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Estimation procedure of *J*-resistance curves for through wall cracked steam generator tubes

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Abstract

The assessment of the structural integrity of steam generator (SG) tubes in nuclear power plants deserved increasing attention in the last years due to the negative impact related to their failures. In this context, elastic plastic fracture mechanics (EPFM) methodology appears as a potential tool for the analysis. The application of EPFM requires, necessarily, knowledge of two aspects, i.e., the driving force estimation in terms of an elastic plastic toughness parameter (e.g., *J*) and the experimental measurement of the fracture toughness of the material (e.g., the material *J*-resistance curve). The present work describes the development of a non standardized experimental technique aimed to determine *J*-resistance curves for SG tubes with circumferential through wall cracks (TWCs). The tubes were made of Incoloy 800 (*Ni*: 30.0-35.0; *Cr*: 19.0-23.0; *Fe*: 35.5 min, % in weight). Due to its austenitic microstructure, this alloy shows very high toughness and is widely used in applications where a good corrosion resistance in aqueous environment or an excellent oxidation resistance in high temperature environment is required.

Keywords: fracture toughness; *J*-resistance curve; steam generator; tube; Incoloy 800

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1. Introduction

The SGs are heat exchangers consisting in several thousands of tubes arranged inside a pressure vessel. The tubes separate the primary and secondary cooling systems of a nuclear power reactor, isolating the primary coolant and thus avoiding the leak of radioactive elements to the secondary circuit. Due to the negative impact related to their failures, the structural integrity assessment of these components has started receiving more attention recently, Huh et al., 2006.

An extended and excessively conservative rule in the nuclear industry requires that tubes with defects exceeding 40% of the wall thickness should be repaired or plugged (NRC Regulatory Guide 1.121, 1976). The NRC criterion, as well as many studies of the structural integrity of cracked tubes, relies on limit load analysis. This is understandable because of the austenitic microstructure of the material and the thin thicknesses of the tubes. However, for a particular cracked geometry, there are different definitions and expressions for the limit loads. The choice of the appropriate limit load expression is usually based on comparisons with experimental results, requiring extensive test data in order to gain confidence with these analyses. Thus, although the limit load analysis seems to be simple in practice, they could be time-consuming and expensive, Huh et al., 2006.

Fracture mechanics has then appeared as an alternative to assess the structural integrity of cracked SG tubes. There are some models presented in the literature, which use concepts from the linear elastic fracture mechanics, Flesch and Cochet, 1990, Cizelj et al., 1995, Wang and Reinhardt, 2003, and from the EPFM, Huh et al., 2006. The advantage of these types of approaches resides in the fact that the analysis can be easily generalized to different loading conditions, without the need of an extensive experimental validation.

Application of the EPFM to the structural integrity assessment of cracked SG tubes requires, on the one hand, estimating the driving force in terms of the elastic plastic parameter J and, on the other hand, the experimental evaluation of the fracture toughness of the tube material in terms of the J-resistance curve.

Due to the reduced dimensions of the tubes and to its very high fracture toughness, it is impossible in practice to obtain standardized specimens for J-resistance curve determination, which assure plane strain conditions. It is therefore the objective of the present work to contribute to the development of an appropriate experimental technique (non standardized) that allows reliable assessment of the J-resistance curve for SG tubes with circumferential TWCs.

2. Experimental Procedure

Tubes were made of Incoloy 800 (*Ni*: 30.0-35.0; *Cr*: 19.0-23.0; *Fe*: 35.5 min, % in weight), an alloy widely used due to its excellent corrosion resistance in the high temperature environment of nuclear SGs. Tubes of 15.88 mm external diameter and 1.13 mm thickness wall were tested in laboratory. Mechanical testing was performed by loading the specimens in axial tension under displacement control, using a servo-hydraulic testing machine (MTS 793). The applied load, P, and the load-line displacement, δ, were recorded during tests. Tests were performed at room temperature.

As mentioned before, the reduced dimensions of the tubes prevent the construction of standardized specimens for fracture toughness testing. Hence an alternative non standardized test technique was developed, using specimens obtained from straight pieces of tubes 200 mm in length loaded in axial tension.

Two different specimens were designed, with one circumferential TWC (called P1) and with two opposite circumferential TWCs (called P2), see Fig 1. The cracks were grown by fatigue from initial mechanical notches introduced by electro discharge machining. The specimen ends (approximately 30 mm) were clamped using hydraulic grips (MTS 647.10) in the machine jaws, avoiding the ends rotation. An internal plug made of bronze was employed along the gripped length in order to avoid the collapse of the tube ends due to the normal (radial) force applied by the hydraulic wedges.

In the hypothetical case a free tube like the P1 specimen were subjected to pure axial tensile stress, its ends will tend to rotate due to the unsymmetrical circumferential crack. However, in a real experiment, the constraint imposed by the clamped ends avoids this rotation introducing a bending moment, which will exert a closing action on the crack faces, Wang and Reinhardt, 2003. This effect is not usually taken into account in the limit load solutions for tubes with one circumferential TWC subjected to axial loading available in the literature. The use of those solutions will therefore result in overly conservative failure predictions.

To overcome this effect, the P2 specimen illustrated in Fig 1 (a) was introduced in the present work. The presence of two symmetric opposing cracks represents a more symmetrical condition for axial loading.

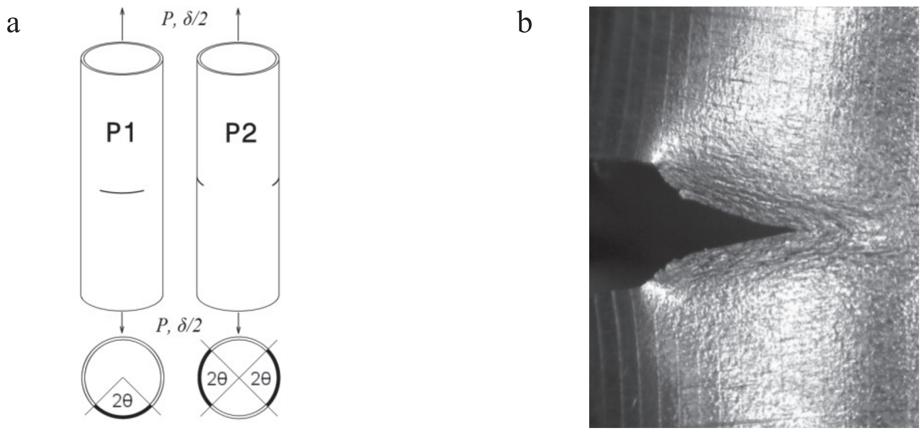

Fig. 1. (a) P1 and P2 specimens; (b) tube surface photograph during a test, with marks 0.318 mm separated among them

A yield strength of 260 MPa and an ultimate strength of 610 MPa were obtained from uniaxial tensile tests performed on similar geometry but uncracked tube specimens.

The *J*-resistance curve construction also needs the measurement of the crack extension throughout the tests. Due to the thin thickness of the tubes, it was assumed that the crack front remains quite flat, and an optical technique was employed to measure the crack growth. Then, the average crack extension can be precisely estimated measuring the crack length at the surface of the tube specimen. A digital microscope was used to detect the crack tip at the surface of the specimens. In order to facilitate the length measurement, mechanical straight marks were performed on the surface of the tubes in the axial direction, separated 0.318 mm among them. Fig 1 (b) is a photograph taken during a test, showing the crack tip and the marks on the tube surface. The relative crack tip extension was determined by analyzing consecutive digital images acquired at increasing displacements. All the crack fronts showed an even growth for both specimen types. Then, an average was used for the individual crack front growth when constructing the *J*-resistance curve.

An additional advantage of the optical method here employed is the possibility of the simultaneous measurements of the crack tip opening displacement, *CTOD*, or the displacement $\delta 5$ proposed by Hellmann and Schwalbe, 1984. In this way, a comparison between the *J* and *CTOD*-resistance curves can be performed from experimental data obtained from a unique test.

3. Experimental Results

3.1. J-integral estimation using the η-factor

Rice et al., 1973, proposed splitting the *J*-integral value in elastic and plastic components,

$$J = J_{el} + J_{pl} = \frac{K_I^2}{E/(1-\nu^2)} + J_{pl} \tag{1}$$

where K_I is the (Mode I) linear elastic stress intensity factor, E is the Young´s elastic modulus, ν is the Poisson´s ratio, and $K_I = \sigma_\infty (\pi R_m \theta)^{1/2} F(\theta)$ with σ_∞ as the remote tension, $F(\theta)$ is a shape factor, R_m is the mean radius of the tube and θ is the half-crack angle (see Fig 1 (a)), Zahoor, 1989-1991.

Rice et al., 1973, interpreted the plastic component, J_{pl}, as the rate of change of potential energy per unit cracked area. Based on this energetic definition, Sumpter and Turner, 1976, proposed relating the plastic *J*-integral to the plastic area under the P vs. δ. Then,

$$J_{pl} = -\frac{1}{B}\frac{dU_{pl}}{da} = \eta \frac{U_{pl}}{Bb} \tag{2}$$

where B is the net section specimen thickness, U_{pl} is the plastic area under the P vs. δ record, a is the crack length, η is a calibration factor and b is the uncracked ligament length. The η-factor is a non dimensional parameter which is assumed to be a function of the flawed geometry and loading type (e.g., bending or tension), but independent on loading magnitude. The main advantage related to the η-method is the possibility of *J*-integral evaluation using the smallest possible number of specimens, in contrast with the multispecimen technique based on the energetic definition of *J*-integral, Ernst et al., 1981. The possibility of estimating an appropriate η-factor for the cases of interest in the present work will be addressed in the next section.

It is important to remark here that the load-line displacement, δ, involved in Eq 2 includes only the contribution due to the presence of the crack, Ernst et al., 1981. This means that displacement δ of the defect-free specimen should be subtracted from the total displacement to evaluate the plastic area U_{pl}. In most cases the displacement of the uncracked specimen is negligible and the subtraction is unnecessary (that is the case of standardized specimens with deep cracks).

3.2. The η-factor

The η-factor is a parameter which relates the *J*-integral with the area under the P vs. δ record. This method is widely used for the *J*-resistance curve determination due to its simplicity and reduced number of specimens needed. Nevertheless Paris et al., 1980, and Ernst et al., 1981, have shown that it is not always possible to express the *J*-integral through the η-factor. In their work, Paris et al., 1980 and Ernst et al., 1981, explored the necessary and sufficient conditions for the existence of the η-factor. In general, the existence of η implies that it can be expressed solely as a function of the geometry of the flawed specimen (usually in terms of a/W, where a is the crack length and W is the specimen width), being independent of the level of deformation.

Paris et al., 1980 and Ernst et al., 1981, showed that the η-factor will always exist if and only if a separation of variables can be found for the load, P, in terms of a/W and the plastic displacement, δ_{pl}. This means that plots of P vs. δ_{pl}, for different relations a/W, must show a scaling relationship if the separation of variables exists, at least for certain ranges of a/W and δ_{pl}.

There are some expressions available in the literature for the η-factors for tubes with circumferential TWCs subjected to axial tension (specimen P1), Zahoor, 1989-1991, Takahashi, 2002, Huh et al., 2006. The comparison among these proposals shows an important disparity in the values, motivating a deeper study to verify the correctness of the definitions of η-factors for the particular geometries under research.

Accordingly, a finite element analysis was conducted to estimate the η-factor values for the specimen and material used here. Numerical 3D models for both type of specimens (P1 and P2) were developed, varying the crack lengths from 10 to 24 mm for the P1 specimen geometry ($2a/W$ = 0.22-0.52), and from 10 to 14 mm (each crack) for the P2 specimen geometry ($4a/W$ = 0.43-0.60), with increments of 2 mm. The actual material stress vs. strain curve measured by means of laboratory tensile tests was used for the numerical simulations. A focused mesh was designed to provide detailed resolution of the near-tip stress-strain fields, using 3D 20-node quadratic brick elements with reduced integration. The J-integral is calculated by the contour integral definition through the domain integral or "virtual crack extension" method, Brocks and Scheider, 2001.

The η-factors were calculated solving Eq 2, from the J-integral values and the plastic area under the P vs. δ numerical results. Fig 2 (a) shows the η-factor evolution for increasing the J-integral values (normalized by $b\sigma_0$, where b is the uncracked ligament and σ_0 is the yield strength), for both P1 and P2 specimens. It can be seen that the η-factor depends on the loading or deformation levels, displaying a strong variation at low deformations and reaching an approximately constant value for higher deformation levels. Cravero and Ruggieri, 2007, found in their numerical work a similar behavior for single edge notch tension specimens. They assumed that for low deformation levels, the elastic and plastic areas under the P vs. δ have similar magnitudes, thereby affecting the calculated η-factors. For the region where the plateau is reached, Cravero and Ruggieri, 2007, considered reasonable to use an averaging procedure to compute the η-factors, arguing that the typical values of experimentally measured J-integrals in fracture testing are reached for the higher deformation levels. Following the same criterion, η-factors were estimated from the results in Fig 2 (a) considering the last part of the curves, taking an averaged value for each crack length. Fig 2 (b) displays the η-factor dependence with the crack length for P1 and P2 specimens.

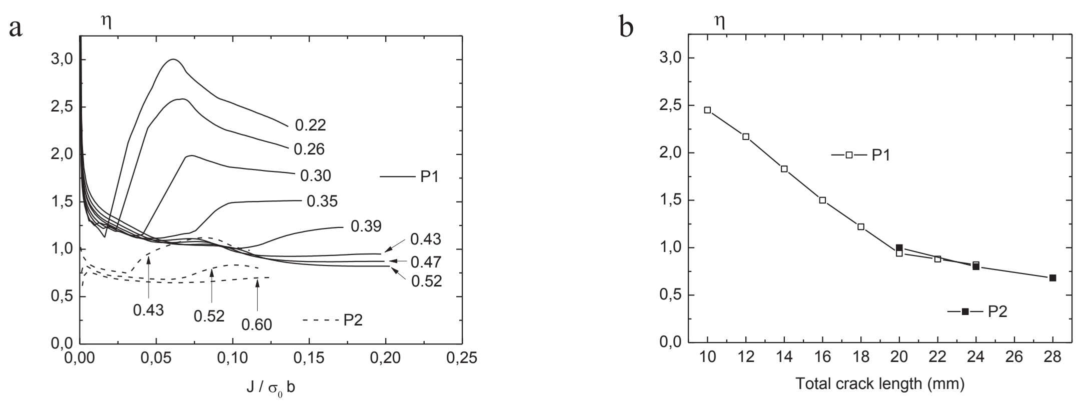

Fig. 2. (a) η-factor variation with normalized $J/b\sigma_0$ for different cracked sections; (b) averaged η-factor variation with total crack length

3.3. J-resistance curves

Fig 3 shows all the J-resistance curves obtained for the P1 and the P2 specimens tested in laboratory. To compare the results for both types of specimens, the J-resistance curves were plotted vs. the individual crack tip extension. The blunting line represented by $J = M\,\sigma_f\,\Delta a$, where σ_f is the flow stress (defined as the average of yield strength and ultimate tensile strength) was included in the figure. M = 4 was adopted here which corresponds to the value recommended for low strength and high work hardening materials, Mills, 1981. The J-integral values at the onset of stable crack extension, J_q, are listed in Table 1. Although the technique here developed is non standardized, it is still possible to estimate the maximum crack extension capacities according to ASTM E1820-99, being this values 4.2 mm and 1.7 mm for P1 and P2 specimens respectively (calculated as 25% of the initial remnant ligament).

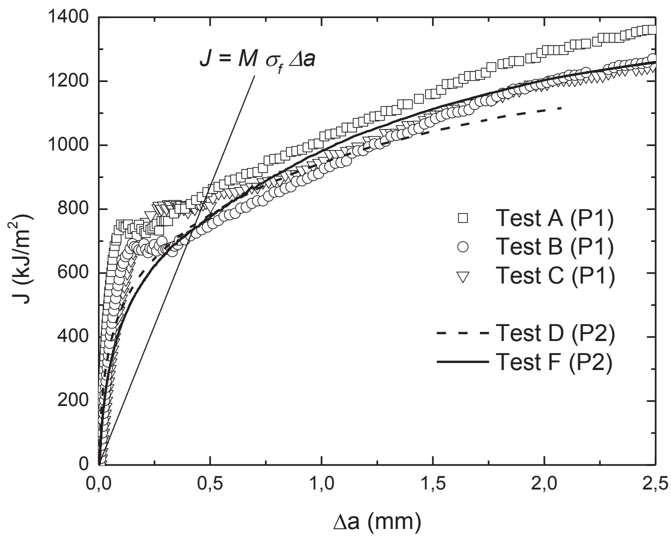

Fig. 3. *J*-resistance curves for A, B and C (P1), D and F (P2) tests

It should be adverted that the crack growth correction for the *J*-resistance curve, through the γ-factor proposed by Ernst et al., 1981, was not considered in this work. Therefore, the *J*-resistance curve presented here will tend to overestimate the actual one, Ernst et al., 1981. Nevertheless the correction term is not very important, until a high crack extension is reached. Then, considering the doubtful validity of the η-factors obtained before, the crack growth correction can be admitted to be second order in the presented results.

Table 1. Summary of tests and J_q-integral values

Test	Specimen type	θ/π	$2a$ (mm) (*each crack approx.)	J_q (kJ/m^2) (for $J = 4\ \sigma_f \Delta a$)
A	P1	0.226	10.46	852
B	P1	0.250	11.57	717
C	P1	0.265	12.29	819
D	P2	0.242	11.23*	752
E	P2	0.240	11.16*	737

4. Discussion

The obtained *J*-resistance curves indicate that there are still some important aspects deserving further discussion. Firstly, and related with the η-factors, it was found a huge dispersion in the values reported in the literature (for the P1 specimen in tensile axial load). This encouraged our numerical studies, which led to some discussion about the existence of the η-factor for our material and cracked specimen geometry. The η-factors displayed dependence on the deformation level, implying that the separation of variables is not valid, nor the η-factor, at least in a strict sense, Paris et al., 1980 and Ernst et al., 1981. However, an averaging procedure was done for the higher deformation levels to estimate the η-factors in an approximate way. From these results, *J*-resistance curves were constructed, showing a similar trend for P1 and P2 specimens tested.

In view of the previous results for the η-factors, it became necessary exploring new alternatives for the J-resistance curves measurement in SG tubes.

One option is the use of the *CTOD* as the fracture toughness parameter, measuring the *CTOD*-resistance curve. This curve can be then expressed in terms of J-integral through the relationship $J = m\ \sigma_y\ CTOD$, where m is a factor between 1 and 3 (depending on the cracked geometry, the work hardening coefficient and the yield strength) and σ_y is the yield strength, Perez Ipiña, 2004. As mentioned before, the *CTOD* (or its equivalent, the $\delta 5$) can be easily measured with the optical method used in this work.

Another alternative is the use of a four-point bending (4P-B) configuration. Gupta et al., 2006, studied this geometry and loading condition for large nuclear power plant pipes. In their numerical study, they found almost constants values for the η-factor. This would imply that the separation of variables is applicable. A preliminar numerical study made for our SG tubes under 4P-B gave a much better η-factor behavior with the deformation levels than the axial tensile load condition.

Regarding the limitations, it is worth mentioning the important geometric distortion suffered by the specimens during the tests. Fig 4 clearly indicates tube wall bending and change in the curvature radii in the cracked section in both P1 and P2 specimens. The numerical simulations do not consider these geometric changes; for all the simulations with different initial crack length, the geometry considered at the beginning of the loading was a perfect cracked tube, undergoing deformation without crack growth as the load increases.

As the crack grows the geometric distortion becomes more important, limiting the validity of the numerical results and the J-resistance curve for higher crack extensions.

a

b

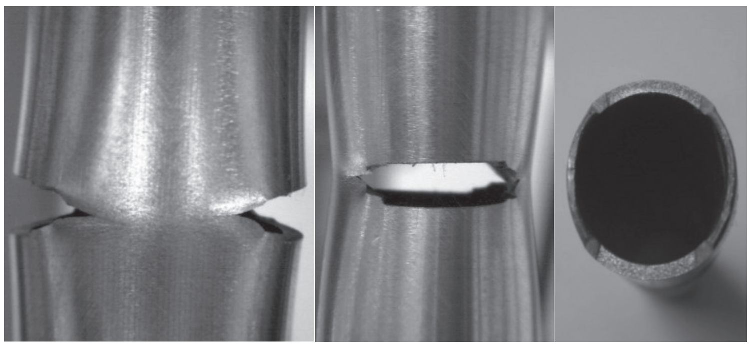

Fig. 4. Geometrical distortion during fracture tests; (a) P1 specimens; (b) P2 specimens

During the loading it was also observed the yield of the gross section remote from the flaw, due to the relative shallow cracks and the low yield strength and high work hardening rate of the material (this occurred specially in the P1 specimen tests). Paris et al., 1980, pointed out that if the nature and location of plasticity present changes radically during loading, then the η-factor may not exist, because the widespread plasticity limits the separation of variables condition for the η-factor existence. However, Turner, 1980, proposed a method to overcome this effect, estimating η-factors in an approximate way by considering only the part of the potential energy related to the crack growth (subtracting the energy associated with the widespread plasticity from the total one). In practice, it is possible to measure the crack mouth opening displacement, *CMOD*, calculating the area under the P vs. *CMOD* (which is mainly associated with the crack growth process), and estimating the J-integral through η-factors derived for *CMOD*. Also Cravero and Ruggieri, 2007, Gupta et al., 2006, and Suh and Kim, 2008, showed that the η-factors derived for *CMOD* are less sensitive to the loading or deformation levels and the material properties than the η-factors derived for δ.

5. Conclusions

A new experimental technique for the *J*-resistance curve determination for SG tubes with circumferential TWCs was developed. Specimens were fabricated from straight tubes 200 mm long, with one and two opposed circumferential TWCs, and were subjected to tensile axial load. The stable crack growth was measured during the test applying an optical method, which showed to be adequate and also allows simultaneous measurement of the *CTOD* or $\delta 5$ fracture toughness parameters.

For the *J*-integral evaluation procedure, some drawbacks were found: the existence of the η-factor and its validity was not confirmed, and the specimen geometric distortions were important. This led to the conclusion that the specimen geometries used were not suitable for the determination of the *J*-resistance curves from the P vs. δ experimental records, at least in a strict sense. In spite of that, using averaged η-factors five *J*-resistance curves were constructed, showing similar values and behavior for both types of specimens.

Alternative testing techniques (e.g., *CTOD* and 4P-B tests) should be studied and explored. The comparison of experimental results could validate the *J*-resistance curves obtained here and the use of the η-factor method in cases where the conditions for existence of η are not strictly met.

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