THE INFLUENCE OF SPECIMEN GEOMETRY ON FRACTURE OF UNWELDED AND WELDED STEEL SPECIMENS: COMPARISON OF EXPERIMENTAL RESULTS WITH FEM-CALCULATION

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ABSTRACT

The experimentally determined plastic stress concentration factors $K_{\sigma p}$ and constraint factors L of unwelded and welded specimens with various notch forms and specimen widths are compared with the values given by the slip-line field theory and with the values obtained from the FEM-calculation.

NOMENCLATURE

a	notch depth	σ_{gy}	general yield stress
a_s	length of the stable crack growth	σ_{ly}	lower yield stress for slip induced yielding
B	test-piece breadth		
F	load	σ_{ty}	yield point at onset of jerky flow
F_f	fracture load	σ_{yy}	normal stress in y-direction
$K_{\sigma p}$	plastic stress concentration factor	ρ	notch root
k	yield stress in pure sheare	δ_p	plastic crack opening displacement
L	constraint factor	$\dot{\varepsilon}$	strain rate
ΔL	elongation	BM	base metal
Q	heat input	WJ	welding joint
S	loading span=4W	WM	weld metal
T_{gy}	temperature at which fracture is coincident with general yield	HAZO.3	heat affected zone notch root to fusion line 0.3 mm
T_s	fibrous/cleavage transition temperature	FL	fusion line
		gy	general yield
W	test-piece width	‖	notch parallel to direction of rolling and sheet surface
X	distance below notch tip		
ω	notch angle	⊥	notch parallel to direction of rolling and perpendicular to the sheet surface
σ_f	fracture stress		
σ_{fc}	cleavage fracture strength		
		f	fracture

INTRODUCTION

Cleavage crack instability is governed by a normal stress criterion (Davidenkov, 1937; Orowan 1948; Knott 1966 and Wilshaw, Rau and Tetelmann, 1968). Cleavage cracks, however, can only be nucleated by slipping or twinning in a region of high stress elevation (Cottrell, 1958; Stroh, 1955, 1957; Oates, 1968, 1969). Therefore, plastic deformation is a necessary pre-requisite for cleavage fracture. For this reason, ahead of notches or cracks, plastic deformation takes place in the plastic zone where plastic stress concentration produces simultaneously a high normal stress σ_{yy}. Many authors (Knott, 1973; and Wilshaw, Rau and Tetelmann 1968) have supposed that cleavage fracture will occur if

$$\sigma_{yymax} = K_{\sigma p}(F/F_{gy}) \cdot \sigma_y(T,\dot{\varepsilon}) = \sigma_{fc} \qquad (1)$$

and for the special case of general yield

$$K_{\sigma p}(F_{gy}) \cdot \sigma_y(T_{gy},\dot{\varepsilon}_{gy}) = \sigma_{fc}. \qquad (2)$$

General yield is in this paper understood in the sense described by McClintock (1971) or Knott (1973) and not as total yielding of a specimen or a component as a whole. Some authors have tried to verify this equation, proving that for cleavage fracture $K_{\sigma p}(F_f/F_{gy}) \cdot \sigma_y(T_f)$ is constant. For example, Knott (1966) has demonstrated this by varying the notch angle of bend specimens and using the slip-line field theory for the calculation $K_{\sigma p}(F_{gy})$. Griffiths and Owen (1971) have proved this by varying F_f/F_{gy} at constant notch angle and calculating $K_{\sigma p}(F/F_{gy})$ by FEM. The disadvantages of these investigations were:

- that relatively small test specimens were used so that it is not clear which stress state was operating at fracture and whether it was constant at different temperatures or not and,
- that the cleavage fracture strength σ_{fc} was not determined directly as a material parameter.

In this paper it was tried to:

- prove that Eqn. 1 and 2 remain valid for larger specimens of different size as well as for wide plate test specimens where plane strain conditions can be expected,
- establish the cleavage fracture strength as a material parameter independently,
- examine the capability of Eqn. 1 and 2 to explain the fracture behaviour of welded specimens and components,
- understand why the fracture behaviour of welded specimens or components is much more favourable than that of weld (HAZ) simulated specimens.

These investigations should be supported by results of FEM-calculations of the stress distribution ahead of cracks and notches.

The aim of this work is:

- to find out whether the temperature T_{gy}, where fracture occurs just at general yield, is a well-defined fracture criterion or not which is only controlled by the stress state ahead of a crack or a notch,
- to demonstrate the applicability of this criterion on welded components.

If these considerations are realistic, the assessment of flaws in not deforming controlled components at temperatures above T_{gy} can be performed on the basis of the load - factor theory. This means the critical crack length is attained when general yield occurs under working stress. This is due to the fact that when considering the load displacement curves of steel specimens after general yield, only a small increase in load is necessary up to failure by stable growth and finally by plastic collapse.

EXPERIMENTAL DETAILS

The material used was a high-strength fine-grain structural steel with a yield point of 470 MPa.

The composition was as follows:

Element	C	Si	Mn	S	P	Cu	Cr	Ni	Mo	V
Wt.%	0.18	0.29	1.63	0.010	0.011	0.032	0.039	0.73	0.013	0.18

The material was supplied as 40 mm thick sheets. Unwelded and welded specimens were prepared and tested:
small-scale tensile specimens, wide-plate tensile specimens with a centre notch (see Fig.1), medium and large bending specimens with various V-notches, fatigue cracks and slots. The bending specimens had a notch angle of 0° (fatigue crack or slot), 45° and 105° with a corresponding critical depth ratio according to Ewing (1967). The notch root radius of the 45°-notched specimens was 0.1 mm. The machined notches and cracks were parallel to the direction of rolling and either parallel or perpendicular to the sheet surface.
The welding was fabricated by a single-head submerged arc welding process using a fused basic flux low in hydrogen. The welding wire had a diameter of 4 mm and 2.5 wt. % Ni. The heat input was 20 or 50 kJ/cm. The weld groove was a 1/3-2/3 double-U. After welding, no heat treatment was carried out. During welding, the thermal cycle was measured in the region of the future notch tip in the HAZ, at a distance of 0.3 mm from the fusion line. These thermal cycles of 20 kJ/cm and 50 kJ/cm weld were transfered by a welding simulator to small-scale specimens.
The tensile tests of the small-scale tensile specimens were performed in the temperature range of 13 K to 300 K in order to determine the lower yield stress σ_{ly}, the yield point σ_{ty}, the fracture stress σ_f, and the percentage reduction of area after fracture Z. The critical cleavage fracture strength was obtained by extrapolation to 0 K of the σ_{ly}-T-function. According to Smidt (1969) a quadratic polynom was used.
During the test on the notched specimens (unwelded and welded), among other things the temperature, load vs. crack opening displacement and the load vs. deflection were plotted. The crosshead speed was approximately 2 mm/min. The calculation of the nominal stresses of the notched specimens were carried out according to

$$\sigma(T) = \frac{F(T) \cdot S}{B\left[W-(a+a_s)\right]^2} \quad \text{for three-point bending and} \qquad (3)$$

$$\sigma(T) = \frac{F(T)}{B\left[W-(a+a_s)\right]} \quad \text{for wide plate testing} \qquad (4)$$

During all the tests the test temperature was constant within an accuracy of ± 1 K

RESULTS AND DISCUSSION

The variation in uniaxial tensile properties of the base metal for the range 13 K to 300 K is shown in Fig. 2. Yielding occurs by jerky flow below 50 K (Basinski, 1957; Ishikawa, 1977).
The tensile properties of the weld metals and of the specimens of simulated HAZ-both 20 kJ/cm and 50 kJ/cm were measured too. σ_{fc} has the following values in MPa:

Base metal	1430
Weld metal, 20 kJ/cm	1388
Weld metal, 50 kJ/cm	1416
HAZ - simulated, 20 kJ/cm	1603
HAZ - simulated, 50 kJ/cm	1522

The results of the three-point bending specimens are shown in Fig. 3a and 3b, where σ_{gy} and σ_f has been plotted in terms of the nominal bending stress ignoring the stress concentration. σ_{gy} can be seen to be in agreement with that predicted from slip-line field theory (Green and Hundy, 1956). It was indicated that in the case of the 80 mm broad specimens the deformation approaches that of plane strain.

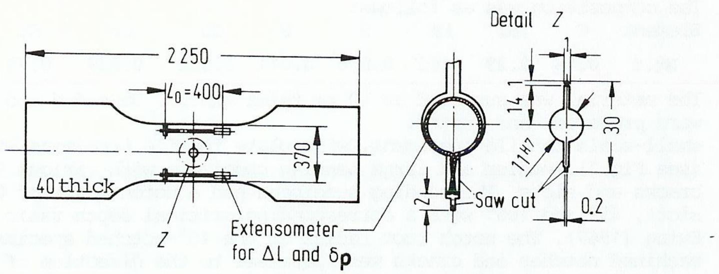

Fig. 1. Form, dimensions in mm, notch configuration and instrumentation of the wide plate specimens

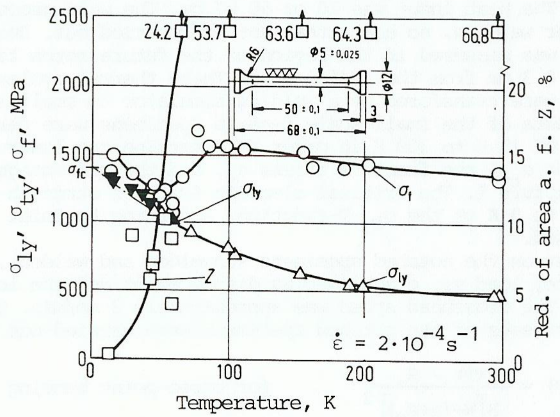

Fig. 2. Lower yield stress σ_{ly}, yield point σ_{ty} (▼), fracture stress σ_f and percentage reduction of area Z as a function of temperature of the base metal.

The transition temperature T_{gy} is defined as the temperature at which fracture occurs when the slip-lines of the specimens just spread over the whole ligament i. e. $\sigma_f = \sigma_{gy}$. This point is reached if the plastic displacement of the notch flanks in this case is 0.2 mm.
The value of the plastic stress concentration factor $K_{\sigma p}(F_{gy})$ was calculated, using Eqn. (2) at the temperature T_{gy} where fracture was coincident with general yield. These values were compared with derived from the slip-line field theory according to the equation (Green and Hundy, 1956)

$$\sigma_{yy\,max} = 2\,k\,\left(1 + \frac{\pi}{2} - \frac{\omega}{2}\right) \qquad 6{,}4° < \omega < 114{,}6° \qquad (5)$$

The influence of the heat input on the temperature T_{gy} is shown in Fig. 3b as an example. At the notch position 0.3 mm from the fusion line the temperature T_{gy} of the 20kJ-welding joint in comparison with the 50kJ-welding joint rose by 80 K.
Fig. 4. shows the experimental results of the wide plate tests of unwelded and welded specimens. The notch position in these specimens was 0.3 mm from the fusion line too, but parallel to the rolling direction an perpendicular to the sheet surface. T_{gy} of the welded specimens is 70 K higher than that of base metal specimens.

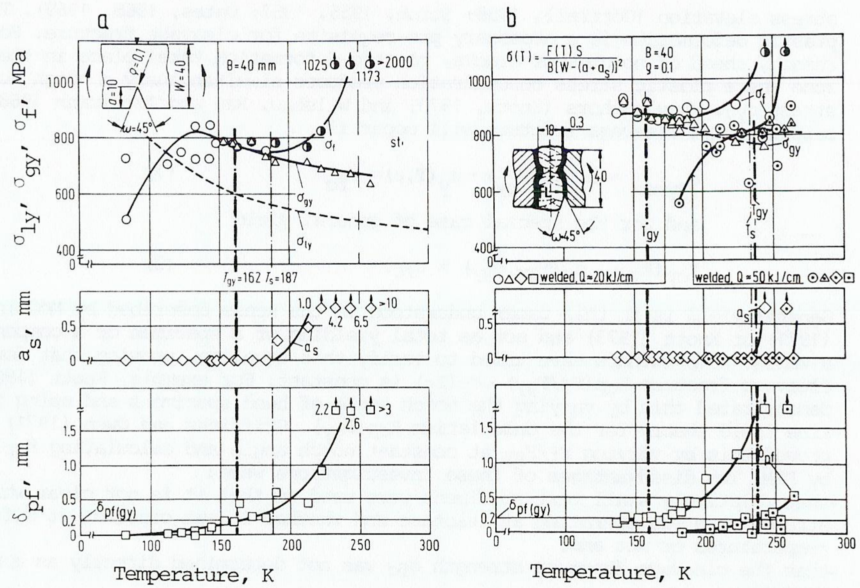

Fig. 3a. and 3b. General yield stress σ_{gy}, fracture stress σ_f, length of the stable crack growth a_s and plastic crack opening displacement δ_{pf}, as a function of temperature of unwelded and welded three-point bending specimens.

Here T_{gy} is connected with δ_{pf} = 0.2 mm too.

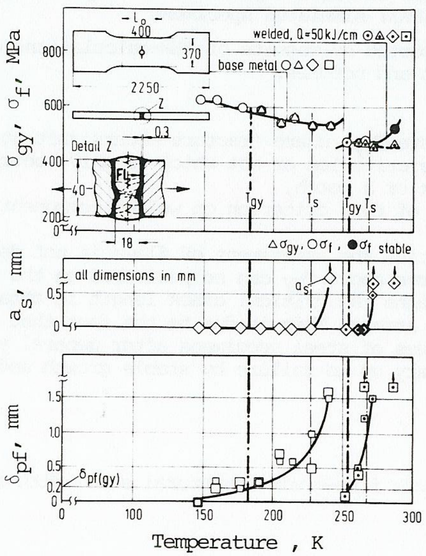

Fig. 4. General yield stress σ_{gy}, fracture stress σ_f, length of the stable crack growth a_s and plastic crack opening displacement δ_{pf} as a function of temperature of unwelded and welded wide plate specimens.

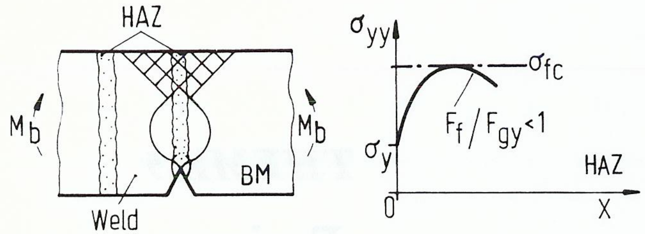

Fig. 5. Slip -line fields in a weld for bending with a V-notch and normal stress σ_{yy} distribution in the HAZ dependent on the notch root X at general yield fracture.

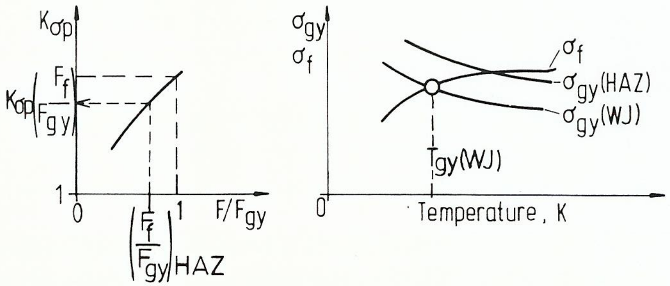

Fig. 6. Dependence of plastic stress concentration factor $K_{\sigma p}$ on appliedload, and the situation of stresses at fracture in welded specimens notched in the HAZ.

$K_{\sigma p}$ (exp.) of the base metal (Table 1a) agrees quite well with $K_{\sigma p}$ (theory) for the bend specimens with fatigue crack and 45° V-notch. The saw cut slot does not bring about the maximum constraint near the notch tip in this specimens. This is evident in the quality in terms of notch theory. $K_{\sigma p}$ (exp.) of the 105° V-notch is greater than the theoretical value. FEM-calculations will be carried out for comparison. The specimen width has no influence on $K_{\sigma p}$ (exp.) in the range of 40 mm to 120 mm. The constraint factor L decreases with increasing width because of increasing plane stress situation far away from the notch tip. The constraint factors for the small bend specimens as well as for the wide plate specimens agree quite well with the theoretical ones. In the slotted wide plate specimens $K_{\sigma p}$ (exp.) is rather near the value after Eqn. (5) but mutch more greater than the FEM-value. This disagreement has not been understood till now.

If $K_{\sigma p}$ (exp.) for the welded specimens is calculated by Eqn. 2 for T_{gy} using σ_{1y} and σ_{fc} from HAZ-simulated specimens Table 1b, one gets lower values than for the base metal specimens with the same geometry (Table 1a). Therefore it can be concluded that general yield in welded specimens is already attained when the load is not yet sufficient to cause the maximum stress elevation $K_{\sigma p}(F_{gy})$ in the HAZ where the notch tip was placed Fig. 5. This arises probably because in welded joints large scale yielding and finally general yielding is manily controlled by the base metal having a lower yield point than the HAZ. To examine these considerations one can calculate the ratio $\sigma_{gy}(WJ)/\sigma_{gy}(HAZ)$ at T_{gy} which should be equal to the load ratio $F_f(HAZ)/F_{gy}(HAZ)$ (Table 1b). Using this load ratio the corresponding $K_{\sigma p}$ can

TABLE 1 Comparison of Measured and Calculated Plastic Stress Concentration Factor $K_{\sigma p}$ and Constraint Factor L at T_{gy}

a) Base metal specimens

Geometry					$K_{\sigma p}$			L	
ω rad	W mm	ρ mm	B mm	Remarks	Experiment	Theory	FEM	Experiment	Theory
Three-point bend specimens with critical depth ratio 1)					Eqn. (2)	Eqn. (5) $k=\sigma_{1y}/\sqrt{3}$		$\sigma_{gy}/1.15\ \sigma_{1y}$	2)
0 (=0°)	40	≪0.1	40	fatigue ∥ crack	2.6			1.23	1.261
			80		2.7	2.9	2.7 3)	1.26	
		≦ 0.1	40	0.2 slot	2.3			1.22	
0.785 (=45°)	40	0.1	40	v-notch ∥	2.4			1.19	
			80		2.4	2.5	2.6 4)	1.23	1.26
1.831 (=105°)	40	0.1	40	v-notch ∥	2.0			1.10	1.23
			80		2.3	1.9		1.22	1.23
0.785	40	0.1	40	v-notch ⊥	2.5	2.5	2.6 4)	1.24	1.26
	120				2.5			1.01	
Wide plate tensile specimens (see Fig. 1.)									
0	370	≦ 0.1	40	0.2 slot ⊥	2.6	2.9?	2.1 3) 4)	1.0	1.0

b) Welded specimens: fabricated with 20 or 50 kJ/cm heat input (Q)

Geometry					$K_{\sigma p}$	$K_{\sigma p}$	$(F_f/F_{gy})_{HAZ}$	$L=\sigma_{gy}/1.15\ \sigma_{1y}$	Q
ω rad	W mm	ρ mm	B mm	notch position	Eqn. (2)	Model			(kJ/cm)
Three-point bend specimens with critical depth ratio 1)									
0	40	≪0.1	40	HAZ0.3 ∥	2.4	2.5	0.94	1.32 5)	20
0.785	40	0.1	40	HAZ0.3 ∥	2.2	2.3	0.95	1.25 5)	20
					2.3	2.3	0.90	1.37 5)	50
0.785	120	0.1	40	HAZ0.3 ⊥	2.4	2.4	0.89	1.12 5)	20
					2.3	2.3	0.84	1.15 5)	50
Wide plate tensile specimens (see Fig. 1.)									
0	370	≦ 0.1	40	HAZ0.3 ⊥	2.4	2.3	0.80	1.0	20
					2.3	2.2	0.71	1.0	50

1) Ewing and Hill (1967)
2) Green and Hundy (1956)
3) Larsson and Carlsson (1973)
4) Erbe (1980)
5) L $(T_{gy})=\sigma_{gy}(WJ)/1,15\sigma_{1y}(BM)$

be determined approximately from the $K_{\sigma p}$ $-F/F_{gy}$ -function given by Griffiths and Owen (1971) and adjusted to $K_{\sigma p}$ (F_{gy}) of the respective specimen Fig. 6. $K_{\sigma p}$ determined by this model and listed in column 3 in (Table 1b) agrees quite well with $K_{\sigma p}$ from Eqn.(2) for all specimens.The model, therefor gives an adequate description of the fracture of weldings at T_{gy}.Moreover the conclusion can be drawn that the cleavage fracture of weldings is not only determined by the HAZ but in a rather pronounced manner by the base metal because the latter controlles the large scale yielding. For the same reason the fracture behavior of welded specimens or components is more favourable than that of weld (HAZ) simulated specimens. The constraint factor L of all the welded specimens is somewhat greater than the constraint factor of the base metal specimens because of the constraint of the welding joint.

The results demonstrate that T_{gy} is only dependent on the stress state and is therefore a well-defined criterion to limit fracture at and beyond general yield opposite to low stress fracture for base metal as wll as for welded joints.

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REFERENCES

Davidenkov, N. N. and F. Wittman (1937). Techn. Phys., USSR, 4, 308.
Orowan, E. (1948). Rep. Prog. Phys. 12, 185.
Knott, J. F. (1966). J. Iron Steel Inst. 204, 104.
Wilshaw, T. R. Rau, C. A. and Tetelman, A. S. (1968). Eng. Fract. Mech. 1, 191.
Cottrell, A. H. (1958). Trans. AIME, 212, 192.
Stroh, A. N. (1955). Phil. Mag. 1, 489.
(1957). Advances in Physics, 6, 418.
Oates, G. (1968). J. Iron Steel Inst. 206, 930.
(1969). Ibid., 207, 353.
McClintock, F. A. (1971). In:Fracture, Vol. 3, Edited by Liebwitz, H. (New York).
Knott, J. F. (1973). Fundamental of Fracture Mechanics, Butterworth, (London).
Ritchie, R. O., Knott, J. F. and Rice, J. R. (1973). J. Mech. Phys. Solids, 21,395.
Green, A. P. and Hundy, B. B. (1956). J. Mech. Phys. Solids, 4, 128.
Smidt jr., F. A. (1969). Acta Metallurg. 17, 381, (New York).
Ewing, D. J. F. and Hill, R. (1967). J. Mech. Phys. Solids, 15, 115.
Griffiths, J. R. and Owen, D. R. J. (1971). J. Mech. Phys. Solids, 19,419.
Larsson, S. G. and Carlsson, A. J. (1973). J. Mech. Phys. Solids, 21
Erbe, H. H. (1980). A + E FM, Rome, Italy, June 23-27.
Basinski, Z. S. (1957). Proc. Roy. Soc. Ser. A, 240, 229.
Ishikawa, K. and Tsuya, K.(1977). Fracture, Vol. 3, Ed.: Taplin,(waterloo), 241.