ABSTRACT

In the frame of a vast programme covering the stress-strain behaviour at various deformation rates of smooth and flawed specimens in AISI 316 type stainless steel, three FM tests with wide plates have been tackled. The test rigs consisted in a 3 MN rigid loading machine and a 5 MN “soft” loading system (LDTF) having a compliance of $1.47 \times 10^{-4}$ mm/N. The specimen configuration is the single-edge cracked plate in tension. The relative crack size was 0.5 (2 tests) or 0.3. The estimation of the material crack-growth-resistance curve $J_{\Delta a}$ was obtained from the analysis of a monotonic displacement controlled test performed with the 3 MN machine on one 0.5 plate. A stability assessment study was carried out testing the 0.3 plate in the LDTF. This analysis used the EPRI Engineering Approach. The calculated crack extension predictions at unstability were conservative, i.e. 35 percent short of the one ascertained by surface inspection. The second 0.5 plate was pulled to rupture in the LDTF. This experiment aimed specifically at the monitoring of crack growth velocity using a high speed camera.

INTRODUCTION

A comprehensive study of the dynamic properties of structural materials over a wide range of strain rates is essential for the analysis of the behaviour of structures under transient loading. To carry out work in this field on relatively large size test specimens a Large Dynamic Test Facility (LDTF) has been set up at the JRC. The LDTF was specifically designed and built to perform fully dynamic tests. In this work the facility has been used as a compliant loading machine using one of the two existing 100 m long cables having a compliance of $1.47 \times 10^{-4}$ mm/N and connecting a hydraulically operated piston to one of the specimen cleaves. This load train could be exploited for the study of the behaviour of flawed plates sustaining extensive plastic deformation and significant crack growth $\Delta a$ prior to unstable crack extension. The results pertaining to three single-edge cracked plates in tension (SECP) are presented. One plate had a crack ratio (c.r.) of 0.3 and the two other plates had a c.r. of 0.5. The first step taken in this study was the $J$-crack extension $\Delta a$ characterization of the AISI 316 type of stainless steel the plates were manufactured from.

One deep notched (c.r. of 0.5) plate was tested to this end in the 3 MN rigid machine. The remaining 0.5 and 0.3 c.r. specimens were pulled to rupture in the LDTF. The scope of these tests was to conduct two analyses enabling to predict the load-load point displacement and associated $\Delta a$ values for which unstable crack extension occurred. In the case of the 0.5 specimen a route is followed that is based on the descending part of the load-displacement curve.
of an analogous monotonically loaded specimen. In the 0.3 case a generalized FM formulation equating the applied tearing modulus to the material tearing modulus yielded the prediction of the unstability point. In addition, and in the 0.5 (unstable) test a high speed camera was used to observe crack growth rates and to obtain information on the mechanisms of surface separation, i.e. when slanted crack planes are created beyond the point of stable flat ductile crack extension. As far as the numerical analysis is concerned, use was made of the fully plastic solutions (Kumar et al., 1981). The method is referred to as the Engineering Approach (EA).

**BASIS OF THE INSTABILITY THEORY**

The theory is thoroughly documented (Hutchinson et al., 1979). A brief overview follows. In Figure 1 a generalized non-linear component loaded in a compliant system is represented. The significant parameters are the applied load (normalized by unit thickness B of the specimen) P, the compliance of the loading system C_f, and the displacements Δ = Δ_s + Δ_s where Δ is the displacement of the loading points (LPD). The compliance of the system associated to the unit thickness of the specimen is C*M = BXC_s. The unstability condition (i.c.) is set as Δ/Δ_a ≤ 0 which leads to the i.c.:  

\[ \frac{dP}{dΔ} ≤ -\frac{1}{C_M} \]  

(1)

In this study B = 30 mm and C*M = 4.41 mm²/KN. It follows from (1) that the i.c. is met on the descending part of the P versus LPD curve where the specimen has a "rebound compliance" C_r = -dΔ/ΔP. Thus (1) is equivalent to:

\[ C_r ≤ C_M \]  

(2)

This concept leads to a simple estimate of the unstability point of a structure having a known P-LPD-Δa history and for a given C*M. Generalization of the i.c. to different configurations entails the use of the J-Δa curve. A generalized expression for the i.c. can then be written (Hutchinson et al., 1979):

\[ (\frac{dJ}{dΔ})_ΔT ≥ (\frac{dJ}{dΔ})_{material} \]  

(3)

where the left-hand term refers to the applied J = J(P,a) and the right-hand term to the material resistance curve J(Δa). In addition, equilibrium between J(P,a) and J(Δa) during crack extension is required. Further, a general expression for (\frac{dJ}{dΔ})_ΔT can be developed by assuming that J and Δ are functions of P and a only. This yields:

\[ (\frac{dJ}{dΔ})_ΔT = (\frac{dJ}{dP})_P - (\frac{dJ}{dP})^2 [C_M + (\frac{dJ}{dP})a]^{-1} \]  

(4)

Equations (4) and (3) have been used in the stability assessment analysis of the shallow notched (0.3) plate tested in the LDHF.

**EXPERIMENTAL PROCEDURES**

The geometry of the deep-notched specimens (c.r. of 0.5), LDHF52 (monotonic test) and LDHF58 (instability study) appear in Figure 2. The LDHF53 (stability assessment study) is similar but for the relative crack size of 0.3. Motrè grids have been used, 110 mm wide and centered with respect to the crack line. The grids have two lines/mm running parallel to the crack line. Strain gages were used to measure and displacements. A V-shaped device instrumented with strain gages measured the displacements between U and V, see Figure 2. The loading points displacements were derived from the UV value considering also the rotation of the specimen halves (from clamps 1-2;3-4 data). A LVDT measured the distance fg (see figure 3) between a fixed bar set parallel to the specimen axis and its back face at the crack line position, and point 0 on
the crack line could be thus located. The data acquisition and reduction is taken care of by a Hewlett-Packard 1000 computer. In the case where high acquisition rates are required, two instruments, one VUKO and one DIPA were used. To save the last memorized data before the specimen rupture, a simple trigger was built up from a brass strip attached to the back face and that was severed when the specimen broke (see figure 4). The crack growths for LDTF52 and 53 were obtained from photograms taken from the crack line area. In the LDTF58 experiment a high speed camera taking 33 photograms/s was used to observe the Møller pattern and the crack extension. The change in slope from negative to positive of the strain versus time curves of the strain gages close to the specimen back face lead to the estimations of the zone of stress reversal (notional rotation joint) that could be compared with the numerical analysis of the LDTF52 experiment.

ANALYSIS

Since the numerical analysis uses extensively the EA, the hypothesis that the material obeys the Ramberg-Osgood (R-O) law stands. Thus the material true stress, σ and associated true strain ε derived from a tensile test are related as follows:

\[ \varepsilon / \varepsilon_0 = \sigma / \sigma_0 + \alpha (\sigma / \sigma_0)^n \]  

(5)

where \( \sigma_0 \) and \( \varepsilon_0 \) are a reference stress and a reference strain, \( \varepsilon_0 = \sigma_0 / E \) where E is the modulus of elasticity; \( \alpha \) and \( n \) are material constants. By best fitting a pure power law to the plastic part of (5): \( \varepsilon_p / \varepsilon_0 = \alpha (\sigma / \sigma_0)^n \) and covering an increasing amount of strain \( \varepsilon \) from 2 to 34 percent, a relationship between \( \alpha \) and \( n \) is obtained and entered in the analysis. In this work \( \alpha = (0.221/n)^{0.85} \), \( \sigma_0 \) was set at the material yield strength value of 220 MPa and \( \varepsilon_0 = 0.00114 \) is the associated yield strain. The analysis is based on plane strain and with the modulus E = 213.7 KPa. The calculation of the J-\( \Delta a \) curve follows a theory by Ernst (Ernst, 1983). The data needed in this method appear in Figure 3. They are \( \delta d \), \( \delta f \) (calculated; \( \varepsilon \) is the point of stress reversal), \( f_g \), current ligament b and P. The \( f_g \) value was compared with the data of the strain gages near the back face at 4, 14 and 24 mm from it. The trends obtained at different time intervals and for loads also beyond the maximum load gave results within the expected 10 mm ranges. In addition it is demonstrated that if a pure power law is assumed for the material and is entered in the fully plastic solutions (as in Kumar et al.) then working factors \( n(\delta d / \delta f, n) \) can be used in the formulation of J. The J-\( \Delta a \) equation is then:

\[ J = K_I^2 / E + \int_0^{\Delta a} \eta \delta P / b \delta \Delta P \]  

(6)

where \( K_I \) (a/w,P) is the LEFM stress intensity factor from a LEFM handbook, and \( \Delta P \) the plastic part of the LPD. The resulting curve appears in Fig. 5. A value of \( \delta J / \Delta a \) of ca.220 MPa is found in the zone where \( \Delta a \) varies from 7 to 15 mm. Concerning the LDTF53 test a curve n as a function of \( \Delta P \) was derived based on the equation

\[ 1/n = [(U^* + U)/U] - 1 \]  

(7)

that follows from the EA hypothesis and where \( U^* \) and U are the complementary energy and the strain energy of the cracked body, respectively. The maximum \( \Delta P \) entered is inferior to the \( \Delta P \) at crack initiation. A value of \( n = 3.5 \) is asymptotically attained (see Figure 6). The derivation of \( \delta J / \delta a \) equation (4), was carried out with \( n = 3.5 \). To circumvent the inconvenience deriving from the use of tabulated values, Lagrange interpolation polynomials through five consecutive EPR1 data associated either to \( h_1 \), \( h_2 \) or \( h_3 \) fully plastic solutions were calculated. The stability assessment diagram appears in
Figure 7. The $\Delta a$ at instability is ca.9.5 mm or 65.5 percent from the measured flat fracture surface (i.e. 14.5 mm). In the LDTF58 experiment, data reduction carried out on a series of photograms taken just before the specimen broke and at 30.6 ms intervals, yielded the $\Delta a$/LPD curve shown in Figure 8. The corresponding LDTF52 data is also shown. It is found that, at instability, the $\Delta a$ of LDTF58 is 30 mm and for LDTF52 it is 31.5 mm. The slopes in the corresponding P-LPD joints (see Figure 9) are consistent with the $C_S$ of the LDTF cable. Since, in addition, the $\Delta a$ measured on the flat ductile part of LDTF58 is 32 mm, these experimental results confirm wholly the validity of equation (2). Similar results have been found by researchers in this field (Yagawa et al., 1982). The $da/d(t)$ of LDTF58 (Figure 8) diminishes abruptly beyond $\Delta a = 30$ mm conveying the idea that the axial elongation derives mostly from the formation of the slanted planes in this time interval (ca.30.6 ms). The rupture of the remaining ligament of ca.54.0 mm occurred subsequently in less than 30.6 ms yielding a lower bound crack velocity of 1.7 m/s. In Figure 10 three crack surfaces are shown. Indication of COD at incipient crack growth could be derived from the Moiré patterns. A relatively high value of ca.2.5 mm was found. A Moiré pattern appears in Figure 11. It corresponds to the last pattern observed before rupture of LDTF53.

DISCUSSION

The change in relative crack size from 0.5 to 0.3 leads to a considerable increase of the $\sigma_T/\sigma_B$ ratio, from 0.333 to 0.879 in the case of the SECP in tension. $\sigma_T$ and $\sigma_B$ are the notional stresses at the crack tip, in the ligament, that are obtained from an elastic analysis. This change in loading mode is significant and considered of interest when addressing the stability assessment analysis of LDTF53 based on the $J-\Delta a$ characterization derived from the LDTF52 test. As far as the experimental analysis is concerned the crux of the EA is the $\alpha$ with $\gamma$ coupling and setting an appropriate value of $n$ in the analysis. The suggestions given in this study have been found quite adequate when addressing these points.

CONCLUSIONS

The "floating" line of force in the SECP in tension has led to experimental and analytical complexities. The transition from stable to unstable crack extensions was calculated from two theories. The one based on the load-load point displacement yielded excellent predictions.

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REFERENCES


Fig. 1: Generalized non-linear component loaded in a compliant system.

Fig. 2: SECP specimen dimensions and instrumentation.

Fig. 3: Identification of significant parameters.

Fig. 4: LDTF58 experiment. Clips 1-2;3-4, V-shaped device, LVDT and brass strip trigger at the back face shown.

Fig. 5: Crack-growth-resistance curve from LDTF52 experiment.

Fig. 6: Variation of $n$ versus plastic part of the load-point displacement.

Fig. 7: Calculated stability assessment diagram for LDTF53.
Fig. 8: LDTF52 and 58 crack growth values. $\Delta a$ at instability deduced from the diagrams of Fig. 9.

Fig. 9: LDTF52 and 58 load-displacement diagrams. Loading cable behaviour and alleged instability points are indicated.

Fig. 10: Crack surfaces of the specimens. LVDT53, 52 and 58 on the left, the middle and the right, respectively.

Fig. 11: LDTF53. Last photograph of Moiré pattern 2.25 s before collapse.