Load and resistance factor design approach for seismic buckling of fast reactor vessels

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Abstract
Seismic buckling of vessels is one of the main concerns for the design of nuclear power plants in Japan. Rational design is important, especially for fast reactor plants. Although thicker walls are preferable in terms of prevention of seismic buckling, excessively thick walls cause unacceptable creep–fatigue interaction damage. In a previous study, we proposed an evaluation method for the seismic buckling probability of a reactor vessel considering seismic hazards and showed that among the random variables considered in the evaluation, seismic load had the most significant impact on buckling probability. This suggests that more rational vessel design can be realized by taking appropriate account of seismic load variations. The load and resistance factor design (LRFD) method enables us to determine design factors corresponding to target reliability by considering the variations of random variables. Therefore, in this study, we used the LRFD method to develop a new design rule for the prevention of seismic buckling of vessels. The equation in the proposed rule is almost the same as that in the Japan Society of Mechanical Engineers fast reactor codes, but every random variable, seismic load and yield stress, has its own design factor. In addition, mean or median values are used in the evaluation instead of design values including conservativeness. The effectiveness of the new design rule was illustrated in comparison with the current provision.

Key words : LRFD, Target reliability, Partial safety factor, Seismic hazard, Fast reactor, Vessel

1. Introduction

Seismic buckling of vessels is one of the main concerns for the design of nuclear power plants in Japan. Rational design is important, especially for fast reactor plants. Although thicker walls are preferable in terms of prevention of seismic buckling, excessively thick walls introduce large thermal stress causing unacceptable creep–fatigue interaction damage. In a previous study (Takaya et al., 2015a), we proposed an evaluation method for the seismic buckling probability of a reactor vessel considering seismic hazards. It was shown that among the random variables in the evaluation, seismic load had the most significant impact on buckling probability. This result suggests that more rational vessel design can be realized by taking appropriate account of seismic load variations in the evaluation, although the current design rule for the prevention of buckling of vessels in the Japan Society of Mechanical Engineers (JSME) fast reactor codes is deterministic (The Japan Society of Mechanical Engineers, 2015).

The load and resistance factor design (LRFD) method is one of the promising options. One of the key features of this method is to use multiple design factors determined by considering the probabilistic characteristics of random variables. These design factors are called partial safety factors. The American Society of Mechanical Engineers has been working to develop the LRFD for nuclear piping systems (Gupta, A. and Choi, B., 2003, the American Society of
Mechanical Engineers, 2007, Avrithi, K. and Ayyub, B.M., 2009). It is expected that large variations in the seismic load can be appropriately considered in buckling evaluation by applying the LRFD method. However, there are few studies on the development of LRFD for seismic buckling of vessels. Therefore, in this study, a new design rule for seismic buckling of vessels is investigated according to the LRFD method.

2. Calculation of partial safety factors by the LRFD method

Details of the LRFD method have already been explained elsewhere (Avrithi, K. and Ayyub, B.M., 2009, Haldar, A. and Mahadevan, S., 2000). Therefore, the calculation of partial safety factors is explained here only briefly.

In the LRFD method, failure is defined using random variables, $X_i, i = 1, ..., n$, and a limit state function $g(X_1, X_2, ..., X_n)$, as follows:

$$z = g(x_1, x_2, ..., x_n) < 0$$  \tag{1}

Failure probability, $P_f$, can be written as

$$P_f = \int_{z<0} f(x_1, x_2, ..., x_n)dx_1dx_2...dx_n$$  \tag{2}

where $f$ is the joint probability density function for the random variables. In this study, the first-order Gaussian approximation method, one of the advanced first-order second-moment methods for non-normal variables, is used to estimate failure probability. There are six steps as follows:

1. **Step 1.** Assume an initial value of the design point. The design point, $x_i^*, i = 1, 2, ..., n$, is the nearest point on the limit state function to the origin in the standard normal space. The distance between the origin and the design point is defined as the reliability index, $\beta$, which has the following relationship with $P_f$:

$$P_f = \Phi(-\beta)$$  \tag{3}

where $\Phi$ is the standard normal probability distribution function.

2. **Step 2.** Approximate non-normal random variables as equivalent normal random variables at the assumed design point. Equivalent means that approximate normal random variables have the same values of probability density function and probability distribution function with the original non-normal random variables at the assumed design point.

3. **Step 3.** Standardize the random variables according to the relation:

$$x_i' = \frac{x_i - \mu_{x_i}}{\sigma_{x_i}}$$  \tag{4}

where $\mu_{x_i}$ and $\sigma_{x_i}$ are the mean and standard deviation of the random variables $X_i$, respectively. Those of the equivalent normal random variables are used for the non-normal random variables.

4. **Step 4.** Linearize the limit state function at the assumed design point by using a Taylor expansion.

5. **Step 5.** Calculate the reliability index using the following equation:

$$\beta = \frac{\mu_z}{\sigma_z}$$  \tag{5}

where $\mu_z$ and $\sigma_z$ are the mean and standard deviation of the linearized limit state function at the assumed design point, respectively. Then, a new design point is calculated using the reliability index and the direction cosines, $\alpha_i$, as follows:

$$x_i^* = \mu_{x_i} - \alpha_i \beta \sigma_{x_i}$$  \tag{6}

The mean and standard deviation of the equivalent normal random variables are used for the non-normal random variables.

6. **Step 6.** Iterate steps 2–5 until the reliability index converges to a predetermined tolerance level. If the calculated $P_f$ meets a target, partial safety factors, $PSF_i$, are calculated as follows:
where \( \bar{x} \) is a representative value such as mean or median. Otherwise, a mean value of one of the variables is changed whereas the coefficient of variance (COV) is kept fixed, and then \( P_f \) is calculated again according to steps 1–6.

3. Evaluation conditions

3.1 Limit state functions and design equations

The time-independent buckling limit for a cylindrical shell in the JSME fast reactor codes is as follows (The Japan Society of Mechanical Engineers, 2015):

\[
\frac{F_c}{A} + \frac{D_0 M}{2 y f_b} \leq S_y
\]

(8)

where

- \( f_b \) : Safety factor (= 1.5 for the service condition D)
- \( E \) : Young’s modulus (MPa)
- \( S_y \) : Design yield strength (MPa)
- \( F_c \) : Axial compressive load (N)
- \( M \) : Bending moment (N·mm)
- \( D_0, t, A \) : Outer diameter (mm), thickness (mm) and cross-sectional area (mm²) of a cylindrical shell
- \( I \) : Moment of inertia of area (mm⁴)
- \( y \) : Ratio of bending buckling strength to axial compression one

Based on Eq. (8), a limit state function for seismic buckling, \( g_1 \), can be defined as follows:

\[
g_1 = \sigma_y - \left( \sigma_{ac} + \frac{\sigma_b}{y} \right)
\]

(11)

where \( \sigma_{ac} \) and \( \sigma_b \) are the axial compressive stress (MPa) and bending stress (MPa) due to the annual maximum earthquake, and \( \sigma_y \) is the actual yield strength (MPa), respectively.

According to our previous work (Takaya et al., 2015a), \( \sigma_{ac} \) and \( \sigma_b \) are evaluated by the following equations with axial compressive stress due to a design basis earthquake, \( \sigma_{ac}^b \), bending stress due to a design basis earthquake, \( \sigma_b^b \), and the annual maximum earthquake normalized by a design basis earthquake, \( X \):

\[
\sigma_{ac} = \sigma_{ac}^b \cdot X
\]

(12)

\[
\sigma_b = \sigma_b^b \cdot X
\]

(13)

The probability distributions of these variables are assumed as follows:

\[
X \sim LN(ln(c), \zeta_{\sigma}^2)
\]

(14)
where \( LN \) means the log-normal distribution. \( c, d_1, \) and \( d_2 \) are medians. \( \zeta_a, \zeta_b, \) and \( \zeta_2 \) are logarithmic standard deviations (LSD).

The following limit state function, \( g_2 \), can be obtained by substituting Eqs. (12) and (13) for Eq. (11):

\[
g_2 = \sigma_y - X \left( \frac{\sigma_{ac}}{y} + \frac{\sigma_b}{y} \right)
\]

(17)

There is another possible limit state function. In general, the variation of the annual maximum earthquake normalized by a design basis earthquake is much larger than that of seismic stresses generated by a design basis earthquake. As a result, an axial compressive stress and bending stress due to the annual maximum earthquake are closely correlated. A perfect correlation is assumed between them by introducing a new probabilistic parameter, \( Y \), as follows:

\[
\sigma_{ac} = cY
\]

(18)

\[
\sigma_b = c \frac{d_2}{d_1} Y
\]

(19)

\[
Y \sim LN(\ln(d_1), \zeta_a^2 + \zeta_b^2)
\]

(20)

where \( \zeta_b \) can be appropriately determined from \( \zeta_b \) and \( \zeta_2 \). For example, it can be determined as follows:

\[
\zeta_b = MAX[\zeta_b, \zeta_2]
\]

(21)

As a result, another limit state function, \( g_3 \), can be obtained:

\[
g_3 = \sigma_y - Y \left[ 1 + \left( \frac{1}{yB} \right)^{2} \right]
\]

(22)

where \( B = d_1/d_2 \).

In this study, these two limit state functions, \( g_2 \) and \( g_3 \), are investigated. A design equation based on \( g_2 \) is as follows:

\[
\phi \sigma_y \geq \gamma^X X \left( \gamma^S_{ac} \sigma_{ac} + \gamma^S_b \sigma_b \right)
\]

(23)

where \( \phi, \gamma_a, \gamma_{ac}, \) and \( \gamma_b \) are partial safety factors for \( \sigma_y, X, \sigma_{ac}, \) and \( \sigma_b \), respectively. Each random variable has its own partial safety factor. Representative values, a mean value for yield stress and median values for other variables, will be substituted to judge whether a vessel has sufficient reliability. Here a median value of \( X \) is substituted in advance, and Eq. (23) is arranged as follows:

\[
\phi \sigma_y \geq \gamma^{ac} \sigma_{ac} + \gamma^b \sigma_b
\]

(24)

where

\[
\gamma^{ac} \equiv c \gamma^X \gamma^S_{ac}
\]

(25)

\[
\gamma^b \equiv c \gamma^X \gamma^S_b
\]

(26)

On the other hand, a design equation based on \( g_3 \) is as follows:

\[
\phi \sigma_y \geq c \left( 1 + \frac{1}{yB} \right)^{2} Y
\]

(27)
where $\gamma$ is a partial safety factor for $Y$. Equation (27) is arranged as follows:

$$
\phi \sigma_y \geq \gamma^S \left( 1 + \frac{1}{yB} \right) Y
$$

(28)

where

$$
\gamma^S \equiv c \gamma^Y
$$

(29)

As explained later, $\gamma^S$ is independent of $B$, and the following equation can be obtained:

$$
\phi \sigma_y \geq \gamma^S \left( \sigma^n_w + \frac{\sigma^n_{sc}}{y} \right)
$$

(30)

### 3.2 Random variables

Partial safety factors are calculated by taking account of the probabilistic characteristics of variables. Hereafter, the general practical ranges of the variations are estimated.

There are three kinds of random variables in this study: yield stress, the annual maximum earthquake normalized by a design basis earthquake, and stress generated by a design basis earthquake. The statistical characteristics of austenitic stainless steels that are typical structural materials for a fast reactor have already been analyzed (Takaya et al., 2015b). The probability distribution of yield stress can be expressed by a normal distribution, where the COV is about 0.1 for type 304 stainless steel and fast reactor grade type 316 stainless steel. Mean values can be calculated using the following equations (The Japan Society of Mechanical Engineers, 2015):

- **Type 304 stainless steel** ($315 \leq T \leq 650$):

  $$
  \sigma_y = 2.50712 \times 10^2 - 5.48130 \times 10^{-1} T + 1.02366 \times 10^{-3} T^2 - 7.28178 \times 10^{-2} T^3
  $$

  (31)

- **Fast reactor grade 316 stainless steel** ($315 \leq T \leq 650$):

  $$
  \sigma_y = 2.67619 \times 10^2 - 6.87719 \times 10^{-1} T + 1.28849 \times 10^{-3} T^2 - 8.54036 \times 10^{-2} T^3
  $$

  (32)

where $T$ is the temperature (°C).

Procedures for assessing uncertainties of the annual maximum earthquake have already been prepared by the Atomic Energy Society of Japan (AESJ) (The Atomic Energy Society of Japan, 2007). However, it is hard to obtain the information needed for assessments. Therefore, in this study, uncertainties of the annual maximum earthquake are estimated by using the uniform hazard spectrum reported by nuclear power plant operators in Japan (Tohoku Electric Power Co., 2014a, 2014b, The Japan Atomic Power Company, 2014, The Kansai Electric Power Co., Inc., 2014a, 2015, The Chugoku Electric Power Co., Inc., 2014, Shikoku Electric Power Co., Inc., 2015, Kyushu Electric Power Co., Inc., 2014a, 2014b). Acceleration at a period of 0.1 s was read at several annual exceedance probabilities and was normalized by a design basis earthquake at each site. Then, the seismic hazard, $H$, was estimated by approximation using a log-normal distribution, as shown in Fig. 1. The annual maximum earthquake normalized by a design basis earthquake, $X$, has the following relationship with the seismic hazard, $H$:

$$
X = 1 - H
$$

(33)

The estimated medians and LSDs of the annual maximum earthquake normalized by a design basis earthquake at sites are plotted in Fig. 2. The range of median is from about 0.002 to 0.1, and that of LSD is from about 0.5 to 1.5. LSD tends to decrease as median of the annual maximum earthquake approaches the design basis earthquake at each site.

Several procedures for assessing the uncertainties of seismic loads are also provided by the AESJ (The Atomic Energy Society of Japan, 2007). In this study, the stress response factor method which is basically the same as a method proposed by the Electric Power Research Institute (Electric Power Research Institute, 2009). In the stress response factor method, an uncertainty of actual stress is evaluated by the square root of the sum of squares of LSDs of the subresponse factors corresponding to various conservatisms and uncertainties in the design method. Kennedy et al.
suggested the values of subresponse factors for civil and mechanical structures (Pisharady, A.S. and Basu, P.C., 2010). The LSD of a seismic load is about 0.5 if all of the subresponse factors are considered.

Fig. 1  Example of seismic hazard estimation

Fig. 2  Estimated medians and LSDs of the annual maximum earthquake normalized by a design basis earthquake at nuclear power plants in Japan

4. Results and discussions
4.1 Comparison of limit state functions

Two possible limit state functions, $g_2$ and $g_3$, were proposed in Section 3.1. The latter limit state function was obtained by simplifying the former one based on the assumption of perfect correlation between an axial compressive stress and a bending stress. It is difficult to define precise conditions where this assumption is satisfied. However, the limit state function of $g_3$ can be used practically if it is confirmed that this limit state function has moderate conservatism. Therefore, partial safety factors of Eq. (23) based on the limit state function of $g_2$ and Eq. (28) based on the limit state function of $g_3$ are compared. The three assumed sites in Table 1 are used as examples. The other conditions are shown in Tables 2 and 3. A mean value of $\sigma_{sc}^{Ss}$ or $Y$ was changed to meet a target buckling probability whereas the COV was kept fixed. It should be noted that the LSD does not change if the COV is kept fixed. A comparison of partial safety factors for seismic stress is shown in Fig. 3. The partial safety factor $\gamma^{Ss}$ is always larger than $\gamma^{acSs}$ and $\gamma^{bSs}$, which means that an evaluation based on $g_3$ gives more conservative results than one based on $g_2$. $\gamma^{acSs}/\gamma^{Ss}$ decreases as $B$ decreases, whereas $\gamma^{bSs}/\gamma^{Ss}$ decreases as $B$ increases. This tendency becomes more apparent as the LSD of $X$ decreases. In fact, $\gamma^{Ss}$ is less than half of $\gamma^{Ss}$ when $B$ is larger than about 1.0 at the site C where the LSD of $X$ is the smallest.

| Table 1  Sites considered | Table 2  Evaluation conditions for definite values |
|--------------------------|-----------------------------------------------|
| Site  | Median of $X$  | LSD of $X$  |
| A   | 0.002  | 1.5  |
| B   | 0.01  | 1.0  |
| C   | 0.1  | 0.5  |

| Target buckling probability [\$/y]  | 10^{-6} |
|-------------------------------------|---------|
| $y\ [\cdot]$                       | 1.3     |
| Temperature [°C]                   | 550     |
| Young’s modulus $E$ [MPa]          | 156000  |

| Table 3  Evaluation conditions for random variables |
|----------|---------------|----------------|
| Parameter | Type          | Mean or Median | COV or LSD    |
| $\sigma_Y$ | Normal       | 137.1         | 0.10          |
| $\sigma_{ac}^{Ss}$ | Log-normal | —             | 0.2, 0.3, 0.4 |
| $\sigma_S^{Ss}$ | Log-normal | About 5~80    | The same with $\sigma_{ac}^{Ss}$ |
| $Y$ | Log-normal | —             | 0.2, 0.3, 0.4 (for $\zeta_b$) |
These tendencies might introduce a large difference of allowable stress between the two limit state functions, $g_2$ and $g_3$. Therefore, the maximum allowable stresses were compared, and the results are shown in Fig. 4. The difference in the maximum allowable stresses is relatively small in the region where the difference of the partial safety factors is large because the partial safety factors for the dominant stress are almost the same in those regions. On the other hand, in the region where the contributions by axial compressive stress and bending stress are the same ($B = 1/y = 0.77$), the difference in the maximum allowable stresses increases. In addition, this difference becomes larger as the LSD of $X$ decreases. However, the largest difference is less than about 40% even at site C. The difference in the maximum allowable stresses is relatively small. Furthermore, $\gamma_{Ss}$ is independent of $B$, as explained later, which is convenient for practical use. Therefore, it was decided to make a new design rule based on $g_3$.

4.2 Evaluation of partial safety factors

The partial safety factors in Eq. (30) are evaluated in this section. There are only two random variables. In this case, the safety partial factors are independent of the coefficients in the equation: $y$ and $B$ in this study. In addition, the variation of a mean value of yield strength does not affect partial safety factors as long as the COV is the same. Therefore, three parameters, target buckling probability, COV of yield strength, and LSD of $Y$, were chosen as parameters for calculating the partial safety factors. The conditions are shown in Table 4, and the calculated partial safety factors are shown in Figs. 5 and 6.

| Table 4 | Conditions for calculating partial safety factors |
|---------|-----------------------------------------------|
| Target buckling probability | $10^{-5}$, $10^{-6}$, $10^{-7}$, $10^{-8}$ |
| COV of yield strength | 0.05, 0.10, 0.15 |
| LSD of $Y$, $\zeta$ | 0.6 ~ 1.6 |
If the partial safety factors are provided by simple equations, the evaluation is much easier than reading the values from figures or tables. Therefore, approximate equations were investigated. These are also plotted in Figs. 5 and 6. The value of $\phi$ was approximated using the following equation, and $a$ and $b$ were estimated by the least squares method:

$$\phi \approx a \zeta + b$$  \hspace{1cm} (34)

where $\zeta$ is LSD of $Y$.

On the other hand, $\gamma_{Ys}$ can be obtained analytically as follows:

$$\gamma_{Ys} = c \exp(- \alpha_Y \zeta)$$  \hspace{1cm} (35)

where $\alpha_Y$ is a direction cosine of $Y$. Seismic stress has a significant influence on the buckling probability, so $\alpha_Y$ is expected to be close to $-1$. The following approximate equation for $\gamma_{Ys}$ is obtained by substituting $\alpha_Y = -1$ in Eq. (35):

$$\gamma_{Ys} \approx c \exp(\beta \zeta)$$  \hspace{1cm} (36)

Although $\alpha_Y$ is approximately $-1$, it must actually be larger than $-1$. Therefore, Eq. (36) always gives a conservative value.

Figures 5 and 6 show that the approximate equations agree well with the calculated partial safety factors. However, it is important to confirm the conservatism of the approximate equations. Figure 7 shows a comparison between the values of $\gamma_{Ys}/\phi$ evaluated by the approximate equations and those calculated using true values. If the ratio is more than 1, evaluation using the partial safety factors evaluated by the approximate equations gives a more conservative result. As shown in Fig. 7, the ratio is always more than 1 in the evaluated range. It increases as $\zeta$ decreases, and the COV of $\sigma_Y$ increases. However, it is just about 1.15 even as a maximum.

The maximum allowable stresses and failure probabilities evaluated using partial safety factors from the approximate equations and those evaluated using true partial safety factors were also compared when $\zeta$ was 0.6. This is the condition in which the conservatism influence of the approximate equations was most significant in Fig.7. The results are shown in Figs. 8 and 9. The difference in the maximum allowable stress is less than about 10%, and the
Fig. 6  Partial safety factor for seismic stress generated by design basis earthquake, $\gamma_{Ss}$

(a) Target buckling probability = $10^{-5}$

(b) Target buckling probability = $10^{-6}$

(c) Target buckling probability = $10^{-7}$

(d) Target buckling probability = $10^{-8}$

Fig. 7  Comparison between the approximate and true $\gamma_{Ss}/\phi$

(a) Target buckling probability = $10^{-5}$

(b) Target buckling probability = $10^{-6}$

(c) Target buckling probability = $10^{-7}$

(d) Target buckling probability = $10^{-8}$
buckling probabilities evaluated using the approximate partial safety factors are in the same order as the target reliabilities. Therefore, it was concluded that these approximated equations were sufficiently practical.

4.3 Proposal of a new design rule in the LRFD format

A new design rule for the time-independent buckling of a reactor vessel in service condition D, in the LRFD format, is proposed. The target reliability for a reactor vessel is assumed to be $10^{-6}$/yr ($\beta = 4.75$). In lieu of the requirement of the JSME fast reactor codes for the time-independent buckling of in service condition D, the following requirement may be used:

$$\gamma_S \left( \frac{F_{cS}}{A} + \frac{D_0 M_{Ss}}{2 y I} \right) \leq \phi \sigma_y,$$

(37)

$$y = \begin{cases} 1.3 & \left( \frac{D_0}{t} \leq 140 \right) \\ 1.3 - \frac{0.3 \left( \frac{D_0}{t} - 140 \right)}{2 E \frac{S_y}{5S_y} - 140} & \left( 140 < \frac{D_0}{t} < \frac{2 E}{5S_y} \right) \end{cases}$$

(38)

(39)

where

- $\gamma_S$: Partial safety factor calculated using Eq. (40)
- $\phi$: Partial safety factor calculated using Eq. (41)
- $E$: Young’s modulus (MPa)
- $S_y$: Design yield strength (MPa)
- $\sigma_y$: Yield strength (MPa)
- $F_{cS}$: Axial compressive load generated by a design basis earthquake (N)
- $M_{Ss}$: Bending moment generated by a design basis earthquake (N•mm)
- $D_0$, $t$, $A$: Outer diameter (mm), thickness (mm), and cross-sectional area (mm$^2$) of a cylindrical shell
- $I$: Moment of inertia of area (mm$^4$)
- $y$: Ratio of bending buckling strength to axial compression one

$$\gamma_S = e^{\exp(4.75 \zeta)}$$

(40)

where
4.4 Comparison with current provision

The safety factor for service condition D is 1.5 in the current provision. As an example, we consider a simple case in which the axial compressive stress is the same as the bending stress \( B = 1 \). In this case, the maximum allowable stress is 42.2 MPa. It should be noted that the design values used in the current provision are conservative and are larger than the actual stresses. Kurisaka et al. calculated the fragilities of the components of a fast reactor plant (Kurisaka, K. and Okamura, S., 2011), and they showed that the median of the seismic load generated by a design basis earthquake was 2.44 times smaller than the design value, and the LSD was 0.57. If this ratio is applied to our case, the actual stress has to be smaller than 17.3 MPa in the current provision. On the other hand, the maximum allowable stress in our proposed design rule is 31.0 MPa when it is evaluated under the conditions shown in Table 6. In this case, the proposed rule can contribute to widening the design window.

5. Conclusions

Two possible limit state functions for the seismic buckling of a vessel were proposed based on the provisions of the JSME fast reactor codes. Their practicability and conservatism were compared, and then the limit state function obtained by assuming a perfect correlation between the axial compressive stress and the bending stress due to the annual maximum earthquake was selected. The partial safety factors were calculated in the general practical ranges in Japan. The approximate equations of the partial safety factors were also provided. Finally, a new design rule of the seismic buckling of reactor vessel was proposed in the LRFD format. This rule will contribute to more rational designs of a reactor vessel for seismic buckling.

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