FAILURE ASSESSMENT OF WIDE PLATES USING ELASTIC-PLASTIC FRACTURE MECHANICS

D. Dormagen+ und W. Dahl+

This paper describes the influence of deformation and fracture behaviour on the failure assessment of wide plates using the COD-Design-curve and the J-estimation scheme. The influence of the flow behaviour and the state of stress is discussed in the transition region from ductile tearing to cleavage fracture and in the fully ductile regime.

INTRODUCTION

In the field of failure assessment of structures elastic-plastic fracture mechanics is widely used to predict the failure behaviour being characterized either by plastic instability or by tearing instability. When bcc metal alloys are used there is also request for a fully ductile behaviour of the structure as cleavage fracture can also be observed after general yielding conditions. In this context wide plate tests are an adequate tool to investigate the failure and fracture behaviour being influenced by the state of stress, temperature and loading rate. Thus not only the transferability of test results with small scale specimens to structures but also the weakness of existing concepts may be exposed and thus improved, if possible.

This paper deals with the application of the COD-Design-curve and the J-estimation scheme for the predictions of failure loads of wide plates.

THE COD-DESIGN-CURVE

Based upon the Dugdale-Model and experimental results of wide plates Burdekin and Stone (1) developed a design-curve which correlates the normalized COD-value with the stress or the strain ratio. The Design-curve is given by:

$$\phi = \frac{\sigma}{2\pi \cdot a \cdot \varepsilon_y} = \left(\frac{\varepsilon}{\varepsilon_y}\right)^2 \qquad \text{for } \frac{\varepsilon}{\varepsilon_y} \leq 0.5 \qquad (1)$$

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Institute of Ferrous Metallurgy, Technical University Aachen, West Germany

$$\Phi = \frac{\delta}{2\pi \cdot a \cdot \varepsilon_y} = \frac{\varepsilon}{\varepsilon_y} - 0.25 \qquad \text{for } \frac{\varepsilon}{\varepsilon_y} \geq 0.5 \tag{2}$$

where a is the crack length and ε_y is the yield strain.

Maximum load COD-values derived from CT- or SENB-specimens having the same thickness as the structure are used to determine the tolerable flaw size of a loaded component. In fig. 1 the evaluation of wide plates with the COD-Design-curve is shown for four different steels, comparing the results of CT- and SENB-specimens. Concerning the wide plates only specimens failed after gross section yielding were used. The results show that the Design-curve is not always conservative when SENB-specimens are used. Although the determination of COD-values according to BS 5762 is attuned to the SENB-type, the standard is sometimes vague e.g. in the definition of characteristic values. Thus a clarification is necessary.

Moreover, maximum load values strongly depend on specimen type and geometry. If small scale test results are transfered to components there is need for geometry independent values and/or conservative values as long as the stress state of component and specimen differs.

Using initiation instead of maximum load values leads to conservative predictions, even if the wide plate failure is characterized by net section yielding, only. This is demonstrated in fig. 2. When the Design-curve is varified by wide plates only failure conditions like contained yielding or gross section yielding at fracture are allowed according to Dawes (2). But in CCP-specimens with a crack length ratio of 2a/2W < 0.1 first net section yielding occurs before the gross section is deformed plastically. After Rosezin (3) only in specimens with very short cracks (2a/2W < 0.01) plastic deformation of the gross section started just after reaching the yield stress in the net section.

If gross section yielding occurs, the severity of the stress state is reduced as shown in fig. 3. In the left part, the stresses and in the right part the strain, the COD and the amount of stable crack growth is plotted as a function of a/W-ratio. When the fracture of the CCP occurs under net section yielding the toughness of the material is low as shown by the small values of strain to fracture at a/W $\geq$ 0.05. Thus, it is very important to assure either a more severe or at least the same state of stress in the small and large scale test when the failure shall be predicted in a conservative manner.

THE J-ESTIMATION SCHEME

If the material behaviour is fully ductile the critical failure loads for tearing instability should be calculated after Paris et al (4) by comparing the material's J_R-curve to the J-integral crack driving force obtained from FE-calculations or from an estimation procedure. Instability is defined when

$$\left.\frac{dJ}{da}\right|_{Appl} \geq \left.\frac{dJ}{da}\right|_{Mat} \tag{3}$$

where a is the crack length.

The estimation procedure after Shih et al (5) combines the elastic (Tada et al (6)) and the fully plastic solutions to calculate the J-Integral crack driving force for various laboratory specimens.

The J-analysis in the elastic-plastic regime suffers from a displacement which is of course not present in LEFM; J does not only depend on the applied load and specimen configuration, but also on the flow characteristics of the material, expressed by the yield stress and the strain hardening behaviour. In the estimation analysis the material is assumed to be governed by the Ramberg-Osgood relation

$$\frac{\varepsilon}{\varepsilon_o} = \frac{\sigma}{\sigma_o} + \alpha \left(\frac{\sigma}{\sigma_o}\right)^N \tag{4}$$

where σ_o, α, N are the material constants, σ_o is the effective yield stress, $\varepsilon_o = \sigma_o/E$ and N is the strain hardening exponent.

The fully plastic solutions were derived from FE calculations with constant $\sigma_o/E = 0.02$ (σ_o = 420 MPa) α is taken to be 3/7 and the strain hardening exponent was varied over a practical range for structural metals. In fig. 4 it is demonstrated that this assumed material behaviour considerably deviates from those of existing bcc metals, although the strain hardening exponent varies in the same manner. As pointed out by Hesse and Dahl (7) the strain hardening exponent does not describe the strain hardening behaviour of bcc metals. The variation in N depends on the yield stress level, whereas the true strain hardening $d\sigma/d\varepsilon$ is very similar for all bcc steels.

The influence of this misinterpretation of the strain hardening behaviour is demonstrated in fig. 5. The comparison of applied J as a function of P/P_o (P is the applied load and P_o is the plastic limit load) evaluated by FE-calculations and the estimation scheme, both using the same stress strain data, results in a deviation of predicted J-values of more than 20 % for σ_o = 820 MPa.

In many cases the exact state of stress of a specimen configuration is unknown and is generally situated between plane strain and plane stress. But only the boundary situations can be calculated in the estimation scheme. This is demonstrated in fig. 6 a. Thus if the stress state is not known exactly one should prefer the prediction under plane stress conditions.

The application of the estimation scheme to wide plates is shown in fig. 6 b. The ratio of calculated and measured failure load F_{cal}/F_{meas} is smaller than 1 independent of the approximation of the stress strain curve only when the material's J_R-curve is derived from side-grooved specimens. This emphasizes the important role of state of stress in the field of failure assessument.

Due to the restrictions of J-controlled crack growth which allow only a limited amount of stable crack advance

$$\Delta a_{max} \leq 0.06\ b = 0.06(W - a_o) \qquad (5)$$

it is sometimes not possible to measure J_R-curves in the entire range of ductile tearing for a given plate thickness. Than an extrapolation technique has to be used up to several milimeters. This can of course lead to uncertainties in the determination of J_{crit}, so instability loads can be overestimated.

This is demonstrated in fig. 7, where calculated and measured maximum loads are compared for steel A. The J_R-curve was derived from non-side grooved CT-specimens and the calculation was done in plane stress. The calculated loads agree within ± 10 % with the experimentally determined values, but there are also some non-conservative results. Examination of fracture surfaces demonstrates afterwards that the wide plate specimens were not J-controlled due to the fact that the ligament was too small.

In bcc metals a transition from stable crack growth to cleavage fracture is observed even in a regime where the plastic limit load is reached. Therefore it is necessary to know the influence of stress state, temperature and strain rate on the fracture behaviour. In fig. 8 the influence of stress state on the fracture behaviour of wide plates is demonstrated. For CCP- and DENT-specimens the stresses and strains are plotted as a function of temperature. Four characteristic regions can be defined. Below T_{gy} the fracture stress is smaller than σ_y, between T_{gy} and T_i the yield stress is reached in the net section, above T_i stable crack growth precedes cleavage fracture and above T_a the fracture is characterized by stable crack growth only.

Comparing the CCP- and DENT-specimen a shift of the characteristic temperatures to higher values is observed for the DENT-specimen. A shift of 20 K is observed concerning T_i (T_i = 261 K) and at room temperature there is still a transition from ductile tearing to cleavage fracture. As the beginning of stable crack growth in CT-specimens was observed at T = 220 K, the application of the estimation scheme is not possible because the small scale specimens behave fully ductile whereas a transition from ductile to cleavage fracture is observed in large scale specimens even at room temperature.

These differences in transition behaviour of small scale and large scale specimens can of course result in non-conservative predictions, when the COD-Design-curve is used. At T = 250 K a $CTOD_{max}$-value of 1.2 mm was determined for CT-13 specimens, whereas a COD-value of 0.31 mm was observed in 30 mm thick DENT-specimens.

CONCLUSIONS

In the field of failure assessment wide plate testing is an adequate tool to investigate the failure and fracture behaviour. For the transferability of small scale results it is very important to know the influence of stress state on the deformation and fracture behaviour, especially in the transition region from ductile to cleavage fracture. Thus the concepts of failure assessment can be checked and, if possible, improved. If the influence of the stress state is not well-known the additional testing of wide plates can give insight in the behaviour of the structure so that non-conservative predictions are avoided.

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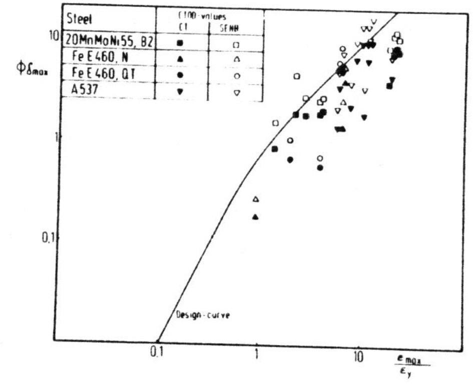

Fig. 1: Evaluation of wide plates with the COD-Design curve using CT- and SENB-specimens

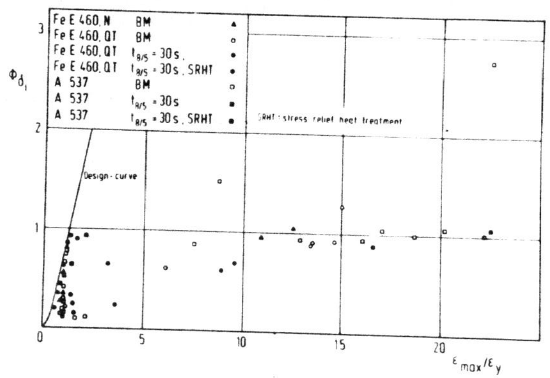

Fig. 2: Evaluation of wide plates with the COD-Design curve using initiation-instead of maximum load COD values[3]

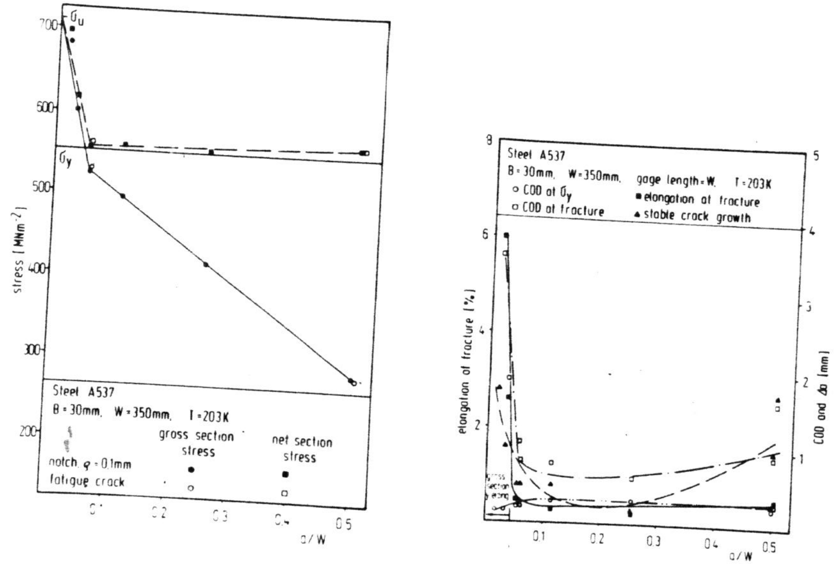

Fig. 3: Net-section and gross-section stress, strain, COD and stable crack growth as a function of a/W ratio for the steel A 537[3]

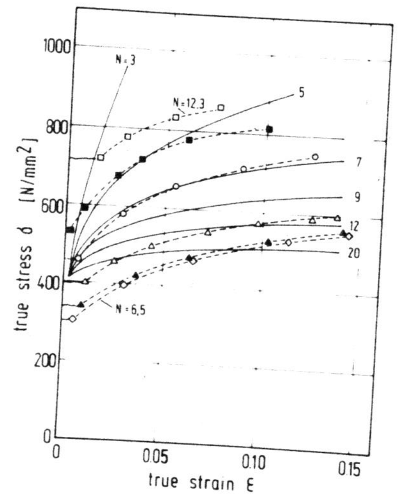

Fig. 4: True stress as a function of true strain
a) --- for many bcc steels (6.5 $\leq$ N $\leq$ 12.5)
b) —— according to Ramberg-Osgood for σ_0 = 420 MPa, α = 1.0, 3 $\leq$ N $\leq$ 20[3]

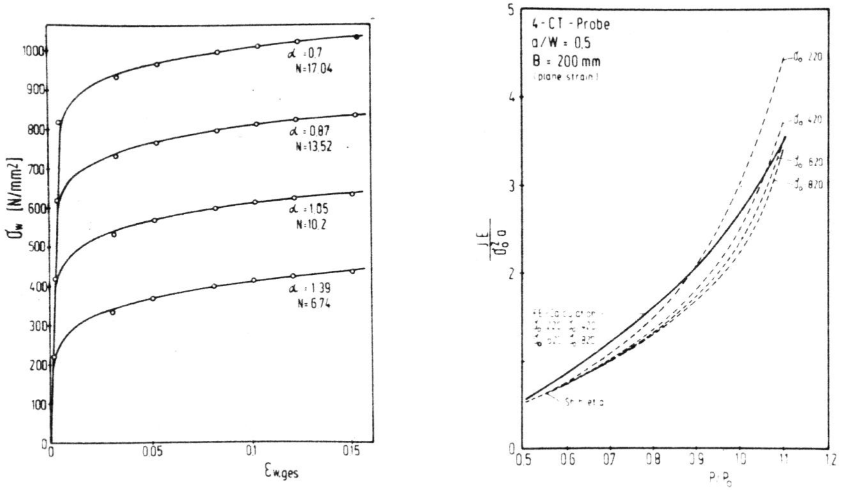

Fig. 5: J-Integral crack driving force as a function of P/P_O for theoretical material behaviour with the same strain hardening at different yield strengths[3]

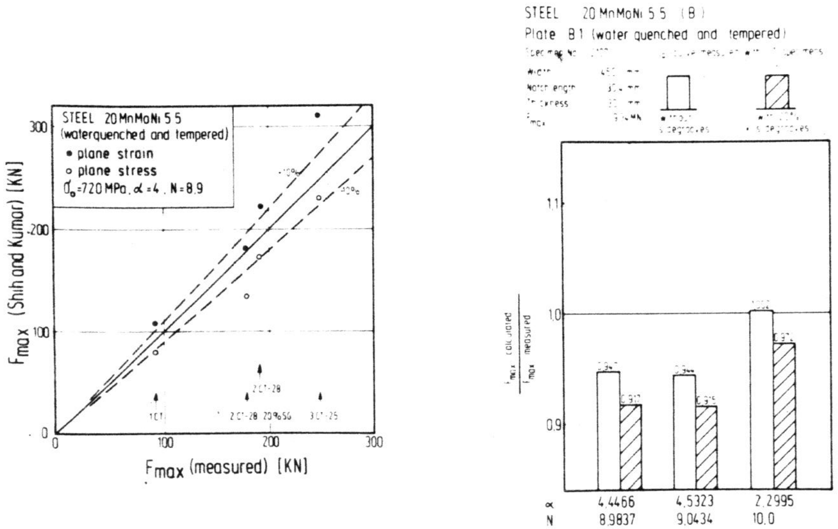

Fig. 6: Influence of stress state on the failure predictions of CT and CCP-specimens[3]

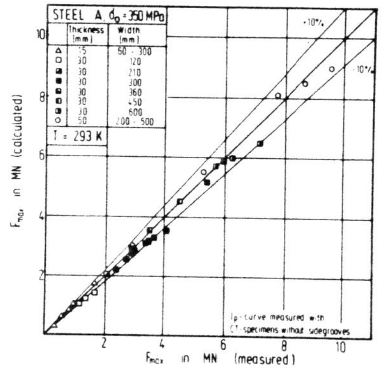

Fig. 7: Evaluation of failure loads of wide plates using the J-estimation scheme[8]

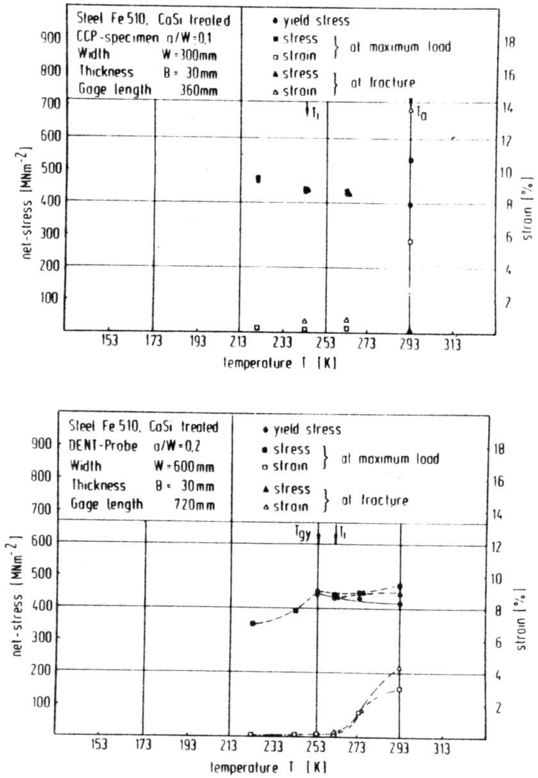

Fig. 8: Comparison of fracture behaviour between CCP- and DENT-wide plates