# 6. TRIAXIALITY EFFECTS IN CLEAVAGE FRACTURE OF FERRITIC STEELS

by

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SUMMARY

Triaxiality at failure was varied in sharp cracked geometries by changing crack length to specimen width ratio and specimen thickness, and in notched bend geometries by changing the root radius. The toughness for cleavage failure increased as triaxiality decreased. This is interpreted in terms of a decrease in the ductile-brittle transition temperature. Similar behaviour was qualitatively displayed by the notched specimens where increasing root radius resulted in a change in failure mode from brittle to ductile. Both these effects can be rationalised in terms of the Ritchie, Knott and Rice cleavage fracture model. When stable crack growth occurs this produces changes in the crack profile and material properties ahead of the advancing crack which can result in turn in an increase in triaxiality and a lowering of toughness. The combined effect is frequently large enough to cause the failure mode to change back to cleavage.

The implications of these effects with respect to failure analysis is discussed, and it is concluded that wherever there is a risk of cleavage failure, even after some ductile crack growth, the analysis must be based upon initiation criteria. As far as possible sub-sized specimens should not be used to evaluate $K_{1C}$ for cleavage. On the other hand, where there is no possibility of cleavage failure, if it is possible to determine the amount of ductile crack growth which occurs prior to maximum load, failure analysis can be based upon this.



## 1. INTRODUCTION

One of the outstanding problems in fracture mechanics is the relationship between critical macroscopic failure parameters, such as the plane strain fracture toughness, $K_{1C}$, or the critical value of the J-integral, $J_{1C}$, to the mechanisms of failure. Although it is now generally accepted that the attainment of $J_{1C}$ (which is equivalent to attaining $K_{1C}$ in small scale yielding) is a necessary condition for fracture, it has still not been established that this condition is also sufficient. To do this it must be shown that $J_{1C}$ characterises the local crack tip stress and strain fields and furthermore that the mechanism of failure is initiated by critical conditions directly related to these fields. Only then is the one to one relationship between mechanism and fracture criterion established.

In this paper we will be principally concerned with cleavage failure in ferritic steels. One of the simplest models of describing this mechanism is due to Ritchie, Knott and Rice (1973). Extending the arguments of Orowan (1948) and Wilshaw, Rau and Tetelman (1968) the model of RKR assumes that cleavage occurs when a critical stress, $\sigma_f$, is exceeded over a micro-structural distance $X_o$ ahead of the crack. The success of this model in explaining fracture behaviour has been demonstrated on several occasions (see, for example, RKR, Ritchie, Francis and Server (1976) and Rawal and Gurland (1977)). If it is further assumed that in plane strain deformation J characterises the stress field $\sigma_{yy}(x)$ ahead of the crack according to an equation of the form

$$\sigma_{yy}(x) = \bar{\sigma}\, F(x\bar{\sigma}^2/EJ) \qquad \ldots \quad (1)$$

where $\bar{\sigma}$ is the yield stress, E is Young's modulus and F(z) a function dependent on work-hardening coefficients, then the RKR cleavage condition that

$$\sigma_{yy}(x) \geq \sigma_f \quad \text{for} \quad x \geq x_o$$

leads to a critical value for J at failure given by

$$J_{1C} = \frac{x_o\bar{\sigma}^2}{E} \Big/ F^{-1}(\sigma_f/\bar{\sigma}) \qquad \ldots \quad (2)$$

(Limited theoretical justification of equation (1), at least in the small scale yielding limit, is given by Hutchinson (1968) and Rice and Rosengren (1968)).

Since the right hand side of equation (2) contains only material constants it would appear that $J_{1C}$ will also be a material constant. This is true provided the function F(z) is independent of geometry and the degree of plastic deformation resulting from the crack. If plane strain conditions prevail, and failure occurs in the small scale yielding regime, these conditions are likely to be satisfied. However, in the large scale yielding regime, at or near general yielding, the constraint on plastic deformation (taken as the ratio of plane strain to plane stress general yield loads) is a function both of testing geometry and $^a/w$ ratio, where a is the crack length and w the width of the specimen. Furthermore if failure occurs after general yield then deformation through the thickness may modify and invalidate the assumption of plane strain conditions, with a resulting change in the stress field $\sigma_{yy}(x)$ and loss of some triaxiality.

In these situations it is to be expected that the function F(z) will depend in some way on the degree of plastic constraint, and that the constraint at or beyond general yielding will be a function of $^a/w$ and specimen size. For the same applied J level, loss of constraint will result in a decrease in the level of $\sigma_{yy}(x)$ with respect to its fully constrained, small scale yielding value (see Fig. 1). From equations (1) and (2) and, as can be seen in Fig. 1 this implies that for cleavage a size dependence of $J_{1C}$ is to be expected, $J_{1C}$ increasing as constraint decreases, even though



J still continues to characterise the stress ahead of the crack. It is the implications resulting from this argument that are discussed in this paper.

## 2. EXPERIMENTAL EVIDENCE OF A TRIAXIALITY EFFECT

In a recent paper Milne and Chell (1978) have tabulated some instances where a size dependence of the fracture toughness was observed due to variations in $^a/w$ for a given specimen size, or changes in size for a given $^a/w$ value. Some of the results, those for single edge notched tension specimens reported by Chell and Davidson (1976), and Chell and Gates (1977), are shown in Fig. 2, where $K_{1J}$ is the fracture toughness value obtained from $J_{1C}$ measurements using the formula

$$K_{1J} = \left(EJ_{1C}/(1-\nu^2)\right)^{\frac{1}{2}}$$

where $\nu$ is Poisson's ratio. No particular significance is attached to the normalisation parameters used in Fig. 2, and $\sigma_u$ refers to the ultimate tensile strength of the materials tested, one of which was a rotor forging steel while the other was a high temperature bolting steel. All the failures shown in Fig. 2 were by a cleavage mechanism. Fractographic observations of the fractured surfaces showed that no stable crack growth had occurred prior to failure for the specimens whose data is shown in Fig. 2. Indeed in these materials stable crack growth was characterised by void growth around inclusions, the voids eventually linking together with the stretch zone, via bridges of ductile dimples, to result in ductile tearing.

## 3. EFFECTS OF LOSS OF TRIAXIALITY ON THE BRITTLE-DUCTILE TRANSITION

The increase in $K_{1J}$ ($J_{1C}$) value with decreasing triaxility (constraint) resulting from size effects can be considered analogous to the increase in toughness of ferritics with temperature during the brittle ductile transition, and, at least in principal, the size effect can be described in terms of a shift in the brittle-ductile transition temperature in the same way that Orowan (1948) explained an associated notch sensitivity in body-centred cubic alloys. This shift is shown schematically in Fig. 3 and provided the effect of constraint on $\sigma_{yy}(x)$ is quantifiable, it is calculable direct from the RKR model as follows.

For a given temperature, T, in the transition region $K_{1C}(T)$ may be determined using the appropriate values of $\sigma_f$ and $X_o$ and assuming full plane strain, small scale yielding conditions prevail. (This should be equivalent to a value of $K_{1C}$ obtained from a valid, plane strain fracture toughness test at this temperature). Next from an invalid, sub-sized specimen the value of $K_{1J}(T)$ needed for cleavage can be calculated using the stress field appropriate to the specimen i.e. taking into account loss of triaxiality due to $^a/w$ effects and/or through thickness deformation. The difference in the value of $K_{1J}(T)$ and $K_{1C}(T)$ can be attributed to a temperature shift $\Delta T$ such that

$$K_{1J}(T) \quad = \quad K_{1C}(T + \Delta T).$$

Another important feature resulting from loss of stress triaxiality is an increase in strain ahead of the notch. Plane strain produces large stress elevations ahead of the crack but only small strains (Rice and Johnson, 1970). Crack tip blunting resulting from stretch zone formation creates a localised, unconstrained region where large strains can be generated. If this region extends over an area comparable with $x_o$, it can contribute to a reduction in triaxiality and produce a similar increase in toughness to that attributed to loss of through thickness constraint. This build up of large strains while a reasonable degree of stress elevation is still maintained, leads to an increase in the probability that void growth and coalescence will occur (Rice and Jonnson 1970, McClintock, 1968) before the cleavage stress $\sigma_f$ is reached at $x_o$. A consequence of this would be crack extension by ductile tearing. In other words the shift in transition temperature would be sufficient to cause crack propagation in a failure mode similar to that associated with the upper shelf in the toughness-temperature curve.

Once crack growth has occurred, however, several factors come into play which tend to result in effects which produce an effective shift in



since the susceptible parts of these structures are generally in highly stressed regions, where triaxiality may also be expected to be high, no excessive penalty will be incurred by adopting this policy. Besides, the presence of residual or thermal stresses can increase the stress triaxiality even in the regions of low triaxiality.

### 4.2 Assessment in the Presence of Ductile Failures

There are two main differences between ductile crack growth and cleavage; (1) the failure event is strain controlled rather than stress controlled and (2) the structure does not necessarily become unstable during crack propagation. However, as with cleavage, the initiation of cracking can occur at any stress level. In plastic collapse the maximum load is the limit load and is again independant of crack initiation, or growth and so independant of $K_{1J}$. In the small scale yielding limit the maximum load is often not achieved until considerable crack growth has occurred so that in this regime, assessing to the initiation event may considerably underestimate the failure load. This initiation event, however, being strain controlled, appears relatively unaffected by stress triaxiality so that tests on small specimens can be used to obtain the relevant initiation data.

To take advantage of the full load bearing capacity of a structure, a way has to be found which allows calculation of the load as a function of crack growth (Path 4 in Fig. 4). For thin section structures with through thickness cracks this problem can be avoided by testing full section thickness specimens and performing an R-curve analysis or assessing to maximum load. For thick sections or for part thickness defects this approach is not acceptable. Recently a technique has been developed which can perform the appropriate type of analysis, and good agreement has been obtained between measured and predicted maximum loads for a variety of materials and test specimen geometries (Milne 1978).

the opposite direction, that is, towards a brittle cleavage failure. These are, firstly that the crack tends to extend as a thumb nail; this crack front geometry change reduces through thickness deformation at the extended crack tip, causing an enhancement in triaxiality. Secondly, as the crack extends it sharpens and blunting effects become less significant. Thirdly, the crack propagates into material which has already been pre-strained and has had, therefore, some of its ductility removed. These three phenomena can combine to produce conditions suitable for the initiation of cleavage and hence instability. This is often observed in fracture toughness testing of ferritics in the transition region.

This competition between brittle and ductile fracture mechanisms is well illustrated by recent fracture tests on blunt notched bend specimens of varying root radii recently reported by Milne, Chell and Worthington (1978). Here loss of triaxiality was mainly due to the increase in root radius and the associated increase in the region of triaxial stress relaxation due to the plane stress conditions prevailing at the notch root. As a consequence of this for the particular material tested (a 1%Cr, 1%Mo, ¼%V steel which had a moderate toughness of ≈ 60 MPa$\sqrt{m}$, a yield stress of 580 MPa and an ultimate tensile stress of 740 MPa,) the mechanism of failure changed from cleavage (stress controlled) to ductile (strain controlled) for root radii in excess of 1mm.

## 4. IMPLICATIONS WITH RESPECT TO FAILURE ASSESSMENTS

The triaxiality effects discussed above have important implications on the failure analysis of ferritic components. Here final failure may either be by ductile or cleavage mechanisms with the possibility of ductile crack growth changing to cleavage as the crack advances. This situation requires an instability criterion which is not expressable purely in terms of J



A complete description of the role of triaxiality effects in ferritic steels is only possible if the mechanisms can also be included.

A schematic representation of the possible mechanistic paths to failure that a ferritic structure can follow is shown in Fig. 4. Regardless of the crack growth mechanisms, there are only two mechanical descriptions of failure, brittle fracture (meaning cleavage fracture) and plastic collapse.

### 4.1 Assessment Against Cleavage Failures

Cleavage failure can occur at any stress level below plastic collapse provided the appropriate mechanism can be activated. In small scale yielding the failure load is determined by the plane strain fracture toughness, $K_{1C}$ (failure path 1 in Fig. 4). At plastic collapse it is determined by the plastic limit load of the structure (Path 5 in Fig. 4). At this limit the actual value of $K_{1J}$ relative to $K_{1C}$ is of little significance. In the intermediate regime on the other hand, the initiation of cleavage determines the failure load (Path 2), but only after some plasticity has occurred. Here the appropriate parameter is $K_{1J}$ rather than $K_{1C}$ since this automatically accounts for the loss of triaxiality due to the plasticity. This degree of triaxiality will, of course, vary from point to point in a structure, depending among other things on the crack orientation relative to the maximum principle stress, the proximity of the crack tip to the nearest free surface and the shape of the crack. Because of the uncertainty inevitably present in all of these factors there is little alternative but to design to the lower bound of $K_{1J}$, i.e. $K_{1C}$, and this obviates data generated from small specimens. This latter point is especially important when assessing structures which have a biaxial stress field, e.g. pressure vessels, or at geometric discontinuities. Undoubtedly in regions of such structures where the triaxiality is low this approach will underestimate the failure stress, but

4.3 The Ductile-cleavage Transition

The above mentioned techniques for assessing against ductile fracture are satisfactory only in the absence of a ductile-cleavage transition. For ferritic materials this means that the temperature has to be such that at the section of interest the structure is operating well into the ductile regime. At temperatures close to the ductile-cleavage transition the triaxiality effects discussed above are difficult to predict even at initiation; they are impossible to predict during growth. At these temperatures it is therefore prudent to avoid crack growth altogether, and assess against the possibility of cleavage, as discussed in section 4.1. These type of failures are signified as Path 3 in Fig. 4.

## 5. CONCLUSIONS

(1) When structural failure can only be by ductile mechanisms failure analysis techniques can be adopted which determine how much ductile crack growth is permissible.

(2) Where there is any risk of cleavage failure, even after some ductile crack growth, failure analysis should be based upon initiation criteria.

(3) For cleavage failure, triaxiality effects resulting from geometrically inadequate specimens can cause significant increases in the derived fracture toughness over the plane strain fracture toughness.

## 6. ACKNOWLEDGEMENTS

This work was carried out at the Central Electricity Research Laboratories and the paper is published by permission of the Central Electricity Generating Board.



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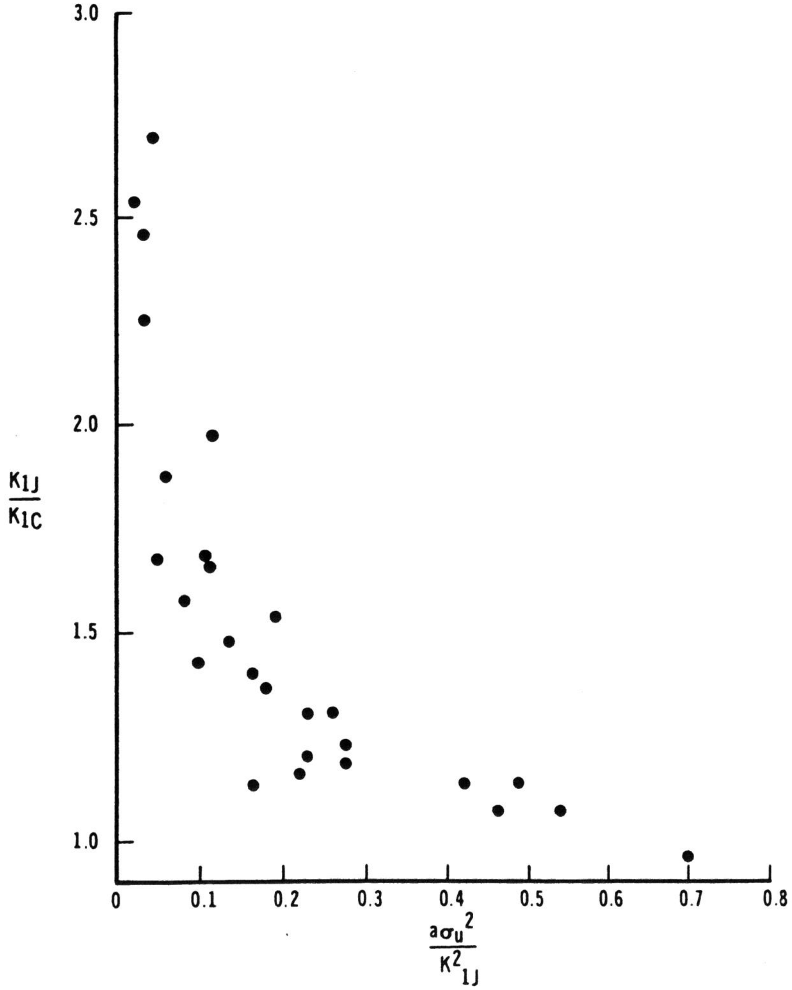


FIG. 1 EFFECT OF SIZE ON $K_{1J}$ FOR SINGLE EDGE NOTCHED TENSION SPECIMENS



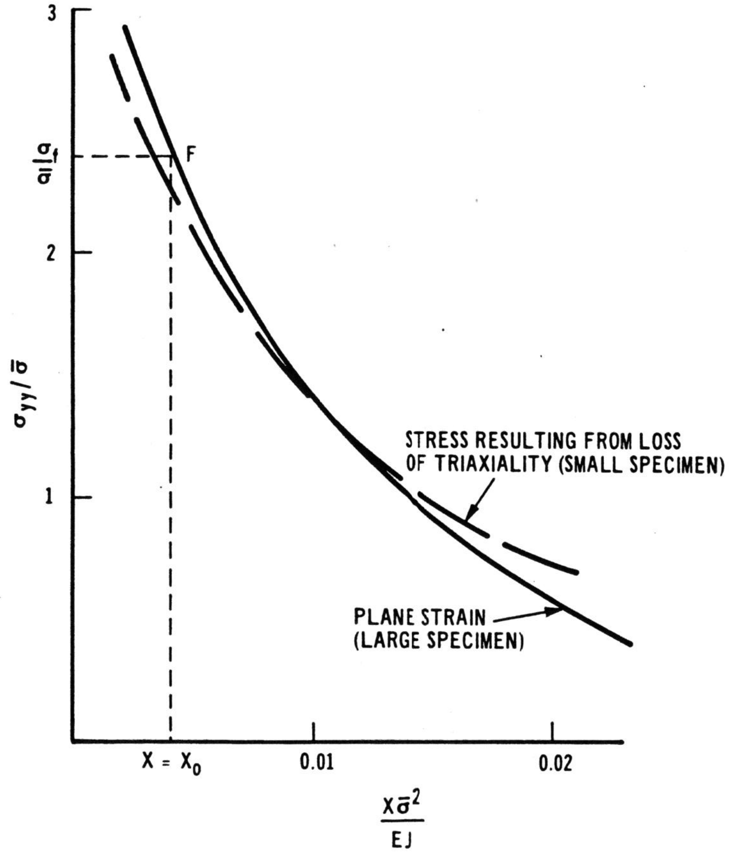


FIG. 2 SCHEMATIC REPRESENTATION OF THE STRESS PROFILES AHEAD OF CRACKS IN LARGE AND SMALL SPECIMENS WITH THE SAME 'J' VALUE

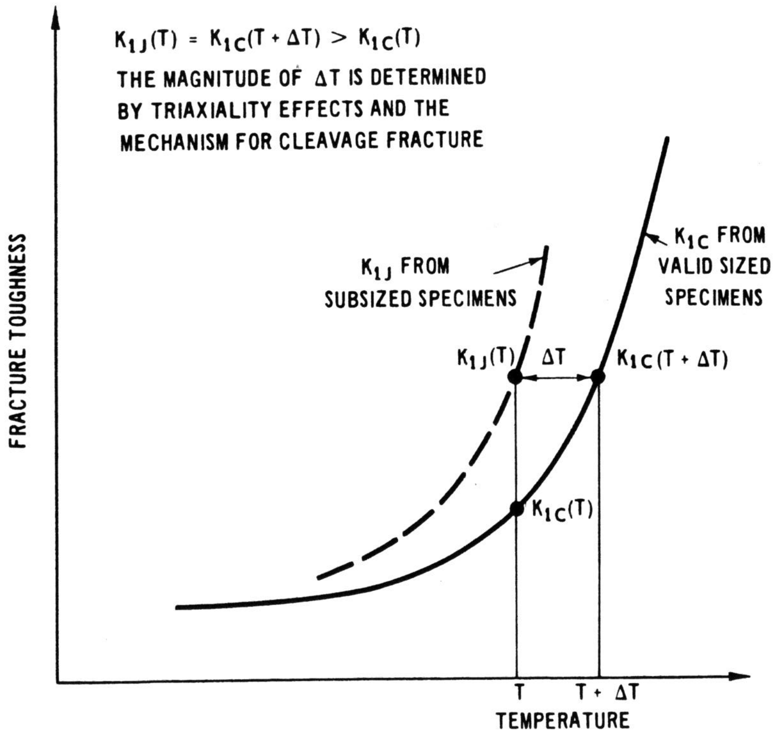


FIG. 3 REPRESENTATION OF THE TRIAXIALITY EFFECTS ON $K_{1J}$ IN TERMS OF A SHIFT IN THE DUCTILE-BRITTLE TRANSITION TEMPERATURE



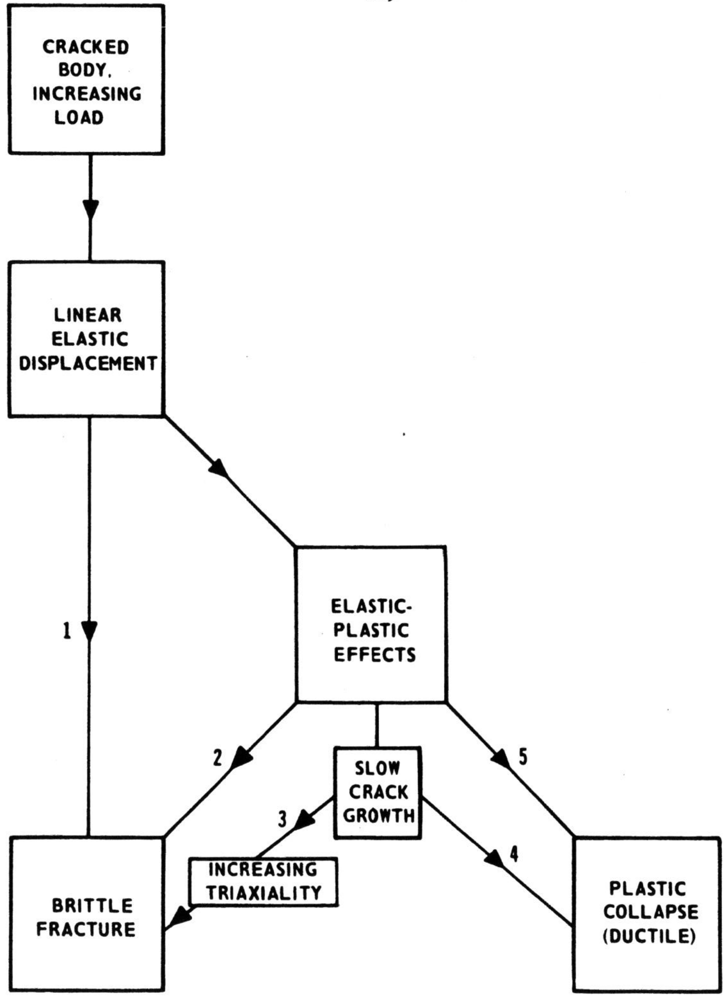


FIG. 4 THE DIFFERENT FAILURE PATHS WHICH A CRACKED BODY CAN FOLLOW