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The fracture behaviour of a welded tubular joint: an ESIS round robin on failure assessment methods Part V: screening method by required toughness and plastic stability considerations

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Abstract

A simplified failure assessment method, which is meant to be appropriate for a first screening of the considered structure with respect to its defect sensitivity, is applied to the T-joint of the ESIS round robin [Fracture behaviour of a welded tubular joint—round robin on failure assessment methods, First Information Package GKSS, 1994; Proceedings of 10th European Conference on Fracture, Berlin, vol. 1, 1994, p. 787]. The method enables one to identify fracture critical parts and to predict roughly the behaviour of a crack-like defect. It consists of an overall required toughness criterion to predict whether or not cleavage fracture can be initiated by a surface crack, and of a simplified stability analysis of the subsequent tearing process. This information can provide the basis to decide whether or not a more detailed failure assessment is required. Combined with analytical calculations of the overall plastic limit loads, the loading and fracture behaviour of the T-joint could be predicted by this simple and cost-effective method with reasonably good accuracy. © 2002 Published by Elsevier Science Ltd.

1. Introduction

Depending on the ductility and toughness of the material, the maximum load a component can bear is given either by fracture or by plastic collapse due to excessive yielding. Usually, in the case of engineering structures, the ductility of the material is sufficient such that a structure yields before it breaks. Therefore the maximum load is governed by plastic mechanisms such as plastic hinges or slip line fields. The situation is different if a sharp notch or a crack-like defect is present. In this case even components made of ductile materials might fail in a ‘‘brittle’’ manner by unstable crack growth. The latter is possible either by cleavage or micro-ductile (quasi-brittle) mechanisms. Consequently defect assessment is an important part of a

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safety analysis of a potentially critical structural part. To assess the behaviour of a component containing a crack several methods have been developed in recent years, the R-6-method [3], the ETM method [4] and the EPRI method [5] being the best known. They provide engineering procedures that are necessary to perform a simplified fracture mechanics analysis in a straightforward and unambiguous way. However, they are still rather time consuming and costly, since they require in general a rather detailed static or dynamic stress analysis.

Unfortunately, some of the crucial parameters required in a failure assessment analysis, for example the fracture toughness of the material under the special loading conditions or the size and shape of the representative defects, are often missing or not exactly known. Therefore the results of such an analysis may still be ambiguous. Furthermore, since the toughness of today's structural materials is normally sufficiently high, the above mentioned assessment methods often reveal a rather high safety with respect to spontaneous fracture. Thus, before taking the effort of performing a detailed, time-consuming analysis, it is often advantageous to start with a simple screening procedure to identify the potentially critical parts and to decide whether or not a more detailed failure analysis is necessary. An appropriate analysis procedure for approximate failure assessment and first screening is the method of required toughness as described in [6] in combination with the tearing stability assessment of surface cracks as described in [7,8]. In the present paper these methods are applied to the welded tubular joint of the ESIS round robin [1,2]. Based on the information given in these references predictions of the global behaviour of the joint and especially of the inserted crack are made. It is shown that this simple and low-cost analysis, which does normally not require a detailed stress analysis, provides some helpful first insight into the nature of the fracture problem considered and that the results are consistent with the conventional failure assessment methods.

2. Engineering failure assessment

2.1. Cleavage fracture vs. ductile tearing

It is well known that cleavage fracture can occur at local stress raisers if the maximum stress exceeds a certain critical value in a certain minimum volume of the material. For engineering materials this is usually only possible under the constraints of a sharp notch or crack in plane strain. According to linear elastic fracture mechanics the condition for which a crack remains stable is

$$K_{\mathrm{I}} < K_{\mathrm{Ic}} \qquad (1)$$

with K_{Ic} the plane strain fracture toughness of the material and K_{I} the actual stress intensity factor (SIF) which depends on the local stress field and defect size. Thus evaluation of Eq. (1) requires knowledge of these quantities which is often not the case or needs costly investigations.

However, even in the presence of cracks the constraints are often not high enough to trigger unstable cleavage fracture in engineering materials. Due to their toughness the constraints are reduced by local yielding, either out-of-plane yielding in case of thin structures or in-plane yielding in case of plane strain conditions. A representative case for the latter is a surface crack, where the constraints are drastically decaying when slip lines are formed between the crack tip and the rear surface, i.e., in the case of general yielding of the ligament [8]. Once reached the state of general yielding of the ligament, initiation of unstable cleavage is very unlikely. Based on these general theoretical grounds, the minimum fracture toughness which prevents initiation of crack growth before general yielding in the ligament of the cracked section occurs is estimated in [6] to be

$$K_{\mathrm{req}} = 1.36 R_{\mathrm{p}} \sqrt{t} \qquad (2)$$

where R_p denotes the yield strength of the material and t the thickness of the component. K_{req} according to Eq. (2) is called the required toughness. If the actual fracture toughness of the material, K_{Ic}, is higher than K_{req}, then an arbitrary surface crack will not become unstable before a state of general yielding in the ligament is reached. Thus, if the material fulfils the criterion

$$K_{Ic} > K_{req} \tag{3}$$

then ductile tearing can be expected rather than unstable cleavage, which means that an applied stress close to the plastic collapse load is required to initiate crack growth and fracture. In this case, a more detailed fracture mechanics analysis is usually not necessary, since the applied stresses are seldom high enough to initiate ductile tearing. Nevertheless one should make sure that the tearing process in the case of an overload remains stable. Otherwise, although the fracture process is ductile tearing in a local consideration, the component might fail in an unstable, spontaneous manner from a global point of view, e.g., by bursting in case of a pressure vessel. The corresponding analysis for the case of a surface crack in a plate is outlined in the next section.

2.2. Tearing stability of a surface crack

Unstable tearing is a special type of local plastic instability. If a surface crack as shown in Fig. 1 is growing in depth, a redistribution of the local stresses takes place, shifting the released stresses that act in the ligament towards the lateral regions of the crack and increasing, thereby, the crack tip loading. This process can be self-accelerating, thus leading to an unstable crack growth. It has been derived in [7,8] that the tearing process is stable for a surface crack of half-width c and depth a in a plate of thickness t (Fig. 1) if the stress σ that acts perpendicular to the crack fulfils the condition

$$\sigma \leqslant \sigma_u(a) = \sigma_f \left[1 - \frac{a}{t} \frac{2\pi}{2\pi + \mathrm{CTOA}\,\frac{Et}{\sigma_f c} - 4} \right] \tag{4}$$

In Eq. (4) σ_f denotes the flow stress defined by $\sigma_f = (R_p + R_m)/2$, R_m being the ultimate tensile strength. CTOA is the crack tip opening angle under plane strain condition, which can be assumed to be about 0.2 for medium-strength structural steel [6]. According to Eq. (4) $\sigma_u(a)$ represents a straight line in the σ vs. a plane, dividing it in a stable and an unstable region (Fig. 2, line a).

Under an increasing stress, a crack of initial depth a_0 will start to grow when the crack tip loading in terms of J-Integral or crack tip opening displacement δ reaches the critical value. Since J or δ as a function of the load σ are known to be steeply rising as soon as general yielding in the ligament occurs [5], it is assumed that crack extension will start when the ligament becomes fully plastic which is the case when line

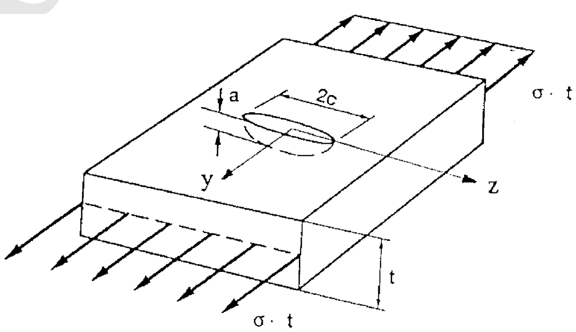

Fig. 1. Representative semi-elliptical surface crack.

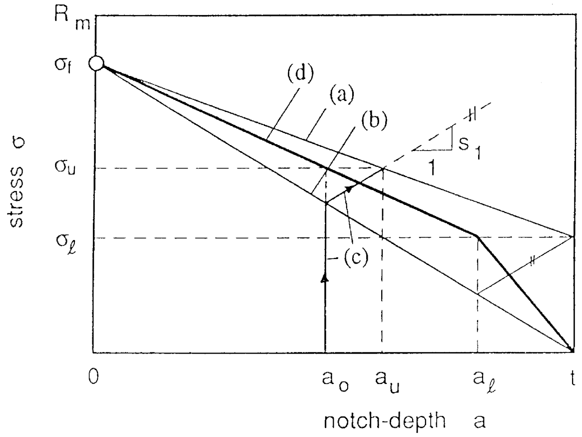

Fig. 2. Behaviour of a surface crack as a function of depth and applied stress (see text for meaning of lines (a)–(d)).

(b) in Fig. 2 is reached and exceeded. Such a rough estimate of the initiation behaviour is justified in the first order assessment method, since the failure process according to the present model is governed by tearing stability rather than initiation of crack growth. According to [7,8] the relation between the applied stress and the amount of stable crack growth Δa $(= a - a_0)$ is given by

$$\sigma(a_0, \Delta a) = \sigma_f\left(1 - \frac{a_0}{t}\right) + s\Delta a \tag{5}$$

where

$$s = \mathrm{CTOA}\frac{E}{c} - 4\frac{\sigma_f}{t} \tag{5a}$$

is the slope of the corresponding curve (c) in the σ vs. a plane, Fig. 2. If $s < 0$, the crack growth becomes unstable as soon as it is initiated. In this case performing a more detailed fracture analysis to determine the conditions for crack initiation is necessary. If $s > 0$, the tearing crack growth process is stable. Stability lasts until the corresponding $\sigma(a - a_0)$-line intersects with the line $\sigma_u(a)$ (see Fig. 2, line a) at $a = a_u$. Representing the corresponding stress $\sigma_u(a)$ as a function of the initial crack depth a_0 one obtains from Eqs. (4) and (5) the following stability criterion for the applied stress σ:

$$\begin{aligned} &\sigma < \sigma_f\left[1 - \frac{a_0}{t}\,\frac{(1 - \sigma_\ell/\sigma_f)}{a_\ell/t}\right] && \text{for } a_0 < a_\ell \\ &\sigma < \frac{Et}{4c}\mathrm{CTOA}\left(1 - \frac{a_0}{t}\right) && \text{for } a_0 > a_\ell \end{aligned} \tag{6}$$

where

$$a_\ell = \left(1 - \frac{4\sigma_\ell c}{Et\,\mathrm{CTOA}}\right)t \tag{6a}$$

$$\sigma_\ell = \sigma_f\left[1 - \frac{2\pi}{2.283 + \frac{\mathrm{CTOA}\,Et}{\sigma_f c}}\right] \tag{6b}$$

Graphically, the criterions in Eq. (6) represent the area left from line d in Fig. 2. Summarising, the failure behaviour can be roughly assessed as follows: If Eq. (3) is fulfilled, then the mode of crack growth is ductile tearing, i.e. there is no risk of unstable cleavage. Initiation of substantial tearing crack growth of a surface crack occurs at an applied stress of about $\sigma = \sigma_f(1 - a_0/t)$. The tearing process is stable if s according to Eq. (5a) is positive. The tearing process remains stable as long as condition in Eq. (6) is fulfilled. In this case, a further failure assessment analysis is not necessary.

In principle the amount of stable tearing crack growth Δa corresponding to the applied stress σ can be also obtained from Eq. (5). However, due to the rough estimation of crack initiation and the fact that crack tip blunting and local bending—which are inherently present in case of a surface crack—have been disregarded, the prediction of Δa by Eq. (5) is rather inaccurate, especially for small amounts of crack growth.

3. Application to the ESIS T-joint

3.1. Global plastic behaviour of the tubular joint

In this section we consider the loading behaviour of the joint described in [1] (Fig. 3) by simple rigid-plastic models according to the basic theory of strength of materials. The existence of a crack at point B is disregarded in this context, since there is hardly any evidence of such local plastic events in the global force–displacement behaviour considered here. Thus, only full through-the-thickness yielding of tube walls is assumed to be significant.

When increasing the load F the first through-the-thickness yielding is expected to occur at location A in the wall of the chord, since there is the bending stress σ_{bA} interacting with the shear stress τ_A that is introduced to the chord from the vertical the brace by the weld material. According to simple bending theory the former is given by

$$\sigma_{bA} = \frac{F(S/2 - R_2)R_1}{2I} \tag{7}$$

where R_2 and R_1 denote the radii of the tubes as indicated in Fig. 3, S is the span between the lateral supports of the chord and I is the second moment of area of the cross-section of the chord, i.e.

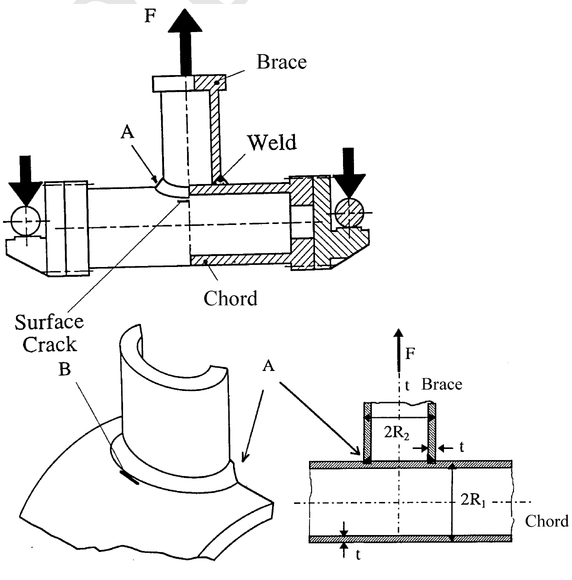

Fig. 3. Geometry of the T-joint and corresponding parameters.

$$I = \frac{\pi}{4}\left(R_1^4 - r_1^4\right) \quad \text{with } r_1 = R_1 - t \tag{8}$$

Assuming that the normal stress in the brace is homogeneously distributed along the circumference of the brace, the shear stress in the chord at A is given by

$$\tau_A = \frac{F}{2R_2\pi t} \tag{9}$$

According to Tresca's criterion yielding at A occurs if

$$\sqrt{\sigma_{bA}^2 + 4\tau_A^2} = R_p \tag{10}$$

From [1] one finds $R_p = 450$ MPa, which leads with Eqs. (7)–(10) to

$$F =: F_{yA} = 1.23 \text{ MN} \tag{11}$$

for the first yielding. When the load F is further increased, a plastic zone will spread out from A into the chord in two main directions: On the one hand along the circumferential weld toe and on the other perpendicular to the tube axis deeper into the chord. Correspondingly two modes of plastic collapse can occur when these zones are saturated: Plastic pull-out of the brace by shear slip in the chord (Fig. 4a), and formation of plastic hinges in the chord at cross-sections $x = \pm R_2$ (Fig. 4b). The corresponding plastic collapse loads F_{pp} and F_{ph} are estimated in the following.

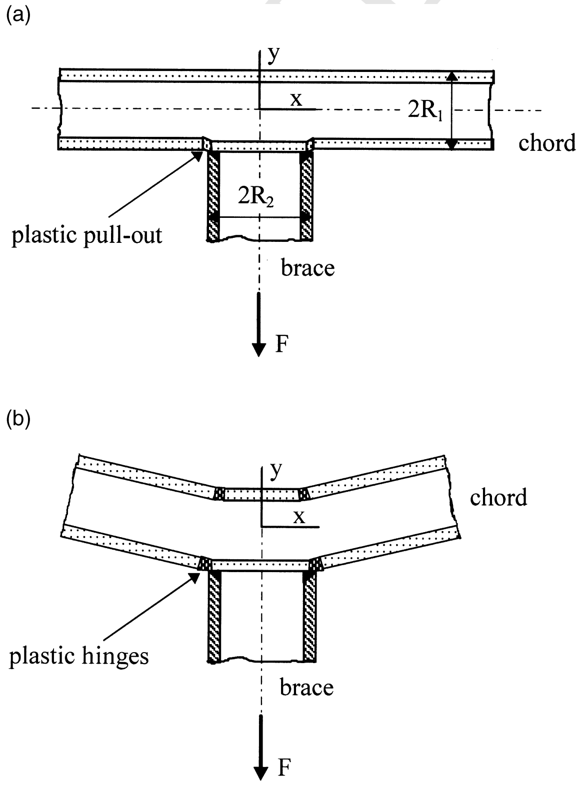

Fig. 4. Considered plastic collapse modes: (a) pull-out of the brace by plastic shearing, (b) plastic hinge mechanism.

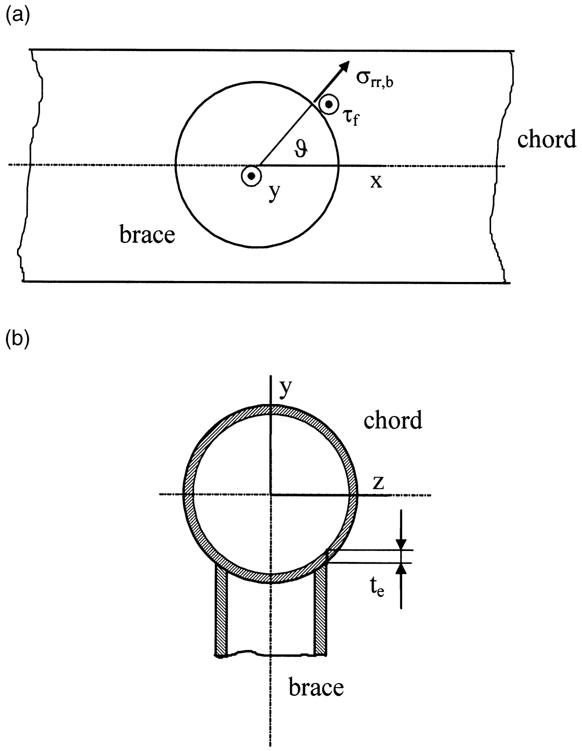

Fig. 5. Definition of geometrical and statical parameters for plastic pull-out calculation.

Extending the model used at location A to an arbitrary location near the weld toe (defined by the angular coordinate ϑ, Fig. 5a) the yield condition Eq. (10) becomes

$$\sqrt{\sigma_{\mathrm{rr,b}}^2 + 4\tau_{\mathrm{s}}^2} = R_{\mathrm{p}} \tag{12}$$

where $\sigma_{\mathrm{rr,b}}$ is the radial component (with respect to the brace) of the bending stress in the chord which is, according to fundamental solid mechanics,

$$\sigma_{\mathrm{rr,b}} = \frac{F(l/2 - R_2)}{4I} \cos^2\vartheta \sqrt{R_1^2 - R_2^2 \sin^2\vartheta} \tag{13}$$

An infinitesimal wall segment given by $\mathrm{d}\vartheta$ transmits a load increment of

$$\mathrm{d}F_{\mathrm{pp}} = \tau_{\mathrm{s}} t_{\mathrm{e}} R_2 \,\mathrm{d}\vartheta \tag{14}$$

where τ_{s} is the local shear stress that fulfils the yield condition Eq. (12) and t_{e} is the effective wall thickness, i.e. the length of a slip area in the wall of the chord in axial direction of the brace, Fig. 5b.

$$t_{\mathrm{e}} = \frac{tR_1}{\sqrt{R_1^2 - R_2^2 \sin^2\vartheta}} \tag{15}$$

Using Eqs. (12)–(15) leads to

$$F_{\mathrm{pp}} = \int_0^{2\pi} \left\{ \frac{R_{\mathrm{p}}}{R_1^2 - R_2^2 \sin^2 \vartheta} - \left[\frac{F_{\mathrm{pp}}(l/2 - R_2)}{4I} \cos^2 \vartheta \sqrt{R_1^2 - R_2^2 \sin^2 \vartheta} \right]^2 \right\}^{1/2} R_1 R_2 t \,\mathrm{d}\vartheta \tag{16}$$

Numerical integration of Eq. (16) delivers

$$F_{\mathrm{pp}} = 1.93 \text{ MN} \tag{17}$$

Some basic relations of the elastic perfectly plastic behaviour of beams in bending are sufficient to calculate the load required for plastic hinge formation as shown in Fig. 4b. One obtains

$$F_{\mathrm{ph}} = 2.37 \text{ MN} \tag{18}$$

Thus, plastic pull-out of the brace occurs before plastic hinge formation in the chord. Therefore the representative plastic limit load F_{p} is the one as given by Eq. (17), $F_{\mathrm{p}} = F_{\mathrm{pp}} = 1.93$ MN.

3.2. Tearing stability of the crack

As described in [1,2] the tubular joint shown in Fig. 3 contains an almost semi-elliptical surface crack near the weld toe at location B. The detailed crack configuration is shown in Fig. 6a. Using the relevant system parameters $R_{\mathrm{p}} = R_{\mathrm{p0.2}} = 450$ MPa, $R_{\mathrm{m}} = 535$ MPa and $t = 20$ mm as provided in [1], the required toughness determined by Eq. (2) turns out to be

$$K_{\mathrm{req}} = 2740 \text{ N/mm}^{3/2} \tag{19}$$

The fracture toughness of the material is obtained from the J-value given in [1] at $\Delta a = 0.2$ mm, $J_{0.2}$, by

$$K_{\mathrm{Ic}} \cong K_{\mathrm{IJ}} = \sqrt{\frac{J_{0.2} E}{1 - \nu^2}} = 8470 \text{ N/mm}^{3/2} \tag{20}$$

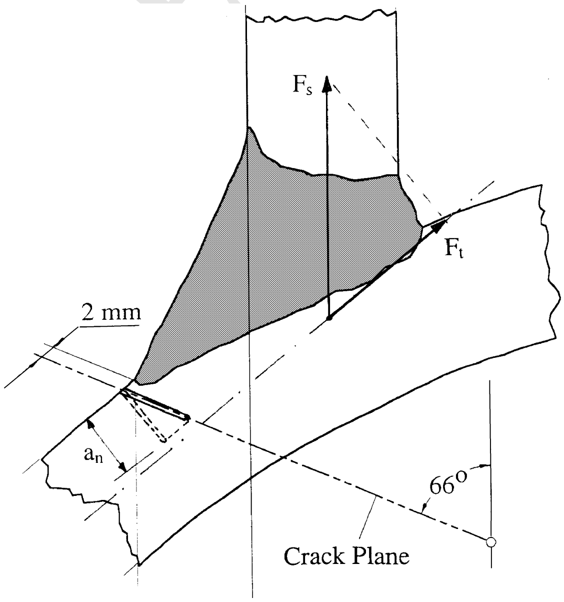

Fig. 6. (a) Crack location and orientation and (b) corresponding simplified model.

hence exceeding the required toughness substantially. According to Section 2.1 this means that the crack is expected to extend by ductile tearing rather than by cleavage.

The external vertical force (per unit length) that is acting by the brace on the crack is denoted by F_s and corresponds to dF_{pp} as given by Eq. (14) for $\vartheta_1 = 90°$,

$$F_s = \frac{\tau_f t R_1}{\sqrt{R_1^2 - R_2^2}} \tag{21}$$

where τ_f is the shear stress that fulfils Eq. (12) with $\sigma_{rr,b}(\vartheta = 90°) = 0$.

Whereas using Tresca's yield criterion and neglecting strain hardening is conservative in the plastic analysis of Section 4, it is non-conservative in the present context, since the corresponding stresses act as loads on the crack. For this reason Eq. (12) is replaced with the Von-Mises criterion which results in higher stress levels for a given yield strength, and R_p is replaced with the flow stress $\sigma_f = (R_p + R_m)/2$ to account for the hardening behaviour. This results in

$$\tau_f = \frac{(R_p + R_m)}{2\sqrt{3}} \tag{22}$$

To predict whether or not the tearing process is stable we apply Eq. (6). These formulas hold for a perpendicular surface crack in a plate under a uniaxial stress σ, whereas in the present case both the crack plane as well as the main load direction (F_s) are inclined with respect to the tangential direction of the tube wall, Fig. 6. Thus, a representative model has to be chosen. One possibility is to consider the crack as well as the load in their tangential projections. This means that a crack of length

$$a_n = a \cos\alpha = 8.73 \text{ mm} \quad (\text{with } \alpha \approx 29.2°) \tag{23}$$

in a plate of thickness t is assumed to be representative (dotted crack in Fig. 6). Similarly, the effective load per unit length F_t is the tangential component of F_s. This corresponds to a representative stress σ_t which is calculated to be

$$\sigma_t = \frac{\tau_f R_1}{\sqrt{R_1^2 - R_2^2}} \cos\beta = 307 \text{ MPa} \quad (\text{with } \beta \approx 59.3°) \tag{24}$$

For the given parameters ($a_0 = a_n = 8.73$ mm, $c = 23.25$ mm, $\sigma_f = 492.5$ MPa) and CTOA = 0.2 (assumed according to [8]), the stress required for unstable growth according to Eq. (6) is $\sigma_u = 473.7$ MPa, which is considerably higher than the applied stress σ_t. This is still true if—for the sake of conservatism—σ_f in Eq. (6) was replaced with R_p. Thus the tearing process will be stable, and, according to Eq. (5), there is only a very small amount of crack growth ($\Delta a = 0.054$ mm) to be expected.

Usually, in practical applications, this information about the crack behaviour is sufficient, so a first engineering failure assessment could stop here. However, for the present round robin, some further predictions shall be made, namely the amount of crack growth, the crack driving force (in terms of J or δ) and the load at initiation of stable tearing [7]. Actually such predictions are beyond the scope of the models presented. However, for the purpose of the round robin, some rough extensions are made in the next section.

3.3. Estimation of crack extension

To calculate the amount of stable crack extension Δa one can in principle use Eq. (5), but as noted in Section 2, two contributions, local secondary bending (due to the eccentric ligament stresses) and crack tip blunting, are disregarded therein. These effects, which usually are small compared with the ligament but are important in the range of small crack extension ($\Delta a \ll t - a$) as in the present case, are estimated in the

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following. A rough estimation of the local bending moment of the ligament stresses with respect to the center line of the wall is

$$M = \sigma_f (t - a) \frac{a}{2} \tag{25}$$

In the following only its elastic effect on the crack behaviour is considered. It gives rise to an extra SIF of

$$K_{\mathrm{Ibend}} \cong \frac{4M}{(t - a)^{3/2}} \tag{26}$$

(approximation following from [11]), which causes an additional crack tip opening displacement of

$$\delta_{\mathrm{bend}} = \frac{K_{\mathrm{Ibend}}^2}{E\sigma_f} = \frac{4\sigma_f a^2}{(t - a)E} \tag{27}$$

Therefrom, the corresponding crack extension Δa_{bend} can be obtained from the simple linear relation

$$\Delta a = \frac{\delta}{\mathrm{CTOA}} \tag{28}$$

which is used in the model underlying Eq. (4). By inserting the above given parameters (with $a = a_n = 8.73$ mm) one finds from Eq. (27) $\delta = 0.0634$ mm. With CTOA estimated according to [8] to be CTOA = 0.2, Eq. (28) delivers a crack extension of $\Delta a_{\mathrm{bend}} = 0.317$ mm. The blunting crack growth at initiation of tearing is roughly assumed to be $\Delta a_{\mathrm{blunt}} = 0.2$ mm according to [1]. From Eq. (5), the additional tearing crack growth is $\Delta a_t = 0.054$ mm. Thus the total stable crack growth is

$$\Delta a_s = \Delta a_{\mathrm{blunt}} + \Delta a_{\mathrm{bend}} + \Delta a_t = 0.57 \text{ mm} \tag{29}$$

The crack driving force as a function of the applied load cannot be obtained by the present model. However, tentatively, from the crack extension as given above, the corresponding J can be taken from the experimental relation between J and Δa as provided by Table 3 in [1], which leads to

$$J_{\max} = 815 \text{ N/mm} \tag{30}$$

$J_{\max}$ is the predicted maximum J the crack will experience. However, the load at initiation of crack growth, which also is requested in [1], can not be estimated by this model.

4. Discussion and conclusions

The main results of the present calculations are the following:

- The first yielding that manifests on the global load vs. load-line displacement curve occurs at a load of 1.23 MN and the ultimate load is 1.923 MN. The failure mode will be a plastic shear instability along a slip line around the weld toe (pull-out of the brace), which is not significantly affected by the crack.
- The analysis shows that neither unstable cleavage fracture nor unstable ductile tearing will occur for loads below the ultimate plastic collapse load.
- Initiation of crack growth will take place, but there will be only a small amount of stable crack growth. The maximum extension of the crack at plastic collapse is estimated to be 0.57 mm.

Actually, the models underlying the present theory are too rough to enable crack extensions as small as the above results to be calculated with reasonable accuracy. Thus, the corresponding extensions dealt with in Section 3.3 shall be considered as tentative, performed just in order to predict as many of the measurements made on the present tubular T-joint as possible. If this joint was a part of a real structure, the

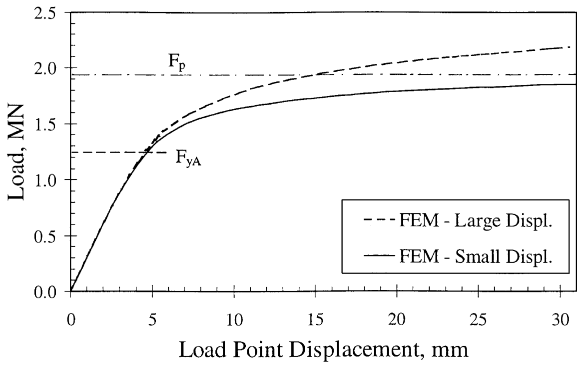

Fig. 7. Comparison of calculated yield load and ultimate load with results of finite element calculations.

task would be to identify zones that are susceptible to spontaneous fracture. The corresponding conclusion just would be that the material is tough enough to prevent cleavage fracture and that location A is not defect sensitive, so a more detailed fracture analysis would not be necessary.

To compare the predictions of the present analysis and to explore its accuracy a finite element analysis of the considered joint was additionally performed. The results are shown in Fig. 7. The load at which the first yielding occurs is in good agreement with F_y. The plastic limit load corresponds well to the maximum load in the case where large displacements are not accounted for. If the latter are considered, which means that ovalisation of the cross-section of the chord is modelled, the maximum load is somewhat increased and corresponds better to the experimental behaviour [12]. Thus the result of the simple model is—as expected—conservative. Note that the predictions made in the present paper were done just on the basis of [1], without knowing the further experimental results now documented in [12]. Comparison between the predictions in the present paper and the measurements given in [12] shows a relatively good agreement.

In conclusion it has been demonstrated that the cost-effective engineering failure assessment technique presented in this paper, which does not require any expensive stress calculations, is a powerful tool for an initial analysis of the failure behaviour of structures.

5. Uncited references

[9,10].

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