RESISTANCE TO DUCTILE TEARING OF A STRUCTURAL STEEL IN THREE AND FOUR POINT BENDING

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ABSTRACT

The resistance to ductile tearing of a carbon manganese steel has been examined in plane sided and side grooved test pieces loaded in three point and four point bending. Side grooving reduced the slopes of the R-curves. The loading configuration was found to have little effect on the J R-curves, but some effect was observed on the COD R-curves, where these were calculated using an assumed constant value of rotational factor, r. This factor, however, was found to vary with crack extension, and when COD was recalculated using measured values of r, this effect of loading on configuration was largely removed. Significantly, three point bend gave lower estimates than four point bend of toughness at maximum load.

KEYWORDS

R-curves, J-integral, COD, three point bend, four point bend, rotational factor, side grooves.

INTRODUCTION

The problem of designing a structure against ductile fracture is receiving considerable attention at present. The uncertainties are usually aggravated by the onset of slow tearing at loads well below those at which the cracked structure fails. Recently, methods have been proposed to predict instability after stable tearing [Paris and colleagues, 1979; Milne, 1979], which use the slope of the R-curve, (usually measured in terms of the J integral). Alternatively, Towers and Garwood [1980], suggest that the value of toughness (in terms of COD or J) measured at the maximum load in the test piece should be employed when assessing the significance of defects. The latter approach requires that the conditions of the test should be such as to give conservative estimates of toughness. Hence three point bend test pieces of the same thickness as the cracked member are recommended.

The dependence on configuration of R-curves (and hence maximum load toughness) is not entirely clear. Where plane sided test pieces in bending and in tension are employed, initiation toughness is similar but the slope is found to be lower in the former configuration [Begley, Logsdon and Landes, 1977; Garwood, 1979]. This effect is undoubtedly aided by the greater shear lip development in tension, which is encouraged by the lower plastic constraint. One possible solution to this problem (and also to the effect of thickness) is to side groove test pieces so as to eliminate shear lips. This, however, causes a drastic lowering of the R-curve in very ductile materials and it has been suggested that the result may be more conservative than that obtained by testing an infinite thickness [Garwood, 1979]. Hence, the most practical method at present is to use full thickness, plane sided specimens which are tested in a configuration which gives conservative toughnesses when compared to the structural situation. Towers and Garwood [1980] suggest that this configuration should be three point bending.

However, earlier work by Smith [1973] and by Green [1975] on three and four point bending suggested that the latter configuration might provide a more severe test of a material's resistance to tearing. In terms of COD, the R-curves for plane sided test pieces were found to be less steep in four point bending, although the initiation CODs coincided. The experiments described herein were designed to examine this effect. R-curves, in terms of both COD and J, were measured in three and four point bending, on both plane sided and side grooved test pieces. In addition, the rotatational factor (used in calculating COD) was derived from surface measurements on the test pieces.

EXPERIMENTAL DETAILS

Material and Test Piece Details

The material used was En 8 in the form of rolled bar. The chemical composition is given in Table 1. It was heat treated by austenitising at 870°C for 30minutes, and

TABLE 1 Chemical Composition of Steel (En 8) in weight %

Al	C	Cr	Cu	Mn	Mo	Ni	P	Si	S
0.035	0.28	0.09	0.11	0.86	0.02	0.09	0.12	0.27	0.027

then furnace cooled. This was designed to produce a yield stress which was sufficiently high to ensure that well defined shear lips were developed. The tensile properties of the heat treated steel are given in Table 2.

TABLE 2 Tensile Properties

Lower yield stress	σ_Y	=	390N/mm^2
Ultimate tensile strength	σ_u	=	637N/mm^2
True stress at failure	σ_f	=	1150N/mm^2
Reduction in area		=	55%
Work hardening exponent	n	≃	0.18

Bend test pieces of dimensions 12.5mm x 25mm x 125mm were extracted in the longitudinal orientation. Half of these contained side grooves of depth 2.5mm (see Fig. 1)

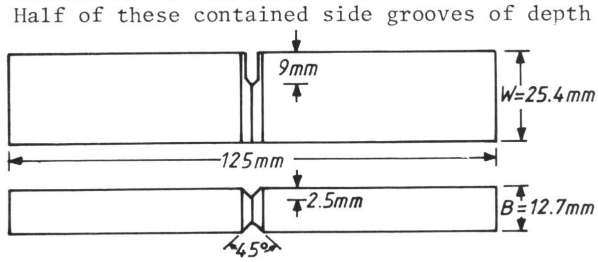

Fig. 1. Geometry of testpiece employed (side-grooved testpiece only is shown).

All test pieces were notched and fatigue cracked to give an initial crack length to width ratio (a/W) of approximately 0.5.

Experimental Procedure

Bend tests were performed at room temperature. Test pieces were loaded with a total span of 100mm (=4W) & 25mm diameter rollers. In four point bending the upper rollers were placed at the quarter points with a separation of 50mm. The opening at the mouth of the crack was monitored by means of a clip gauge located between knife edges screwed to the test piece. An LVDT transducer was mounted between the upper and lower loading jigs in order to monitor load point displacements.

R-curves were obtained by means of the multiple test piece technique. Several test pieces were loaded to different displacements, whilst recording load point displacement and clip gauge displacement against load. They were then unloaded and heat tinted at 250°C in order to mark the region of slow crack growth. It was found that if test pieces were broken open by low temperature cleavage at this point, they frequently did not cleave from the tip of the crack, rendering the subsequent determination of crack length inaccurate. Consequently, the crack was extended further by reloading at room temperature before finally breaking the test piece open by cleavage at -196°C. Crack lengths were determined with a travelling microscope at nine points along the crack fronts. The crack length at the end of the heat tinted region and the value at the start of microvoid coalescence, including the stretch zone line was measured. The crack extension was calculated from the difference, thus excluding the stretch zone. This method was chosen because the alternative approach of including the stretch zone in the crack extension and then constructing a blunting line to intersect the R-curve is complicated by the fact that the true slope of the blunting line varies with material properties (since $J = M\sigma_Y\delta_i = M\sigma_Y \cdot 2\Delta a_i$, where Δa_i is the stretch zone width, and M is a material dependent factor lying between approximately 1 and 2.5).

The J-integral was estimated from the formula of Rice, Paris and Merkle [1973].

$$J_o = \frac{2U}{B(W-a_o)} \qquad [1]$$

where U is the area under the load displacement plot, corrected for extraneous displacements [Robinson and Tetelman, 1976], B is the thickness of the test piece (for side grooved test pieces the net thickness was used), W is the width and a_o the original crack length. The subscript o denotes that the original crack length is used, and that J is not corrected for the effects of crack extension [Garwood, Robinson and Turner, 1975].

The COD, δ_o, at the position of the original crack tip, was calculated using the formula of Ingham and colleagues [1971].

$$\delta_o = V_g\left[\frac{rb + \Delta a}{a_o + z + rb + \Delta a}\right] \qquad [2]$$

where V_g is the clip gauge displacement, r is the rotational factor, $b(=W-a_o-\Delta a)$ is the net ligament width and z is the height of the knife edges. It is generally assumed that r has a constant value during tearing. In order to check this r was estimated by attaching a transfer of a grid pattern to the specimen surface (see Fig. 2) and by photographing the grid before and after deformation (in the latter case with the specimen held "on load"). Thus the apparent centre of rotation of the two test piece halves could be determined. Values of r were calculated from the expression

$$r = \frac{x - a_o + \Delta a}{b} \qquad [3]$$

where x is the distance between the centre of rotation and the top surface of the test piece.

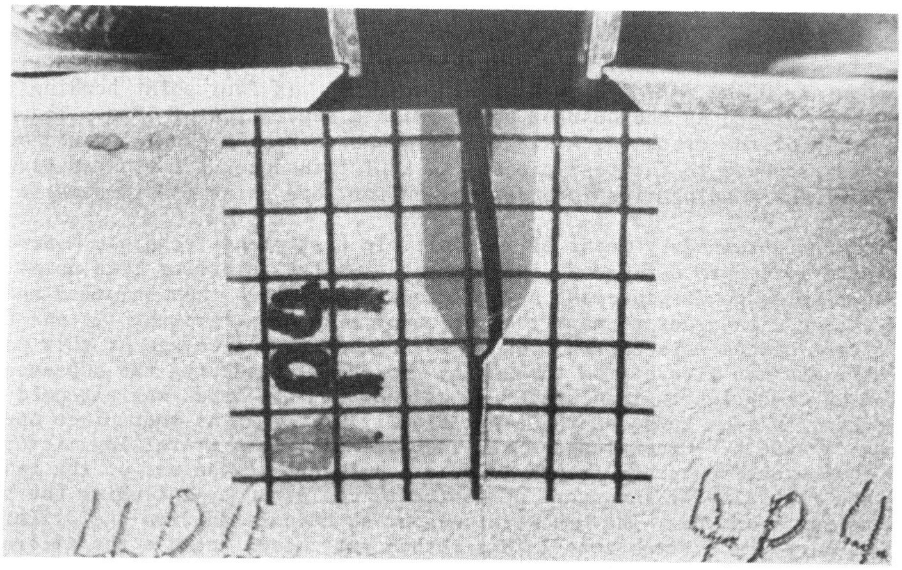

Fig.2. Photograph of grid on side of testpiece, x 5.5.

The influence of shear lip development on the resistance to crack extension was investigated by measuring the width of central flat fracture on the fracture surfaces of broken test pieces. It should be noted that this approach ignores the contraction in thickness which occurs in the plane stress region at the edges of the test piece. Finally, the angle subtended by the flanks of the growing crack was measured on several test pieces, which were sectioned after the initial bending operations. These measurements were made "off load" of necessity.

RESULTS

Figure 3 shows the variation of J_o with Δa. The data fall into two overlapping sets, with the side grooved test pieces forming the lower group. Since measurements of crack extension exclude the stretch zone, a blunting line is not required and the initiation toughness J_i, at $\Delta a = 0$, is the same within the scatter of the data, for the two groups at approximately 70N/mm. Estimates of J_i and dJ/da are given in Table 3a.

Figure 4 shows the variation of δ_o with Δa, assuming that r = 0.4, a commonly accepted value [BS.5762, 1979]. The same pattern emerges, with the two side groove results forming a lower bound to the data. There is no appreciable difference between three and four point bending for the side grooved test pieces, but for the plane sided ones the COD in three point bending lies consistently above that obtained in four point bending. A statistical analysis was performed on the data, assuming that the δ_o R-curves are linear. For side grooved test pieces this confirmed that the loading configuration caused no significance difference, whereas for plane sided test pieces the initiation point appeared to be higher in three point bend (see Table 3b). Alternatively, if it is assumed that the initiation points were the same, in agreement with Smith [1973], three point bending appeared to produce a steeper R-curve. These differences, which were only apparent in the

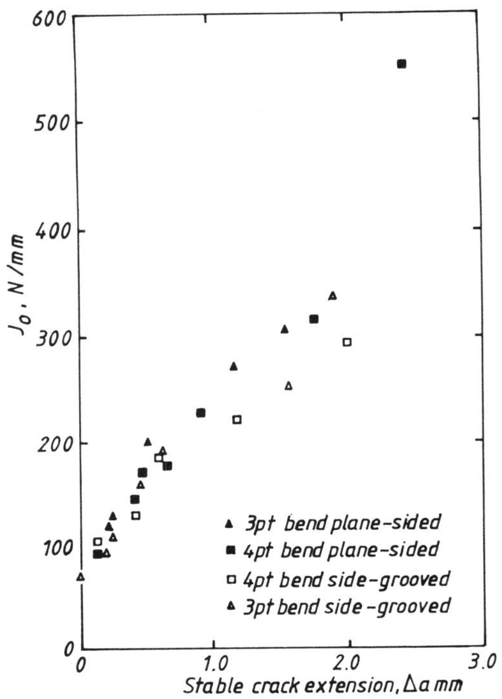

Fig.3. Variation of J_o with crack extension.

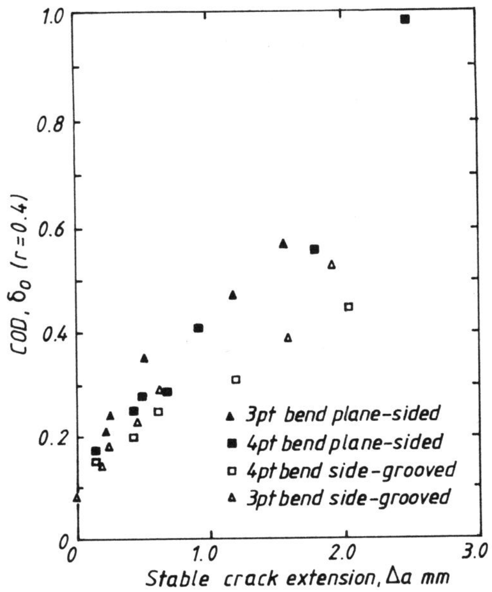

Fig.4. Variation with crack extension of COD calculated by assuming r = 0.4.

TABLE 3 Intercepts and Slopes of R-curves

(a) J_o results (manual estimates)

	Plane sided	Side grooved
J_o at initiation, J_i N/mm	70	65
Initial slope, dJ_o/da N/mm^2	220	195

(b) COD results, calculated assuming r = 0.4

	Three point bend, plane sided	Four point bend, plane sided	Side grooved
COD at initiation, δ_i mm	0.21	0.10	0.14
Slope $d\delta_o/da$	0.20	0.33	0.18

(c) "True" COD results, calculated using measured r

	Plane sided	Side grooved
COD at initiation, δ_i mm	0.09	0.12
Slope $d\delta_o/da$	0.28	0.15

plane sided test pieces, could not be attributed to the difference in shear lip development, which as seen in Fig. 5 was indistinguishable in the two configurations. The variation of rotational factor with Δa is shown in Fig. 6. Despite the

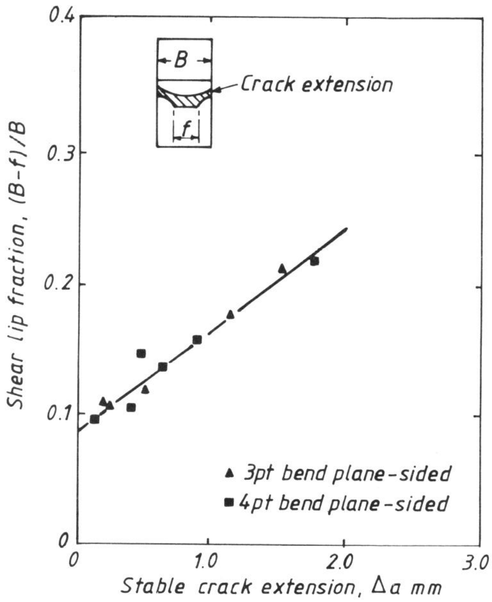

Fig.5. Development of shear lips with crack extension.

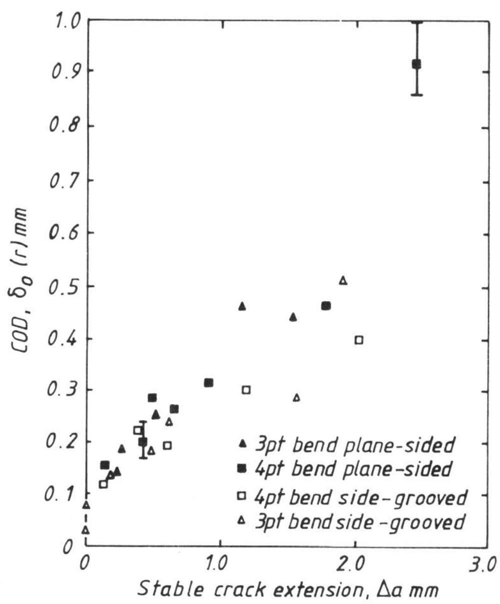

Fig.7. Variation with crack extension of 'true' COD, calculated using the measured values of rotational factor r.

large errors indicated, which were caused by the relative coarseness of the grid lines, the rotational factor lies significantly below the assumed value of 0.4, at an average of about 0.3. Using the measured r, the "true" COD for each test piece was recalculated, and the resulting δ_0 R-curve is shown in Fig. 7. Compared to the

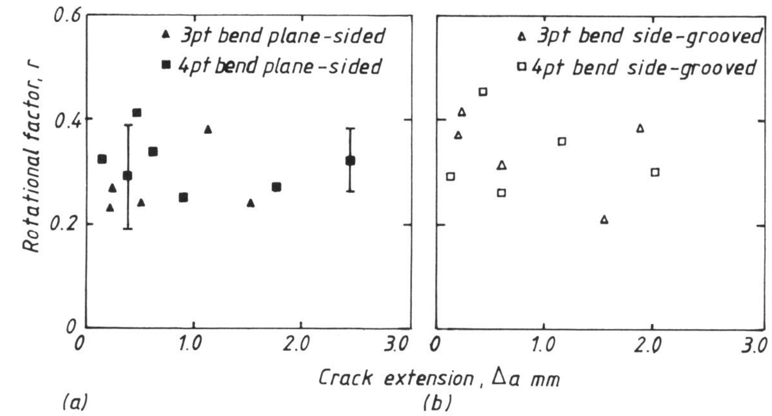

Fig.6. Variation of measured rotational factor with crack extension in: (a) Plane-sided and (b) side-grooved testpieces.

apparent COD results shown in Fig. 3, a difference between the results in three and four point bending becomes less marked, and the results lie within the same error band (the major cause of the errors indicated was the error in r). The intercepts and slopes were calculated by linear regression and are given in Table 3(c).

Values of COD and J at maximum load are given in Table 4. The CODs were calculated using the measured rotation factor at the final tear length. Unfortunately only one specimen in each category was taken past maximum load, so the results are subject to some uncertainty. Nevertheless, the maximum load toughness appears substantially lower in three point bend than in four point bend, for both plane sided and side grooved test pieces.

TABLE 4 Values of J_o and δ_o at maximum load (one result in each category)

	J_{max} N/mm	δ_{max} mm
Three point bend, plane sided	247	0.35
Four point bend, plane sided	408	0.64
Three point bend, side grooved	148	0.17
Four point bend, side grooved	200	0.25

Microscopic examination of the sectioned test pieces gave an estimate of the flank angle of the growing crack, the results of which are given in Table 5. The average value is 11.2° with no significance difference between three and four point bend.

TABLE 5 Values of Flank Angle measured "off-load" on sectioned specimens

	Crack extension, Δa mm	Flank Angle
Three point bend,	0.5	9.6°
	0.5	10.0°
	1.0	14.4°
	Mean	11.3°
Four point bend,	1.4	11.9°
	1.5	9.6°
	1.7	11.8°
		11.1°

DISCUSSION

For the plane sided test pieces, there appeared to be a small effect of testing configuration on the COD R-curve when a constant value of rotational factor was assumed. When the measured value of r was used in the calculation of COD, the differences disappeared. This result was also confirmed by observations of the flank angle [Green and Knott, 1975(a)], which at approximately 11.2° ($\approx$0.2 radians), agrees with the measured slopes $d\delta/da$ (Table 3). A rather greater effect configuration had been reported by Smith [1973]. However, the test pieces in this work were extracted in the transverse orientation, where propagation toughness is considerably lower [Willoughby, 1979], and no correction was made for changes in r.

Despite the substantial errors introduced in the measurement of r, the general trend is for r to decrease as tearing proceeds, in agreement with the observations of Willoughby [1979]. The average value at about 0.3 is somewhat below the values of 0.4 or 0.45 usually assumed. This may introduce a slight nonconservatism into the use of COD values calculated according to BS.5762 [1979] when there is substantial tearing preceding maximum load. However, the discrepancy is likely to be small compared to the scatter in material data. The slightly different values of r found in the three and four point bending may result from the presence of the back roller in the former case. This will introduce extra compressive stresses into the plastic zone ahead of the crack tip. Support for this hypothesis is provided by Ewing [1968], who found that the roller diameter had an effect on plastic constraint factor in three point bending.

There is possibly a small effect of configuration on the J_o R-curves for plane sided test pieces, which is evident during the first millimetre of tearing (Fig. 2). Linear regression on this region gave the slope dJ/da as 282 and 183N/mm^2 in three and four point bending respectively, a ratio of 1.54. Such a difference is predicted by the relationship postulated between J and COD during tearing [Willoughby and Pratt, 1978; Curry, 1979; Willoughby, Pratt, and Turner, 1980].

$$\frac{dJ}{da} = \frac{L}{2r} \sigma_{flow} \frac{d\delta}{da} \qquad [4]$$

where L is the plastic constraint factor and σ_{flow} is an average flow stress. It is assumed that $d\delta/da$ is independent of configuration, and L is 1.22 and 1.26 in three and four point bend respectively [Ewing, 1968]. Taking the average values of r in three and four point bending as 0.27 and 0.31 respectively, gives the ratio of the two terms L/2r as 1.4, in reasonable agreement with the ratio of 1.54 in dJ/da. However, this hypothesis is tentative, since the scatter in the data prevents precise comparisons.

The effect of side grooving the test pieces was to reduce the slope of the R-curves while having little influence on initiation. This is in agreement with previous work [Garwood and Turner, 1977] and may be explained in terms of the role of side grooves in suppressing shear lip formation [Green and Knott, 1975(b)]. It is interesting to note that the side grooved test pieces produced similar COD and J R-curves, irrespective of testing configuration. This may be due in part to the lateral constraint imposed by the side grooves overriding any effect of the back roller on plastic stress distribution, and in part to an effect on the rotational factor (average values of r in three and four point bending were almost identical at 0.33 and 0.34 respectively). Ewing [1980] has suggested that the plastic constraint factor may be increased by the presence of deep side grooves, from about 1.3 to 2.1 in extreme cases. Certainly, comparing the slopes dJ/da and $d\delta/da$ (Table 3) by means of Equation [4] would suggest that L is higher in the case of side grooved test pieces.

Considering the values of COD and J at maximum load, it was found that three point loading produced the more conservative results in both plane sided and side grooved test pieces. This same result was found by Green [1975] for plane sided test pieces. (Smith, [1973] implies the opposite, but his CODs were calculated using the final tear length. For a flatter R-curve, as was found in four point loading, this would give lower values of COD, other factors being equal). One possible explanation is that the shear stresses in three point bending may lower the limit load. Another is that the elastic plastic driving force curves may be steeper in three point bend, thus giving instability at lower loads (assuming that the R-curves are the same). However, without detailed analyses these explanations can only be considered as speculative. The important point is that if maximum load toughness is to be used for defect assessment in the ductile regime, three point bending would appear to give more conservative values than four point bending.

CONCLUSIONS

1. Values of J and COD at initiation of tearing were found to be similar in three and four point bending, for both plane sided and side grooved test pieces.

2. Side grooving was found to reduce both slope of the R-curves and the maximum load values of toughness.

3. Loading configuration appeared to have little effect on the J R-curves.

4. COD R-curves were also independent of loading configuration, for side grooved test pieces. However, for plane sided test pieces, CODs calculated using a constant value of rotational factor were steeper in three point loading.

5. The rotational factors were found to decrease slightly with tearing. When the "true" COD was recalculated using the measured rotational factor, the variation in COD R-curves for plane sided test pieces was removed. This result was supported by measurements of the flank angle of tearing in the two loading configurations.

6. Values of J and "true" COD at maximum load were found to be substantially lower in three point bending than in four point bending.

ACKNOWLEDGEMENTS

The authors wish to thank Dr J L Phillips for his assistance with statistical tests, and the Central Electricity Generating Board for permission to publish this paper.

REFERENCES

BS 5762:1979 "Methods for Crack Opening Displacement (COD) Testing", The British Standards Institution, 1979.
Begley, J.A., W.A. Logsdon and J.D. Landes, [1977], ASTM-STP-631, 112-120.
Curry, D.A. [1979], Int.J.of Fracture, 15, R59-R62.
Ewing, D.J.F. [1968], J.Mech.Phys.Solids, 16, 205.
Ewing, D.J.F. [1980], Private communication.
Garwood, S.J. [1979], ASTM-STP-677, 511-532.
Garwood, S.J., J.N. Robinson and C.E. Turner, [1975], Int.J.of Fracture, 11, 528-530.
Garwood, S.J., and C.E. Turner, [1977], Fracture 1977, University of Waterloo, 3, 279-284.
Green, G. [1975], PhD Thesis, University of Cambridge.
Green, G. and J.F. Knott, [1975a], J.Mech.Phys.Solids, 23(3), 1975. 167-83.
Green, G. and J.F. Knott, [1975b], Metals Technology, 2, 422-427.
Ingham, T., G.R. Egan, D. Elliott and T.C. Harrison, [1971], Paper C54/71, I.Mech.E. Conf. on "Practical Application of Fracture Mechanics to Pressure Vessel Technology", London, May 1971.
Milne, I. [1979], C.E.R.L. Report RD/L/N179/78.
Paris, P.C., H. Tada, A. Zahoor and H. Ernst, [1979], ASTM-STP-668, 5-36.
Rice, J.R., P.C. Paris and J.G. Merkle [1973], ASTM-STP-536, 1973, 231-45.
Robinson, J.R. and A.S. Tetelman, [1976], Eng.Fract.Mechanics, 8, 301.
Smith, R.F., [1973], The Practical Implications of Fracture Mechanisms, Inst. of Metallurgists Spring Meeting, Paper 4.
Towers, O.L. and S.J. Garwood, The Geometry Dependence and Significance of Maximum Load Toughness Values, (see paper this conference).
Willoughby, A.A. [1979], PhD Thesis, London University.
Willoughby, A.A. and P.L. Pratt, [1979], Proc. of 2nd European Conference on Fracture (ECF2), Darmstadt.
Willoughby, A.A., P.L. Pratt and C.E. Turner, [1980], to be published in Int.J. of Fracture, 1980.