Post - Yield Fracture Mechanics and its Application in Turbo - Generator Problems

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(1) INTRODUCTION

The severity of defects in large steam turbine forgings has to be assessed from non-destructive testing, and without knowledge to the contrary it is assumed that all defects are brittle and behave as cracks and acceptance standards are based on Charpy/K_{IC} correlations and the AVG sizing system. However, it is necessary to design for the avoidance of brittle fracture at any conceivable over-speed (e.g. 50%) and hence to consider fracture well beyond yield. For the past three years a simple post-yield fracture mechanics analysis has been used based on the results of some large tensile tests containing defects.(1) The analysis is related to others currently being investigated and has been used for the analysis of surface cracked specimens.

(2) POST-YIELD ANALYSIS

For a crack that is small compared with other dimensions under a remote normal tensile stress this analysis proposes that the apparent stress intensity at fracture K_Q (based on true stress) is a function of K_{IC} and the secant modulus E_{SQ} associated with the true stress at fracture.

$$K_Q = K_{IC} \left\{ \rho \frac{E_{SQ}}{E} \right\}^{\frac{1}{2}} \qquad \ldots \ldots (1)$$

where ρ represents the degree of constraint at the crack tip and is assumed to equal $^3/M$ where $M\sigma_Q$ is the raise local yield stress ($M = 3$ approx and $\rho = 1$ for plane strain). This equation can be derived in a number of semi-analytic ways if it is assumed that fracture is governed by conditions around the crack tip irrespective of whether conclusions elsewhere are elastic or plastic.

C.O.D. analysis

For non-work hardening materials assuming a critical C.O.D. at fracture, equation (1) is derived if it is assumed that the C.O.D. δ, is given by $\delta = \varepsilon_Q \pi a/p$ (where ε_Q is the remote strain).

Elasto-plastic stress analysis

Photo-elastic tests by Dixon$^{(2)}$ show that the equivalent stress in the plastic zone $\bar{\sigma}_r = \bar{\sigma}_{or} \left\{ \frac{Esr}{E} \right\}^{\frac{1}{2}}$, where Esr is the local secant modulus, and $\bar{\sigma}_{or}$ the local stress given by elastic analysis. If it is assumed that, as in the Stowell$^{(3)}$ and Hardrath and Ohman$^{(4)}$ treatment, that E may be replaced by the remote Es for gross plastic conditions equation (1) is obtained if fracture is governed by a local stress criterion.

Energy balance analysis

Using Andrews$^{(5)}$ interpretation of fracture tests on rubber,$^{(6)}$ equation (1) can also be derived from a simple non-work hardening analysis using the "strain energy release rate", G, and the remote strain energy density W; $G = 2kWa$, where k depends on strain and $\pi > k > 2$.

J-contour integral

If it is assumed that $J = J_{IC}$ ($= K_{IC}^2/E$ for elastic fracture) at fracture equation (1) gives $J_{IC} = \sigma_Q c \varepsilon_Q c \pi a/p$ which implies for a non-work hardening material and overall displacement Δ over a large gauge length L that $J = \frac{M\sigma y}{3} \frac{\Delta}{L} . \pi a$, and although similar does not correspond to the definition $J = -dP/da$, where P is the limit load.

(3) SURFACE CRACKED SPECIMENS

There is a great interest in surface cracked specimens because they reproduce a type of defect commonly found in service, but their analysis particularly in the post-yield regime has been found to be difficult. It has therefore been attempted to extend the use of equation (1) in the analysis of some post-yield surface cracked specimens, 1" x 2" section, of a 1 CMV steel $\left[K_{IC} = 50 \text{ k.s.i.}\sqrt{\text{in}}, 0.2\% \text{ PS} = 83 \text{ k.s.i.}\right]$. For this purpose it was necessary to assume a relationship between ρ or M and effective crack radius a^* and K_Q. It was therefore assumed that when plane strain conditions prevail $\left[\beta = a^* \left[\sigma y/K_{IC}\right]^2 > 2.5\right]$, M = 3; that when the minimum ligament (W-a) stress is about equal to yield, M = 2 [from Levy etal (7)]; and that at $\beta \rightarrow o$ M = 1. Although not strictly correct it is convenient to plot M against $\beta = a^*. \left[(1-a/_w) \sigma y/K_{IC}\right]^2$ as in Fig. 1, and using this curve and equation (1) the relationships between β and K_Q are readily obtained from the true stress/strain curve of the material. Fig. 1 shows the theoretical curves obtained in this way and the data points obtained. In fact the shape of these curves is insensitive to M in the region tested; the shape of the σ/ε curve being predominate. Fig.2 shows the same results and theoretical curves for true stress and strain at fracture.

(4) APPLICATION

For the purpose of design applications it is best at present to assume that $\rho = 1$. Equation 1 is very convenient for the analysis of a bored disc because the stresses are similar to those in a bi-axially stressed plate with a hole and using Nueber ($K_t^2 = K_\sigma - K_\varepsilon$), $\sigma_Q \varepsilon_Q E = \sigma o^2$ (σo = theoretical elastic stress, which is proportional to the square of speed, N). Hence from equation 1 the critical crack size is proportional to $(K_{IC}/N^2)^2$.

(5) REFERENCES

1 G.T. Jones Post-yield fracture mechanics GEC Power Engg. TP/R 12,078

2 J.R. Dixon N.E.L. Report No. 141 March 1964.

3 E.Z. Stowell U.S. Nat.Advis. Cttee Aeronautics Tech. Note (2073) 1950

4 H.F. Hardrath and L. Ohman ibid (1117) 1953

5 E.H. Andrews 2nd Int.Conf. on Fracture Brighton, paper 75 1969

6 G.J. Lake etal ibid Paper 43

7 N.Levy etal Nuclear Engg & Design 17 (1971) 64-75

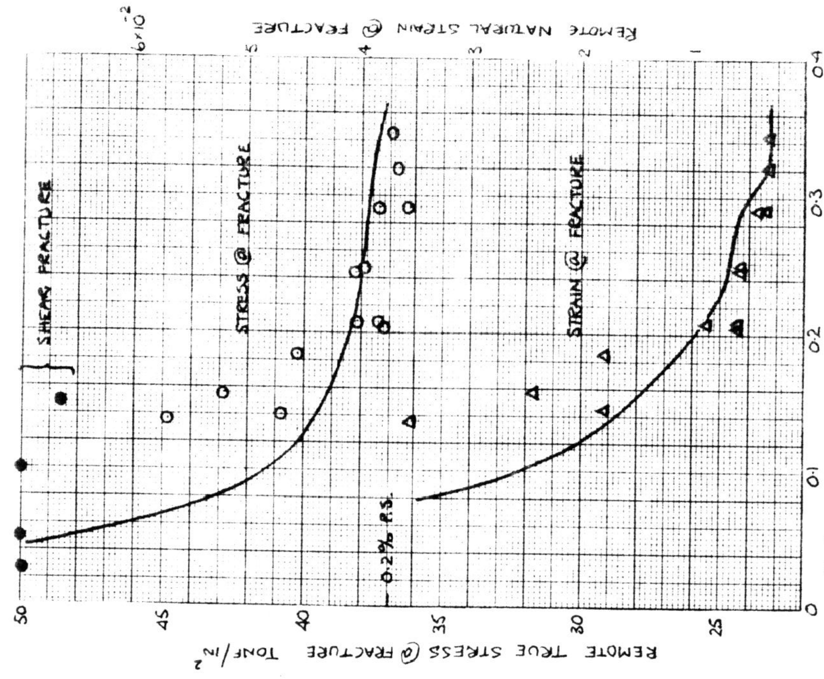

FIG 2

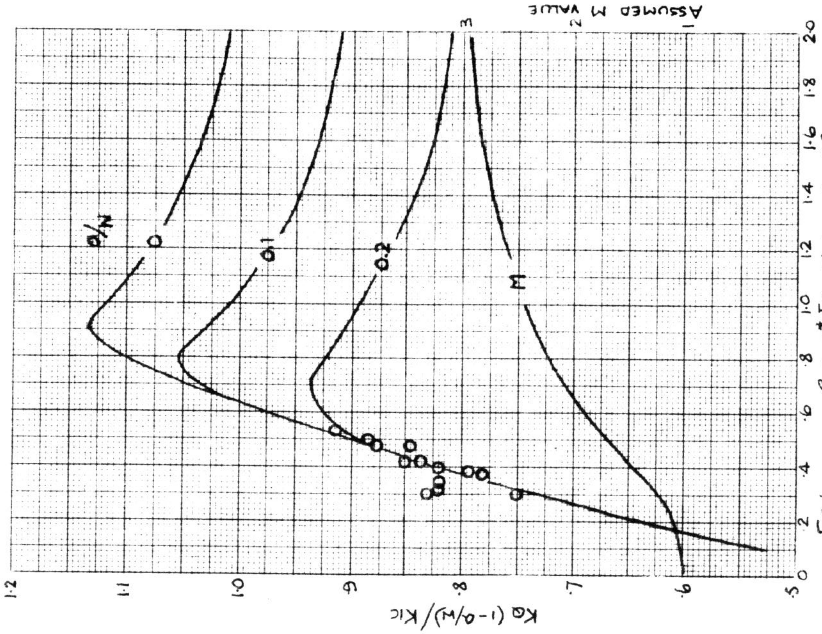

FIG 1