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Measurement of tearing toughness on BS4360:50D structural steel

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Toughness was measured and crack growth resistance curves determined on BS4360:50D steel, over a range of orientations and temperatures from + 20 to − 50° C in terms of both J-integral and crack opening displacement (COD) values, for a number of configurations in static bending. All fractures occurred in a ductile micro-mode. The results are discussed here only in terms of J, but several different methods of estimation are used. These fall into two main groups: for measurement of work done, the results are some 10% higher than those determined from load and clip gauge. Values of J_{IC} found according to the standard ASTM method were some 30% higher than a value for no growth. Maximum load toughness was about twice the J_{IC} value. Over the range studied ($0.3 < a/W < 0.5$; $W/2 < B < W$; $20\,\text{mm} < B < 50\,\text{mm}$; $\Delta a/b < 0.1$ where a is crack length, W and B specimen width and thickness, and $b = W - a$) both J and COD values depended strongly on orientation, but J by either type of analysis was invariant with respect to temperature and sensitive to geometry only in respect of thickness in one case. Conditions of testing do not therefore seem at all critical in this regime, although the values found depend on the methods of analysis used.

1. Introduction

For thick-section materials the plane strain fracture toughness has come to be regarded as a property of the material in the same way that the yield stress is so regarded, i.e. varying with composition, heat treatment, temperature, strain rate and so on, but not significantly dependent on test-piece size or configuration. Indeed, the prime object of fracture mechanics is to uncouple the geometric effects of a sharp crack that do not scale with size from the component-size effects that do scale with size. It has long been accepted that in thin material conditions of plane strain are lost even at the crack tip, and fracture toughness is not then a unique material property. It can be represented as a crack growth resistance or R curve of toughness against crack growth [1, 2].

In recent years there has been a growing realization that even for thick-section material fracture toughness is not a single term, so that provided the material is ductile on the micro-scale an R curve representation is desirable [3–5]. Controversy still exists on whether such an R curve is sufficiently independent of geometry to be regarded as a material property, somewhat analagous to the extension of yield strength to a complete work-hardening curve, or whether the conditions of size and configuration under which a unique R curve can be obtained are so restrictive that the concept of a property of "tearing resistance" is not of practical use. The object of the present study is to contribute to a better understanding of several of the proposals that have been made for elastic–plastic test methods suitable for structural mild steels. The longer-term purpose is to encourage the establishment of a range of test methods in the UK to parallel the existing COD method [6]. It is particularly remarked that the best-known existing

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TABLE IA Composition of BS4360D:50D

Thickness (mm)	C	Mn	Si	P	S	Al	Nb	V
50	0.14	1.33	0.35	0.022	0.015	–	0.041	0.003
30	0.14	1.29	0.36	0.013	0.007	0.035	0.026	–

standard method for J testing [7] permits the use of rather small pieces with a ruling ligament of $b > 25J/\sigma_{fl}$, where σ_{fl} is the flow stress (Table I), whereas the COD method [6] does not.

The questions examined here are whether the onset of tearing, the so-called "initiation toughness", is itself independent of test method and geometry; whether the conditions specified in the standard tests for measurement of toughness [7, 8] are satisfactory; and whether there is a clear advantage in the use of work-based formulae [7, 8] rather than clip-gauge methods [9] for measuring toughness. The second question is whether R-curves are independent of test conditions, albeit within a rather restricted range of circumstances, because of the many variables of both test and analysis that enter into the question. Despite the obvious parallels between J and COD, only the J aspects of these questions are discussed here, since a number of points of detail arise which tend to obscure the broader picture when the results in terms of J and COD are compared.

2. Experimental programme

The material used was a medium-strength weldable structural steel, specified to BS4360:50D, obtained as 50 mm × 250 mm × 1540 mm plates of node quality. Its specified composition [10] is given in Table IA. Room-temperature mechanical properties were checked by testing two standard tension specimens in longitudinal and transverse directions of the test plates. The work-hardening exponent n was estimated at the same time. The values of yield stress σ_y, and tensile strength σ_u, at low temperatures were taken from the literature. All the properties that were used in this investigation are summarized in Table IB. The node-quality steel has very low sulphur content and good through-thickness properties not explicitly given in the specification [10].

Pre-cracked three-point bend specimens with a span $S \simeq 4W$ were used exclusively, where W is the width of the specimen. Specimens were divided into two batches. In the standard batch, the width of the test piece was constant with $W = 46$ mm. The thickness varied between $B = W/2 = 23$ mm and $B = W = 46$ mm. Fourteen specimen groups in this batch included three possible different specimen orientations: LT, TS, and LS.† They were notched and fatigue-cracked to give a crack length to specimen width ratio a/W, of about 0.5, using a mechanical fatigue machine with a fixed loading frequency of 33 Hz at $K_f < 0.63\sigma_y B$ and $R = 0.1$, where K_f is the stress intensity factor at pre-cracking and R is the ratio of minimum to maximum loads in the cycle (Q_{min}/Q_{max}). Some less deeply notched specimens with $a/W \simeq 0.3$ were also made and tested for comparison. This batch of specimens was tested over a wider temperature range (though still above the ductile–brittle transition) at $-50°$ C, $-23°$ C, and room temperature.

TABLE IB Properties of BS4360D:50D

Temperature (° C)	Thickness (mm)	Yield stress ($MN\,m^{-2}$)	Tensile strength ($MN\,m^{-2}$)	Flow stress ($MN\,m^{-2}$)
+ 20 ± 2	50	370	530	–
	30	415	530	450
− 23 ± 2	50	388	560	474
− 50 ± 10	50	404	586	495

Young's Modulus $E = 213 \times 10^3$ $MN\,m^{-2}$.
Poisson's ratio $\nu = 0.29$.
Work hardening exponent $n = 0.22$.
Charpy energy (30 mm) 146 J at − 40° C; (50 mm) 38 to 73 J at − 30° C.

†L = Longitudinal, T = transverse, S = short transverse (thickness) direction. The first letter denotes the direction of the major axis of the test piece and hence the direction of applied stress; the second denotes the direction of crack advance.

A second batch of specimens had a smaller size and were made in LT orientation only, since the LT specimen had the lowest toughness of the three orientations tested. Thicknesses $B = W/2$ and $B = W$ were used, but the width (24 mm) was only about half that of the first batch. Six groups of specimen with deep notches of $a/W \simeq 0.5$ were made and tested in a temperature range from room temperature down to $-50°$ C.

Tests were conducted on a screw-driven machine at a displacement rate of about 0.4 to 0.5 mm min^{-1}. The multi-specimen R curve technique was used, in which several test pieces were bent to different displacements and unloaded. A double cantilever clip gauge was mounted between two knife edges screwed on to the edge of the test piece to monitor the crack mount opening displacement, and an LVDT initially mounted on the neutral axis was used to measure the load-point displacement of the mid-span relative to the ends. Later tests showed this rather cumbersome arrangement was not necessary for ductile behaviour, a calibration correction for extraneous displacements being adequate. The traces of load Q against load-point displacement, q, and of Q against clip-gauge readings V_g, were recorded simultaneously on an $X-Y$ plotter during the loading and unloading process. After testing, specimens were treated at 500° C to heat-tint the newly extended slow crack growth area. They were then cooled by liquid nitrogen and broken open. The amounts of crack-length length extension were measured at nine evenly distributed points across the thickness and averaged, with weighting against the surface values.

Low-temperature tests were conducted with the test rig in a reverse arrangement, i.e. the specimen was positioned with the notch upwards for convenience of mounting the clip gauge. Specimens were cooled by immersion in a methanol–dry ice fluid bath; the temperature was monitored continuously. The same recordings were made as for the room-temperature tests, except for the smaller-sized specimens where the LVDT only was used because there was not enough space to house the clip gauge.

2.1. Experimental procedure

A first study was made on the effect of the estimation formula used for J or COD. Such formulae were originally devised for determination of a toughness value in the absence of slow growth. Some have been modified formally, and others

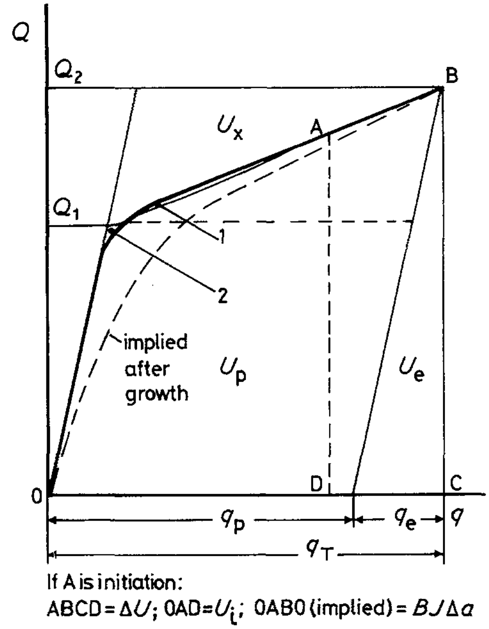

Figure 1 Curve of load against load-point displacement and the division of elastic and plastic energy components U_e and U_p. For $J\{5\}$ where Q_1 and Q_2 are used, area 1 = area 2.

in an *ad hoc* manner to allow for small amounts of slow stable growth. Suggestions directed to large amounts of growth [5, 11] are not discussed here, nor are the theories for predicting the final unstable crack growth. The requirement for J-controlled growth would restrict crack advance to $\Delta a/b < 0.06$ where $b = W - a$ [12] or, say, 0.10 at most, so that by implication uncertainty of that magnitude is accepted here for possible inconsistencies in the use of original crack length a_0 or final crack length $a_f = a_0 + \Delta a$. The original method of determining J [13, 14] from

$$J = -\frac{1}{B}\frac{dP}{da} \tag{1}$$

where P is potential energy at fixed displacement, though perhaps the most directly related to theory, is time-consuming and ill-adapted to use where growth is deliberately allowed. There is certainty that for elastic–plastic material with linear elastic unloading (epe material) as distinct from nonlinear elastic material (nle), the term P does not represent energy available for fracture. There is less certainty where the relevant term to describe energy absorbed should be actual work done U or internal energy absorbed w. These are the same up to initiation but therafter are related as in Fig. 1 (for nle material) by

$$dw = dU - BJ da \quad (2)$$

and "small amounts of growth" is here interpreted [15] to mean that

$$dw \simeq dU \quad \text{or} \quad dU \gg bJ da \quad (3)$$

If that is strictly adhered to, then differences over the use of a_0 or a_f are trivial. A directly equivalent statement in terms of COD is not apparent.

A single test-piece method for J was introduced [16] for deep-notch bending:

$$J = \eta U/Bb \quad (4)$$

where U is work done and η is a geometry dependent factor; $\eta = 2.0$ for deep-notch bending and $\eta \simeq 2.2$ for compact tension with $a/W \simeq 0.5$. B is thickness and b is the width of the ligament, $W - a$. This expression is exact for no crack growth. It is used with small amounts of growth by taking U as the total work done. If growth has indeed occurred, then the total work done is strictly $U_i + dU$ (where U_i is the work done to initiation) but the simplicity of the above method is that initiation need not be identified closely, since U_{total} is not split into its component parts. It was further argued [16] and derived in a different manner [17] that if, despite $\Delta a/b$ small, a correction is to be made, then (for deep-notch bending)

$$J_{corr} = J_{approx}\,[1 - (\Delta a/b)] \quad (5)$$

where J_{approx} is the direct value from Equation 4 found by using a value of U that is strictly (U_i + dU). Note that this correction reduces the value of J, whereas use in Equation 4 of a_f instead of a_0 in $b = (W - a)$ would increase the value of J.

3. Formulae for J

Six expressions for evaluating J were examined, with the restriction of $\Delta a/b \leqslant 0.1$. They will be denoted $J\{1\}, J\{2\}, J\{3\}$ etc. The first is

$$J\{1\} = \eta U/Bb$$

where $\eta = 2$ for deep-notch bending. Neglect of Equation 5 might imply up to 10% error (overestimating from Equation 4), the error increasing as the crack grows. When used with Equation 5, $J\{1\}$ is denoted $J\{1\}_{corr}$. The second expression is

$$J\{2\} = \phi U/Bb$$

where ϕ is a property of material as well as the geometry, written [18] as

$$\phi = 1 - \{(gb/2B)/[1 - (gb/B)]\}$$

where g is a material constant, suggested to be dependent on strength and hardening, such that when $n \simeq 0.23$, $g \simeq 0.22$. The third expression [19] is

$$J\{3\} = Qq_t/Bb_f + L\sigma_y b_f q_p/S$$

where q_t is total displacement, q_p is plastic displacement, L is the constraint factor relating the collapse moments of notched and un-notched pieces of the same ligament size; $L = 1.3$ [19]. S is the span.

All of these formulae are based directly on work done. The basis of $J\{3\}$ is that the term $2U$ required in Equation 4 can be written (Fig. 1) as $(U_p + 2U_e + U_x) + (U_p - U_x)$. The first of these terms is Qq_t. The second is given by $Q_L q_p$ where Q_L is the estimated plastic limit load $L\sigma_y Bb_f^2/S$ so that, neglecting certain small areas (1 and 2 in Fig. 1, which tend to zero for bi-linear behaviour) $U_p - U_x \simeq U$ (up to Q_L). The use of b_f rather than b_0 increases the first term but decreases the second. Overall reduction by $[1 - (\Delta a/b)]$ as in Equation 5 would seem more appropriate.

Another suggested method [9] will now be examined. This is not based directly on measurement of U but on clip-gauge opening, since it was proposed in order to be compatible with the COD method.‡ The relation is

$$J\{4\} = \frac{K^2}{E'} + \frac{2Q_L}{Bb}\left(\frac{WV_p}{a + Z + r_p b}\right)$$

where V_p is the plastic component of the clip gauge (mouth) opening; r_p is the rotational factor for the plastic component, usually taken as $r_p = 0.4$ for $a/W > 0.45$. Q_L is the limit load notionally measured on the diagram, or possibly as defined above (though L may be chosen to be perhaps rather more than the value of 1.3 [19]); Z is the height of the clip gauge fixture above the surface. Note that the elastic component is taken separately in terms of $K^2/E' = G(= J_{el})$ where E' is the effective modulus for plane strain, and K is evaluated from the actual load. The procedure is designed to be compatible with K_{IC} testing, and partly to overcome the variations of

‡At the time this method was proposed the COD method [6] had not been formulated, its predecessor DD19 (published by the British Standards Institution) being used instead. DD19 employed clip-gauge methods but did not derive the elastic component from K as is done in the COD method [6, 9].

r_p with degree of defomation in the near-lefm regime.

A small variation on this method was also examined:

$$J\{5\} = \frac{K^2}{E'} + \frac{(Q_1 + Q_2)WV_p}{Bb(a + Z + r_p b)}$$

This was proposed [20] because Q_L is not readily evaluated on load displacement records, whereas Q_1 and Q_2 are (see Fig. 1). To allow for slow crack growth the $J\{4\}$ equation was also modified [9] to give

$$J\{6\} = \frac{K_f^2}{E} + \frac{2Q_L(W - a_f)}{B(W - a_1)^2} \frac{W(V_{gt} - \alpha V_e)}{(a_f + r_p b_f + Z)}$$

where the suffix f denotes "evaluation after growth"; V_e is the elastic component and α is a function of a_f/a_0 [9]; $\alpha = 1.34$ for $a/W = 0.5$, $\Delta a/b \simeq 0.1$ as here.

4. Results and discussion

4.1. Comparison of different methods of analysis for J

A comparison of the first three methods shows that $J\{2\}$ values are some 5 or 10% less than $J\{1\}$ for $a/W = 0.5$, and $J\{3\}$ values are some 10% higher than $J\{1\}$. The $J\{1\}$ values used for these comparisons are not corrected by $[1 - (\Delta a/b)]$ as in Equation 5. Applying this correction would reduce $J\{1\}$ by, for example, 5% for $\Delta a/b = 0.5$. The correction does not explicitly enter into $J\{2\}$, although it is not known whether it is absorbed into the g term. There are no data on $J\{2\}$ for $a/W = 0.3$. Because of these restrictions, $J\{2\}$ is not further pursued here. As already noted, use of b_f in $J\{3\}$ seems inconsistent because of the different effect on the two similar terms. Though relatively small, it would if eliminated tend to bring the values of $J\{1\}$ and $J\{3\}$ together. $J\{1\}_{corr}$ is regarded as the most rational formula and these values are taken as representative of the energy-based estimates, although it must be admitted that the most appropriate definition of J for use after crack growth is not yet fully clear.

Comparison of $J\{4\}$ and $J\{1\}$ (both uncorrected for growth) suggests that $J\{4\} < J\{1\}$ by some $10 \pm 2\%$ for $\Delta a/b < 0.06$, except in isolated cases. $J\{5\}$ values are lower than $J\{4\}$, typically by 5%. Note that $J\{5\}$ does not contain a correction for crack growth. Factors which may contribute to these differences are the use of E' to convert K^2 to J_{el} (note that the ASTM standard uses E); under-estimation of Q_L for $J\{4\}$ or of Q_1 and Q_2 for $J\{5\}$ (see Fig. 1); over-estimation of r_p; and under-estimation of U_p from the measured total clip-gauge reading. There may also be an under-estimate of displacement from just rotation and span which, though clearly the dominant term, neglects shear displacement. Under-estimation of the extraneous indentation displacements would increase $J\{1\}$, but not $J\{4\}$ or $J\{5\}$.

A comparison within the clip-gauge methods shows $J\{6\}$ some 10% lower than $J\{4\}$ (all with $\Delta a/b < 0.08$), increasing with the amount of growth. Note that the correction for crack growth is here in the same sense as in Equation 5, contrary to the method for $J\{3\}$. For $J\{6\}$, an estimate of Q_L based on σ_y rather than σ_{fl} was permitted [9] where conservative values of toughness were intended, and that estimate was used here. Some values were clearly too low, since they were less than measured values of Q_2 (Fig. 1). Use of an experimental value of Q_L as also envisaged [9] may well be preferable, although choosing an appropriate value of Q_L (for $J\{4\}$ and $J\{6\}$) or Q_1 and Q_2 (for $J\{5\}$) is not entirely certain for the well-rounded diagrams obtained here.

In summary, the clip-gauge methods for $J\{4\}$, $J\{5\}$ and $J\{6\}$ give results rather lower than the energy method for $J\{1\}_{corr}$, although $J\{6\}$ based on experimental values of Q_L might not be much different from $J\{1\}_{corr}$, depending on just how a value of Q_L is assessed.

A further point relates to the definition of "initiation". In the foregoing, all data were analysed in various ways so that R curves were found according to different formulae for J. Having selected one, say $J\{1\}$, the R-curve must then be analysed (e.g. [7]) to find J_{IC} or J_i, or some other arbitrary value representative of the initiation event. Values of J_{IC} were found, valid by the ASTM criteria [7] except in two cases where there were insufficient valid data points. Only the data for $a/W = 0.5$ were analysed, since $a/W = 0.3$ is not a standardized test [7]. Values were also found for J_i (at $\Delta a = 0$), $J_{0.2}$ (at $\Delta a = 0.2$ mm), and J_{max} at maximum load. The ratios of various mean data are given in Table II.

Examples of J and Δa results as found by all methods other than $J\{5\}$ shown in Figs. 2 and 3. In Fig. 2 the data are amenable to an analysis for J_{IC} [7], but the data of Fig. 3 cannot be treated by that method. Note that J is found by extrapolation of the R-curve to $\Delta a = 0$, not by direct

TABLE II Effect of definition of J on the value of toughness based on $J\{1\}$ data

Temperature (°C)	Orientations	J_i/J_{IC}	$J_{0.2}/J_{IC}$	J_{max}/J_{IC}
+ 20	All	0.638	0.994	2.001
− 23	All	0.891	1.13	1.973
~ − 50	All	0.735	0.97	1.828
All	LT	0.737	1.01	1.964
All	All	0.734	1.02	1.928

measurement of no growth. Reliable determination of a precise point of crack initiation was not achieved. For this material J_{IC} is clearly some 30% above the J_i initiation value, and corresponds closely to $J_{0.2}$ (i.e. at 0.2 mm growth). J_{max}, at the onset of maximum load for these particular bending configurations, is about twice J_{IC}.

4.2. Mean toughness data

The major feature affecting the present data is that of orientation (Fig. 4). These graphs are plotted in terms of $J\{6\}$, since this offers good corrections for crack growth. Despite the obvious distinctions between the TS, LS and LT directions (in reducing order of the height of the R-curve) it is noted that the initiation value is not really separable within the general scatter. However, when looked at in terms of a blunting line [7], $J = 2\sigma_{fl}\Delta a$, there are three-fold differences (Fig. 4) between TS and LT and some 50% between LS and LT, as well as inadequate data for a valid procedure [7] in the TS case. The difference is seen more acutely if only small amounts of growth are examined as Fig. 3, which may itself seem incorrect in the light of the standard test [7]. However, in the light of all the data in Fig. 4, it is the standard procedure [7] that seems inadequate in respect of the blunting line, for this material. This problem will be discussed more fully elsewhere [21]. It must be recalled that the R curves of Fig. 4 are not strictly comparable with the standard test [7] because of the use of $J\{6\}$ value, rather than $J\{1\}$ corrected for growth, but this would raise the whole data by some 10% rather than alter the trends.

A main physical feature, irrespective of how it is measured, is that over the range considered, which for this material and size is from just above the (static) ductile transition to well above it, there is no effect of temperature within the scatter of results. Furthermore, within the range $W = 23$ to 46 mm there is no effect of width at a thickness of $B = 23$ mm; see Fig. 4 for LT and also Fig. 5, which is an alternative plot of some of the sets of data in terms of $J\{1\}$, showing a similar independence of width within the same range. Fig. 6 shows that there is no effect of a/W ratio between 0.3 and 0.5. These data are again in terms of $J\{6\}$, since the standard method [7] does not apply to $a/W = 0.3$. Fig. 7 shows no effect of thickness at $-50°$ C for $a/W = 0.5$, LS orientation, but for the TS orientation, $a/W \approx 0.3$, there is an effect since for $B = 23$ mm and $W = 46$ mm the R curve is some 30% higher than for $B = 46$ mm, $W = 46$ mm. This difference is presumably not due to the orientation or the temperature, but to the

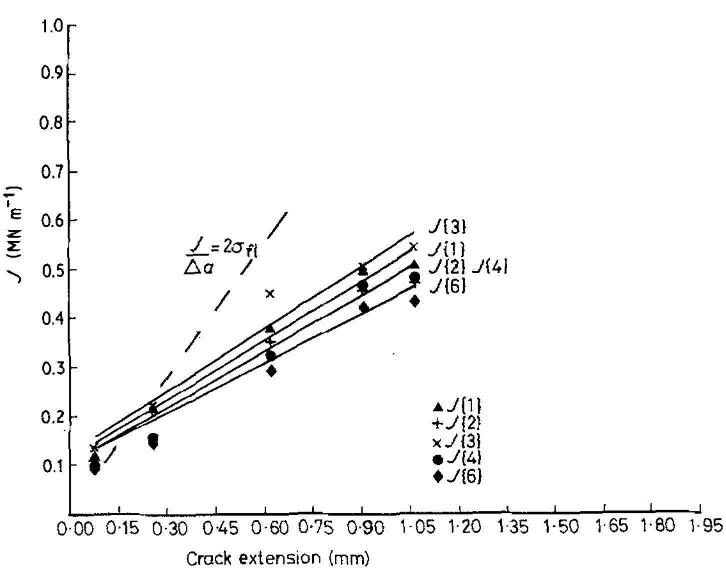

Figure 2 Sample J against Δa, LS orientation, $a/W = 0.5$. $B = W/2 = 23$ mm.

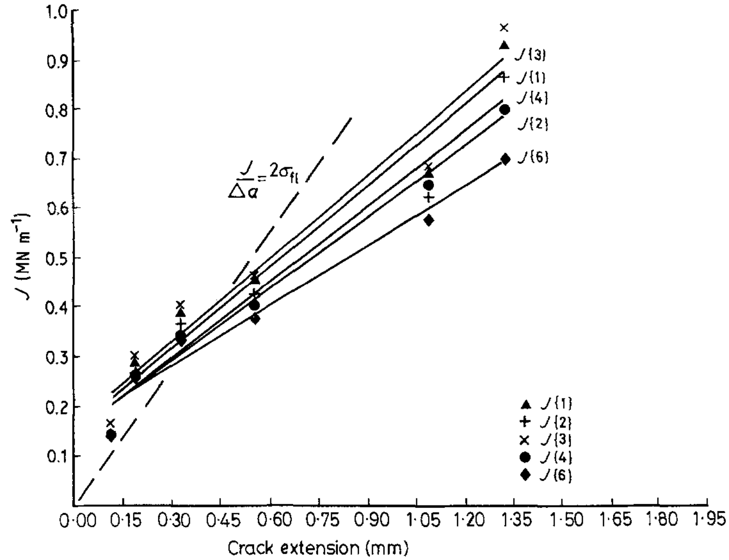

Figure 3 Sample J against Δa, TS orientation, $a/W = 0.5$. $B = W/2 = 23$ mm.

interaction of absolute thickness with ligament. For the one series ($-50°$ C, LT, $a/W = 0.5$), $B > b$ for both values of B; whereas for the other series ($+20°$ C, TS, $a/W = 0.3$), $B > b$ for $B = 46$ mm but $B < b$ for $B = 23$ mm. This last would be expected to give a tendency towards out-of-plane shearing and a higher R curve, which is indeed the case. Fig. 8 shows the effect of absolute size for $a/W = 0.5$, $B = W = 24$ and 46 mm. In both cases $B > b$ so that in-plane shearing might be expected, but the feature not at first expected is that the thicker material has the higher R curve. Supposing that this is not just scatter§, it implies that the tendency to plane stress (σ_z small) is not dominant over the geometrical constraint $B > b$. To allow the domination of plane stress $B < b$ is required, and the comment on Fig. 4 of "no effect of W" must be taken in the light of the fact that for both sizes $B > b$. In short, the effect of the thickness to ligament ratio on the collapse mechanism is more important than that of absolute thickness once extensive plasticity occurs, whereas in small-scale yielding there is no doubt that absolute thickness dominates the degree of plane strain, as in the well-known requirements for valid lefm conditions of $B > 2.5(K/\sigma_y)^2$.

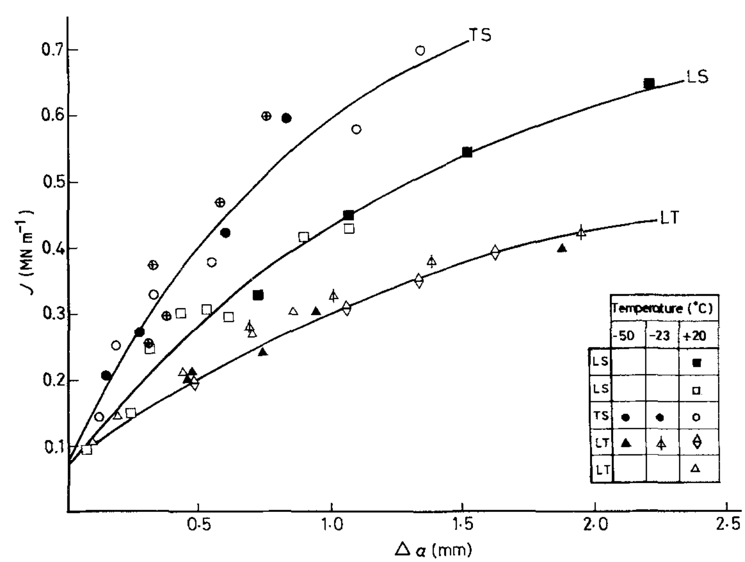

Figure 4 $J\{6\}$ against Δa, showing the dependence on specimen orientation. Note that the data cover various sizes and temperatures, with $B = 23$ mm. In general $W = 46$ mm, $a/W = 0.5$, but for some LT at $+20°$ C, $W = 23$ mm and for LS at $+20°$ C (and $-23°$ C) $a/W = 0.3$.

§Results are for different temperatures, but LT data (Fig. 4) showed no effect of temperature for the same configurations.

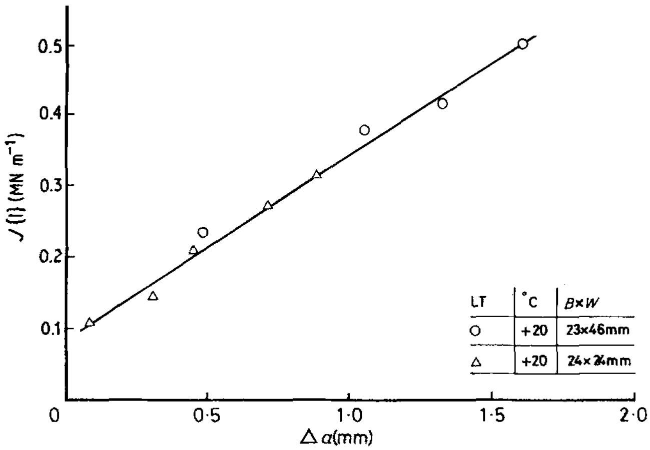

Figure 5 Effect of specimen width on the *R* curve of ductile steel BS4360: 50D, using $J\{1\}$. (Same data as in Fig. 4).

Despite these small trends, when judged overall, data read off graphs such as Fig. 2 or 3 (using one particular formula and blunting line) can show appreciable variations in toughness according to the definition and test conditions used. This is shown Table III.

5. Conclusions

Static three-point bend tests were made on BS4350:50D steel, in the size range $0.3 < a/W < 0.5$; $W/2 < B < W$; $20\,\text{mm} < B < 50\,\text{mm}$, and within the temperature range -50 to $+20°$ C. All the tests conducted gave fractures ductile in the micro-mode, although it is known that brittle cleavage fractures can be induced over much of this temperature band by impact loading. The conclusions here do not encompass the risk of a fast-running crack caused possibly by impulsive loading. With that proviso, the results showed no trend of toughness with temperature, but a marked trend with respect to orientation where the *R* curve increased in the order TS > LS > LT by about 30% in each case. No doubt this is accounted for by the distribution and orientation of inclusions, as reported by others but not examined here, although the toughness for no crack growth was identical in each orientation to within the experimental accuracy.

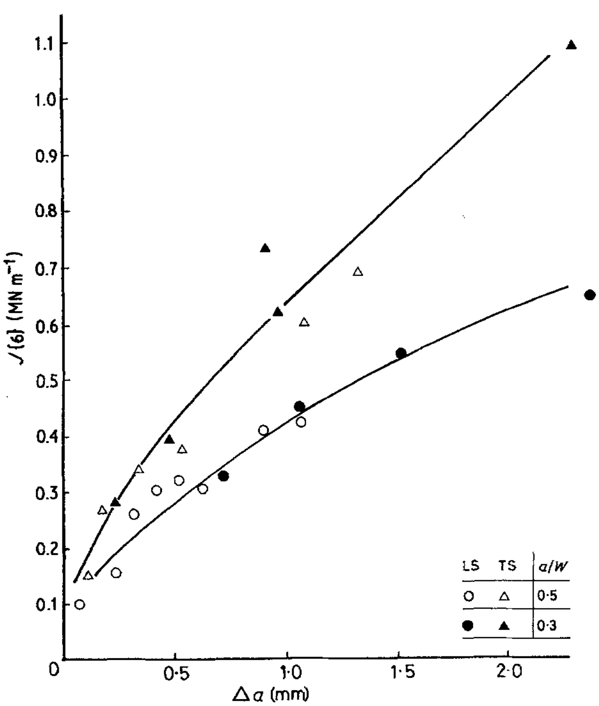

Figure 6 Effect of notch depth on the *R* curve of BS4360:50D steel. $B = W/2 = 23$ mm.

TABLE III Mean toughness data using $J\{1\}$

Temperature (°C)	Orientation	J_i (MN m^{-1})	J_{IC} (MN m^{-1})	$J_{0.2}$ (MN m^{-1})	J_{max} (MN m^{-1})	J_i/J_{IC}	$J_{0.2}/J_{IC}$	J_{max}/J_{IC}
− 50	LT	0.164	0.227	0.218	0.398	0.72	0.96	1.75
− 23	LT	0.176	0.223	0.229	0.425	0.78	1.03	1.90
+ 20	LT	0.103	0.144*	0.156	0.319	0.71	1.07	2.04
			(0.160)			(0.61)	(0.92)	
–		0.148	0.198	0.201	0.381	0.75	1.01	1.93
	LT (Mean)		(0.203)			(0.72)	(0.98)	(1.85)
+ 20	LS	0.111	0.271	0.180	0.421	0.41	0.66	1.55
+ 20	TS	0.190	0.289	0.291	0.515	0.55	1.01	1.78

*Not valid according to standard test [7]. Values in brackets are best estimates.

A single value of toughness representative of the onset of tearing was not sensitive to configuration or thickness, within the quite limited range tested. It was, however, sensitive to the method used to account for stretch zone formation, a typical spread of results for a given formula for J being $J_i \simeq 0.7 J_{IC}$; $J_{IC} \simeq 0.5 J_{max}$, where J_{IC} is in accordance with the standard ASTM method. That method was not applicable to tests in some circumstances, because of the steepness of the R curve with respect to the usual blunting line of $J = 2\sigma_{fl}\Delta a$ at small amounts of growth, so that a standard value cannot always be determined.

Typical values of J_i (at $\Delta a = 0$) for 20 to 50 mm thick pieces of this steel, in the configurations tested and for the temperature range − 50 to + 20° C, are about 0.15 MN m^{-1} in all orientations. J_{IC} values are about 0.20 MN m^{-1} for the LT direction and up to about 0.29 MN m^{-1} for the TS direction, with J_{max} (i.e. J at the onset of maximum load in these tests) about 0.38 MN m^{-1} for LT.

The values obtained from a given set of experimental data depended on the method of analysis, the conventional energy-based method giving results some 10 to 15% higher than a formula based on load and clip-gauge readings. Some of that difference, and similar discrepancies between other methods, depend on the consistency or otherwise of allowing for the amount of slow crack growth, although this is barely significant if $\Delta a/b < 0.6$ as recommended for J-controlled growth; and several other factors are also seen to be relevant to the differences between the test

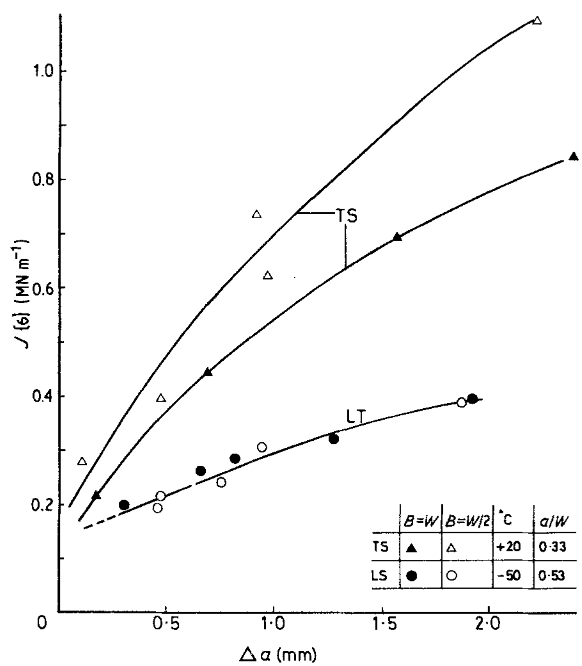

Figure 7 Effect of specimen thickness on the R curve of ductile material BS4360:50D, for $W = 46$ mm.

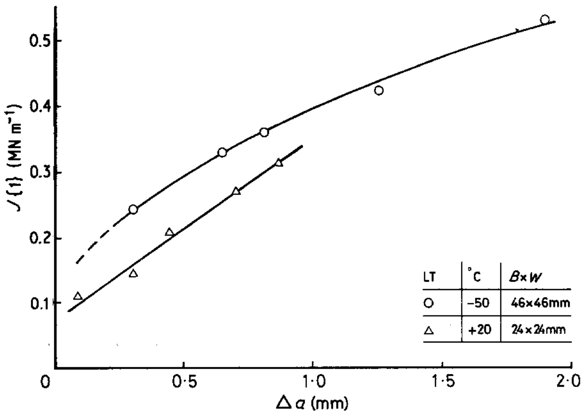

Figure 8 Effect of specimen size on the R curve of BS4360:50D steel with $B = W$.

methods. The energy-based formula of Equation 5 is judged most appropriate¶ despite the fact that some of the particular recommendations of the standard test [7], notably the blunting line procedure, are not always suitable for this steel.

Irrespective of the method of analysis, the R curves up to $\Delta a/b \simeq 0.1$ ($\Delta a < 2.5$ mm) were not influenced by in-plane geometry within the rather limited range tested, except in one case where the thickness to ligament ratio $B/b \simeq 0.7$ induced some degree of plane stress. On the present limited evidence it seems that, for extensive plasticity, the tendency to plane stress is influenced more by the ligament ratio ($B/b > 1 \rightarrow$ plane strain, $B/b < 1 \rightarrow$ plane stress) than by the absolute value of thickness which dominates in the lefm regime.

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¶Equation 5 was relevant to the tentative version of [7] current when these tests were conducted. The present version uses a different formula that gives results very similar to Equation 5 for small amounts of growth.