WIDTH INFLUENCE ON R-CURVES OBTAINED WITH THIN SHEET TEST-PIECES BEARING BLUNT CENTRAL NOTCHES

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ABSTRACT

Crack growth resistance curves obtained from 2 series of central notch AIS 4340 steel samples, quenched and tempered at 250°C, have been compared. series was characterized by a 76.2 mm width and a notch length/width ratio o 0.3, the other by a 25.4 mm width and a notch length/width ratio of 0.4 Notch root radius (ρ) was varied between 0 (fatigue pre-crack) and 0.8 mm Specimen thickness was 1.6 mm for both series. $K_{eff}-\Delta a_{eff}$ curves show tha the behavior of the two types of samples is similar, provided that the net se tion stress is somewhat smaller than the yield stress. Also, the $K_i-\sqrt{\rho}$ an $J_c-\rho$ series of data are clustered around the same interpolating lines as fa as $\sigma_n < h\cdot\sigma_y$. h has been seen to vary between 0.93 and 0.99, according to th specimen width, and can be conservatively set at 0.9 for standardizatio purposes.

KEYWORDS

Blunt central notch specimen; blunt notch R-curves - geometry dependance - condition of validity; blunt notch plastic zone size; effective crack growt from blunt notches; effective radius; $K_i-\sqrt{\rho}$ relationship; $J_c-\rho$ relationship.

INTRODUCTION

Up to now, no research program dealing with R-curves determination using blun notch thin sheet specimens has been referred to in the literature. In fact researchers who have dealt with plane stress ruptures have focussed their at- tention on the resistance to the onset and propagation of fracture from fa- tigue pre-crack or very severe stress concentrations; as a result of these studies ASTM Standard E561-81 has been developed, as well as proposals to mod- ify it (Wheeler and co-workers, 1982).

In the framework of a broad plan of experiments aimed to the AISI 4340 stee fracture resistance characterization under various heat treatment and loadin mode conditions (Firrao and De Benedetti,1977; Firrao and collegues,1979,1982 researches have been undertaken to verify the applicability of the above refe red standards also in experiments where ruptures originated from a blunt notc

The importance of performing reliable experiments with specimens bearing blunt notches instead of sharp cracks stems from the repeated observation (Chipperfield and Knott, 1975; Firrao and Roberti, 1984) that the combined use of blunt notch fracture toughness tests and automated second-phase particle distribution measurements can lead to well approximated assessments of sharp crack fracture toughness (K_{Ic} or J_{Ic}). Thus, the lengthy pre-cracking operations can be avoided especially in routine control procedures.

A previous article (Firrao and Ferrario, 1982) has described the first set of R-curve determinations with 76.2 mm wide tensile specimens obtained from 1.6 mm thick sheets of the above steel, quenched and tempered at 250°C. The central notch tension (CNT) samples beared slots characterized by an average length ($2a$) of 22.9 mm and end-radii (ρ) varying from nil (fatigue pre-crack) to 1.6 mm. It had been observed that $K_{eff}-\Delta a_{eff}$ R-curves (correlating effective stress intensity factors with effective half-crack length growths) allow to adequately describe the increase of plasticity at the notch tip and the subsequent crack advance stages.

When testing samples with ρ larger than 0.7 mm ca. it had been ascertained that the onset of crack growth occurred at a level of nominal stress at the reduced section (σ_n) very close or greater than the yield stress (σ_y). Thus, according to ASTM Standard E561-81, data obtained with these specimens cannot be taken as valid to determine plane stress fracture toughness values. On the other hand, Schwalbe and Setz (1981), who performed experiments on Al alloys CCT fatigue pre-cracked samples, had already observed that, at σ_n values > $0.9 \cdot \sigma_y$, K_{eff} is no longer a correlation parameter for crack growth. Furthermore, with ρ values in the 0.05-0.30 mm field, stress intensity factors applied at the onset of crack advance, K_i, and corresponding values of J-integral, J_c, varied linearly as a function of $\sqrt{\rho}$ the formers and of ρ the latters. In both cases the straight line interpolating the results passed through the origin.

All lateral ligament widths were larger than $25 \cdot J_c/\sigma_y$. Such a limitation is set in ASTM Standard 813-81 to ensure constrained plasticity in the direction normal to the principal stress in plane strain fracture toughness (J_{Ic}) determinations with CT or bending specimens. Thus, the plastic collapse of the ligaments is prevented (Turner, 1979).

From the above reported findings it was possible to state that, in the case of the steel there used, 76.2 mm wide central notch specimens with 26.5 wide ligaments allow to obtain valid both R-curves and fracture toughness values, provided that the notch-end radius is not too large.

It has now been decided to perform a further series of tests on 25.4 mm wide specimens with notch to width ratio, $2a/w$, equal to 0.4 to completely ascertain the validity of previously obtained results and to derive useful indications about the acceptable dimensions of CNT specimens to be employed in R-curve determinations; in agreement with what has been signalled before about the general yielding of ligaments when notch-end radii are too large, the maximum value of ρ has been kept at 0.8 mm ca.

EXPERIMENTAL PART

The geometry of employed specimens is shown in Fig. 1. They were fabricated from 1.6 mm thick steel sheets from the same stock from which larger samples had been obtained. Furthermore, they underwent a heat-treatment identical to that previously used by Firrao and Ferrario (1982), namely oil-quench from 870°C and 2h temper at 250°C . In the same reference, the specimen fabrication and test procedures have been detailed.

To evaluate the effective half-crack length, a_{eff} (physical half-crack length, a_{phys}, plus plastic zone dimension) secant lines were drawn from the origin of the experimental load-displacement diagrams to increasing load points, and their slopes measured to derive a_{eff} by the following formula proposed by Eftis and Liebowitz (1972):

$$\frac{E \cdot (2v)}{\sigma_G \cdot w} = 2 \cdot \left\{ \left(\frac{\Pi a}{w}\right) / \sin\left(\frac{\Pi a}{w}\right) \right\}^{1/2} \cdot \left\{ \frac{2w}{\Pi Y} \cosh^{-1}\left(\frac{\cosh\left(\frac{\Pi Y}{w}\right)}{\cos\left(\frac{\Pi a}{w}\right)} \right) - \frac{1+\nu}{\left[1 + \left(\frac{\sin\left(\frac{\Pi a}{w}\right)}{\sinh\left(\frac{\Pi Y}{w}\right)} \right)^2 \right]^{1/2}} + \nu \right\} \cdot Y/\nu \qquad (1)$$

E and ν are the Young's and Poisson's moduluses respectively; 2v is the central slot opening measured between 2 points at an initial distance 2Y (13 mm) one from the other; σ_G is the gross section stress.

No method encompassing first the determination of a_{phys}, and second, the plastic zone size correction by Irwin's formula (1960) could be employed to determine a_{eff} values, since it had already been seen that such a procedure yields erroneous results when notches have blunt ends.

Eq. 1 was also employed to derive a_{phys} values by the partial unloading compliance procedure already described by Firrao and Ferrario (1982).

Effective stress intensity factors, K_{eff}, have been calculated inserting a_{eff} values into the following formula, as suggested in ASTM Standard E561-81:

$$K = \left(\frac{P}{w\,B}\right) \cdot \sqrt{a} \cdot \left[1.77 - 0.177 \left(\frac{2a}{w}\right) + 1.77 \left(\frac{2a}{w}\right)^2 \right] \qquad (2)$$

Eq. 2 has been also used to compute J-integral values; in fact, K values obtained inserting into it a_{phys} have been subsequently used in the following equation (Landes, Walker, and Clarke, 1979):

$$J = K^2/E + A/\left[B \cdot (w/2 - a)\right] \qquad (3)$$

where A is the area between the load displacement curve and the secant line drawn from the origin to the particular point to which a given value of the load and of a_{phys} is associated.

RESULTS AND DISCUSSION

Tensile and hardness properties of AISI 4340 steel, quenched and tempered at 250°C , are: σ_{uts} = 1780 and σ_y = 1430 MPa; e_f = 7%; HRC = 49.

$K_{eff}-\Delta a_{eff}$ curves related to samples with different notch-end radii have been reported in Fig. 2, where arrows indicate the stress intensity factor level applied when the nominal stress at the reduced section reaches $0.9 \cdot \sigma_y$.

The plotted lines indicate that the behavior of 25.4 mm sample is similar to that already reported Firrao and Ferrario (1982) for the 76.2 mm wide ones: K_{eff} values at which Δa_{eff} becomes significant increase as the notch root radius increases. Such a phenomenon can be rationalized on the basis of plastic deformation beginning at progressively lower applied load levels as the notch-end radius decreases. Furthermore, different curves do not overlap in the Δa_{eff} region where only plastic deformation occurs. What can be taken as a further proof that, with blunt notches, the plastic zone size does not depend only on the applied stress intensity factor, but also on the root radius so that, at constant K_{eff}, it decreases as the notch is progressively blunter.

The R-curve pertaining to the sample with $\rho \to 0$ and the ones related to specimens with notch-end radius of 0.07 mm ca. almost coincide. What once more indicates that there is a limiting value of ρ below which a mechanical notch behaves like a fatigue crack (Chipperfield and Knott, 1975; Firrao and co-workers, 1979, 1982; Firrao and Ferrario, 1982; Firrao and Roberti, 1984).

A point-to-point comparison between R-curves obtained with samples of different widths was not possible at all values of notch root radii since the electrical discharge machining of notches did not allow to obtain identical values of ρ in the case of the sharpest ones. Fig. 3 illustrates such a comparison at two different ρ levels.

It can be seen that, when ρ is very small, experimental points pertaining to specimens of different widths lie on a unique R-curve. When the notch root radius increases, 25.4 mm wide test-pieces enter a region close to general yielding at lower levels of Δa_{eff} (note the different position of arrows signalling that σ_n reaches $0.9 \cdot \sigma_y$). From there on R-curves depart one from the other.

To identify the values of the stress intensity factor applied at fracture nucleation from notches, K_i, it is useful to consider $\sigma_G - 2a_{phys}$ diagrams

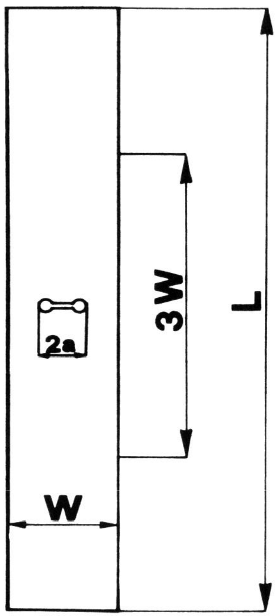

Fig. 1. Test-piece geometry; w = 25.4 mm, $2a/w$ = 0.4, L = $6w$. Notch orientation: LT.

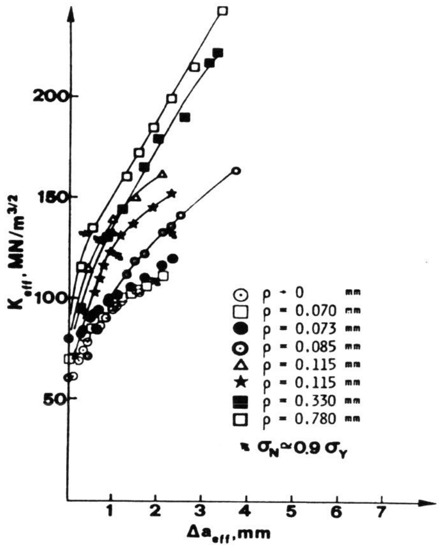

Fig. 2. Variation of effective stress intensity factors with the effective half-crack growth.

constructed in Fig. 4 following Feddersen's (1971) recommendations. It can be seen that the onset of crack growth occurs at applied K values very similar (≈80 MPa√m) in the case of the pre-cracked specimen or of samples with ρ in the vicinity of 0.07 mm, even if the initial crack length was different. It is therefore confirmed that there is an effective notch root radius, ρ_{eff}, below which all notched or pre-cracked test-pieces display the same behavior as regarding fracture nucleation.

It is also possible to note that, with samples with ρ > 0.3 mm, points associated with crack growths are considerably above the straight line (indicated with b) that indicates the gross section stress variation when, as the crack advances, the net section stress reaches σ_y. Therefore, it can be said that fracture nucleation in these specimens occurred after a general yielding situation was reached in the reduced section. Furthermore, experimental points lying above the straight line indicated with a can be seen. Since such a line corresponds to the net section stress reaching the ultimate tensile strength of the material, a notch strengthening effect at relatively large values of ρ is substantiated.

K_i values singled out by means of diagrams in Fig. 4 have been plotted in Fig. 5 as a function of the squared root of ρ. In the same figure, the solid line is the one interpolating 76.2 mm wide specimens results (Firrao and Ferrario, 1982). It is evident that, as far as ρ is below 0.115 mm, small sample data agree with larger test-piece results. In particular, K_i values obtained with specimens characterized by ρ values between 0.07 and 0.115 mm belong to the same statistical population as the wider ones which allowed to draw the sloped segment extrapolating to the origin.

Instead, with w = 25.4 mm and ρ equal to 0.33 or 0.78 mm, K_i values are marked

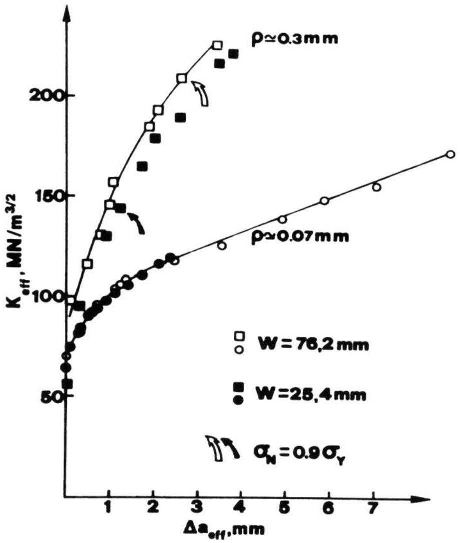

Fig. 3. Comparison of R-curves pertaining to samples with different width and similar notch root.

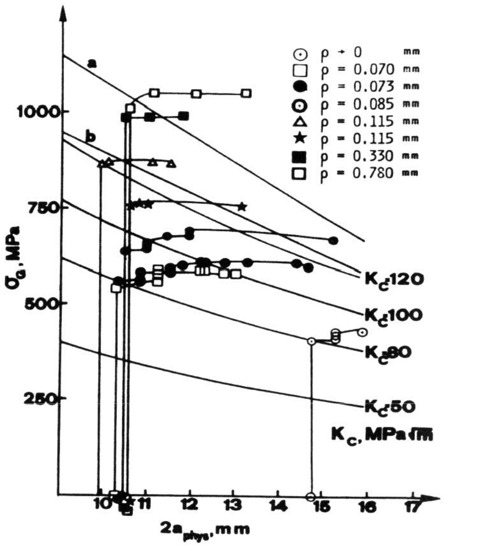

Fig. 4. Variation of gross section nominal stress as a function of the physical crack length.

samples. The provision that at individual points of the $J-\Delta a_{phys}$ diagrams both a and b be larger than $15 \cdot J_Q/\sigma_y$ was also satisfied. It can thus be concluded that the validity of the tests has to be ascertained on the basis of more restrictive limitations based on the maximum value of σ_n.

Values of J_c have been reported in Fig. 7 as a function of ρ. There, the solid line represents again the interpolation of data pertaining to 76.2 mm wide specimens.

The plot verifies that present results yielded by specimens with $\rho \leq 0.115$ mm agree with those determined with wider samples. Instead, no concordance arises when fracture nucleation occurs in a completely plasticized net section as is the case of test-pieces with $\rho = 0.33$ or 0.78 mm.

The J_c values that, for both types of specimens, are clustered around a straight line passing through the origin of the $J-\rho$ field have been reported in Fig. 8 and the equation of the line calculated as $J_c = 0.928 \cdot \rho - 0.02$. Since such a line has a very small intercept and was obtained with data pertaining to specimens of different width it is possible to state that it is of the $J_c = A' \cdot \rho$ type already verified in previous researches (Firrao and co-workers, 1982; Roberti and collegues, 1981), with A' being again a material constant for the steel thickness and the metallurgical condition employed; together with the horizontal segment drawn at the J_c level averaging fatigue pre-cracked test-pieces data, it allows to compute a ρ_{eff} value of 0.05 mm ca., below which mechanical notches can be a safe, suitable alternative to fatigue cracks.

As regards, the conditions of validity of fracture toughness results, it has been seen that with 25.4 mm wide samples K_i and J_c values are acceptable

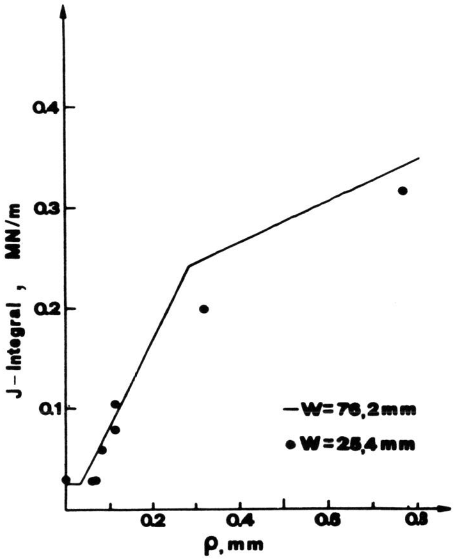

Fig. 7. Critical values of J-integrals applied at fracture in blunt

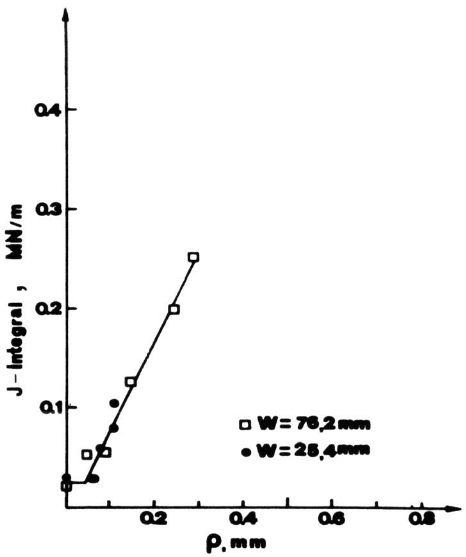

Fig. 8. Overall plot of valid J_c values pertaining to differ-

ly lower than those pertaining to wider test-pieces with similar notch-e radii. The above leads to state that, when σ_n passes σ_y, neither values are in a univocal correlation with the squared root of ρ, nor follow $K_i = A \cdot \sqrt{\rho}$ relationship. Instead, when σ_n is below σ_y, K_i values not depend on the overall sample geometry, but only on the end curvature the stress concentration, with A in the above formula thus being a materi parameter, at least for a given thickness.

$J-\Delta a_{phys}$ diagrams determined with various samples are reported in Fig. As already emphasized previously (Firrao and Ferrario, 1982; Roberti a co-workers, 1981), when testing blunt notch specimens there is no stage of f titious crack advance along the blunting line (ASTM Standard E813-81); thu critical values of applied J-integral, J_c, are to be identified at the inte section of the J-R curve and the ordinate axis. It has to be noted that, the sake of simplicity, at $\Delta a_{phys} = 0$, only the largest J points have be reported for each specimen in the figure. It can be also seen once more th J-R curves pertaining to samples with $\rho < 0.073$ mm are very close to each ot and point to a unique value of the J-integral applied at onset of fracture.

The slope of lines in Fig. 6 is practically constant as long as ρ does rea 0.115 mm and then tends to increase in the case of the two bluntest notch sa ples which displayed crack nucleation and growth in a completely plasticiz net section.

It is here to be noted that the condition that both the half crack leng a, and the ligament width, b, be larger than $25 \cdot J_c/\sigma_y$ was met for all the

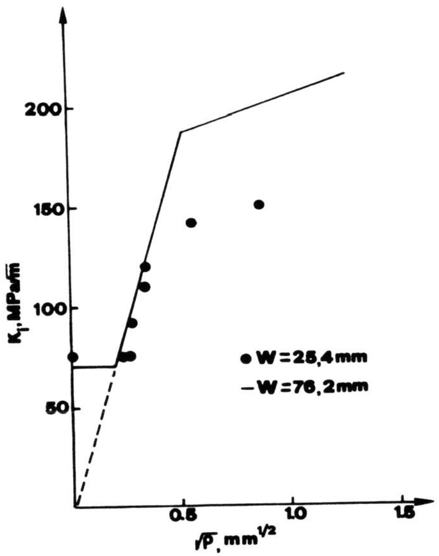

Fig. 5. Stress intensity factors applied at onset of fracture in notched samples.

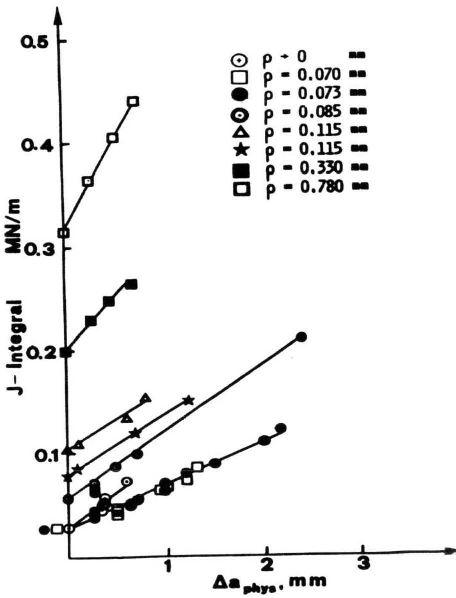

Fig. 6. Variation of applied J-integrals as a function of physical half crack growth.

as long as σ_n is almost equal to σ_y; in the case of 76.2 mm wide specimens, the limit is lowered at 0.93 σ_y. With a conservative approach to the problem and taking into account the whole series of data here discussed, it can be concluded that the limit of 0.9σ_y, set forth by Wheeler and co-workers (1982) for the validity of Al alloy sharp crack tension R-curve , can be applied also to blunt notch high-strength steel specimens.

CONCLUSIONS

R-curves measured in terms of either effective stress intensity factors or J-integrals applied to 1.6 mm thick, quenched and tempered AISI 4340 steel specimens, with central blunt notches, have been found to be independent of test-piece width as long as the net section nominal stress does not go beyond $h\cdot\sigma_y$; h can be conservatively set at 0.9.

In the field of validity of results, the existence of relationships of the $K_i = A\cdot\sqrt{\rho}$ and $J_c = A'\cdot\rho$ type has been confirmed. Since A and A' have been determined as being independent of specimen width, they can be considered as being material characteristics for the steel here used, at the here employed thickness.

Finally, the existence of a minimum value of the notch-end radius below which a blunt notch behaves like a fatigue pre-crack has been ascertained.

REFERENCES

Chipperfield, G. C., and J. F. Knott (1975). Microstructure and Toughness of Structural Steels. Metals Technol., 2, 45-51.

Eftis, J. and H. Liebowitz (1972). On the Modified Westergaard Equation for Certain Plane Crack Problems. Int. J. Fract. Mech., 4, 383-392.

Feddersen, C. E. (1971).Evaluation and Prediction of the Residual Strength of Center Cracked Tension Panels. In ASTM STP 486, ASTM, pp.16-38.

Firrao, D., and B. De Benedetti (1977). Influenza della Posizione e della Acutezza dell'Intaglio sulla Resistenza a Trazione di Provini Piatti di Acciaio da Bonifica al Ni-Cr-Mo. In Atti V Conv. Naz. AIAS, Bari, Italy 29/9-1/10/1977, pp. 36.1-36.18.

Firrao, D., G. Silva, R. Roberti, B. De Benedetti, and J. A. Begley (1979). The Blunt Notch Fracture Toughness of As-Quenched AISI 4340 Steel. In VDI-Z Fortschr.-Berichte, series 18, no. 6, pp. 157-169.

Firrao, D., J.A. Begley, G. Silva, R. Roberti, and B. De Benedetti (1982). Metall. Trans. A, 13A, 1003-1014.

Firrao, D., and R. Ferrario (1982). Metall. Ital., 74, 508-518, 565.

Firrao, D., and R. Roberti (1984). Interrelation among Microstructure, Crack-Tip Blunting and Ductile Fracture Toughness in Mild Steels. In ICF6 Proc.

Irwin, G. R. (1960).Plastic Zone Near a Crack and Fracture Toughness. In Proc. 7th Sagamore Ordnance Materials Conf., Syracuse Univ. Res. Inst., pp. 4-63.

Landes, J. D., N. Walker, and G. A. Clarke (1979). Evaluation of Estimation Procedures Used in J-Integral Testing. In ASTM STP 668, ASTM, pp 266-287.

Roberti, R., G. Silva, D. Firrao, and B. De Benedetti (1981). Int. J. Fatigue, 3, 133-141.

Schwalbe, K. H., and W. Setz (1981). J. Testing Eval. 9, 182-194.

Turner, C. E. (1981). Methods for Post-Yield Fracture Safety Assessment. In Post Yield Fracture Mechanics, Applied Science, London, pp. 23-210.

Wheeler, C., J. N. Eastabrook, D.P. Rook, K.-H. Schwalbe, W. Setz, and A. U. De Koning (1982). Recommendations for the Measurement of R-Curves Using Centre-Cracked Panels. J. Strain Anal., 17, 205-213.