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Fracture assessment of mismatched girth welds in oval-shaped clad pipes subjected to bending moment

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Abstract

This paper provides an engineering assessment of an oval-shaped clad pipes with a circumferential part-through surface crack subjected to bending moment based upon equivalent stress-strain relationship method (ESSRM) in conjunction with EPRI J -estimation procedure. Plastic limit load equations are developed specially to determine the equivalent stress-strain relationship of the welded clad pipe. Then, the conventional EPRI J -estimation procedure is extended to calculate J values of the equivalent configuration with oval cross-section converted from the welded clad pipe using the ESSRM. The validation of the ESSRM/EPRI estimation framework mentioned in the above is carried out by comparing with the results obtained from 3-D elastic-plastic finite element (FE) analysis.

Keywords: EPRI J -estimation procedure; Equivalent stress-strain relationship method; Finite element analysis; Oval-shaped clad pipe; Plastic limit load; Surface crack

1. Introduction

With increasing demand of recoverable corrosive hydrocarbons, the metallurgically-bonded clad C-Mn steel pipes with an inner corrosion resistant alloy (CRA) layer are widely used to transport these natural resources. During the installation process or in-service operation, the pipes undergo complicated loading conditions as shown in Fig. 1 [1]. They have to withstand internal pressure induced by the transported oil or gas products and external pressure exerted by the sea water. Moreover, longitudinal and transversal forces generated by seismic waves or other types of ground displacements are applied to the pipes, eventually resulting in the loading

combinations of pressure, axial/shear forces and bending moment. However, it is cumbersome and almost impossible to consider all the loading conditions in one study. Hence, the structural response of pipes subjected to bending moment is the focus of the current research due to its importance and universality [2-4]. In reality, the cross-section of a pipe under bending moment inevitably exists with some degree of ovality, which results in the reductions of the bending capacity of the pipe. Brazier [5] first investigated the effect of ovalization on the bending capacity of elastic cylindrical shells, and further research was carried out by Ades [6] on the mechanical responses of elastic-plastic tubes with uniform ovalization.

Crack-like defects are frequently discovered in pipes exposed to aggressive environment or during welding fabrication [7,8]. Therefore, the severity of these flaws or defects needs to be evaluated in order to guarantee the structural integrity of offshore pipelines. Considerable research has been carried out to develop structural integrity assessment procedures according to fracture mechanics [9-16]. Kim et al. [17] proposed the fully plastic J -estimation expression of circumferentially cracked pipes based on EPRI J -estimation procedure [18] and reference stress approach. A modified reference stress solution was established by Tkaczyk et al. [10] to conduct the fracture assessment of elastic-plastic pipes with external surface defects. NourPanah and Taheri [12] developed a reference strain approach for cracked pipes subjected to plastic bending based on previous works carried out by Linkens et al. [19]. However, the procedures indicated above are applicable only for assessing critical homogeneous structures. Zhao et al. [20] studied the evolution tendency of crack tip opening displacement (CTOD) versus global strain for welded clad pipes with a circumferential part-through surface crack, while it is only suited to a specific pipe configuration. An equivalent stress-strain relationship method (ESSRM) is proposed by Lei and Ainsworth [21] to estimate J values of the cracked mismatched structure using J determined from the homogenous structure composed of the ‘equivalent’ material. Based on the ESSRM, Souza et al. [22] provided a concise fracture assessment framework for dissimilar girth welds by incorporating the influence of weld strength mismatch.

It is worth noting that the previous works are all limited to circular pipes subjected to plastic loading. When the same load is applied to an oval-shaped pipe, a larger plastic deformation is induced due to reduction of the bending capacity caused by ovalization [23,24], significantly increasing the risk of crack initiation and propagation. Therefore, in the present study, the fracture responses of oval-shaped clad pipes with a circumferential part-through surface crack are investigated based on the ESSRM coupled with the EPRI J -estimation procedure [18]. Extensive 3-D finite element (FE) analyses are carried out first to propose the plastic limit load solutions of the welded clad pipe. Then, the ESSRM is employed to characterize the mechanical properties of the clad pipe in terms of corresponding properties of the homogeneous pipe made of an equivalent material. Subsequently, the J values for a wide range of pipe geometries and crack sizes are calculated by extending the conventional EPRI J -estimation procedure [18] for an oval-shaped homogeneous pipe. Finally, the accuracy of the ESSRM/EPRI estimation framework is validated by comparing it with the FE results.

2. Fracture estimation methodology of welded clad pipes

This section presents the development of fracture assessment methodology for mismatched clad pipes containing a circumferential part-through surface crack as shown in Fig. 2.

2.1 The equivalent stress-plastic strain relationship

The ESSRM is proposed to evaluate the J -integral values of the mismatched configuration using homogeneous one made of an equivalent material, and it is expressed in the following form,

$$\sigma_{eq}(\varepsilon_p) = \left[\frac{P_0^w - P_0^{mis}}{P_0^w - P_0^b} \right] \sigma_w(\varepsilon_p) + \left[\frac{P_0^{mis} - P_0^b}{P_0^w - P_0^b} \right] \sigma_b(\varepsilon_p) \quad (1)$$

where ε_p is the plastic strain, $\sigma_{eq}(\varepsilon_p)$ represents the equivalent stress-strain relationship, $\sigma_w(\varepsilon_p)$ and $\sigma_b(\varepsilon_p)$ are the stress-strain relationship of the weld metal and the base material, while P_0^{mis} is the plastic limit load of the mismatched configuration, P_0^w and P_0^b denote the corresponding plastic limit load of the homogeneous configurations composed of the weld metal

and the base material, respectively. The welded clad pipe consists of three distinct materials (Fig. 2): outer carbon steel, inner CRA material and weld metal. Therefore, in this study, the ESSRM is employed twice to transfer the welded clad pipe into the homogeneous one made of only one material. In the first step, the mechanical response of the bi-material pipe composed of the outer carbon steel and the inner CRA material is treated in terms of the ESSRM using the following expression,

$$\sigma_{eq}^{bi}(\varepsilon_p) = \left[\frac{P_0^{in} - P_0^{bi}}{P_0^{in} - P_0^{out}} \right] \sigma_{in}(\varepsilon_p) + \left[\frac{P_0^{bi} - P_0^{out}}{P_0^{in} - P_0^{out}} \right] \sigma_{out}(\varepsilon_p) \quad (2)$$

where $\sigma_{eq}^{bi}(\varepsilon_p)$ is the equivalent stress-strain relationship of the bi-material pipe, $\sigma_{in}(\varepsilon_p)$ and $\sigma_{out}(\varepsilon_p)$ are the stress-strain relationship of the inner CRA material and the outer carbon steel, while P_0^{bi} is the plastic limit load of the bi-material pipe, P_0^{in} and P_0^{out} denote the plastic limit load of the inner CRA material and the outer carbon steel. Then, the ESSRM is employed again to determine the mechanical property of the equivalent material for the welded clad pipe,

$$\sigma_{eq}^{clad}(\varepsilon_p) = \left[\frac{P_0^{bi} - P_0^{clad}}{P_0^{bi} - P_0^w} \right] \sigma_{eq}^{bi}(\varepsilon_p) + \left[\frac{P_0^{clad} - P_0^w}{P_0^{bi} - P_0^w} \right] \sigma_w(\varepsilon_p) \quad (3)$$

where $\sigma_{eq}^{clad}(\varepsilon_p)$ and P_0^{clad} are the equivalent stress-strain relationship and the plastic limit load of the welded clad pipe, respectively.

The equivalent stress-strain curve described by $\sigma_{eq}^{clad}(\varepsilon_p)$ can characterize the mechanical response of the welded clad pipe as long as the plastic limit loads (P_0^{bi} , P_0^{in} , P_0^{out} and P_0^{clad}) are determined, which are proposed in Section 3.

2.2 The EPRI *J*-estimation procedure of homogeneous pipes with ovalisation

As indicated previously, the welded clad pipe is transformed into the homogeneous one made of an equivalent material using the ESSRM. Hence, the *J*-integral estimation values for the homogeneous pipe can be used to predict the fracture responses of the welded clad pipe. The fracture assessment strategy for the homogeneous pipe with oval cross-section is developed in this sub-section by incorporating the effect of ovality into the conventional EPRI *J*-estimation

procedure [18].

The EPRI J -estimation procedure [18] provides an approach in estimating the J -integral over the full range of elastic-plastic regime by assuming the total J equal to the sum of the elastic and plastic components,

$$J = J_{el} + J_{pl} \quad (4)$$

The first term on the right side of Eq. (4) is in fact the elastic release rate which can be calculated from the stress intensity factor (K), and it is expressed in the following,

$$J_{el} = \frac{K^2}{E'} \quad (5)$$

where $E' = E$ and $E' = E/(1 - \nu^2)$ are for plane stress and plane strain conditions with E and ν representing the elastic modulus and the Poisson's ratio, respectively.

In the FE analyses, a Ramberg-Osgood model [25] is usually employed to describe the uniaxial stress-strain curve of an elastic-plastic material, and it is given as follows:

$$\frac{\varepsilon}{\varepsilon_y} = \frac{\sigma}{\sigma_y} + \alpha \left(\frac{\sigma}{\sigma_y} \right)^n \quad (6)$$

where α is a dimensionless constant, n is the strain hardening exponent, σ_y and ε_y denote the yield stress and the yield strain, respectively. Based on the above power-hardening model and the HRR singularity derived by Hutchinson, Rice and Rosengren [26,27], the plastic component, J_{pl} , of Eq. (4) is given in the following to evaluate the J -integral of a circumferential cracked pipe under bending moment,

$$J_{pl} = \alpha \varepsilon_y \sigma_y b h_1(a/t, D_e/t, \theta, n) \left(\frac{M}{M_L} \right)^{n+1} \quad (7)$$

where a is the crack depth, θ represents the surface crack length, D_e and t are the pipe outer diameter and the wall thickness, respectively, $b = t - a$ denotes the un-cracked ligament, M and M_L are the applied bending moment and the limit bending moment, respectively, h_1 is the dimensionless parameter that depends on pipe geometry, crack size and strain hardening exponent. The h_1 -factor characterizes the linear relationship between J_{pl} and M^{n+1}

quantitatively and can be determined by numerical analysis of the configuration of interest.

For M_L , the following expression is used in the present study [28],

$$M_L = 2\sigma_y R_m^2 t \left(2\sin\beta - \frac{a}{t} \sin\theta \right) \quad (8)$$

where R_m is the pipe mean radius with $R_m = (D_e - t)/2$, and β is denoted as

$$\beta = \frac{\pi}{2} \left[1 - \left(\frac{\theta}{\pi} \right) \left(\frac{a}{t} \right) \right] \quad (9)$$

It is worth noting that the limit bending moment given in Eq. (8) is applicable for $(\theta + \beta) \leq \pi$.

In this study, the effect of ovality on fracture responses in the cracked pipe subjected to bending moment is investigated in detail. The degree of ovality [1] is determined by the difference between the maximum and the minimum measured outer diameters ($D_e^{\max}$ and $D_e^{\min}$) divided by the nominal outer diameter as depicted in Fig. 3, and it is expressed as follows:

$$C_O = \frac{(D_e^{\max} - D_e^{\min})}{D_e} \times 100\% \quad (10)$$

Since the only difference between the circular pipe and the oval-shaped one is characterized by C_O , it is assumed that the above J -estimation methodology for a circular pipe can be extended directly to serve for the corresponding J calculation of an oval-shaped pipe by inserting C_O into the h_1 -factor in Eq. (7), and the new expression is given in the following,

$$J_{pl} = \alpha \varepsilon_y \sigma_y b h_1(a/t, D_e/t, \theta, n, C_O) \left(\frac{M}{M_L} \right)^{n+1} \quad (11)$$

The verification of Eq. (11) and the determination of h_1 -factor are presented in Sub-section 4.3. Extensive 3-D elastic and elastic-plastic FE analyses are conducted to determine the J_{el} and J values of the oval-shaped homogeneous pipe with a circumferential surface part-through crack. Then, the fully plastic part, J_{pl} , of J is calculated using Eq. (4), and the values of h_1 -factor in Eq. (11) is determined according to the estimation procedure developed by Yun-Jae Kim et al. [17], Chiodo and Ruggieri [3].

3. Determination of plastic limit load of welded clad pipe

The determination of ESSRM depends strongly to the plastic limit load solutions of the structure. In this section, the limit load solutions for the welded clad pipe with a circumferential part-through external surface crack subjected to bending moment are built up through 3-D elastic-perfectly plastic FE analyses.

3.1 Analysis matrix and FE modelling

Fig. 2 presents the clad pipe configuration with a V-groove butt weld containing a circumferential part-through external surface crack, and the weld geometry is defined by the root opening width ($2h$) and the groove angle (ϕ). The analysis matrix [22] considers the cracked pipe with a constant wall thickness $t = 20.6$ mm, and the CRA layer thickness of $t_{\text{CRA}} = 3$ mm is applied in this study. The CRA thickness is representative of the typical value for the commonly-used layer thickness encountered in practice. Three different values for D_e/t are adopted: $D_e/t = 10, 15$ and 20 . The crack depth ratio (a/t) and the crack length ratio (θ/π) are ranging from 0.1 to 0.4 and from 0.04 to 0.20 , respectively. For the weld root opening width, the values of h/t employed in this analysis are 0.1 and 0.2 , and the groove angle varying from 20° to 60° is considered, covering the wide range of butt weld profiles found in real application. The weld strength mismatch factor (M_y) [29] is usually defined as the ratio of the yield stress between the weld metal (σ_y^w) and the outer carbon steel (σ_y^{out}), and it is expressed as

$$M_y = \frac{\sigma_y^w}{\sigma_y^{\text{out}}} \quad (12)$$

Five different M_y values, $M_y = 0.5, 0.75, 1.0, 1.25, 1.5$, are included in present analyses. The clad pipe segments are usually welded together using a filler material, such as nickel-chromium alloy 625 (UNS N06625), which eventually has the same mechanical properties and metallurgical composition as the inner CRA layer [22]. Hence, the identical material properties are employed for the CRA layer and the weld metal in this study.

A FE mesh generator is developed specially to generate models of the welded clad pipe with a circumferential part-through surface crack, and a typical mesh for this configuration is depicted in Fig. 4. 3-D elastic-perfectly plastic FE analyses are performed using ABAQUS, a general purpose FE software [30], and a 20-node brick element with reduced integration (C3D20R) is used throughout the modelling. In this analysis, materials are assumed to be elastic-perfectly plastic with non-hardening J_2 flow theory in small geometry change setting. Symmetric conditions are fully utilized in the FE models to reduce the number of elements and computing time. Multi-point constraint (MPC) is imposed at the end surface of the pipe where a single node located at the center of the surface is connected to the nodes, and a rotation displacement is applied to this node to provide the bending loading condition. The required loads can be obtained directly from the nodal force, and the twice elastic slope method as illustrated in Fig. 5 is used to determine the final plastic limit load.

3.2 Accuracy of present FE limit load analyses

The reliability of FE models employed in the present study is verified first before it is used extensively to generate results of plastic limit loads. The mesh generator mentioned previously is modified to create FE models for the homogeneous pipe with a circumferential through-wall crack under bending moment, and the FE results obtained are compared with the analytical limit load (M_0^{Miller}) proposed by Miller [31], which is expressed in the following,

$$\frac{M_0^{\text{Miller}}}{4\sigma_y R_m^2 t} = \cos\left(\frac{\theta}{2}\right) - \frac{\sin\theta}{2} \quad (13)$$

Fig. 6 illustrates the comparison results, and a good agreement is observed. Hence, the mesh generator is considered to be well suited for further analyses of more complicated configurations where sufficient elements are used to guarantee good convergence. FE models for the welded clad pipe with a circumferential part-through external surface crack are generated by the mesh generator, and convergent tests covering the range of parameters presented in Sub-section 3.1 are carried out to determine the optimum number of elements which vary from 8,043 to 16,011

based on the crack size and pipe geometry. The case shown in Fig. 7 is one of these convergent tests. The above operations provide confidence of the present FE analyses to obtain accurate plastic limit loads.

3.3 Plastic limit load solutions of homogeneous and bi-material pipes

Based on present FE analyses, the approximation expression of plastic limit loads for the homogeneous pipe with a circumferential part-through external surface crack is proposed in the following,

$$\frac{M_0^{\text{homo}}}{4\sigma_y R_m^2 t} = 1 + A_1 \left(\frac{a}{t}\right) + A_2 \left(\frac{a}{t}\right)^2 \quad (14)$$

where

$$A_1 = 1.338708 \left(\frac{\theta}{\pi}\right)^2 - 0.321014 \left(\frac{\theta}{\pi}\right) - 0.002893 \quad (15)$$

$$A_2 = -12.607045 \left(\frac{\theta}{\pi}\right)^2 + 1.335322 \left(\frac{\theta}{\pi}\right) - 0.067332 \quad (16)$$

They agree very well with the FE results as depicted in Fig. 8. It is worth noting that the plastic limit loads for the homogeneous pipe made of outer carbon steel, inner CRA material or weld metal, can all be represented using Eq. (14) by substituting the corresponding yield stress into it. For the bi-material pipe containing the identical crack profile with the homogeneous one, the plastic limit load approximation solution is written as

$$\frac{M_0^{\text{bi}}}{M_0^{\text{homo}}} = 0.132426 M_y + 0.867041 \quad (17)$$

Figs. 9-10 show comparison results of plastic limit loads obtained from the FE results and Eq. (17) for varying a/t , θ/π , D_e/t and M_y , and the agreement between them is excellent, indicating that the proposed expression is sufficiently valid to predict the plastic limit loads of the cracked bi-material pipe.

3.4 Plastic limit load solutions of welded clad pipe

Based on research works performed by Sang-Hyun Kim et al. [32], the plastic limit load

solutions for the V-groove welded clad pipe can be characterized by three parameters: the groove weld angle (ϕ), the slenderness of the weld (ψ) and the mismatch factor (M_y). As depicted in Fig. 11, the V-groove weld configuration is simplified as an equivalent strip one using the effective weld width (h_{eff}) which is defined as average of the un-cracked weld ligament,

$$h_{\text{eff}} = h + \frac{(t - a)}{2} \tan(\phi/2) \quad (18)$$

Therefore, in this case, the limit loads for the simplified strip weld can be represented by ψ and M_y , and ψ is denoted using the following expression,

$$\psi = \frac{(t - a)}{h_{\text{eff}}} + 5 \left[\cos\left(\frac{\theta}{2}\right) - \frac{\sin\theta}{2} \right] \quad (19)$$

Eventually, the limit load equation for the welded clad pipe is built up in terms of ψ and M_y , and it is given as

$$\frac{M_0^{\text{clad}}}{M_0^{\text{bi}}} = \frac{24(M_y - 1)}{25} \left(\frac{\psi_1}{\psi} \right) + \frac{(M_y + 24)}{25} \quad (20)$$

where

$$\psi_1 = \exp \left[-\frac{2(M_y - 1)}{5} \right] \quad (21)$$

It is noted that Eq. (20) has a similar expression in form as that established by Sang-Hyun Kim et al. [32] except that the denominator at the left side of this equation is denoted by the limit load solution (M_0^{bi}) of the cracked bi-material pipe. Fig. 12 depicts the comparison results of the plastic limit loads obtained from the FE analyses, $M_0^{\text{clad}}/M_0^{\text{bi}}$ (FEA) and Eq. (20), $M_0^{\text{clad}}/M_0^{\text{bi}}$ (Eq.(20)), and two dotted lines represent a deviation of $\pm 5\%$ illustrating the difference between these two sets of results. The analysis matrix covers the analytical parameters of the pipe with $D_e/t = 10, 20$, $a/t = 0.1, 0.4$, $\theta/\pi = 0.04, 0.20$, $h/t = 0.1, 0.2$ and $M_y = 0.5, 0.8, 1.5$. It can be seen that the limit load solutions given by Eq. (20) deviate significantly from the results obtained by the FE analyses for the weld mismatch case of

$M_y = 0.5$. However, for M_y values ranging from 0.8 to 1.5, the limit load values of Eq. (20) agree very well with the FE results in view of all the data points located within the deviation of $\pm 5\%$. Therefore, the validity range of the proposed limit load equation for M_y is varying from 0.8 to 1.5.

4. J -estimation procedure based on the h_1 -factor

In this section, extensive 3-D elastic-plastic FE analyses are performed on oval-shaped homogeneous pipes with a circumferential part-through surface crack under bending moment.

4.1 Analysis matrix and material models for elastic-plastic analyses

The analysis matrix presented in Table 1 includes four crack depths ($a/t = 0.1, 0.2, 0.3$ and 0.4) and three crack lengths ($\theta/\pi = 0.04$ to 0.20 with an increment of 0.08). The American Lifelines Alliance (ALA) [33] considers the maximum allowable degree of ovality to be 15%. Hence, C_O values of 0, 5%, 10% and 15% are employed in the present study. The mechanical properties of pipes are varying based on the typical range of high strength low alloy (HSLA) steels as: $n = 5$ and $E/\sigma_y = 800$ (high hardening material), $n = 10$ and $E/\sigma_y = 500$ (moderate hardening material), $n = 20$ and $E/\sigma_y = 300$ (low hardening material) with $E = 206$ GPa and $\nu = 0.3$, which reflect the upward trend in yield stress with the increase of strain hardening exponent. The above materials in the FE analyses are assumed to follow the Ramberg-Osgood model [25] with $\alpha = 1$ as illustrated in Eq. (6), and the FE analyses are carried out using deformation plasticity and small geometry change setting.

4.2 FE analysis and validity of FE models

In the present study, 288 cases of different geometries of pipe configurations are analysed, and the FE models are built up using the mesh generator mentioned in Sub-section 3.1. The element type, boundary and loading conditions used for the J -integral estimation in the elastic-plastic analyses are identical with that described in Sub-section 3.1 aiming at determining the plastic limit load solutions.

The transformation procedure from the circular pipe to the oval-shaped one is depicted in Fig. 13, and the detailed methodology description is listed in Appendix A. A coordinate transformation relationship between the Y-Z and the η - ζ system is appended to the mesh generator to create the oval-shaped pipe models, and the expression is given in the following,

$$\begin{aligned}\eta &= \left(\frac{\pi D + \pi D_e C_0 - 2 D_e C_0}{\pi D} \right) Y \\ \zeta &= \left(\frac{\pi D - 2 D_e C_0}{\pi D} \right) Z\end{aligned}\tag{22}$$

In order to evaluate the J -integral values accurately, a focused spider web mesh as shown in Fig. 4 is employed at the crack tip zone, and the crack tip is surrounded by 12 concentric rings of elements. The J -integral at the crack deepest point is determined from the average value of 2nd-12th contours, and the corresponding J -integral for the first contour is ignored due to the numerical errors. Confidence in the FE analyses is gained from the path independence of the J -integral considering that the difference of the J values for these 11 contours is marginal as shown in Fig. 14. Moreover, it is worth noting that the J values are sensitive to the mesh design, especially to the number of elements. Therefore, the convergent tests covering the range of parameters listed in Table 1 are carried out to determine the optimal number of elements used in the analyses, and Fig. 15 presents one of these cases. It is found that the number of elements ranging from 15,901 to 24,317 is adequate based on the crack configuration and pipe geometry. Further confidence in the validity of FE models is obtained through the above mentioned sensitivity analysis.

4.3 Verification of Eq. (11) and determination of h_1 -factor

In Sub-section 2.2, the conventional EPRI J -estimation expression is developed to serve for the oval-shaped pipe with a circumferential part-through external surface crack by considering the effect of ovality. In this subsection, the validity of the new J -estimation expression given in Eq. (11) is demonstrated. Then, the h_1 -factor is determined according to the procedure proposed by Yun-Jae Kim et al. [17], Chiodo and Ruggieri [3].

For the conventional J -estimation expression (Eq. (7)), J_{pl} is in a proportion to $(M/M_L)^{n+1}$ based on the power-hardening model and the HRR singularity [26,27]. Hence, the validity of Eq. (11) can be ascertained if the linear relationship between J_{pl} and $(M/M_L)^{n+1}$ still holds true. Eq. (11) is rewritten into the following form to conveniently facilitate the verification procedure,

$$\frac{J_{pl}}{\alpha \varepsilon_y \sigma_y b} = h_1(a/t, D_e/t, \theta, n, C_O) \left(\frac{M}{M_L} \right)^{n+1} \quad (23)$$

Fig. 16 shows the evolution trend of $J_{pl}/\alpha \varepsilon_y \sigma_y b$ with $(M/M_L)^{n+1}$ for an oval-shaped pipe with $D_e/t = 10$, $a/t = 0.4$, $n = 10$ and $C_O = 15\%$, and a linear relationship between them is clearly observed. More verification tests for various parameter combinations are conducted to further confirm the linear dependence, verifying the validity of Eq. (11). It is noted that the factor h_1 can be determined directly by measuring the slope of the evolution curve of $J_{pl}/\alpha \varepsilon_y \sigma_y b$ with $(M/M_L)^{n+1}$. The detailed evaluation procedure of the h_1 -factor is also shown in Fig. 16, and the corresponding expression is listed in the following,

$$h_1 = [A_1(C_O)^2 + A_2 C_O + A_3] \left(\frac{a}{t} \right)^{A_4} \quad (24)$$

where the values of A_i ($i = 1, 2, 3, 4$) are tabulated in Table 2.

The influence of the degree of ovality, C_O , on the h_1 -factor for the cracked pipes with varying a/t and D_e/t is investigated in detail. It is noted from Fig. 17 that the h_1 value for a given a/t increases as the C_O increases, and the h_1 -factor displays a relatively strong sensitivity to C_O , especially for deep cracks. It is due to ovalization of the pipe cross-section resulting in reduction of the bending capacity. When the same bending moment is applied to the configurations with circular and oval cross-sections, the oval-shaped pipe undergoes a larger plastic deformation, inducing a larger J -integral value in the crack tip zone. The dependence of h_1 on θ/π for the pipe configuration is not discussed here due to the similar trend observed in the present study with that in the paper published by Chiodo and Ruggieri [3].

5. Validation study

In previous sections, using the ESSRM, the mechanical properties of the welded clad pipe are characterized by the properties of a homogeneous pipe. Then, the EPRI J -estimation procedure [18] is employed to assess the J -integral values of the homogeneous pipe configuration, and the h_1 -factor values covering the range of parameters outlined in Table 1 are calculated. Finally, the J -integral predictions of the welded clad pipe can be implemented through the ESSRM in combination with the EPRI framework [18]. The aim of this section is to demonstrate the accuracy of the method presented in the above by comparing the J -integral values obtained from the ESSRM/EPRI approach and the FE analyses.

A typical clad pipe geometry with $t = 20.6$ mm, $t_{\text{CRA}} = 3$ mm and $D_e/t = 15$ is considered in the present study, and two weld groove angles ($\phi = 20^\circ$ and 60°) with a fixed root opening width ($h/t = 0.15$) are used, representing the narrow and wide weld configurations. The degree of ovality is varying from 0% to 15% in this analysis, covering the range of the parameter, C_0 , outlined in Table 1. A representative pipe steel, API 5L Grade X60, is employed, and its yield stress and strain hardening exponent are 483MPa and 12, respectively. The mismatch level ranging from 0.5 to 1.5 is incorporated in this study to reflect the effects of the weld metal and CRA material mismatch strength. Figs. 18-20 present the comparison results. It is observed that the J -integral values obtained by the ESSRM/EPRI method are in good agreement with the FE results except for the case of $M_y = 0.5$. It is because the application range of the proposed limit load expression given by Eq. (20) is from $M_y = 0.8$ to $M_y = 1.5$. Therefore, the ESSRM coupled with the EPRI J -estimation procedure can predict well the J -integral values for the oval-shaped clad pipe with the mismatch ratio varying from 0.8 to 1.5.

6. Conclusions

The present study provides an engineering integrity assessment of mismatched girth welds in oval-shaped clad pipes subjected to bending moment based on the ESSRM/EPRI estimation

framework. Extensive 3-D FE analyses are performed to determine the plastic limit loads applicable to the welded clad pipes with a circumferential part-through surface crack by considering the effects of weld strength mismatch and weld bevel geometry. The proposed limit load equation agrees very well with the FE results for the mismatch factor varying from 0.8 to 1.5. Based on the limit load equation, the ESSRM is used to convert a complex tri-material structure into a homogeneous configuration made of only one equivalent material, reducing significantly the computation complexity and saving the calculation time. Then, the conventional EPRI J -estimation procedure [18] is extended to serve for the homogeneous pipes with oval cross-section by appending the degree of ovality to the h_1 -factor function. The h_1 -factor for a wide range of pipe geometries and crack sizes are calculated and formulated in Eq. (24). Moreover, the influence of ovality on the h_1 -factor is investigated in detail, and the factor displays a relatively strong sensitivity to the ovality of the pipe cross-section. Finally, validation studies are carried out by comparing the results obtained from the ESSRM/EPRI estimation framework and the elastic-plastic FE analyses, and a good agreement between them is observed for the mismatch factor ranging from 0.8 to 1.5, indicating the validity of the ESSRM/EPRI estimation framework.

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Appendix A Ovalization of pipe cross-section

Because the oval-shaped pipe is analysed in the present study, the methodology for transforming the circular cross-section of a pipe to oval shape one needs to be introduced, and the following assumptions are made for the transformation procedure:

- The cross section of the pipe is assumed to be perfectly oval [23].
- The change in the outer perimeter of the pipe is not considered during the ovalization of pipe cross-section [1].
- The variation of the pipe thickness induced by the cross-sectional ovalization is negligible, and this assumption is reasonable especially for thin-walled pipes employed in this analysis.

According to the first assumption, the oval outer contour of the pipe can be expressed using the standard equation of ellipse,

$$\frac{\eta^2}{(D_e^{\max}/2)^2} + \frac{\zeta^2}{(D_e^{\min}/2)^2} = 1 \quad (\text{A.1})$$

and the equation describing the circular profile is written as

$$\frac{Y^2}{(D_e/2)^2} + \frac{Z^2}{(D_e/2)^2} = 1 \quad (\text{A.2})$$

The second assumption indicating equal perimeters between the circular and the oval outer contours of the pipe produces

$$\pi D_e = \pi D_e^{\min} + 2(D_e^{\max} - D_e^{\min}) \quad (\text{A.3})$$

Based on Eq. (10) and Eq. (A.3), the expressions for $D_e^{\max}$ and $D_e^{\min}$ are obtained,

$$\begin{aligned} D_e^{\max} &= \left(\frac{\pi + \pi C_0 - 2C_0}{\pi} \right) D_e \\ D_e^{\min} &= \left(\frac{\pi - 2C_0}{\pi} \right) D_e \end{aligned} \quad (\text{A.4})$$

Substituting Eq. (A.4) into Eq. (A.1) and making the corresponding terms in Eq. (A.1) and Eq. (A.2) equal, the conversion relationship for the two coordinate systems shown in Fig. 13(a) is given as follows:

$$\begin{aligned}\eta &= \left(\frac{\pi + \pi C_0 - 2C_0}{\pi} \right) Y \\ \zeta &= \left(\frac{\pi - 2C_0}{\pi} \right) Z\end{aligned}\tag{A.5}$$

Eq. (A.5) provides the means for transforming the circular outer contour of the pipe into the oval shape, then in the following, the circular cross section of the pipe in Y-Z system is projected to the oval cross-section in η - ζ system as depicted in Fig. 13(b). The third assumption illustrates that the thickness of the pipe keeps constant during the ovalization process, hence, the following expression is obtained,

$$D_e^{\max} - D_e^{\min} = D^{\max} - D^{\min}\tag{A.6}$$

where $D^{\max}$ and $D^{\min}$ are the maximum and the minimum diameters of an arbitrary ellipse in the pipe cross-section as outlined in Fig. 13(b), and the degree of ovality of this ellipse is denoted as

$$C_0^{\text{temp}} = \frac{(D^{\max} - D^{\min})}{D} \times 100\% = \frac{(D_e^{\max} - D_e^{\min})}{D} \times 100\%\tag{A.7}$$

where D is the nominal diameter of the circle relative to this ellipse. The relationship between C_0 and C_0^{temp} is built up by substituting Eq. (A.4) into Eq. (A.7),

$$C_0^{\text{temp}} = \left(\frac{D_e}{D} \right) C_0\tag{A.8}$$

Substituting Eq. (A.8) into Eq. (A.5) produces

$$\begin{aligned}\eta &= \left(\frac{\pi D + \pi D_e C_0 - 2D_e C_0}{\pi D} \right) Y \\ \zeta &= \left(\frac{\pi D - 2D_e C_0}{\pi D} \right) Z\end{aligned}\tag{A.9}$$

It is noted that the circular cross-section of the pipe can be converted into the oval shape using Eq. (A.9).

Fig. 1 The complicated loading conditions withstood by the pipe [1]

Fig. 2 Details of welded clad pipes with a circumferential part-through surface crack

Fig. 3 Transformation of a circular clad pipe to an oval-shaped one

Fig. 4 Typical FE model of welded clad pipes containing a circumferential part-through surface crack

Fig. 5 Determination of plastic limit load using the twice elastic slope method for a pipe with $D_e/t = 15$, $a/t = 0.2$, $\theta/\pi = 0.12$, $h/t = 0.15$, $\phi = 20^\circ$ and $M_y = 1.25$

Fig. 6 Comparison of plastic limit loads obtained from Eq. (13) [31] and FE analyses for homogeneous pipes with a circumferential through-wall crack under bending moment

Fig. 7 The convergent test case results

Fig. 8 Plastic limit loads of homogeneous pipes with a circumferential part-through surface crack

Fig. 9 Plastic limit loads of bi-material pipes with a circumferential part-through surface crack for varying a/t and θ/π but constant D_e/t

Fig. 10 Plastic limit loads of bi-material pipes with a circumferential part-through surface crack for varying D_e/t

Fig. 11 Converting V-groove butt weld into idealized one having a rectangular cross-section using effective weld width method

Fig. 12 Comparison results of limit loads obtained from FE analyses and Eq. (20)

(a) Transforming the circular outer contour to the oval outer contour

(b) Projecting the circular cross-section to the oval cross-section

Fig. 13 Transformation procedure from the circular cross-section to the oval shape

Fig. 14 Path independence of numerical values of the J -integral

Fig. 15 The convergent test case results

Fig. 16 Evolution trend of $J_{pl}/\alpha\epsilon_y\sigma_y b$ with $(M/M_L)^{n+1}$

Fig. 17 Effect of C_O on h_1 -factor with increasing a/t

Fig. 18 Comparison of J -integral values obtained from ESSRM/EPRI method (red line) and FE analyses (black line) for $M_y = 0.8$

Fig. 19 Comparison of J -integral values obtained from ESSRM/EPRI method (red line) and FE analyses (black line) for $M_y = 1.5$

Fig. 20 Comparison of J -integral values obtained from ESSRM/EPRI method (red line) and FE analyses (black line) for $M_y = 0.5$

Table 1 Table of analysis matrix

D_e/t	n	a/t	θ/π	C_O (%)
10	5	0.1, 0.2, 0.3, 0.4	0.04, 0.12, 0.20	0, 5, 10, 15
	10	0.1, 0.2, 0.3, 0.4	0.04, 0.12, 0.20	0, 5, 10, 15
	20	0.1, 0.2, 0.3, 0.4	0.04, 0.12, 0.20	0, 5, 10, 15
20	5	0.1, 0.2, 0.3, 0.4	0.04, 0.12, 0.20	0, 5, 10, 15
	10	0.1, 0.2, 0.3, 0.4	0.04, 0.12, 0.20	0, 5, 10, 15
	20	0.1, 0.2, 0.3, 0.4	0.04, 0.12, 0.20	0, 5, 10, 15

Table 2 Values of A_i ($i = 1,2,3,4$) in Eq. (24)

D_e/t	n	θ/π	A_1	A_2	A_3	A_4
10	5	0.04	0.0218	0.4894	12.8501	1.1570
		0.12	0.0564	1.9240	45.6702	1.8471
		0.20	0.0811	1.7951	47.3512	1.9583
	10	0.04	0.1143	0.8991	17.1204	1.2492
		0.12	0.2529	2.1714	38.8214	1.6480
		0.20	0.2692	2.3821	41.3122	1.8643
	20	0.04	0.7757	-1.3471	19.8322	1.1851
		0.12	0.9674	-1.2662	25.1102	1.3340
		0.20	0.7421	-1.4630	17.4414	1.3872
20	5	0.04	0.0643	1.8171	46.4423	1.7804
		0.12	0.2445	3.9513	133.8022	2.4391
		0.20	0.1759	4.9822	129.2033	2.5732
	10	0.04	0.3736	3.0112	59.6404	1.7553
		0.12	0.9564	9.0573	160.1033	2.3884
		0.20	0.7689	6.7363	129.9022	2.4034
	20	0.04	2.5174	-2.2422	74.8331	1.6573
		0.12	3.4770	-3.9312	102.4055	1.8512
		0.20	2.3974	-2.4453	68.9804	1.9394