FFS was applied to the crack growth calculation. In-service inspections are done periodically. Accuracy of flaw detection and sizing for the in-service
inspection are modeled by using the data of Ultrasonic Test & Evaluation for Maintenance Standards (UTS) project [7] by Japan Power Engineering and Inspection Corporation (JAPEIC) and Japan Nuclear Energy Safety organization (JNES). When SCC is detected, sizing of the crack is performed. Then, the integrity assessment by JSME FFS is conducted. The integrity assessment judges repair-replace or continuous use with virtual crack growth in a time period.

The results of failure judgment are classified into intact, leak, break and maintenance-replacement. The piping is evaluated as leak when flaws grow through the wall and leak is detectable. Leak rate is calculated by the method of Shinokawa, et al. [8] which is based on the researches of Henry [9] and Moody [10]. The surface roughness on the crack is not considered as a random variable. The piping is evaluated as break when applied stress exceeds failure stress. Failure judgment is also performed during flaw growth evaluation. The piping is judged as maintenance-replacement when a detected flaw at in-service inspection has influence on the integrity within five years operation according to the defect evaluation method of JSME FFS Codes.

The procedures above are repeated many times to evaluate failure probabilities (Monte Carlo method). A concept of segment, which is a group of welding lines, is introduced to evaluate entire failure probabilities for multiple welding lines. The failure probability of each segment is evaluated after evaluation of that of each welding line. The system failure probability is then evaluated based on the failure probability of each segment. Large numbers of calculation by Monte Carlo methods generally takes a long time. Hence, a function of parallel calculation in PC cluster environment has been introduced. It reduces the calculation time effectively. For example, a calculation which takes 7,000 seconds in a single CPU PC finishes only at 1,000 seconds in 8 parallel CPU calculations.

Fig. 4.2-1 Evaluation flowchart of PASCAL-SP
4.2.2 PFM code (PEPPER-M)

PEPPER-M code can perform the reliability assessment for austenitic stainless steel piping with flaws due to SCC [5]. The method is based on Monte-Carlo technique considering many sample cases in a piping section, where the initiation and growth of cracks are calculated and piping failures, including leaks and rupture, are evaluated. A notable feature is that multiple cracks can be treated, consequently, assessment of coalescence of cracks and intricate break evaluation of piping section have been included. Moreover, the in-service inspection (ISI) and integrity evaluation by Fitness-for-Service (FFS) code are integrated into the analysis, and the contribution to decrease of failure probability can be assessed. Key parameters are determined on a probability basis with the designated probability type of failure throughout the procedure. Size, location and time of crack initiation, coefficients of crack growth due to SCC and factors for piping failure assessment are included in those parameters. With this method the reliability level of the piping through the operation periods can be estimated and the contribution of various parameters including ISI can be quantitatively evaluated.

A flow chart for reliability assessment of piping with SCC is shown in Fig. 4.2-2. The evaluation is performed for many sampling cases in a cross section (weld joint) of a pipe with various cracking conditions. Procedures are executed in designated time steps from the start to the end of the plant operation. The time step can be selected for each procedure. First, crack initiation conditions are sampled, and constants for crack growth and material properties are determined on a probability

![Fig. 4.2-2 Evaluation flowchart of PEPPER-M](image-url)
basis. Then, crack growth due to SCC and fatigue is calculated, and possible leak or pipe break is evaluated. In the year that ISI is implemented, inspections to detect cracks are performed and integrity is assessed. This is shown on the right side of the calculation flow in Fig. 4.2-2. Finally, each sample case falls into either the integral, leak, break, or repair category, and each probability is calculated from the number of samples in each category.

4.2.3 Benchmark analysis

4.2.3.1 Analysis condition  The benchmark analysis conditions of Example 1 are listed in Table 4.2-1. Because the purpose of this study was a benchmark analysis of two codes, the condition of the crack growth assessment and the stable assessment was made simple. By the same reason, crack growth due to fatigue is not considered in this condition. The fundamentals are set based on the information in the instruction manual [11] of Nuclear and Industrial Safety Agency (NISA) and the study [12] performed by Machida et al. who statistically processed the data in the instruction manual of NISA. In Example 1, when there is a single initial crack of a depth a of 0.5 mm in the piping joint, the conditional failure probability is evaluated up to the 40th year of operation. In Example 2, the failure probability is evaluated by taking the number of cracks of a depth of 0.5 mm and the period in which the cracks are generated as the probability models. The crack initiation condition is given in Table 4.2-2. The number of cracks and the crack occurrence period are cited from the reference [12] and are taken as random variables. In Example 3, the failure probability is evaluated by implementing ISI in Example 2. The crack detection conditions are given in Table 4.2-3 and Fig. 4.2-3. A non-detection probability of 0.5% in which a condition of a 100% detection every five years is considered for ISI crack detection. The pipe selected for evaluation was a 16 inch dia. Sch. 100 pipe of the primary loop recirculation system in a BWR plant. The crack growth rate of SCC (stress corrosion crack) is changed as the crack that developed in a heat affected zone (HAZ) on the inner surface of the pipe spreads to the weld metal. Fig. 4.2-4 shows the crack growth along the sheet gauge. Stress due to internal pressure, bending stress due to gravity, bending stress due to thermal expansion, and welding residual stress were taken as factors contributing to SCC crack growth. The welding residual stress distribution was cited from NISA instruction manual. Residual stress distribution is shown in Fig. 4.2-5. Models were made by taking residual stress distribution on the sheet gauge position as a sixth-order polynomial in PEPPER-M and a fourth-order polynomial in PASCAL-SP. The conditions for coalescence differ for the two codes. In PASCAL-SP, \( S \leq \max(0.5d_1,0.5d_2) \) shown in Fig. 4.2-6 is used as the condition for coalescence according to the fitness-for-service standards. The determination of whether or not two cracks are on the same plane is made by the equation \( H \leq 10 \text{ mm} \) when \( S \leq 5 \text{ mm} \), and by the equation \( H < 2S \) when \( S > 5 \text{ mm} \). In PEPPER-M, all the cracks are presumed to be on one plane, coalescence occurs when \( S < 0 \).
Table 4.2-1 Analysis conditions of Example 1

<table>
<thead>
<tr>
<th>Condition</th>
<th>Value</th>
</tr>
</thead>
<tbody>
<tr>
<td>Base condition</td>
<td>16 inch Sch.100 Pipes</td>
</tr>
<tr>
<td>Pipe diameter $D_0$ [mm]</td>
<td>406.4</td>
</tr>
<tr>
<td>Wall thickness $t$ [mm]</td>
<td>26.2</td>
</tr>
<tr>
<td>SIF</td>
<td>API RP579</td>
</tr>
<tr>
<td>Initial crack depth $a$ [mm]</td>
<td>0.5 (Constant)</td>
</tr>
<tr>
<td>Initial crack length $2c$ [mm]</td>
<td>Exponential distribution $f = \frac{1}{\lambda} \exp \left( \frac{x}{\lambda} \right)$, $\lambda = 14$ [mm]</td>
</tr>
<tr>
<td>Crack depth reaching weld metal $dc$ [mm] (referring to Fig. 4.2-4)</td>
<td>$dc = L + \alpha$</td>
</tr>
<tr>
<td>Distance from weld metal to a crack $L$ [mm] (referring to Fig. 4.2-4)</td>
<td>Log-normal distribution $\mu_{LN} = 1.15$ [mm], $\sigma_{LN} = 1.39$</td>
</tr>
<tr>
<td>Adding length $\alpha$ [mm] (referring to Fig. 4.2-4)</td>
<td>Normal distribution $\mu = 2.99$ [mm], $\sigma = 1.31$ [mm]</td>
</tr>
<tr>
<td>Flow stress $\sigma_f$ [MPa]</td>
<td>Normal distribution $\mu = 307$ [MPa], $\sigma = 17$ [MPa]</td>
</tr>
<tr>
<td>Fatigue crack growth rate [m/Cycles]</td>
<td>$da/dN = 8.17 \times 10^{-12} t_{r}^{0.5} (\Delta K)^{3.0} / (1 - R)^{2.12}$, $t_{r} = 1000$ (MPa $\sqrt{m}$)</td>
</tr>
<tr>
<td>Crack growth rate due to SCC (HAZ) [m/s]</td>
<td>Coefficient : $C$, Median : $\mu_{LN} = 9.018 \times 10^{-14}$, Standard deviation : $\sigma_{LN} = 0.303$, Exponent : $m$, Constant : $m = 2.161$</td>
</tr>
<tr>
<td>Crack growth rate due to SCC (Weld metal) [m/s]</td>
<td>Coefficient : $C$, Median : $\mu_{LN} = 1.017 \times 10^{-14}$, Standard deviation : $\sigma_{LN} = 1.120$, Exponent : $m$, Constant : $m = 2.161$</td>
</tr>
<tr>
<td>Weld Residual Stress (Fig. 4.2-5)</td>
<td>Normal operating loads [MPa] Stress due to internal pressure $Pm = 34.9$ (pressure 9MPa) Stress due to dead weight $Pb = 10$ Stress due to thermal expansion $Pe = 40$</td>
</tr>
<tr>
<td>Transient load</td>
<td>Frequency 1 [event/year] Stress due to internal pressure $Pm = 34.9$ (pressure 9MPa) Stress due to dead weight $Pb = 10$ (*Regular stress) Stress due to thermal expansion $Pe = 40$</td>
</tr>
<tr>
<td>Load for stability assessment</td>
<td>Stress due to internal pressure $Pm = 34.9$ (pressure 9MPa) Stress due to dead weight $Pb = 10$ Stress due to thermal expansion $Pe = 40$</td>
</tr>
<tr>
<td>operation time [year]</td>
<td>40</td>
</tr>
</tbody>
</table>
For the position of the coalesced cracks, the position of larger crack projection surface area is referred to in PASCAL-SP, and the position of deeper crack is referred to in PEPPER-M. Because the position of the coalesced cracks pertains to the switching of the crack progression style after coalescence, it can exert influence on the final result. As unstable fracture assessment methods, the extreme load evaluation method is used when there is a single crack, and an expanded form of the extreme load evaluation method is used when there are multiple cracks. In PEPPER-M, the angle at which the section modulus is least is determined for calculating the collapse load of the pipe having multiple cracks. In PASCAL-SP, the collapse load is calculated using the method by Li et al. [13]. If no unstable fracture occurs with the operating load, the SCCs continue to grow and eventually break through the sheet gauge. The point at which the depth of the crack reaches the sheet gauge is deemed to be a case of 'break'.

Table 4.2-2 Condition of crack initiation

<table>
<thead>
<tr>
<th>The number of cracks per weld joint [-]</th>
<th>Log-normal distribution</th>
</tr>
</thead>
<tbody>
<tr>
<td>Median : $\mu_{LN} = 2.19$</td>
<td></td>
</tr>
<tr>
<td>Standard deviation : $\sigma_{LN} = 0.873$</td>
<td></td>
</tr>
</tbody>
</table>

<table>
<thead>
<tr>
<th>Crack initiation time [year]</th>
<th>Normal distribution</th>
</tr>
</thead>
<tbody>
<tr>
<td>Median : $\mu = 9.21$</td>
<td></td>
</tr>
<tr>
<td>Standard deviation : $\sigma = 0.485$</td>
<td></td>
</tr>
</tbody>
</table>

Table 4.2-3 Probability of detection

<table>
<thead>
<tr>
<th>POD curve</th>
<th>$POD (\alpha) = 1 - \exp[-(a - 0.3434)]$</th>
</tr>
</thead>
<tbody>
<tr>
<td>$a$ [mm]</td>
<td>$\alpha \approx 0.005$ ($=0.5%$)</td>
</tr>
</tbody>
</table>

<table>
<thead>
<tr>
<th>ISI frequency</th>
<th>5 [years/event]</th>
</tr>
</thead>
<tbody>
<tr>
<td>ISI rate</td>
<td>1.00 [-/event]</td>
</tr>
</tbody>
</table>
Fig. 4.2-3 Probability of detection

Fig. 4.2-4 Concept of SCC model

Fig. 4.2-5 Weld residual stress distribution in a pipe

Fig. 4.2-6 Concept of crack coalescence
4.2.3.2 Analysis Results  Fig. 4.2-7 shows the conditional failure probabilities by PEPPER-M and PASCAL-SP in which an initial crack is assumed. In this figure, vertical axis denotes cumulative probability of leak/break. If the PLR piping with a crack is calculated to become unstable fracture and/or plastic collapse, it is judged as 'Break'. If the piping is not calculated to result in unstable fracture but through-wall crack, it is judged as 'Leak'. The cumulative leak probability in the 10th year is 3.8E-02 by PEPPER-M and 2.0E-02 by PASCAL-SP, indicating a difference of about two times between these values. In the 40th year, the cumulative leak probabilities by the two codes are more or less in agreement, with a difference of about 20%. The break probabilities by the two codes in the 10th year differ by a factor of two, and are in good agreement in the 40th year, differing only by about 6%. The cumulative failure probability of PEPPER-M is evaluated to be higher than that of PASCAL-SP. In the areas with smaller cumulative break probability, the difference in the 40th year is smaller, the results being well in agreement.

Fig. 4.2-8 shows the cumulative failure probabilities of Example 2 where the multiple cracks are assumed. In Example 2 in which the number of cracks and the crack occurrence period are taken as random variables, the cumulative break probabilities in the 10th year differ by a factor of about 2 to 4, and are therefore good in agreement, and in the 40th year they differ by only 10% to 50%, indicating better agreement.

![Fig. 4.2-7 Comparisons of cumulative failure probabilities for Example 1](image-url)
This benchmark analysis was carried out after careful review of all the calculation routines. The influences of each item and random variables on the failure probabilities are given in Table 4.2-4. Differences were identified in the extrapolation of stress intensity factor calculation in probabilistic analysis. This difference was understood to be the difference in the extrapolated stress intensity factor values through careful comparisons of the values in each Monte-Carlo calculation. Table 4.2-5 shows the difference in the stress intensity factor for each crack size corresponding to a certain stress/pipe condition. The difference in the stress intensity factor gets larger for relatively longer cracks. Because the stress intensity factor directly affects the crack growth behavior, it is anticipated here that the difference will affect the failure probability. Coefficient C in crack growth rate relationship due to SCC affected the failure probability. In using Stratified Monte-Carlo method for efficient calculation, this coefficient is recommended to be treated in detail. In this study, the random valuable for SCC coefficient C was recommended to be ranged from $-4\sigma$ to $+4\sigma$. Even though there are minor differences in the conditions for fracture evaluation method, etc., for multiple cracks, it has been ascertained by detailed comparison of both codes that these differences have little influence on the failure probability.

Fig. 4.2-8 Comparisons of cumulative failure probabilities for Example 2
Table 4.2-4 Influence of input parameters

<table>
<thead>
<tr>
<th>Item</th>
<th>Condition</th>
<th>Influence</th>
<th>Note on influence</th>
</tr>
</thead>
<tbody>
<tr>
<td>Extrapolation of the stress intensity factor to crack size limit</td>
<td>Extrapolation of coefficient table using CEA (4th order polynomial function)</td>
<td>Large</td>
<td>Concerning LEAK judgement</td>
</tr>
<tr>
<td>Limit of distance from weld metal to a crack; L</td>
<td>L=0[mm] if L&lt;0[mm]</td>
<td>Large</td>
<td>The failure probabilities are 10 times diff. in the low probability case. The same conditions as PEPPER-M were applied.</td>
</tr>
<tr>
<td>Limit of crack depth reaching weld metal; dc</td>
<td>dc=0[mm] if dc&lt;0[mm]</td>
<td>Large</td>
<td></td>
</tr>
<tr>
<td>Limit of aspect ratio</td>
<td>No</td>
<td>Small</td>
<td></td>
</tr>
<tr>
<td>Crack growth rate Coefficient C for HAZ and weld metal</td>
<td>before; Classical Monte-Carlo</td>
<td>Large</td>
<td></td>
</tr>
<tr>
<td>Coalescence of crack</td>
<td>Coalescence if S ≤ max(0.5d₁,0.5d₂)</td>
<td>Small</td>
<td></td>
</tr>
<tr>
<td>Conditions of considering single planar flaws</td>
<td>H ≤ 10[mm] if S ≤ 5[mm]</td>
<td>Small</td>
<td></td>
</tr>
<tr>
<td>Location of a considered single planar flaws</td>
<td>Larger projected area</td>
<td>Small</td>
<td></td>
</tr>
<tr>
<td>Stability assessment of multiple flaws</td>
<td>Seek the angle of minimum section modulus sequentially</td>
<td>Large</td>
<td></td>
</tr>
<tr>
<td>Definition of &quot;LEAK&quot;</td>
<td>&quot;LEAK&quot; if a/t &gt; 1.0</td>
<td>Small</td>
<td></td>
</tr>
</tbody>
</table>

Definition of "LEAK" before; "LEAK" if a/t > 1.0 and Q > 1gpm Revised; "LEAK" if a/t > 1.0
Fig. 4.2-9 shows the cumulative failure probabilities of Example 3, where the multiple cracks are assumed and in-service inspection (ISI) is performed. In Example 3, the cumulative failure probabilities by both codes are more or less agreed. At the 40th year, the cumulative leak probability by PEPPER-M is six times higher than that by PASCAL-SP. The break probability by PEPPER-M is two times higher. As compared to Example 2, the values of failure probability lowered depending on the effect of the inspection. The failure probabilities increase up to 10th year and tend to saturate, because a relatively large size of crack that has an influence on the failure probability is detected by periodic inspections. The difference in the leak probability up to the 10th year is thought as the difference in the calculation error of comparatively large cracks that occur in the early period. No significant rise is seen in the failure probability thereafter. A relatively large difference in the leak probabilities is considered to come from the fact that the leak probability for Example 3 is smaller by more than 2 orders of magnitude as compared to Example 2. The existence of extremely large cracks and the accuracy of stress intensity factor calculation of long and deep cracks are believed to be the contributing factors for the difference shown here. Further study will be necessary on the extrapolation method of stress intensity factor for a very long or deep crack in the future. Otherwise a conservative way for replacement of such a crack with a through-wall crack or circumferential crack may be considered if this difference provides a significant difference in failure probability analysis using these codes.

<table>
<thead>
<tr>
<th>Crack length $2c$ [deg.]</th>
<th>Crack depth $a/t$ [-]</th>
<th>Diff.$\text{SIF}_{Ka}$</th>
<th>Diff.$\text{SIF}_{Kc}$</th>
<th>Diff.$\text{CGR}_{\Delta a}$</th>
<th>Diff.$\text{CGR}_{\Delta c}$</th>
</tr>
</thead>
<tbody>
<tr>
<td>60</td>
<td>0.25</td>
<td>0.1</td>
<td>0.0</td>
<td>0.5</td>
<td>0.2</td>
</tr>
<tr>
<td></td>
<td>0.50</td>
<td>1.1</td>
<td>0.0</td>
<td>3.3</td>
<td>0.3</td>
</tr>
<tr>
<td></td>
<td>0.75</td>
<td>0.7</td>
<td>0.3</td>
<td>2.2</td>
<td>1.1</td>
</tr>
<tr>
<td>180</td>
<td>0.25</td>
<td>0.8</td>
<td>-0.5</td>
<td>2.5</td>
<td>-1.3</td>
</tr>
<tr>
<td></td>
<td>0.50</td>
<td>0.9</td>
<td>2.1</td>
<td>3.1</td>
<td>6.7</td>
</tr>
<tr>
<td></td>
<td>0.75</td>
<td>0.5</td>
<td>0.1</td>
<td>1.6</td>
<td>0.3</td>
</tr>
<tr>
<td>360</td>
<td>0.25</td>
<td>0.1</td>
<td>-</td>
<td>0.4</td>
<td>-</td>
</tr>
<tr>
<td></td>
<td>0.50</td>
<td>0.7</td>
<td>-</td>
<td>2.4</td>
<td>-</td>
</tr>
<tr>
<td></td>
<td>0.75</td>
<td>0.5</td>
<td>-</td>
<td>1.6</td>
<td>-</td>
</tr>
</tbody>
</table>
4.2.4 Conclusions

The benchmark analysis was executed for the failure probability of the cracked piping due to SCC in typical conditions using the independently developed PFM codes. The cumulative failure probabilities were more or less agreed well within those conditions. Since the small difference is found in some results, the influencing factors for the differences were studied in detail. When studying the reason for the difference, some items and input parameters which have influences on failure probabilities were identified and evaluated. It is important to have the clear understanding of the code when a PFM analysis code is used because there is no standard. When the reason for the difference depends on the difference in calculation procedures of the codes, it may be treated as the characteristics of each code. Through the benchmark analysis, the adequacy of each code was ascertained comparatively with well-defined problems on PLR piping integrity. Thus, it is suggested that each code should be applied as an analysis tool for PLR piping reliability assessment considering the results of benchmark analysis.

ACKNOWLEDGMENTS

This benchmark analysis was executed as part of the activity of the PFM Sub-committee of The Japan Welding Engineering Society (JWES). The authors thank to Prof. Shinobu Yoshimura, who is a chairperson of the committee, and all members for grateful discussions and opinions.
REFERENCES


(Appendix 1) Main Features of PFM Codes
A) PASCAL4

Code name: PASCAL4

(PFM Analysis of Structural Components in Aging LWR version 4)

· Developer

  Jinya Katsuyama*1、Koichi Masaki*1、2、Yuhei Miyaomto*2、Yinsheng Li*1

  *1 Japan Atomic Energy Agency
  *2 Mizuho Information & Research Institute, Inc.

Reference: JAEA-Data/Code 2017-015

· Target and methodology

Failure frequency of reactor pressure vessel due to transients such as pressurized thermal shock events are evaluated considering neutron irradiation embrittlement based on a probabilistic fracture mechanics. It is available to consider uncertainties of chemical composition, potential cracks, reference temperature, neutron fluence distribution, fracture toughness, and crack arrest toughness, etc in the code.

· Features

Features of PASCAL code are listed as follows:

  (1) Fracture criteria based on linear fracture mechanics, elastic-plastic fracture mechanics can be taken into account
  (2) Multiple models for calculating crack growth for surface and embedded cracks
  (3) Brittle fracture evaluation model focusing on the cladding
  (4) The latest irradiation embrittlement prediction method and fracture toughness model for domestic reactor pressure vessel steels
  (5) Confidence evaluation function considering the epistemic and aleatory uncertainties of related influence factors
  (6) Detailed neutron fluence map in the core region can be considered
  (7) Functions to improve the usability
B) PASCAL-SP

Code name: PASCAL-SP

(PFM Analysis of Structural Components in Aging LWR – Stress Corrosion Cracking at Welded Joints of Piping)

Version: 1

- Developer

Kunio Onizawa*1, Hiroto Ito*2, Daisuke Kato*3, Kazuya Osakabe*3, Hiroyuki Nishikawa*3

Current code is improved by Yoshihito Yamaguchi*1, Koichi Masaki*2, Yinsheng Li*1

*1 Japan Atomic Energy Agency
*2 Advanced Algorithm and Systems Co., Ltd.
*3 Mizuho Information & Research Institute, Inc.

Reference: JAEA-Data/Code 2009-025

- Target and methodology

Failure probability including break and leak probabilities for piping welds can be evaluated considering crack growth due to stress corrosion cracking and fatigue based on probabilistic fracture mechanics. The code is applicable to consider the stress corrosion cracking (SCC) at welds in stainless steel piping, primary water environment stress corrosion cracking (PWSCC) in Ni-based alloy welds, and fatigue crack growth due to operation, transients, and seismic loading including beyond design base. Uncertainties of the crack initiation time, crack growth rate, weld residual stress, and in-service inspection, etc, can be treated based on their statistical distributions.

- Features

Features of PASCAL-SP code are listed as follows:

1. It is available for evaluating crack growth due to SCC and fatigue, and for evaluating failure of piping welds based on Japanese codes and standards.
2. Crack detection accuracy and sizing models for in-service inspection based on domestic data is implemented in the code.
3. The uncertainties of weld residual stress distributions at stainless steel piping welds obtained from parametric finite element analyses can be taken into account.
4. Confidence evaluations of failure probabilities are available by considering epistemic and aleatory uncertainties of related influence factors such as seismic loading.
5. Multi-core calculation is available that is suitable for large-scale probabilistic evaluation.
C) PASCAL-EC

Code name: PASCAL-EC

(PFM Analysis of Structural Components in Aging LWR – Erosion Corrosion)

Version: 1

• Developer

Kunio Onizawa*1, Hiroto Ito*2, Daisuke Kato*3, Katsuyuki Shibata*1

Current code is improved by Yoshihito Yamaguchi*1, Noriya Ebine*1, Koichi Masaki*2, Yinsheng Li*1

*1 Japan Atomic Energy Agency
*2 Advanced Algorithm and Systems Co., Ltd.
*3 Mizuho Information & Research Institute, Inc.

Reference: JAEA-Data/Code 2006-001

• Target and methodology

Failure probability due to erosion and corrosion at carbon steel piping are evaluated considering the effect of seismic loading based on probabilistic fracture mechanics. The uncertainties of wall thinning rate and water chemistry environment during operation, and seismic loading, etc, are treated as probabilistic variables in the code.

• Features

Features of PASCAL-EC code are listed as follows:

(1) Uncertainties of flow temperature, flow rate, oxidization density, pH, etc, can be considered in the code.

(2) Failure criteria such as internal pressure burst evaluation equation, empirical base equation for internal pressure and bending moment, net-section criterion against bending moment are implemented. The ratchet evaluation equation due to cyclic loading can also be taken into account.

(3) Failure probabilities for straight, elbow, and T-shape piping can be evaluated considering wall thinning.

(4) A probabilistic evaluation model of wall thinning based on domestic data is also implemented in the code.

(5) Confidence evaluations of failure probabilities are available by considering epistemic and aleatory uncertainties of related influence factors such as seismic loading.
D) MSS-REAL

- Code name: MSS-REAL
  (Material Strength and Structural Reliability Evaluation System)

- Developer
  Shigeru Takaya*1, Naoto Sasaki*2
  *1 Japan Atomic Energy Agency
  *2 Ascend Co., Ltd.

- Contact address
  Unpublished (It will be future public)

- License
  Unpublished (It will be future public)

- Operating environment
  OS: Windows 7 or later
  Memory: More than 320 MB
  Hard Disk: More than 800 MB
  Necessary software: None (REAL-P)

- Target and methodology
  MSS-REAL, Material Strength and Structural Reliability Evaluation System, is a computational tool that supports structural integrity evaluation, mainly of fast reactor components. It consists of five programs that cover from material strength evaluation to structural reliability evaluation. One of them is REAL-P, a structural reliability assessment program based on the probabilistic fracture mechanics approach developed for the evaluation of creep-fatigue crack initiation and propagation probabilities. The features of REAL-P are summarized in the following section.

- Features
  The features of REAL-P are as follows:
  1. It allows three types of evaluations: probability of crack initiation due to creep-fatigue, probability of failure due to crack propagation from a postulated flaw, and probability of failure due to the propagation of an initiated crack.
  2. The evaluation methodologies for crack initiation and propagation, including the formulations of reference stresses and stress intensity factors for deriving a J-integral, comply with “JSME Guidelines on Reliability Evaluation of Fast Reactor Components (to be published)”.
  3. It incorporates material properties of SUS304 and 316FR steel.
  4. Around 40 variables, including material properties and loading conditions, can be treated as random variables.
(5) Both direct- and stratified-sampling are available in applying the Monte-Carlo method.

The development of the MSS-REAL system will be continued to provide standardized tools that incorporate state-of-the-art technologies for material strength evaluation and reliability evaluation of structures and components.
E) PEPPER

- Code name: PEPPER (Probabilistic Evaluation Program for Pipes aiming Economical and Reliable design)

- Developer
  Hideo MACHIDA, Shuhei TAKEDA, Manabu ARAKAWA, Masahiro CHITOSE
  TEPCO Systems Corporation (Original Code)

- Contact address
  Unpublished

- Licence
  Unpublished

- Operating environment
  MS-DOS, UNIX

- Target and methodology
  PEPPER is a nonlinear PFM code applicable to reliability evaluation of pipes having a single crack.

  PEPPER was developed for application to fast reactors initially used in high temperature conditions. In fast reactor piping, plastic and creep deformation not only at a local portion around a crack but also at a general portion of a pipe are assumed. Therefore, in PEPPER, crack growth evaluation using not only a stress intensity factor (fatigue crack growth and SCC (Stress Corrosion Cracking) crack growth) as a linear fracture mechanics parameter but also J-integral (fatigue crack growth) and creep J-integral (creep crack growth) as non-linear fracture mechanics parameters is made possible. Circumferential and axial surface and embedded crack can be evaluated in PEPPER.

  For fracture evaluation, in linear conditions, a linear fracture mechanics, a limit load, an elastic-plastic fracture mechanics and a two-parameter method can be selected while in nonlinear conditions, a limit load evaluation method and a two-parameter evaluation method can be selected.

  A general PFM analysis code targets ‘initial cracks’, whereas PEPPER can perform failure probability (leak and break probability) evaluation taking into account a probability of cracks in service.

- Features
  PEPPER has the following functions.

  (1) Capable to address initiation and growth of a single crack during operation.

  (2) Capable to take into account fatigue, SCC and creep crack growth.
(3) Capable to perform integrity assessment during an evaluation period with the Rules on fitness-for-service, based on ISI (In-Service Inspection) results.

(4) Capable to apply a linear fracture mechanics, a limit load, an elastic-plastic fracture mechanics and a two-parameter method for fracture evaluation.

(5) Enables to calculate a conditional probability of pipe fracture caused by an earthquake.

(6) Performs the stratified Monte Carlo simulation to crack geometry and crack position (only for embedded cracks) in order to improve the calculation efficiency.

(7) Avoids a recurring random number issue when the number of samples is huge enough, by using Mersenne Twister for random numbers.
F) PEPPER-M

- Code name: PEPPER-M
  (Probabilistic Evaluation Program for Pipes aiming Economical and Reliable design for Multiple cracks)

- Developer
  Hideo MACHIDA, Shuhei TAKEDA, Manabu ARAKAWA, Masahiro CHITOSE
  TEPCO Systems Corporation (Original Code)

- Contact address
  Unpublished

- Licence
  Unpublished

- Operating environment
  MS-DOS, UNIX

- Target and methodology
  PEPPER-M is a PFM analysis code developed for the purpose of use in reliability evaluation of recirculation piping of BWR where SCC cracks may occur. According to the observation results of SCC cracks occurred in PLR piping of boiling water reactors (BWRs), there were many cases that several cracks occurred in the same welding line. In addition, since its occurrence timing is largely dispersed, an evaluation with a model considering them is necessary in order to properly evaluate the break probability of actual piping where multiple defects such as SCC may occur. PEPPER-M evaluates the occurrence, growth, coalesce and failure of cracks as a sequence and further can evaluate the reliability of piping by systematic evaluation including management of defects due to in-service inspection considered during operation.

- Features
  PEPPER-M has the following functions.
  (1) Capable to address occurrence, growth and coalescence of multiple circumferential defects of piping.
  (2) Capable to consider fatigue and SCC as a crack growth mode.
  (3) Capable to evaluate integrity assessment during an evaluation period with the Rules on fitness-for-service, based on the results of ISI.
  (4) Can evaluate minimum break strength of a pipe cross-section having plural circumferential cracks.
  (5) Capable to consider fracture of repaired weld lines.
  (6) Performs the stratified Monte Carlo simulation to SCC crack growth rate in order to improve the efficiency of the calculation.
(7) Avoids a recurring random number issue when the number of samples is huge enough, by using Mersenne Twister for random numbers.
G) WinPRAISE

Code name: PRAISE
(Piping Reliability Analysis Including Seismic Event)
Version: WinPRAISE

• Developer
  D. O. Harris*1, D. Dedhia*1
  *1 Engineering Mechanics Technology, Inc.

• Target and methodology
  Failure probability for piping is evaluated considering crack growth due to stress corrosion cracking and fatigue based on probabilistic fracture mechanics.

• Features
  Features of WinPRAISE code are described as follows:
  It is known as a PFM analysis code for evaluating the failure probability of piping. It is possible to evaluate the failure probability considering the seismic loading. It can be used in evaluation analysis on RI-ISI which is being conducted in the United States.
H) FAVOR

Code name: FAVOR (Fracture Analysis of Vessels – Oak Ridge)
Version: v16.1

- Developer
  P. T. Williams*1, T. L. Dickson*1, B. R. Bass*1 and H.B. Klasky*1
  *1 Oak Ridge National Laboratory


- Target and methodology
  The FAVOR computer program has been developed to perform deterministic and probabilistic risk-informed analyses of the structural integrity of a nuclear reactor pressure vessel (RPV) when subjected to a range of thermal-hydraulic events. The focus of these analyses is on the beltline region of the RPV. The PFM model in FAVOR is based on the application of Monte Carlo techniques in which deterministic fracture analyses are performed on a large number of stochastically-generated RPV trials or realizations.

- Features
  Features of FAVOR code are listed as follows:
  1. It is available to consider new detailed flaw-characterization distributions from NRC research obtained via work performed at Pacific Northwest National Laboratory (PNNL).
  2. Detailed neutron fluence maps can be taken into account.
  3. A new ductile-fracture model simulating stable and unstable ductile tearing is implemented in the code.
  4. A new embrittlement correlation is implemented in the code.
  5. It is available that an improved PFM methodology that incorporates modern PRA procedures for the classification and propagation of input uncertainties and the characterization of output uncertainties as statistical distributions.
I) SPEC

- Code name: SPEC
  (Simplified Probabilistic Evaluation System for Cracked Pipes)

- Developer
  Hideo MACHIDA
  TEPCO SYSTEMS CORPORATION (Original Code)

- Contact address
  Unpublished

- Licence
  Unpublished

- Operating environment
  Microsoft Excel VBA (Visual Basic for Applications macros). Windows 2000 or later, Microsoft Office 2000 or later.

- Target and methodology
  SPEC assumes an initial crack (or detected crack) on piping inner surface and analyzes a failure probability (leakage or break) arising from growth of a crack due to SCC and fatigue by the Monte Carlo simulation. In order to efficient probabilistic calculation, a stratified Monte Carlo sampling method is adopted in which the depth and length of the initial crack are divided into two-dimensional cells and Monte Carlo sampling calculation is performed for each cell.

- Features
  SPEC has the following functions.
  
  (1) Capable to address initiation and growth of a single crack during operation.
  (2) Capable to take into account fatigue and SCC crack growth.
  (3) Capable to evaluate integrity assessment during an evaluation period with the Rules on fitness-for-service, based on the results of ISI.
  (4) The limit load and the elastic-plastic fracture mechanics can be applied as fracture assessment methods.
  (5) Performs the stratified Monte Carlo simulation to a crack shape in order to improve the efficiency of the calculation.
J) xLPR

Code name: xLPR (Extremely Low Probability of Rupture)
Version: 1.0

- Developer
  David L. Rudland*1 and Craig Harington*2
  *1 U. S. Nuclear Regulatory Commission
  *2 Electric Power Research Institute


- Target and methodology
  The xLPR is a modular-based probabilistic fracture mechanics computer code for evaluating the risk against pressure boundary integrity failure. The xLPR incorporates a set of deterministic models that represent the full range of physical phenomena necessary to evaluate both fatigue and PWSCC degradation modes from crack initiation through failure. These models are each implemented in a modular form and linked together by a probabilistic framework that contains the logic for xLPR execution, exercises the individual modules as required, and performs necessary administrative and bookkeeping functions.

- Features
  Features of xLPR code are listed as follows:
  (1) Input variables and models include parameter distribution (for example material properties, load boundary conditions, and inspection frequency) and uncertainty quantification.
  (2) Probabilistic Modeling–Probabilistic framework uses Monte Carlo, either simple random sampling or discrete sampling methods (binned analysis), or analytic method.
  (3) The uncertainties both input variables and models are modeled and the code permits assessment of the effects of mitigation efforts on the system.
  (4) Outputs–All data generated are saved for post-processing analyses, such as sensitivity studies or failure (loss of coolant accident - LOCA size) frequency as a function of time.
  (5) The code also models and the code permits assessment of the effects of mitigation efforts on the system.
K) PREFACE

- Code name: PREFACE
  (PRobabilistic Evaluation Code For Aged Cast stainless steel pipE)

- Developer
  Kiminobu HOJO, Takatoshi HIROTA, Mayumi OCHI, Shotaro HAYASHI, and Wataru NISHI
  Mitsubishi Heavy Industries, Ltd.

- Contact address
  Unpublished

- Licence
  Unpublished

- Operating environment
  MS-DOS, UNIX

- Target and methodology
  PREFACE was developed to determine the target flaw size for the performance
demonstration (PD) system for accreditation of non-destructive inspectors of cast stainless
steel. In order to obtain the target flaw size, failure probabilities for several flaw sizes are
calculated with probabilistic variables for material properties and a parameter of stress level.
The flaw size corresponding to the target failure probability is determined from the relation of
failure probability and flaw size.

- Features
  PREFACE is
  (1) Capable to calculate the leak probability of a pipe with probabilistic variables for fatigue
  crack growth rate and flaw shape
  (2) Capable to input probabilistic variables of material properties (tensile properties and
  fracture toughness) of thermally aged cast stainless steel depending on ferrite amount and
  thermal aging condition
  (3) Capable to calculate failure probability of a pipe under the failure modes of plastic
  collapse, ductile crack initiation or ductile instability by incorporating two parameters’
  method with probabilistic variables for flaw shape and material properties
  A basic function of PREFACE was validated by benchmark analysis with participants of
  Japanese organizations and the target flaw size was validated by comparing with the flaw size
  tables in the Code Case N-838.
L) **Dr. Mainte**

Code name: Dr. Mainte

- **Developer:**
  - Shinobu Yoshimura (The University of Tokyo)
  - Kazuo Furuta (The University of Tokyo)
  - Yoshihiro Isobe (Nuclear Fuel Industries Ltd.)
  - Mitsuyuki Sagisaka (Nuclear Fuel Industries Ltd.)
  - Michiyasu Noda (Institute of Nuclear Safety System, Incorporated)
  - Hiroshi Akiba (Allied Engineering Corporation)

- **Contact address:**
  - Yoshihiro Isobe
  - Nuclear Fuel Industries Ltd.
  - Engineering Service Division
  - Senior fellow specialist
  - email: isobe@nfi.co.jp

- **Operating environment**
  - Windows 7

- **Target and methodology**

  General outline

  Dr. Mainte, an integrated simulator for maintenance optimization of LWRs (Light Water Reactors) is based on PFM (Probabilistic Fracture Mechanics) analyses. The concept of the simulator is to provide a decision-making system to optimize maintenance activities for typical components and piping systems in nuclear power plants totally and quantitatively in terms of safety, availability, economic rationality, environmental impact and social acceptance. For the further improvement of the safety and availability of nuclear power plants, the effect of human error and its reduction on the optimization of maintenance activities can be analyzed. In addition, an approach of reducing human error is proposed using the divided multi-dimensional visualization method and AI (Artificial Intelligence). More recently it has been applied to the maintenance optimization of social infrastructures such as expressway facilities.

**History**

A number of PRA (Probabilistic Risk Assessment) studies have been applied to the optimization of maintenance activities in nuclear power plants from a viewpoint of safety focusing on the risk of core meltdown. However, even a small-scale incident of component, which never causes the core meltdown, resulted in reactor shutdown and economic losses. Accordingly, in addition to the safety analysis focusing on the risk of core meltdown, it is very useful to develop a simulator that can establish maintenance strategies in terms of availability.
and economic efficiency of nuclear power plants.

With the background above mentioned, the authors first studied risk and economic models of maintenance activities of SG (Steam Generator) tubes of PWRs (Pressurized Water Reactors) [1,2]. After that, we developed Dr. Mainte, an integrated simulator, for the maintenance optimization of LWRs [3]. The concept of the simulator is to provide a decision-making system to optimize maintenance activities for typical components and piping systems comprehensively and quantitatively in terms of safety, availability and economic rationality (both from cost and profit), environmental impact and social acceptance under various maintenance strategies including altering inspection frequency and inspection accuracy, conducting sampling inspection, repairs and/or replacements, introducing various maintenance rules, long-term fuel cycles, etc. Besides, a function of visualization of the simulated results by a divided multi-dimensional visualization method was also developed in order to support a decision-making process to optimize the maintenance activities. For the further improvement of the safety and availability of nuclear power plants, the effect of human error and its reduction on the optimization of maintenance activities have been studied. In addition, an approach of reducing human error is proposed using the divided multi-dimensional visualization method and AI (Artificial Intelligence) to analyze questionnaire for personnel of maintenance activities. More recently it has been applied to the maintenance optimization of social infrastructures such as expressway facilities including various types of anchor volts and concrete structures.
(Appendix 2) List of Publications and Related Publications
List of Publications and Related Publications

A. Papers in Journals and Bound Volumes (Refereed)

1. Applications of Probabilistic Fracture Mechanics to FBR Components
   Kiminobu HOJO, Makoto TAKENAKA, Hitoshi KAGUCHI, Genki YAGAWA, Shinobu YOSHIMURA
   Nuclear Engineering and Design, 142-1, pp.43-49, 1993

2. Probabilistic Fracture Mechanics Analysis based on Three-Dimensional J-Integral Database
   G.-W. Ye, Genki YAGAWA, Shinobu YOSHIMURA
   Engineering Fracture Mechanics, 44-6, pp.887-893, 1993

3. Study on Life Extension of Aged RPV Material Based on Probabilistic Fracture Mechanics:
   Japanese Round Robin
   Genki YAGAWA, Shinobu YOSHIMURA, Norihiko HANDA, Tetsuro UNO, Katumi WATASHI, Terutaka FUJIoka, Hiroyoshi UEDA, Masayoshi UNO, Kiminobu HOJO, Shuzo UEDA

   Shinobu YOSHIMURA, M-Y.Zhang, Genki YAGAWA
   Nuclear Engineering and Design, 158, pp.341-350, 1995

5. New Probabilistic Fracture Mechanics Approach with Neural Network-Based Crack Modeling:
   Its Application to Multiple Cracks Problem
   Shinobu YOSHIMURA, J.S. LEE, Genki YAGAWA, Kiyoshi SUGIOKA, Tadahiko KAWAI
   Fatigue and Fracture Mechanics in Pressure Vessels and Piping, PVP-Vol. 304, 1995

6. Direct Analysis Method for Probabilistic Fracture Mechanics
   Hiroshi AKIBA, Shinobu YOSHIMURA, Genki YAGAWA

7. Recursive Distribution Method for Probabilistic Fracture Mechanics
   Hiroshi AKIBA, Shinobu YOSHIMURA, Genki YAGAWA
   Computational Mechanics, 18, pp.175-185, 1996

8. A Study on Probabilistic Fracture Mechanics for Nuclear Pressure Vessels and Piping
   Genki YAGAWA, Shinobu YOSHIMURA
   International Journal of Pressure Vessels and Piping, 73, pp.97-107, 1997

9. Probabilistic Fracture Mechanics Analyses of Nuclear Pressure Vessels under PTS Events
   Genki YAGAWA, Shinobu YOSHIMURA, Naoki SONEDA, Masashi HIRANO
   Nuclear Engineering and Design, 174, pp.91-100, 1997

10. Probabilistic Fracture Mechanics of Nuclear Structural Components:
    Consideration of Transition from Embedded Crack to Surface Crack
    Genki YAGAWA, Yasuhiro KANTO, Shinobu YOSHIMURA
    Nuclear Engineering and Design, 191, pp.263-273, 1999

11. Probabilistic Fracture Mechanics Analysis of Nuclear Structural Components:
    A Review of Recent Japanese Activities
    Genki YAGAWA, Yasuhiro KANTO, Shinobu YOSHIMURA, Hideo MACHIDA, Katsuyuki SHIBATA,
12 Risk-Benefit Analyses of SG Tube Maintenance Based on Probabilistic Fracture Mechanics
Yoshihiro ISOBE, Mitsuyuki SAGISAKA, Shinobu YOSHIMURA, Genki YAGAWA

13 Development of a PFM Code for Evaluating Reliability of Pressure Components Subject to
Transient Loading
Katsuyuki SHIBATA, Daisuke KATO, Yinsheng LI
Nuclear Engineering and Design, 208, pp.1-13, 2001

14 Sensitivity Analysis of Failure Probability on PTS Benchmark Problems of Pressure Vessel
Using a Probabilistic Fracture Mechanics Analysis Code
Yinsheng LI, Daisuke KATO, Katsuyuki SHIBATA

15 Improvements to a Probabilistic Fracture Mechanics Code for Evaluating the Integrity of a
RPV under Transient Loading
Yinsheng LI, Daisuke KATO, Katsuyuki SHIBATA, Kunio ONIZAWA

16 Optimization of Operation and Maintenance of Nuclear Power Plant by Probabilistic Fracture
Mechanics
Noriyoshi MAEDA, Shinich NAKAGAWA, Genki YAGAWA, Shinobu YOSHIMURA
Nuclear Engineering and Design, 214, 1-2, pp.1-12, 2002

17 Probabilistic Fracture Mechanics Analysis for Pipe Considering Dispersion of Seismic Loading
Hideo MACHIDA, Manabu ARAKAWA, Yoshio KAMISHIMA
Nuclear Engineering and Design 212, pp.1-12, 2002

18 Probabilistic Fracture Mechanics Analysis of Nuclear Piping Considering Variation of Seismic
Loading
Hideo MACHIDA, Shinobu YOSHIMURA

19 Seismic Loads for Crack Stability Assessment, in a Review of Leak-Before-Break (LBB)
Applicability
Hideo MACHIDA, Norimichi YAMASHITA, Shinobu YOSHIMURA, Genki YAGAWA
Nuclear Engineering & Design, 235, pp.21-31, 2005

20 Recent Japanese Probabilistic Fracture Mechanics Researches Related to Failure Probability of
Aged RPV
Katsuyuki SHIBATA, Yasuhiro KANTO, Shinobu YOSHIMURA, Genki YAGAWA

21 Economic Evaluation of Maintenance Strategies for Steam Generator Tubes Using Probabilistic
Fracture Mechanics and a Financial Method
Yoshihiro ISOBEND, Mitsuyuki SAGISAKA, Shinobu YOSHIMURA, Genki YAGAWA

22 Balancing Material Selection and Inspection Requirements in Structural Design of Fast Breeder
Reactors on “System Based Code” Concept
Tai ASAYAMA, Nobuchika KAWASAKI, Masaki MORISHITA, Hiroshi SHIBAMOTO,
Kazuhiko INOUE
Nuclear Engineering and Design, 238, pp. 417-422, 2008

23 Development of Probabilistic Fracture Mechanics Analysis Code for Pipes with Stress
Corrosion Cracks
Hideo MACHIDA, Manabu ARAKAWA, Norimichi YAMASHITA, Shinobu YOSHIMURA
24 Study on Risk-Informed In-Service Inspection for BWR Piping
Tamio KORIYAMA, Yinsheng LI, Yoshikane HAMAGUCHI, Masahiro YAMASHITA
Mitsumasa HIRANO
Journal of Nuclear Science and Technology, 46(8), pp.846-873, 2009

25 Probabilistic Prediction of Crack Depth Distributions Observed in Structures Subjected to
Thermal Fatigue
Tai ASAYAMA, Hideki TAKSHO, Takehiko KATO
ASME Journal of Pressure Vessel Technology, 131, 011402-1, 2009

26 Development of Probabilistic Fracture Mechanics Analysis Codes for Reactor Pressure Vessels
and Piping Considering Welding Residual Stress
Kunio ONIZAWA, Hiroyuki NISHIKAWA, Hiroto ITOH
International Journal of Pressure Vessels and Piping, 87-1, pp.2-10, 2010

27 Development of System Based Code (1) Reliability Target Derivation of Structures and Components
Kenichi KURISAKA, Ryodai NAKAI, Tai ASAYAMA, Shigeru TAKAYA

28 Development of System Based Code (2) Application of Reliability Target for Configuration of
ISI requirement
Shigeru TAKAYA, Satoshi OKAJIMA, Kenichi KURISAKA, Tai ASAYAMA, Hideo MACHIDA, Yoshio KAMISHIMA

29 Benchmark analysis on PFM analysis codes for aged piping of nuclear power plants
Hiroto ITOH, Yinsheng LI, Kazuya OSAKABE, Kunio ONIZAWA, Shinobu YOSHIMURA

30 Benchmark Analysis and Numerical Investigation on Probabilistic Fracture Mechanics
Analysis Codes for NPPs Piping
Yinsheng LI, Hiroto ITOH, Kazuya OSAKABE, Kunio ONIZAWA, Shinobu YOSHIMURA
International Journal of Pressure Vessels and Piping, 99-100, pp.61-100, 2012

31 Summary of International PFM Round Robin Analyses among Asian Countries on Reactor
Pressure Vessel Integrity during Pressurized Thermal Shock
Yasuhiro KANTO, Myung Jo JHUNG, Kuen TING, Y.-B.He, Kunio ONIZAWA, Shinobu YOSHIMURA

32 Study on the Structural Integrity of RPV Using PFM Analysis Concerning Inhomogeneity of
the Heat-Affected Zone
Koichi MASAKI, Jinya KATSUYAMA, Kunio ONIZAWA

33 Effect of Partial Welding on the Residual Stress and Structural Integrity of Piping Welds
Jinya KATSUYAMA, Koichi MASAKI, Kunio ONIZAWA
Journal of Pressure Vessel Technology, 135(6), pp. 061403 (1-8), 2013

34 Assessment of Residual Stress Due to Overlay-Welded Cladding and Structural Integrity of a
Reactor Pressure Vessel
Jinya KATSUYAMA, Hiroyuki NISHIKAWA, Makoto UDAGAWA, Mitsuyuki NAKAMURA, and Kunio ONIZAWA
Journal of Pressure Vessel Technology, 135(5), pp. 051402 (1-9), 2013
35 Benchmark Analysis on Probabilistic Fracture Mechanics Analysis Codes Concerning Fatigue Crack Growth in Aged Piping of Nuclear Power Plants
Jinya KATSUYAMA, Hiroto ITO, Yinsheng LI, Kazuya OSAKABE, Kunio ONIZAWA, Shinobu YOSHIMURA

36 Probabilistic Structural Integrity Analysis of Reactor Pressure Vessels During Pressurized Thermal Shock Events
Koichi MASAKI, Jinya KATSUYAMA, Kunio ONIZAWA

37 Application of the System Based Code Concept to the Determination of In-Service Inspection Requirements
Shigeru TAKAYA, Tai ASAYAMA, Yoshio KAMISHIMA, Hideo MACHIDA, Daigo WATANABE, Satoru NAKAI, Masaki MORISHITA
Journal of Nuclear Engineering and Radiation Science, 1-1, 011004, 2014

38 Failure Probability Analyses for PWSCC in Ni-based Alloy Welds
Makoto UDAGAWA, Jinya KATSUYAMA, Kunio ONIZAWA, Yinsheng LI

39 Benchmark analyses of probabilistic fracture mechanics for cast stainless steel pipe
Kiminobu HOJO, Shotaro HAYASHI, Wataru NISHI, Masayuki KAMAYA, Jinya KATSUYAMA, Koichi MASAKI, Masaki NAGAI, Toshiki OKAMOTO, Yasukazu TAKADA, Shinobu YOSHIMURA
Mechanical Engineering Journal, 3-4, 16-00083, 2016

40 Determination of in-service inspection requirements for fast reactor components using System Based Code concept
Shigeru TAKAYA, Yoshio KAMISHIMA, Hideo MACHIDA, Daigo WATANABE, Tai ASAYAMA
Nuclear Engineering and Design, 305, pp.270-276, 2016

41 Improvement of Probabilistic Fracture Mechanics Analysis Code PASCAL-SP with Regard to PWSCC
Akihiro MANO, Yoshihito YAMAGUCHI, Jinya KATSUYAMA, Yinsheng LI
ASME Journal of Nuclear Engineering and Radiation Science, 5, pp. 031505(1-8), 2019

42 A New Probabilistic Evaluation Model for Weld Residual Stress
Akihiro MANO, Jinya KATSUYAMA, Yuhei MIYAMOTO, Yoshihito YAMAGUCHI, Yinsheng LI

43 Guideline on Probabilistic Fracture Mechanics Analysis for Japanese Reactor Pressure Vessels
Jinya KATSUYAMA, Kazuya OSAKABE, Yinsheng LI, Shinobu YOSHIMURA
Transactions of ASME, Journal of Pressure Vessel Technology, Accepted for Publication
B. Conference Papers

1. Probabilistic Analysis of Unstable Fracture of LWR Piping Using Recursive Distribution Method
   Hiroshi AKIBA, Shinobu YOSHIMURA, Genki YAGAWA, Masabumi SUZUKI
   Trans. of the 14th International Conference on Structural Mechanics in Reactor Technology, 10, pp.31-38, 1997

   Genki YAGAWA, Shinobu YOSHIMURA
   First International Symposium on Safety and Structural Integrity, pp.25-46, 1997

   Genki YAGAWA, Shinobu YOSHIMURA
   Trans. of the 14th International Conference on Structural Mechanics in Reactor Technology, 4, pp.541-552, 1997

   Hiroshi AKIBA, Masabumi SUZUKI, Shinobu YOSHIMURA, Genki YAGAWA
   The 7th International Conference on Structural Safety and Reliability (ICOSAR '97), pp.1297-1300, 1998

   Noriyoshi MAEDA, Shinichi NAKAGAWA, Genki YAGAWA, Shinobu YOSHIMURA

6. Probabilistic Fracture Mechanics Analyses of Steam Generator Tube : A Sensitivity Study
   Toyokazu AOKI, Yoshihiro ISOBE, Shinobu YOSHIMURA, Genki YAGAWA

7. Probabilistic Fracture Mechanics Analysis of Nuclear Reactor Pressure Vessels : Effects of Transition from Embedded Crack to Surface Crack
   Yasuhiro KANTO, Genki YAGAWA

8. Risk-Benefit Analyses of SG Tube Maintenance Based on Probabilistic Fracture Mechanics
   Yoshihiro ISOBE, Mitsuyuki SAGISAKA, Shinobu YOSHIMURA, Genki YAGAWA

   Yinsheng LI, Daisuke KATO, Katsuyuki SHIBATA
   7th International Conference on Nuclear Engineering (ICONE), CD-ROM, 7266, 1999

    Hideo MACHIDA
11 A Probabilistic Approach to Optimize SG Tube Maintenance
Mitsuyuki SAGISAKA, T. AOKI, Yoshihiro ISOBE, Shinobu YOSHIMURA, Genki YAGAWA
Proc. International Conference on Life Management and Life Extension of Power Plant,
pp.272-283, 2000

12 Evaluation of Maintenance Strategies for Steam Generator Tubes in Pressurized Water Reactors (I)
-Risk Analysis-
Mitsuyuki SAGISAKA, Yoshihiro ISOBE, Shinobu YOSHIMURA, Genki YAGAWA
Probabilistic Safety Assessment and Management (PSAM5), pp.2525-2538, 2000

13 Optimization of SG Tube Maintenance Strategies Using Probabilistic Approach
Mitsuyuki SAGISAKA, Yoshihiro ISOBE, Shinobu YOSHIMURA, Genki YAGAWA
Proc. 8th International Conference on Nuclear Engineering (ICONE 8), CD-ROM, 2000

14 Probabilistic Fracture Mechanics Based Assessments for Aged Nuclear Structural Components
Shinobu YOSHIMURA, Genki YAGAWA, Yasuhiro KANTO, Katsuyuki SHIBATA
16th International Conference on Structural Mechanics in Reactor Technology (SMiRT 16),
p.485, 2001

15 Recent Research Activity of Probabilistic Fracture Mechanics for Nuclear Structural
Components in Japan
Genki YAGAWA, Yasuhiro KANTO, Shinobu YOSHIMURA, Katsuyuki SHIBATA
16th International Conference on Structural Mechanics in Reactor Technology (SMiRT 16),
p.455, 2001

16 Effect of Dispersion of Seismic Load on Integrity of Nuclear Power Plant Piping
Hideo MACHIDA
Transactions, 16th International Conference on Structural Mechanics in Reactor Technology

17 Probabilistic Fracture Mechanics Analysis of Nuclear Piping Considering an Embedded Crack
Hideo MACHIDA, Shinobu YOSHIMURA

18 Recent Japanese PFM Researches Related to Failure Probability of Aged RPV
Katsuyuki SHIBATA, Yasuhiro KANTO, Shinobu YOSHIMURA, Genki YAGAWA
Proceedings of 5th International Workshop on the Integrity of Nuclear Components, pp.99-117,
2004

19 Economic Evaluation of Maintenance Strategies for Steam Generator Tubes Using Probabilistic
Fracture Mechanics and Financial Method
Yoshihiro ISOBE, Mitsuyuki SAGISAKA, Shinobu YOSHIMURA, Genki YAGAWA,
Proceedings of 5th International Workshop on the Integrity of Nuclear Components, pp.127-138,
2004

20 Seismic Loads for Crack Stability Assessment in a Review of Leak-Before- Break(LBB)
Applicability
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Probabilistic Fracture Mechanics for Risk-Informed Activities
- Fundamentals and Applications -

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