Transactions, SMiRT-23
Manchester, United Kingdom - August 10-14, 2015
Division VIII, Paper ID 689

DETERMINATION OF ISI REQUIREMENTS ON THE BASIS OF SYSTEM BASED CODE CONCEPT

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ABSTRACT

In our previous study, a new process for determination of in-service inspection (ISI) requirements was proposed on the basis of the System Based Code concept. The proposed process consists of two complementary evaluations, one focusing on structural integrity and the other on plant safety. First, the structural reliability of a specified component is evaluated considering potential active degradation mechanisms including those which are not addressed explicitly in design codes. If the structural reliability meets a requirement, one can proceed to the second evaluation, where detectability of defects before they would grow to an unacceptable size in light of plant safety is assessed. If there is any feasible way to detect defects, that is adapt as ISI requirement. Otherwise, structural integrity evaluation would be required under sufficiently conservative hypothesis. In other words, if the additional requirements are met, detectability is not an obligation.

In this study, ISI requirements for a reactor guard vessel and a core support structure of the prototype sodium-cooled fast breeder reactor in Japan, Monju, were investigated according to the proposed process. The proposed process is expected to contribute to realize effective and rational ISI by properly taking into account plant-specific features.

INTRODUCTION

In-Service Inspection (ISI) is important for safety and stable operation of nuclear power plants. ISI requirements should be determined while taking into account specific facilities and safety design of each plant. Sodium-cooled fast reactors (SFRs) have some different features from conventional light water reactors, such as operation at elevated temperature, low internal pressure, and almost negligible corrosion in purity-controlled sodium. Efforts to develop rules for ISI in liquid metal cooled plants were made alongside the Clinch River Breeder Reactor project, and resulted in Section XI, Division 3 of the ASME boiler and pressure vessel code (ASME 2001). However, due to cancellation of the Clinch River project, no major revisions were done since the first publication in 1980, and some parts of the code such as acceptance standards for examination of Class 1 and 2 components remain in the course of preparation. Therefore, the JSME/ASME Joint Task Group for System Based Code (SBC) was established in 2012 in the ASME Boiler and Pressure Vessel Code Committee, and is working to develop alternative requirements to Section XI, Division 3, utilizing the SBC concept (Asayama, et al. 2014).

In our previous study, a new process for determination of ISI requirements was proposed on the basis of the SBC concept (Takaya, et al. 2015). The SBC concept was proposed in the course of research and development for Japanese Fast Breeder Reactors in 1990s (Asada, et al. 2002, Asada, et al. 2002, and Asada 2006). One of key concepts is margin optimization, which provides a new framework that intends to allow optimum allocation of margins on structural integrity of components encompassing various

technical aspects in a plant life cycle such as material, design, fabrication, installation, inspection, as well as repair and replacement. By fully taking account of these technical characteristics, the SBC concept pursues improved reliability and economy while meeting plant safety goals. A logic flow diagram of the proposed process is shown in Figure 1. It consists of two complementary evaluations, one focusing on structural integrity (Stage I) and the other on plant safety (Stage II). At the stage I, the structural reliability of a specified component is evaluated considering potential active degradation mechanisms including those which are not addressed explicitly in design codes. If the structural reliability meets a requirement, one can proceed to the stage II, where detectability of defects before they would grow to an unacceptable size in light of plant safety is assessed. If there is any feasible way to detect defects, that is adapt as ISI requirement. Otherwise, structural integrity evaluation would be required under sufficiently conservative hypothesis. In other words, if the additional requirements are met, detectability is not an obligation.

In this study, ISI requirements for a reactor guard vessel (RGV) and a core support structure (CSS) of the prototype sodium-cooled fast breeder reactor in Japan, Monju, are investigated according to the proposed process.

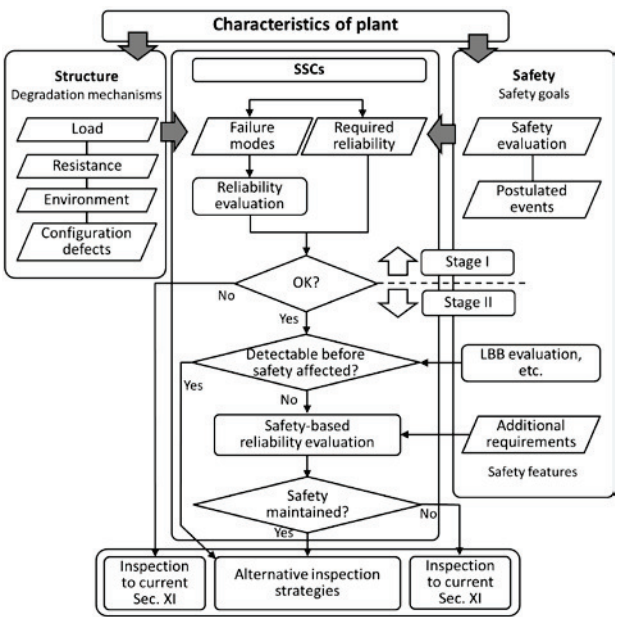


Figure 1. Logic flow diagram of the SBC process

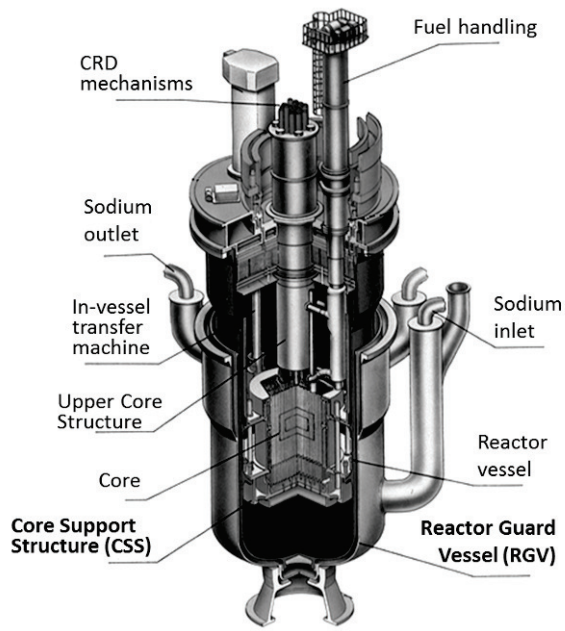


Figure 2. Monju reactor main components

EVALUATION OBJECT COMPONENTS

Reactor Guard Vessel

Figure 2 shows Monju reactor main components. RGV is one of unique components of SFRs, which envelops the reactor vessel (RV) and parts of components of the primary heat transfer system (PHTS). The volume between the RGV and the components in the RGV was designed in order to maintain the reactor coolant surface level required for the decay heat removal in the event of coolant leakage from the RV and the PHTS. In addition, the room for the PHTS is filled with purity-controlled nitrogen gas to prevent sodium fires. The material of the RGV is SUS304, which is equivalent to type 304 stainless steel. The maximum temperature at a high stress region during normal operations is approximately 445°C. Main loads are dead load and that due to thermal transients during start-ups and shutdowns.

The classification of RGVs in Section XI, Division 3 is Class 2, and VTM-3 examinations of welds are required for austenitic and low alloy steel vessels. The VTM-3 examination is one of visual examinations, of which purposes are to determine the general mechanical and structural conditions of components and their supports and to detect discontinuities and imperfections. On the other hand, even a small crack penetration shall not be allowed for the RGV by taking account of the required safety function to maintain the reactor coolant surface level. In general, some deformations are supposed to be accompanied with crack propagation, so the VTM-3 examination is considered effective to some extent. However, it is not certain whether the VTM-3 examinations are sufficient to prevent crack penetration in any lengths. Therefore, ISI requirements for the RGV are derived according to the proposed process on the basis of the SBC concept.

The first step is to determine potential failure modes, which have to be analysed exhaustively even if they are not addressed explicitly in the design code. The environment of the RGV is purity-controlled nitrogen gas, so corrosion can be excluded. Flow of nitrogen gas is mild, and there are no vibration sources, so vibration fatigue can be also neglected. As a result, creep-fatigue interaction damage is left because the RGV is used at elevated temperature and cyclic loads are given by reactor start-ups and shutdowns.

A target failure probability is also needed for the determination of ISI requirements according to the proposed process. Kurisaka et al. (2011) proposed a method for determining structure and component-level required reliabilities from quantitative safety design requirements on the core damage frequency (CDF) and containment failure frequency (CFF) by utilizing probabilistic safety assessment (PSA) models, and applied the proposed method to the Japan Sodium-cooled Fast Reactor (JSFR) which is a demonstration reactor being developed after Monju. The quantitative safety design requirements on the CDF and CFF for JSFR were assumed to be 10^{-5}/site-year and 10^{-6}/site-year, respectively (Kotake et al. 2008). Kurisaka et al. assumed that there were 10 reactors in a single site, so the requirements on the CDF and CDF for a single reactor were derived to be 10^{-6}/reactor-year and 10^{-7}/reactor-year, respectively. The boundary failure of the RGV is related to loss of the reactor coolant surface level required for the decay heat removal, which is one of typical event sequences in SFRs leading to core damage. The derived target failure probability of the RGV was 2×10^{-5}/reactor-year. JSFR has some different design features from Monju (Yamano et al. 2012), and there is only a single reactor in the Monju site, but the above target failure probability of the RGV is used as an example in this study. The design life of Monju is 30 years, so the target failure probability of the RGV for the entire design life is 6×10^{-4}/30 years.

Core Support Structure

The CSS supports a core support plate, a core barrel and other internal components, and transfers their weight to the RV through a mount arm with fixing bolts. The required safety function is to maintain core configuration. The material is SUS304. The environment of CSS is purity-controlled liquid sodium. The maximum temperature during normal operations is approximately 400°C, where creep effect is negligible.

The classification of CSSs in Section XI, Division 3 is Class 1, and VTM-3 examinations are required. Either a literary visual method (e.g., periscope and light) or combination of under-sodium scanning and dimensional gaging is supposed for the VTM-3 examination of the CSS. However, it is impossible to apply a literary visual method to the CSS under sodium. It is also hard to apply the combination of under-sodium scanning and dimensional gaging because the appendix for the under sodium scanning is still in the course of preparation. In addition, significant efforts have been made to develop under-sodium scanning systems, but they are still not up to a practical use level. Therefore, it is needed to determine ISI requirements for the CSS according to the proposed procedure on the basis of the SBC concept.

Failure modes for the CSS also have to be determined. The environment of the CSS is purity-controlled sodium and neutron irradiation. Corrosion in purity-controlled sodium is negligible (Furukawa et al.

2009). Material properties change due to neutron irradiation. For example, decrease in ductility is one of concerns, but a surveillance program during the operation is already planned in Monju to confirm neutron irradiation effects. The looseness of the fixing bolts may be another concern, but preventive measures against the rotation of bolts were taken. The temperature is low enough to neglect creep damage. Therefore, just fatigue damage due to cyclic loads by reactor start-ups and shutdowns is left.

The target failure probability of the CSS was also proposed for JSFR by Kurisaka et al. (2011). Just a single failure of the CSS results in severe core damage and loss of containment function, so the target failure probability of the CSS was proposed to be 2×10^{-10}/reactor-year which was much smaller than the target level of core damage sequences expressed by combination of initiating events with loss of mitigation systems. The target failure probability of the CSS for the entire design life is 6×10^{-9}/30 years in this study.

EVALUATION PROCEDURES

Crack Initiation Evaluation

The following limit to creep-fatigue damage in the JSME design and construction code for fast reactors (JSME FR code; JSME 2013) is used as a crack initiation criterion;

$$D = f\left(D_f, D_c\right) \tag{1}$$

where D is the criterion value that connects points (D_f, D_c) = (1, 0), (0.3, 0.3), and (0, 1). D_f and D_c are fatigue damage and creep damage, respectively. It is assumed that a fully circumferential crack with 1 mm depth is initiated when the fatigue-creep interaction damage reaches D.

D_f and D_c are evaluated basically according to the JSME FR code. The creep-fatigue damage evaluation method in the JSME FR code is originally from the design methods for Monju, which was explained by Iida et al. (1987). D_f is calculated as follows;

$$D_f = n / N_f\left(\varepsilon_t\right) \tag{2}$$

where n is the number of cycles and N_f is the number of cycles to crack initiation for the total strain range, ε_t.

D_c is calculated as the sum of creep damage under a constant stress of S_g, those under a relaxation of a secondary stress, D_0^* for an initial monotonic loading and D^* for successive cyclic loadings, and that under a relaxation of a peak stress, D^{**}, as follows;

$$D_c = \frac{2t}{t_R\left(S_g\right)} + D_0^* + n^* D^* + n^{**} D^{**} \tag{3}$$

where t is the total time at elevated temperature (h) and t_R is the creep rupture time (h). In this study, S_g is settled to be $3(P_L+P_b)$ where P_L and P_b are the long-term local primary membrane and bending stresses (MPa), respectively.

The JSME FR code provides the values of D_0^*, D^*, and D^{**} by charts, but in this study, they are evaluated by the following equations;

$$D_0^* = 2\left[\int_0^{t'} \frac{dt}{t_R(\sigma)} - \frac{t'}{t_R(S_g)}\right] \tag{4}$$

$$D^* = 2\left[\int_0^{t'} \frac{dt}{t_R(\sigma)} - \frac{t'}{t_R(S_g)}\right] \tag{5}$$

$$D^{**} = \int_0^{t'} \frac{dt}{t_R(\sigma)} - \frac{t'}{t_R(S_g)} \tag{6}$$

where t' is originally the time for relaxation stress to become S_g, but it is modified to be the shorter of the time and the holding time at elevated temperature for each cycle (h) in this study in order to avoid excessive overestimation of D_c.

Initial stress for each creep damage is determined as follows;

$$S_i = \begin{cases} P_L + P_b + Q, & for\ D_0^* \\ MIN\left[\frac{1}{2}S_n, S_n - 1.5S_{mC}\right], & for\ D^* \\ MIN\left[\frac{1}{2}\Delta\sigma_R(\varepsilon_t), E\varepsilon_t - 1.5S_{mC}\right], & for\ D^{**} \end{cases} \tag{7}$$

where Q is the long-term secondary stress (MPa), S_n is the intensity of the primary plus secondary stress range (MPa), S_{mC} is the design stress intensity at the compression side (MPa), E is the modulus of longitudinal elasticity (MPa), and $\Delta\sigma_R$ is the stress range in a cyclic stress-strain curve. In the JSME FR code, the same value is used for SUS304 as S_i for D^* regardless of the intensity of actual induced stress, but in this study, S_i for D^* is evaluated using S_n based on the method for ferritic steels in the JSME FR code in order to evaluate D^* by taking account of induced stress.

The relaxation of stress is calculated using the following equation and the strain hardening theory;

$$\dot{\sigma} = -\frac{E\dot{\varepsilon}_c}{q} \tag{8}$$

where q is the elastic follow-up parameter (3 for D_0^* and D^*, and 1 for D^{**}), and ε_c is the creep strain. The dots mean time derivative.

Crack Propagation Evaluation

The following equation was used to calculate crack propagation due to creep-fatigue damage (Fujioka et al. 1995).

$$\frac{da}{dn} = C_f \Delta J_f^{\ m_f} + C_c \Delta J_c^{\ m_c} \tag{9}$$

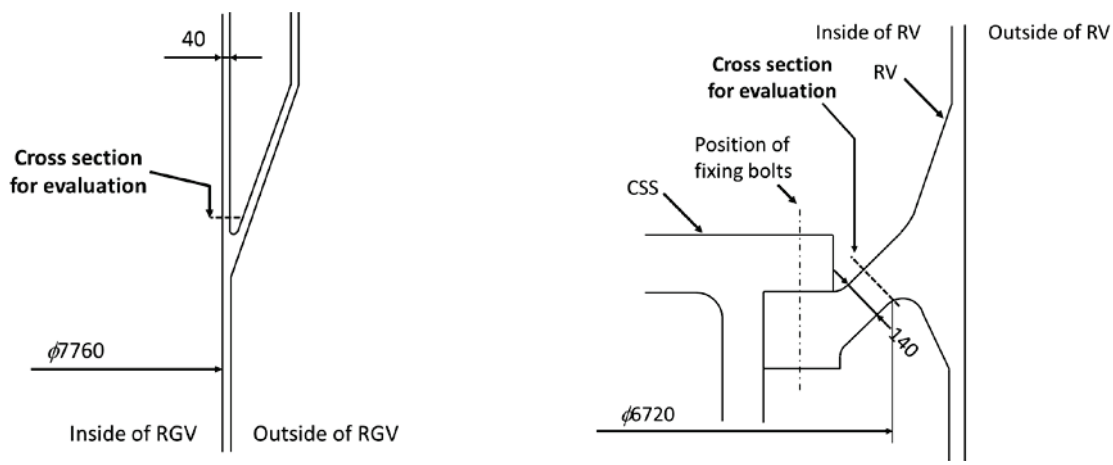
where a is the crack depth (mm), C_f, C_c, m_f, and m_c the material constants. ΔJ_f and ΔJ_c the J-integral range of fatigue and creep, respectively. The reference stress method was used for calculation of J-integral ranges (Miura et al. 2000).

A depth of half the thickness is simply chosen as the critical crack depth in this study although the deeper crack would be accepted by conducting detailed evaluations of unstable fractures.

Reliability Evaluation Conditions

A direct Monte-Carlo method is used for the probabilistic evaluations. The total numbers of trial samples for the RGV and the CSS are 10^7 and 10^9, respectively, corresponding to the target failure probablities.

Cross sections for evaluation of the RGV and the CSS are shown in Figure 3. The position where the load is highest during normal operations is chosen as the cross section for evaluation of the RGV because the structure of the RGV is relatively simple. On the other hand, the mount arm from the RV is chosen as the cross section to evaluate the reliability of the CSS instead of the CSS itself because there are multiple load transfer paths except the selected part, so here is a most essential part to maintain the required safety function of the CSS. The material of the RV is SUS304 as well as the CSS.



(a) RGV (b) Mount arm of RV for CSS

Figure 3. Cross sections for evaluation and dimensions

Tables 1 and 2 show the evaluation parameters and the random variables, respectively. The evaluation parameters are based on the design values for the operating conditions I and II. The thermal stress factor, χ, is chosen as a random variable to consider potential scattering of the temperature range during start-ups and shutdowns. This factor is multiplied with S_n and Q in the Monte-Carlo simulation. The creep rupture time factor, α_R, the creep strain factor, α_c, and the fatigue life factor, α_f, are also chosen as random variables as in our previous study (Takaya et al. 2015). Their distribution types and variations are based on material test results (Takaya et al. 2015). In the JSME FR code, the design creep rupture times and creep strains are determined by using $\alpha_R = 10$ and $\alpha_c = 3$, respectively. The values of the reduction factors are also based on material test results. In this study, α_R and α_c are treated as random variables, so the equations of average creep rupture time and creep strain in the JSME FR code are used as they are to evaluate the average values. On the other hand, the design fatigue life curves are determined by applying a reduction factor of 2 to the mean strain amplitude or 20 to the mean cycles, whichever factor leads to the greater conservatism. It was stated that the factor of 20 on life was regarded as the product of three subfactors; scatter of material test data (=2.0), size effect (=2.5), and other effects such as surface finish,

atmosphere, etc. (=4.0) (ANL 2003). The factor of 2 corresponds to the 5% point of the failure probability for the material test data used to derive the value in Table 2, so the value of the subfactor is reasonable. In addition to the subfactor for scatter of material test data, the other subfactors also should be treated as random variables but there is not enough information yet. Therefore, in this study, the best-fit curves in the JSME FR code lowered by a factor of 2 on strain or 10 on cycles, whichever is more conservative, is used as the average fatigue life curves for reliability evaluation. The variations of the coefficients for crack growth were determined by assuming that the 95% point corresponds to twice the average because most experiment data were within a factor of 2 (Fujioka et al. 1995).

Table 1: Evaluation parameters

		RGV	CSS
Outer diameter (mm)		7,840	6,720
Thickness (mm)		40	140
Operating temperature (°C)		445	405
Number of cycles		460	460
Total time at elevated temperatures for the entire service life (h)		210,000	0
P_L+P_b for creep damage evaluation (MPa)		3	-
Q for creep damage evaluation (MPa)		133	-
S_n (MPa)		238	107
Stress range perpendicular to a crack (MPa)	Primary membrane	0	7
	Primary bending	0	53
	Secondary membrane	2	0
	Secondary bending	236	15
Shakedown range (MPa)		301	323
Strain rate (mm/mm/s)		1E-8	1E-6

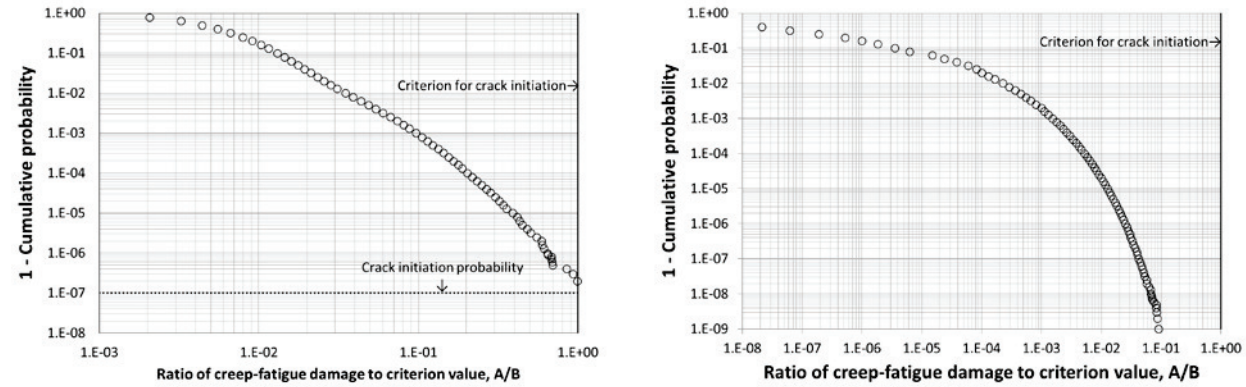
Table 2: Random variables

	Distribution type	Logarithmic standard deviation
Thermal stress factor, χ	Log-normal	0.113 (RGV), 0.258 (CSS)
Creep rupture time factor, α_R	Log-normal	0.560
Creep strain factor, α_c	Log-normal	0.800
Fatigue life factor, α_f	Log-normal	0.420
Coefficient for creep crack growth, C_c	Log-normal	0.422
Coefficient for fatigue crack growth, C_f	Log-normal	0.422

RESULTS AND DISCUSSIONS

Stage I Evaluations

Figure 4 shows the probability distributions of the ratio of creep-fatigue damage at the end of design life to the criterion value for crack initiation calculated by A/B in Figure 5. The number of crack initiation samples was just 1 out of 10^7 and 0 out of 10^9 for the RGV and the CSS, respectively. The crack initiation cycle of the RGV sample was 458, and the crack depth at the end of design life, 460 cycles, was just 1.00115 mm, which was far from the failure criterion, 20 mm. These results show that both the RGV and the CSS have enough structural reliability compared with the targets, 6×10^{-4}/30 years and 6×10^{-9}/30 years, respectively.



(a) RGV (b) CSS

Figure 4. The probability distributions of the ratio of creep-fatigue damage at the end of service life to the criterion value for crack initiation.

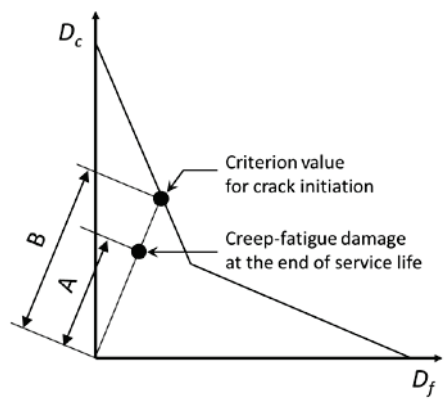


Figure 5. Distances from the origin to creep-fatigue damage at the end of service life and criterion value for crack initiation

Stage II Evaluations

At the stage II, the detectability and/or the probability of a break with the maximum allowable size in terms of the required safety function is assessed. When such a break is not practically detectable, the reliability with an additional requirement is evaluated. If the reliability with the additional requirement meets the target failure probability, detectability, in other words, ISI, is not obligation.

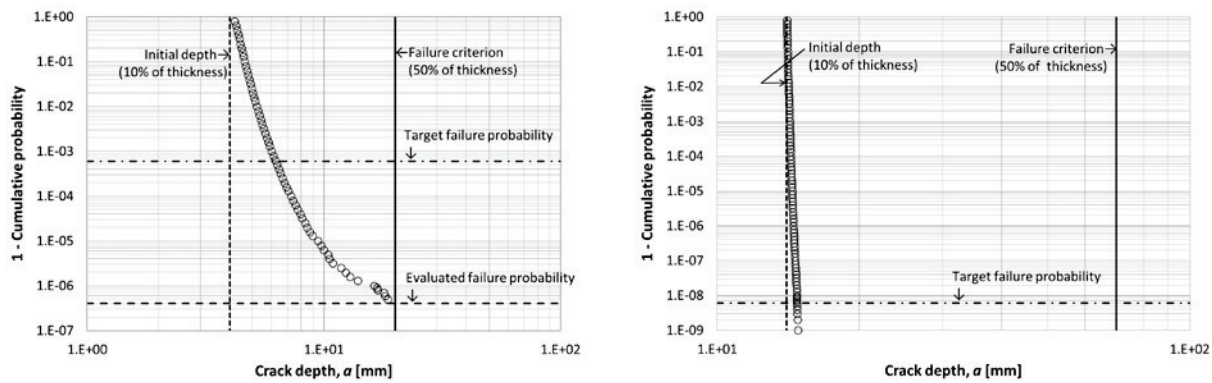
In this study, a crack with the depth of half the thickness was simply chosen as the failure criterion for the both components. Section XI, Division 3 requires the VTM-3 examinations for the RGV, but it will not be effective for the cracks without general deformation as mentioned. Devices for the volumetric test are being developed for the RV of Monju (Tagawa et al. 2006). Although they are expected to be potentially applicable to the RGV, it was assumed in this study that there were not sufficiently effective ways to detect cracks of the RGV. As already explained, the CSS also does not have available methods to detect cracks. Therefore, the reliability evaluations with additional requirements were conducted for them.

The additional requirements shall be determined so that it can be demonstrated that the occurrence of unallowable breaks can be eliminated on a sound basis. The requirements can be categorized into four groups: load, resistance, environment, as well as defects and configuration. Applying only one of these is deemed sufficient. In this study, a fully circumferential crack with depth of 10% of the thickness was

assumed as an initial defect. Such a large initial defect surely can be detected by manufacturing inspections, so it is considered a sufficient conservative requirement.

Figure 6 shows the probability distributions of crack depth at the end of service life with the additional requirement. The number of failure samples was 4 out of 10^7 for the RGV, which means that the failure probability of the RGV is approximately 4×10^{-7}. The target failure probability for the RGV is 6×10^{-4}/30 years, so it is confirmed that the RGV has sufficient reliability even with the additional requirement. On the other hand, the number of failure samples was 0 out of 10^9 for the CSS. The maximum depth in all the samples was about 14.8 mm while the failure criterion depth was 70 mm. The initial crack depth was 14 mm, so the cracks hardly propagated in the case of the CSS. It is difficult to estimate the failure probability of the CSS, but it is obviously much lower than the target, 6×10^{-9}/30 years, even if the additional requirement is considered.

As a result, the occurrence of failures of both the RGV and the CSS can be practically eliminated, hence, no ISI requirements are needed for these components.



(a) RGV (b) CSS

Figure 6. The probability distributions of crack depth at the end of service life with the additional requirement that initial cracks with the depth of 10% of the thickness were assumed.

CONCLUSION

The proposed process for ISI requirements determination based on the SBC concept was applied to the RGV and the CSS of the prototype sodium-cooled fast breeder reactor, Monju, in Japan. The proposed process consists of two stages, structural reliability evaluation for potential degradation mechanisms (Stage I) and defect detectability assessment or reliability evaluation with an additional requirement (Stage II). The potential degradation mechanisms are creep-fatigue and fatigue for the RGV and the CSS, respectively. The stage I evaluations using the Monte-Carlo method showed that both components have sufficient reliabilities. At the stage II, it was assumed that existing techniques were not sufficient to detect cracks of the RGV and the CSS, and then the reliability evaluations with the additional requirement that is an initial fully-circumferential crack with the depth of 10% of the thickness were conducted. As a result, it was shown that the both components had sufficient reliability even with the additional requirement. The occurrence of failures of these components was practically eliminated, hence, it was concluded that no ISI requirements were needed for these components.

ACKNOWLEDGEMENT

The authors thank Mr. Naoto Sasaki of Ascend Co. Ltd for performing the calculation of reliability evaluation.

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