Critical Assessment of Microscopic Cleavage Fracture Stress Measurement Methods by Experiments on Hot Work Tool Steel

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ABSTRACT

The methods to assess the microscopic cleavage fracture stress σ_f^* are briefly reviewed; among the parameters that can influence the measurement of σ_f^*, the root radius in blunt notch specimen hasn't yet received an adequate attention, and only finite element analyses have stressed out its importance. An analysis of the stress distribution ahead of blunt notches of varying root radii is performed by means of the slip line field theory. The measurement of σ_f^* in three hot work tool steels with different microstructure has allowed to assess the interaction between microstructural parameters governing fracture and the radius of the notch. The results show that by means of a slip line field analysis it is possible to design the root radius in a blunt notch specimen to assess σ_f^* and to interpret the results in order to ascertain the critical distance from the crack or notch tip at which σ_f^* must be overcome for cleavage fracture to occur.

KEYWORDS

Brittle fracture, microscopic cleavage fracture stress, critical distance.

INTRODUCTION

Microscopic cleavage fracture stress σ_f^* has always appealed to researchers in the field of brittle fracture, since it is considered a material property, nearly independent of temperature, strain rate and test piece geometry. More recently a renewed interest in the micromechanisms of cleavage fracture has led to an increased interest in the microscopic cleavage fracture stress and in its measurement.

It is generally accepted that initiation of brittle fracture in steels obeys a critical stress criterion, i.e. cleavage crack propagates when the following conditions are satisfied:

a) the maximum local tensile stress σ_{yymax} must exceed the microscopic cleavage fracture stress σ_f^*;

b) the size of the process zone, over which $\sigma_{yymax} \geq \sigma_f^*$, must be equal to or greater than a critical size, which is generally related to the size of the microstructural unit governing failure (grain, subgrain, bainitic or martensitic packet, etc.). In a few theories also the carbide size plays a role.

Several methods have been proposed to measure the microscopic cleavage fracture stress σ_f^*. In order to avoid tensile tests at very low temperatures (Orowan, 1948; Aurich et al., 1981), round notched tensile or bend specimens are usually employed, provided that the stress distribution ahead of the notch tip is known.
For perfectly plastic solids, according to Hill's slip line field theory (1950), the σ_{yy} distribution inside the plastic zone, assuming the Tresca yield criterion, is expressed by:

$$\sigma_{yy} = \sigma_Y \, [1 + \ln(1 + x/\rho)] \qquad (1)$$

where σ_Y is the material yield strength, ρ the notch root radius and x the distance below the notch root.
The maximum longitudinal stress σ_{yymax} is met in correspondence of the elastic-plastic interface. Then, at fracture, σ_f^* can be substituted for σ_{yymax} according to the above reported condition a) and the plastic zone size at fracture r_{pf} in place of x.
The use of Eq. 1 to obtain σ_f^* is possible only before general yield is attained, and in particular when the plastic zone size is small (Wilshaw et al., 1968). When plasticity becomes extensive and general yield is met, the following equation can be used to evaluate $\sigma_f^* = \sigma_{yymax}$ (and hence σ_f^*) according to the slip line field theory:

$$\sigma_{yymax} = \sigma_Y \, [\, 1 + (\pi - \omega)/2 \,] \qquad (2)$$

where ω is the notch flank angle.
To evaluate σ_f^* by means of Eq. 2 it is necessary to test several specimens at different temperatures in order to meet the condition of coincidence between general yield and fracture for the chosen test piece geometry, thus determining the appropriate value of σ_Y to be introduced in the equation.

Deviations from the stress distribution described by Eqs. 1 and 2 are introduced by strain hardening, since real metals do not follow a perfectly plastic behaviour as considered by the slip line field theory.

Eq. 2, rather than Eq. 1, has been extensively used to assess the microscopic cleavage fracture stress in various steels.
Early experiments on cleavage fracture of mild steels indicated that σ_f^* values are different when obtained by Eq. 1 with respect to those measured by means of the general yield technique. Knott (1967) and Griffiths and Oates (1969) performed appropriate experiments to assess such a difference in the σ_f^* level, concluding that the two Eqs. 1 and 2 appear to be incompatible to each other. An explanation of the discrepancy was attempted by Griffiths and Oates (1969), who concluded that, while work hardening and statistical effects cannot account for the difference in σ_f^* level obtained by the two techniques, a critical strain hypothesis for cleavage fracture at general yield can minimize such a difference; on the basis of this critical strain hypothesis the same Authors also concluded that the general yield technique gives the more reliable estimate of the microscopic cleavage fracture stress.

Finite element calculations have also been employed in order to obtain more accurate stress distribution ahead of the notch tip, since it is thus possible to take into account the work hardening of the material within the zone of intense non linearity.
An early finite element solution for pure bending, obtained by Griffiths and Owen (1971), has long been used in many investigations of cleavage fracture of different steels.

More recently, further finite element solutions have been obtained by Kühne, Redmer and Dahl (1982) for double notch rectangular tensile specimens containing notches of different root radius and flank angle; the results for two steels having different strain hardening capacity have demonstrated the marked effect of notch root radius, and flank angle, the latter being apparently less important than the former. Furthermore, it has also been observed a good agreement between Hill's slip line field and finite element solutions in the region of small plastic zone sizes; on the contrary, major discrepancies have been obtained between the maximum value of stress concentration of finite element results and that obtained by the general yield technique; the extent of scatter has been found to increase at increasing notch root radii and decreasing notch flank angles.

In most of the experimental investigations of σ_f^*, as well in the Griffiths and Owen's finite element solution, a fixed notch root radius ρ = 0.25 mm has been adopted. In a few cases the notch flank angle ω has been the only geometrical parameter taken into account.
A team of researchers (Wilshaw et al. (1968); Tetelman et al. (1968)) performed a comprehensive investigation of the effect of the notch root radius and demonstrated the full correlation between σ_f^* and the plastic zone size as outlined in Eq. 1 and hence between the alloy plane strain fracture toughness from a notched specimen from one side and both σ_f^* and the notch root radius on the other side.
Following Wilshaw and co-workers (1968) the plane strain plastic zone size at fracture, $r_{pf} = \alpha \, (K_A/\sigma_Y)^2$, where α = const., can be substituted for x in Eq. 1, in order to obtain the relationship between K_A, the stress intensity factor applied at fracture in a notched specimen, and the notch root radius, ρ:

$$K_A = (1/\alpha)^{\frac{1}{2}} \, [\exp\,(\sigma_f^*/\sigma_Y - 1) - 1]^{\frac{1}{2}} \; \sigma_Y \sqrt{\rho} \qquad (3)$$

Eq. 3 was proved to apply for notch sizes greater than a critical one, ρ_{eff}, which is related to the critical size over which σ_f^* must be exceeded.

Starting from the approach of Tetelman and co-workers (Tetelman et al., 1968; Wilshaw et al., 1968) a further method to evaluate σ_f^* has been proposed by Ritchie, Knott and Rice (1973); in this method precracked specimens are employed and the σ_{yy} must be evaluated at the critical distance x_c. Since the method applies to sharp cracked specimens, a solution for the σ_{yy} distribution ahead of the crack tip must be taken into account. Furthermore, there are uncertainties in the experimental determination of the critical distance , and it is only argued that it is somewhat related to the dimension of the microstructural unit governing failure.

Therefore, it appears that the influence of the notch root radius in the measurement of σ_f^* is quite complex and it is questionable the practice of setting a priori the notch root radius equal to the standard for Charpy V.

In the present paper there are reported the results about the assessment of

the microscopic cleavage fracture stress σ_f^* for a cast hot work tool steel. The use of the slip line field approach is critically reviewed with particular reference to the influence of notch root radius.
It is also shown how with a single test at a temperature in the lower shelf range it is possible to measure the microscopic cleavage fracture stress σ_f^*, and how the critical distance x_c can also be obtained if a plane strain fracture toughness test is performed at the same temperature.

EXPERIMENTAL PART

The materials used in the present investigation were three heats of a cast hot work tool steel, corresponding to AISI H11 specification.
Previous results from tensile and impact tests and fracture mechanics tests with blunt notch specimens (Roberti et al. (1986)) have been implemented with fracture mechanics tests with precracked specimens. Blunt notch Charpy V type samples had a $\pi/4$ flanke angle and a 57.5 μm notch end radius. Crack and notch depths were such that $a/W = .5$.
The tests have been performed at room temperature, which corresponds to the lower shelf of the impact transition curve of the steels. A 100 kN screw driven mod.1195 INSTRON testing machine, at a crosshead speed of 0.0017 mm/s was employed.

EXPERIMENTAL RESULTS AND DISCUSSION

Table I summarizes the tensile and fracture mechanics characteristics of the three steels, together with the stress intensity factor K_A measured at fracture from tests on blunt notch specimens. The prior austenitic grain and martensitic packet sizes are also listed.

According to Eq. 3 fracture mechanics data in Table I can be plotted in a K_A - $\sqrt{\rho}$ diagram as shown in Fig. 1. The K_A values pertaining to blunt notch specimens belong to lines passing through the origin. The linear relationship between K_A and $\sqrt{\rho}$ holds down to the K_{Ic} levels of the steels and the intersection of the K_{Ic} levels with the sloped lines pinpoint the notch root radius values, ρ_{eff}, up to which a blunt notch has the same effect of

Table I - Tensile and fracture mechanics characteristics, prior austenitic grain size (D) and martensitic packet size (d_{mp}) of the investigated hot work tool steels.

Steel	HRC	σ_Y [MN/m²]	σ_{UTS} [MN/m²]	El. [%]	R.A. [%]	K_{Ic} [MPa√m]	K_A ρ=57.5μm [MPa√m]	D [μm]	d_{mp} [μm]
1	44.8	1346	1535	5.9	9.7	41.2	62.6	20/80*	7.2
2	40.6	1118	1162	0.4	0.7	44.1	57.1	>150	35
3	43.7	1294	1480	3.5	8.7	48.9	66.7	30	8.8

* dual distribution

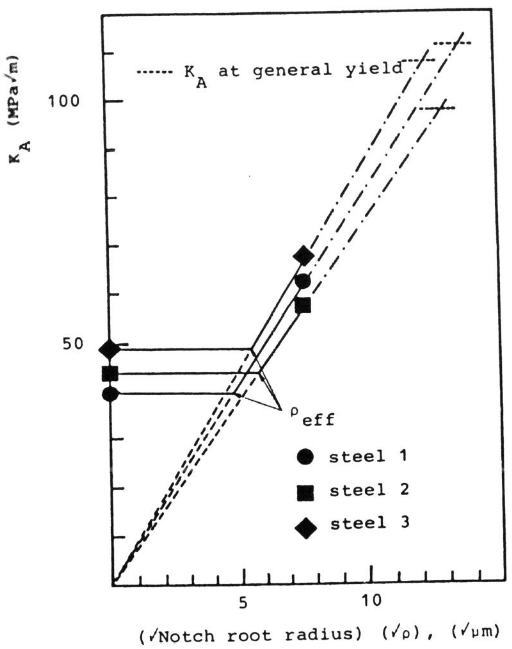

Fig. 1 - Variation of the critical stress intensity factor K_A with the square root of the notch root radius ρ.

a fatigue precrack. ρ_{eff} obtained from Fig. 1 are 25, 35 and 31 μm for steel 1, 2 and 3 respectively.
By means of the slip line field theory an analysis of the stress distribution ahead of the notch tip has been performed for various root radii. The notch flank angle has been held constant, $\omega = 45°$, and a Tresca yield criterion has been assumed.
The analysis has been carried out for the following ρ values: i) $\rho = .01$ mm as representative of the range $0 < \rho < \rho_{eff}$, ii) $\rho = \rho_{eff}$, iii) $\rho = .0575$ mm corresponding to the tested blunt notch specimens and representative of the range of ρ's for which Eq. 3 is valid, iv) $\rho = \rho_{GY}$, being ρ_{GY} the notch root radius for which fracture and general yield coincide (ρ_{GY} is obtained as the intersection of the sloped lines described by Eq. 3 with the K level computed for the general yield load of the considered specimen geometry), v) $\rho = .25$ mm, i.e. the Charpy V type notch root radius, frequently employed for the specimens used to assess σ_f^*.
The results of this analysis are reported in Fig. 2. The dashed lines refer to stresses in the elastic zone, whereas thicker solid lines indicate the stress distribution at fracture within the plastic zone. Plastic zone size at fracture has been computed by means of: $r_{pf} = .16/\pi\ (K_A/\sigma_Y)^2$.

From Fig. 2 it appears that when $\rho \leq \rho_{eff}$ fracture occurs if σ_f^* is exceeded over a distance of constant extension; for $\rho = \rho_{eff}$ this critical distance can be computed from the slip line field theory and is given by

$$x_c = [\exp(\pi/2-\delta/2) - 1] \cdot \rho_{eff} \tag{4}$$

From Eq. 4 it appears that x_c is dependent on the notch flank angle; it is independent of the yield criterion assumed and of the specimen geometry.
For the flank angle employed in the present investigation $x_c = 2.248\ \rho_{eff}$; x_c represents the point below the notch root where σ_{yy} must exceed σ_f^* so that fracture can occur.
Since it has been shown (Ritchie et al. (1976), Firrao et al. (1982)) that ρ_{eff} is related to the microstructural parameters of the steel in the case of cleavage fracture, also x_c depends on the microstructure of the steel.
For the present steels it appears that a relationship exists between ρ_{eff} and the prior austenitic grain size (steels 1 and 3) and the martensite packet size (steel 2). In this respect we might tentatively conclude that of the various microstructural features that can act as critical in the

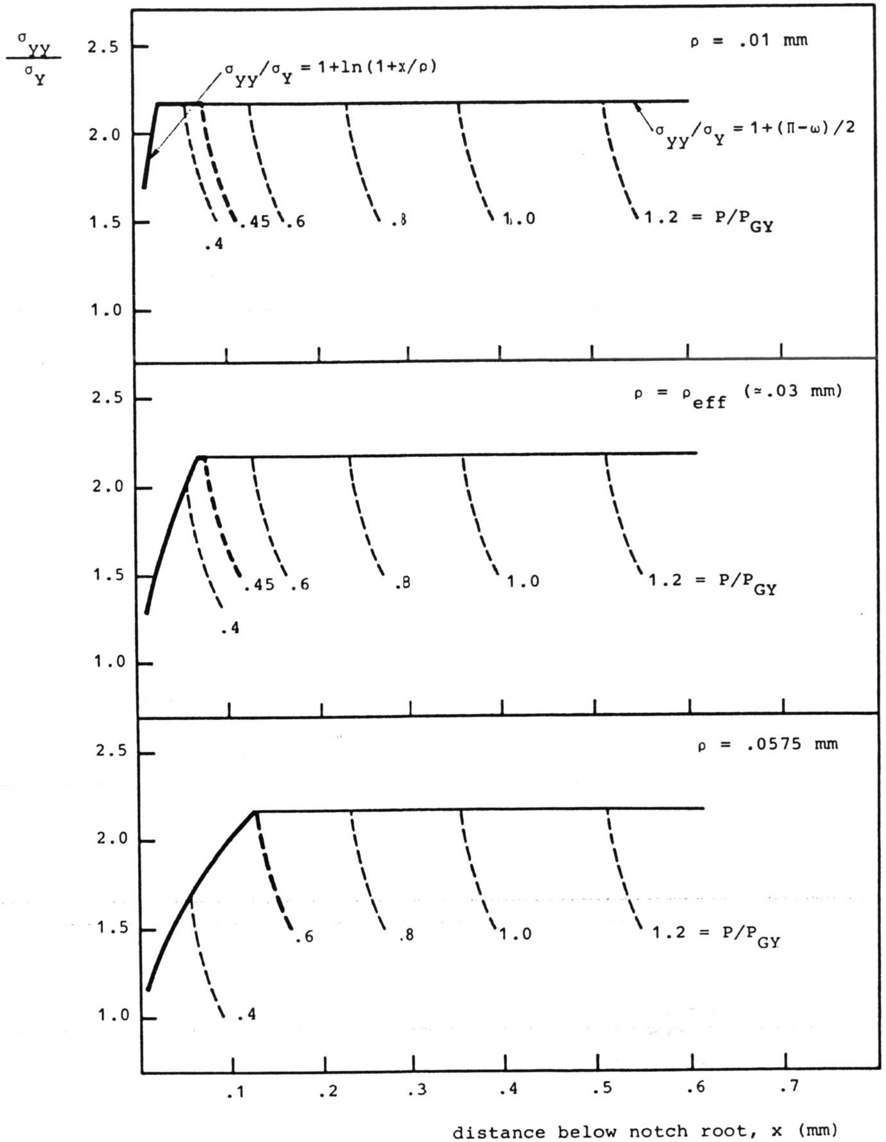

Fig. 2

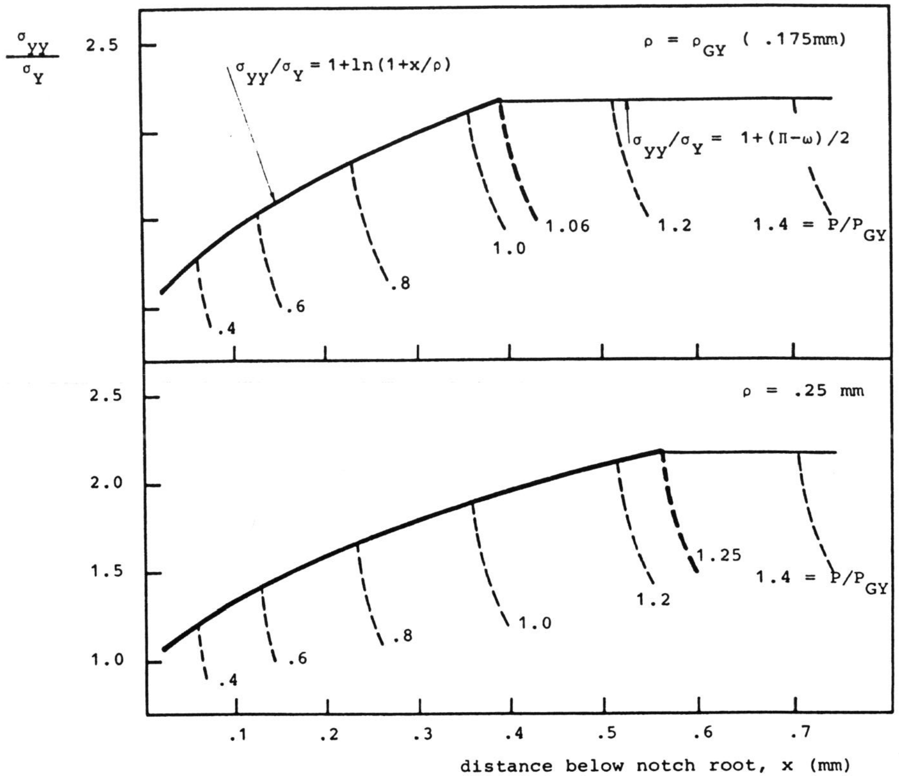

Fig. 2 - Distribution of stress σ_{yy} ahead of the notch tip for different notch root radii.

fracture process, very small ones (with sizes < 10 μm) are not important; on the other hand, very large ones do not reach the stage of being critical if smaller, intermediate size ones are present.

For the blunt notch specimens employed in the fracture tests (ρ = .0575 mm) fracture occurs at $P/P_{GY} \approx .6$, a value for which plasticity hasn't yet become extensive and Eq. 1 can be used to evaluate σ_f^*.
σ_f^* values of 2787.5, 2455.4 and 2860.9 MN/m² have been obtained for steel 1, 2 and 3 respectively.
From Fig. 2 it appears that at increasing notch root radii the load level at which fracture occurs increases, which leads to the conclusion that the condition of coincidence between general yield and fracture can be met also by varying the notch root radius.
It has to be noted that, on the other hand, if the notch root radius is too large, general yield is attained before σ_{yy} has reached σ_f^*; it is to be considered that in this case, for hardening materials, plasticity is too much extensive and the stress distribution cannot be any more described in terms of the slip line field theory.

CONCLUSIONS

Fracture tests performed on precracked and blunt notch specimens, and analysis of stress distributions ahead of a blunt notch carried out by means of the slip line field theory have shown that the notch root radius is a very important parameter in controlling the load level at which fracture occurs. Therefore, a very accurate preliminary choice of the notch root geometry is of importance when tests to assess the microscopic cleavage fracture stress are to be designed.

A metallographic investigation of the alloy microstructure is also necessary; in fact, both too small or too large notch root radii must be avoided. If notch radii smaller than ρ_{eff} are employed the analysis of the stress distribution ahead of the notch tip allows to determine σ_f^* by any means, while if the notch root is too large, even the coincidence of fracture and general yield may occur at stress intensifications at the notch root different in respect to the one forseen by Eq. 2, as expected in most of the current microscopic cleavage fracture stress measurements.

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