THE INFLUENCE OF LOADING RATE ON THE DUCTILE-BRITTLE TRANSITION AND CLEAVAGE FRACTURE STRESS OF 2.25Cr-1Mo STEEL

M. Holzmann, B. Vlach and J. Man*

Influence of broad loading rate spectra on transition-temperature behaviour of Charpy V-notch and small precracked specimen. The loading-rate shifting of propagation, initiation and brittleness temperature. Cleavage fracture stress of bainitic microstructure. Loading-rate shifting of plain strain transition temperature and general yield transition temperature for small precracked specimens. Loading-rate independence of initiation transition temperature for this specimens. Temperature and loading rate dependence of cleavage fracture toughness.

INTRODUCTION

The transition-temperature behaviour of steel is a function of loading rate. The data on the influence of loading rate on the transition behaviour has been obtained mainly from the temperature pattern of CVN absorbed energy determined at both static and impact loadings (Rolfe and Barsom (1)). The influence of loading rate is manifested by temperature shift for the chosen energy level in the transition region. This experimental method does not enable any deeper analysis of the influence of loading rate on the transition temperature and cannot contribute to more detailed analysis of the micromechanistic condition for the nucleation of cleavage fracture.

The precracked specimens show similar transition-temperature behaviour. In this case, the fracture behaviour is characterized by the dependence of fracture toughness on the temperature (Wullaert and Server(2)).

*Institute of Metallurgy, Czechoslovak Academy of Sciences, Brno

From this dependence the transition from ductile to cleavage fracture can be detected also. Below this transition, the cleavage fracture appears at various extent of plastic deformation: the cleavage fracture after general yielding, at and below the general yielding at various crack tip plastic zone size and finally under a very small plastic zone size at the crack tip, so that the plane strain condition prevails. Therefore, various fracture regions and transition temperatures can be defined.

It was shown that, for the ferritic steel, the cleavage fracture stress plays a dominant role in the nucleation of cleavage fracture of notched and precracked specimens. Macroscopically, this stress was proved to be independent of temperature and hydrostatic stress (Knott (3), Oates (4), Griffiths and Owen (5)). Microscopically, the value of this stress is determined by the size of cracked carbide particles (Smith (6)). By applying the cleavage fracture stress concept, the temperature and strain rate dependence of the cleavage fracture toughness of these steels can be predicted (Ritchie et al (7) and Curry (8)).

The aim of the present paper is to investigate the influence of loading rate by using broad spectra of displacement velocities on the transition-temperature behaviour of Charpy V-notch and small precracked specimens and on the cleavage fracture toughness. Further, the validity of the cleavage fracture stress concept for a bainitic microstructure of 2.25% Cr-1% Mo will be verified and the application of this concept for explaining the loading rate dependence of transition temperatures and cleavage fracture toughness will be examined.

MATERIAL; EXPERIMENTAL TECHNIQUE

Commercially produced pressure vessel steel was used. Its chemical composition weight percent was 0.12% C; 0.57% Mn; 0.27% Si; 2.30% Cr; 1.04% Mo; 0.015% S; 0.010 % P. The steel was delivered in form of a plate with dimensions 1800mm x 4200mm x 30mm. The heat treatment was performed by the producer, the microstructure in the received condition was formed by the tempered bainite.

Mechanical properties at ambient temperature: lower yield stress R_{eL} = 308 MPa; ultimate tensile stress R_m = 495 MPa; CV-notch energy 256 J.

Specimens for tensile tests, Charpy V-notch specimens and small precracked specimens with thickness B =

= 15 mm, width W = 15 mm, and length = 75 mm were cut of this plate. The latter specimens were provided with a chevron notch having a fatigue crack, the total length being 7.5 mm $a/W = 0.5$. All the specimens were of L-T orientation.

Both the notched and the precracked specimens were loaded by the three-point bending with constant displacement velocities. The span of supports was 40 mm for the notched specimens and 60 mm for the precracked ones.

The tests at various displacement velocities were carried out on the high speed hydraulic testing machine ZWICK/rel and on the impact tester. The problems connected with the use of the hydraulic testing machine for high speed tests were discussed in detail by Krabiell and Dahl (9, 10). Our experience is practically the same (Holzmann et al (11)). Simultaneously, we found experimentally (Holzmann et al (12))that, for obtaining a load-time record without oscillation, it is possible to apply only certain maximal displacement velocities which must be lower than the limit velocity. The limit velocity is a function of compliance of the loading system, the specimen and the natural frequency of the measuring load cell. For the specimens mentioned above, for the specially designed loading equipment, and for the given testing machine, this velocity was 1 ms^{-1}. Therefore, the highest displacement velocity applied for testing on the hydraulic machine was chosen 0.5 ms^{-1}. The impact tester was used for higher loading rate.

The chosen loading rates for Charpy V-notch specimens and corresponding deflection rates $\dot{f}$ are given in Tab. 1. The displacement velocities and the corresponding stress intensity factor rates $\dot{K}_I$ for precracked specimens are shown in Tab. 2. The tests were carried out in the temperature interval from +20 ^oC to -196 ^oC.

The load vs time records were obtained using a transient recorder and a Hewlett-Packard x-y plotter.

RESULTS AND DISCUSSION

The influence of loading rate on the mechanical behaviour and the transition temperatures of Charpy V-notch specimens.

The assesment of mechanical behaviour of a Charpy V-notch specimen at various loading rates was performed according to the general scheme of fracture behaviour

TABLE 1 - Displacement velocities and deflections rates

Group No.	Displacement velocity mm s^{-1}	Deflection rate $\dot{f}$ mm s^{-1}
1	0.05	1.06×10^{-2}
2	5.0	1.06
3	500.0	1.06×10^{2}
4	5 600.0 x	2.0×10^{3}

x impact tester

TABLE 2 - Displacement velocities and stress intensity factor rates $\dot{K}_I$

Group No.	Displacement velocity mm s^{-1}	$\dot{K}_I$ $MPam^{1/2}s^{-1}$
1	0.05	6.2
2	0.50	6.4×10
3	5.00	6.4×10^{2}
4	50.00	6.4×10^{3}
5	500.00	1.0×10^{5}
6	1 500.00 x	2.3×10^{5}

x impact tester

shown in Fig. 1. This scheme is based on Charpy V-notch testing. For this way of loading it was discussed in detail by Helms et al (13).

In this scheme, the meaning of the characteristics is:

F_{GY} - general yield load;

F_{Max} - maximum load;

F_{FR} - fracture load;

f_{max} - plastic deflection at load F_{MAX}

f_{FR} - plastic deflection at the load F_{FR}

l_s - length of the ductile crack growth below the notch;

t_p - propagation transition temperature (the mode of the fracture propagation is changed from the stable ductile growth to the unstable cleavage);

t_i - the initiation transition temperature (the mode of the initiation is changed from the ductile to the cleavage one);

t_{Bv} - brittleness transition temperature $F_{FR} = F_{GY}$.

The mechanical behaviour of Charpy V-notch specimens according to the scheme in Fig. 1 is given for various displacement velocities (Tab. 2) in Fig. 2. The temperature interval A is the region of the upper shelf CVN-energy, the temperature interval B is the transition region and the temperature interval C is the region with the cleavage fracture initiation (in the region C´, $F_{FR} > F_{GY}$, in the region C´´, $F_{FR} < F_{GY}$).

The dependence of transition temperatures t_p, t_i and t_{Bv} on the deflection rate $\dot{f}$ is illustrated in Fig. 3. From Figs 2 and 3 it follows that, when increasing the deflection rate, the temperature t_{Bv} shows largest loading-rate shift to higher temperature, a smaller temperature shift is observed for the temperature t_i and only very small shift for the temperature t_p. With increasing loading rate, the temperature intervals $t_{Bv}-t_i$ and especially $t_{Bv}-t_p$ become apparently narrow.

To explain the loading rate shift of t_{Bv} and t_i the concept of cleavage fracture stress can be used.

<u>Cleavage fracture stress σ_{CF}</u>. For the project steels with a ferritic-pearlitic microstructure, the role of cleavage fracture stress for the cleavage fracture initiation was already many times proved (3; 5; 6) experimentally. The independence of this stress on the temperature, for these steels, is given by the fact that the cleavage fracture initiation at stress σ_{CF} is microcrack propagation controlled (Chell and Curry (14)). The independence of this micromechanism on the temperature has been discussed in detail by Hahn (15).

The question of the nucleation of cleavage microcracks in bainitic microstructure and the problem of cleavage fracture stress in these microstructures have been discussed by Kotilainen et al(16), Törönen et al(17), Kotilainen(18), Brozzo et al(19), Saario et al(20) and Curry(21). The last author has shown that even bainitic microstructure obeys the cleavage fracture stress criterion for the initiation of cleavage fracture and that this stress for this microstructure is temperature independent.

The results presented in Fig.2 made it possible to examine the problem of the cleavage fracture stress, its dependence on the temperature and on the strain rate. In Fig.4 the general yield loads F_{GY} and fracture loads at and below t_{Bv} for various deflection rates $\dot{f}$ are replotted. At the temperature t_{Bv}, the cleavage fracture stress has been determined by the well known formula(19, 21)

$$\sigma_{CF} = K_{\sigma pl} R_{eL} \qquad \text{..............} \qquad (1)$$

where $K_{\sigma pl}$ is plastic stress concentration factor and R_{eL} is lower yield stress. The determination of the lower yield point at t_{Bv} for various deflection rates $\dot{f}$ represents the main problem. This was done in the following way. The strain rate in the plastic zone below the notch is different in different points of this zone. For the estimation of R_{eL}, we decided to take the strain rate of the maximum principal plastic strain at the notch root $\varepsilon_{1\ pl}$. As this first approximation for estimating $\dot{\varepsilon}_{1\ pl}$, we used the solution by Griffiths and Owen(5); $\dot{\varepsilon}_{1\ pl}$ was then calculated from the relation

$$\dot{\varepsilon}_{1\ pl} \doteq \frac{\Delta\varepsilon_{1\ pl}}{\Delta\tau} \qquad \text{..............} \qquad (2)$$

where $\Delta\varepsilon_{1\ pl}$ is the increase of maximum principal plastic strain between the load 0.7 F_{GY} and F_{GY} and $\Delta\tau$ is the time corresponding to this increase. The value $\Delta\varepsilon_{1\ pl}$ was taken from diagram in reference (5), the time $\Delta\tau$ was read for various $\dot{f}$ from the load vs time record. From the tensile test at various strain rates and temperatures the following equation for R_{EL} has been found (11):

$$R_{eL} = 250 + 1384\exp-(t+273)(854\times10^{-5} - 44\times10^{-5}\ln\dot{\varepsilon}_{pl}) \text{..} \quad (3)$$

where t is temperature in degrees of Celsius. The lower yield stress corresponding to F_{GY} can be calculated by combination of eqs. (2) and (3). The values of $\Delta\varepsilon_{1\ pl}$

taken from the reference (5) are exactly valid for V-notch specimen loaded by four point bending. Fig.5 illustrates the dependence of F_{GY} (general yield load was defined at the atteinment of non-linear displacement of 0.1 mm) measured for various $\dot{f}$ and temperatures on R_{eL} calculated using eqs. (2) and (3). This dependence has the linear form $F_{GY} = CR_{eL}$, the value of constant C determined by the linear regression was $C = 21 \pm 1$ mm^2. This value is in good agreement with the constant C resulting from the relation for F_{GY} of Charpy V-notch specimen loaded by three point impact bending published by Server (26). Therefore either eqs. (2) and (3) or the constant C and F_{GY} may be used for calculation of R_{eL} at F_{GY}. The cleavage fracture stress σ_{CF} determined using this constant, load F_{GY} at temperatures t_{Bv} and $K_{\sigma pl} = 2.24$ (19) is presented as a function of temperature in Fig. 6. It is evident that σ_{CF} is in the temperature interval -90 °C – -180 °C temperature independent and its value $\sigma_{CF} = 1700$ MPa is practically identical with that of measured by Curry (21) for a tempered bainitic pressure vessel steel.

Since we found for the given pressure vessel steel (23) that the packet size influences appreciably temperatures t_{Bv} and t_i, then the micromechanism in which the packet size plays a decisive role, must control the magnitude of the cleavage fracture stress (either the Cottrell dislocation mechanism (22) or the propagation of the packet size microcrack through the packet boundary, Dolby and Knott (24), Hahn (15)).

The shifting of temperature t_{Bv} in Fig.2 and 3 due to the increase of deflection rate is caused exclusively by the influence of strain rate on the lower yield stress. The same conclusion holds for the shift of temperature t_i. To fulfil the condition of cleavage fracture initiation, the maximum principal stress in region C´ must increase due to work hardening in the plastic zone. At a higher strain rate the work hardening capacity decreases and as result of this the condition for cleavage fracture can be fulfilled only for small temperature increment. Because of this, the temperature t_i shows weaker dependence on $\dot{f}$.

<u>The influence of loading rate on the fracture behaviour of small precracked specimens</u>

The fracture toughness as a function of temperature for various $\dot{K}_I$ is shown in Fig.7. The points marked K_{IC} represent the fracture toughness fulfilling the ASTM condition, the points marked K_Q^X do not fulfil this condition, and the value K_Q^X was calculated considering the correction to the size of plastic zone of the crack-tip; the points marked K_{IJ} were determined using J-integral. K_{IJ} points mark the fracture toughness values for which the condition a,B,W - a = 25 $J_{IC}/R_{eL}(\dot{\varepsilon})$ is fulfilled. In all the cases the fracture toughness was calculated for fracture load F_{FR}.

The following transition temperatures are presented in Fig.7: $t_{2.5}$ - the temperature below which the fracture toughness fulfils the ASTM criterion. The lower yield stress for the given $\dot{K}_I$ necessary for this criterion was determined from the dependence of R_{eL} on $\dot{\varepsilon}$ and on temperature. The value of $\dot{\varepsilon}$ for precracked specimens was calculated using the relation $\dot{\varepsilon} = 2\,R_{eL}/\tau E$, where τ is the time to fracture;

t_{GY} - above this temperature fractures after general yielding occurred. The points marked $\perp$ do not represent the values of fracture toughness, but they only show that the ductile initiation still occurred.

The dependence of these transition temperatures on the stress intensity factor rate $\dot{K}_I$ is presented in Fig. 8. With increasing the loading rate the same loading rate shift of temperatures $t_{2.5}$ and t_{GY} to higher temperatures occurred. On the other hand the temperature t_i^c shows only very weak dependence on $\dot{K}_I$; at $\dot{K}_I$ = 2.3x 10^5 MPa $m^{1/2}s^{-1}$ it was found $t_i^c = t_{GY}$. It means that for higher loading rate the temperature interval with the cleavage fracture above the general yielding becomes narrower. A the highest loading rate $\dot{K}_I$ this interval disappears completely and an abrupt transition from the region of low values of the fracture toughness to the upper shelf fracture toughness region appears. This phenomenon was observed for the precracked Charpy specimen also by Logsdon and Begley (25). According to these authors the phenomenon is caused by a violation of the J_I test size criterion due to small specimen dimensions, as the toughness increases with temperature. This explanation does not seem to be generally valid, for at lower $\dot{K}_I$ the region of cleavage fractures after general yielding above the temperature t_{GY} exists (Fig.7) and a J_I - size criterion for small precracked specimens is fulfilled. In our opinion the narrowing of the region with the cleavage fracture above general yielding with increasing $\dot{K}_I$ and the sudden transition to the ductile fracture at the highest $\dot{K}_I$ is very probably associated with adiabatic heating in the plastic zone. The heating causes a decrease of flow stress in the plastic zone, the maximum principal stress decreases and the condition for the cleavage fracture initiation after general yielding cannot be fulfilled.

This phenomenon is obviously in operation at fracture of the Charpy V-notch specimen at the temperature t_p, because this temperature, similarly to the temperature t_i^c, shows very weak dependence on loading rate (Fig.3). It is interesting that both these temperatures have very similar patterns when being plotted against the piston and hammer velocity (Fig. 3 - the numbers at the ordinates).

In Fig.8 the numbers attached to experimental points represent the values of lower yield stress determined for corresponding $\dot{\varepsilon}$. It is evident that both at the temperature $t_{2.5}$ and at the temperature t_{GY}, the yield stress has for various $\dot{K}_I$ the unique value. At all the temperatures $t_{2.5}$ and all the temperatures t_{GY} an identical pattern of maximum principal stress at the crack tip can be expected, the value of which is the function of R_{eL}. Therefore, it may be concluded that the cleavage fracture both at the temperature $t_{2.5}$ and the temperature t_{GY} is controlled by critical tensile criterion.

Fig. 9 presents the fracture toughness data for all $\dot{K}_I$ rates and temperatures as a function of lower yield stress which has been evaluated for corresponding $\dot{\varepsilon}$ and t. The arrows $t_{2.5}$ and t_{GY} designate the average value of the lower yield stress at these temperatures. They divide the dependence of fracture toughness vs lower yield stress into three fracture regions according to the size of plastic deformation at cleavage fracture initiation. Similar results have been presented in (9) and (10). The unique dependence of fracture toughness data K_{IC} and K_Q^x on R_{eL} shows that the RKR model (7) assuming constant cleavage fracture stress is valid for bainitic microstructure of the given pressure vessel steel.

CONCLUSION

The increasing loading rate f results in the temperature shift of the initiation and brittleness transition temperature of V-notch specimens. On the contrary, the propagation transition temperature is influenced by the deflection rate only weekly. The cleavage fracture stress was found to be temperature independent in the temperature interval -90 °C to -180 °C. With small precracked specimens the increase of loading rate causes the temperature shift of plain strain and general yield transition temperatures, brittle-ductile transition tem perature is practically independent of loading rate. The cleavage fracture toughness below the general yield transition temperature shows a unique dependence on lower yield stress. This fact proves that the RKR model can be used for the prediction of the temperature and loading rate dependence of cleavage fracture toughness for Cr-Mo steel with bainitic microstructure.

REFERENCES

(1) Rolfe, S.T. and Barsom, J.M., Fracture and Fatigue Control in Structures, Prentice-Hall, Englewood Cliffs, New Jersey, 1977.

(2) Wullaert, R.A. and Server, W.L., Nuclear Eng. and Design, Vol. 57, 1980, pp.153-173.

(3) Knott, J.F., J.Iron Steel Inst., Vol.204, 1966, pp. 104-111.

(4) Oates, G., J.Iron Steel Inst., Vol. 206, 1968, pp.930 -935.

(5) Griffiths, J.R. and Owen, D.R., J.Mechan.Phys.Solids, Vol. 19, 1971, pp. 419-431.

(6) Smith, E., "Physical Basis of Yield and Fracture", Oxford Proceed. Conf., Inst.Phys. and Phys.Soc., London, 1966.

(7) Ritchie, R.O., Knott, J.F. and Rice, J.R., J.Mech. Phys.Sol., Vol. 21, 1973, pp. 395-410.

(8) Curry, D.A., Mater.Science and Eng., Vol. 43, 1980, pp. 135-144.

(9) Krabiell, A. and Dahl., W., Arch.Eisenhüttenwesen, Vol. 52, 1981, pp. 429-444.

(10) Krabiell, A. and Dahl. W., "Advances in Fracture Research", Proceed. of the 5th Intern.Conf. on Fracture, Cannes, Ed. by D. Francois, Pergamon Press, Paris, France, 1981.

(11) Holzmann, M., Vlach, B., and Man, J., "Trans.and Fract.Behaviour of Bainitic Steel at Various Loading Rate and Temperature", Inst.of Metallurgy, Czechoslovakia, Report No.VZ 567/683, 1984, (in Czech).

(12) Holzmann, M., Vlach, B. and Man, J., Strojírenství, Vol. 34, 1984, pp. 561-567, (in Czech).

(13) Helms, R., Kühn, H.J., and Ledwurski, S., "Assesment of Mechan.Behaviour of Mat. in the Notched Bar Impact Test", BAM West Germany, Research Report No 82, 1982.

(14) Chell, G.G. and Curry, D.A., "Development in Fract. Mechanics - 2", Applied Science Publishers, London, 1981.

(15) Hahn, G.T., Metallurg.Trans., Vol. 15A, 1984, pp. 947-959.

(16) Kotilainen, H., Törönen, K. and Nenonen, P., "Advances in Fracture Research", Proceed. of the 5th Int. Conf. of Fract., Cannes, Ed.by D.Francois, Pergamon Press, Paris, France, 1981.

(17) Törönen, K., Kotilainen, H. and Nenonen, P., Ibid.

(18) Kotilainen, H., "Fracture and the Role of Microstructure", Vol.I, Proceed. 4th Europ.Conf. on Fracture, Leoben, Austria, 1982.

(19) Brozzo, P., Buzzichelli, G., Mascanzoni, A. and Mirabile, M., Metal Science, Vol.11,1977,pp.123-129.

(20) Saario, T., Wallin, K. and Törönen, K., J.Eng.Mater. and Technology, Vol.106, 1984, pp.173-177.

(21) Curry, D.A., Metal Science, Vol.16, 1982,pp.435-440.

(22) Curry, D.A., Ibid, Vol.18, 1984, pp.67-76.

(23) Holzmann, M., Vlach, B. and Man, J., "The Influence of Austenite Grain Size on Failure Micromechanism and Resistance to Brittle Fracture of 2.25%Cr1%Mo Steel", Inst.of Metallurgy,Czechoslovakia,1981.

(24) Dolby, R.E., Knott, J.F., "The Toughness of Weld Heat-Affected Zones", National Seminar, London, The Welding Institute, Cambridge, England.

(25) Logsdon, W.A. and Begley, J.A., Flow Growth and Fracture, ASTM STP 631, Philadelphia, U.S.A., 1977.

(26) Server, W.L., J.of Testing and Evaluation, Vol.6, 1978, pp. 29-34.

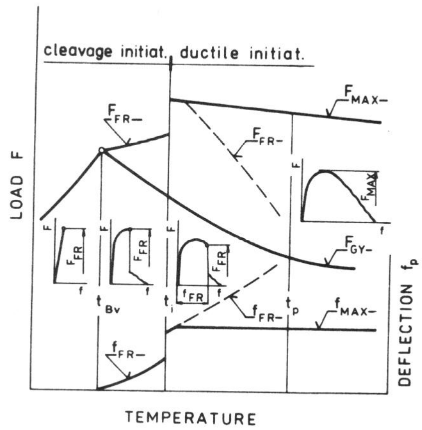


Figure 1 - Mechanical behaviour of V-notch specimen

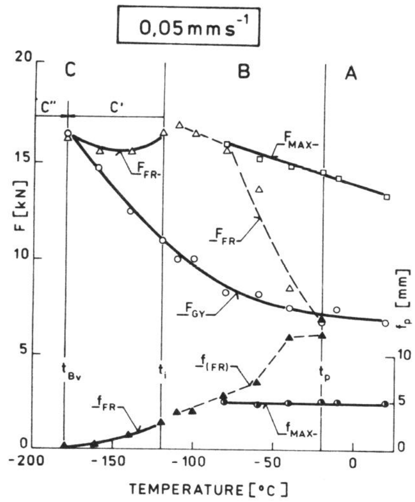


Figure 2a

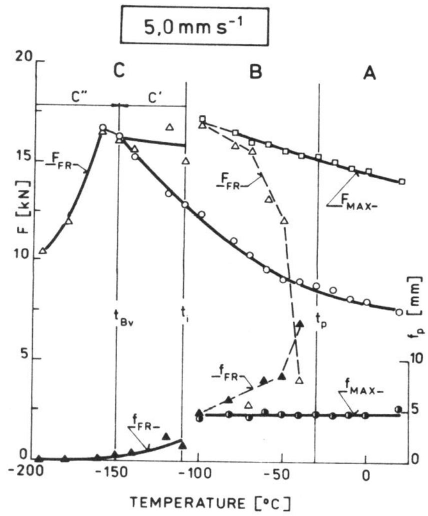


Figure 2b

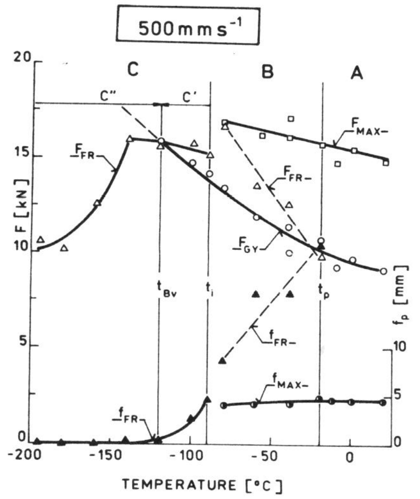


Figure 2c

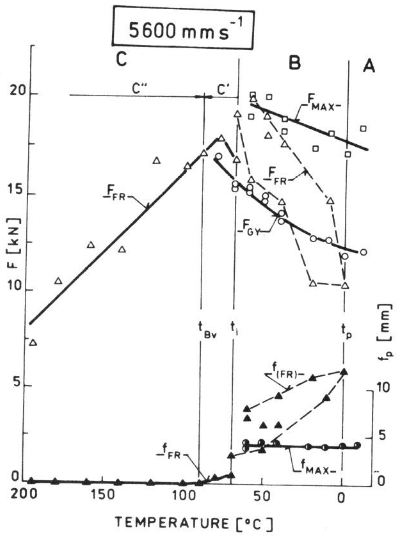


Figure 2d Mechanical behaviour of V-notch specimen

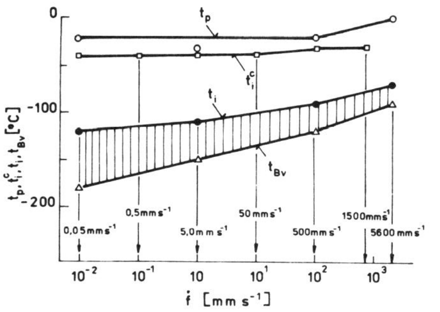


Figure 3 Transition temperatures as a function of $\dot{f}$.

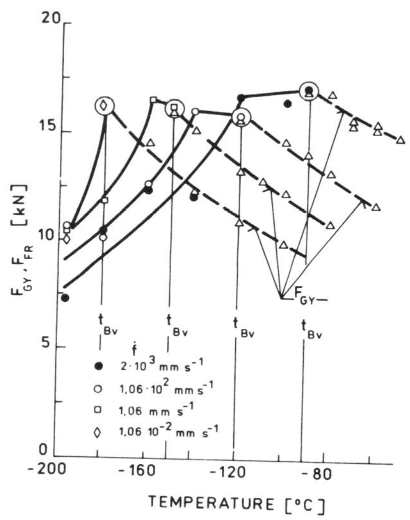


Figure 4 Dependence of F_{GY} and F_{FR} on temperature

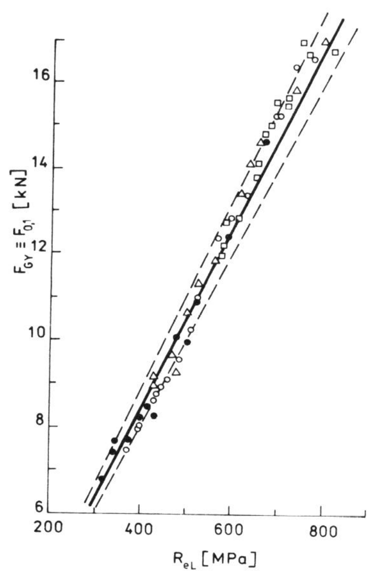
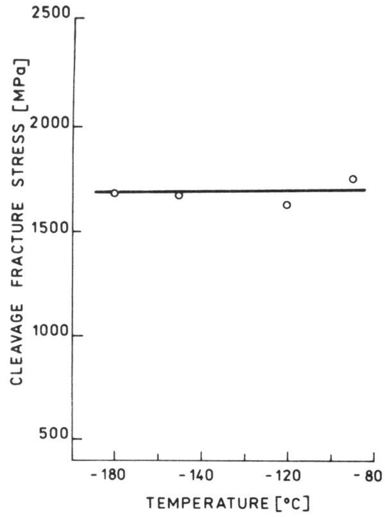


Figure 5 Dependence of F_{GY} vs lower yield stress



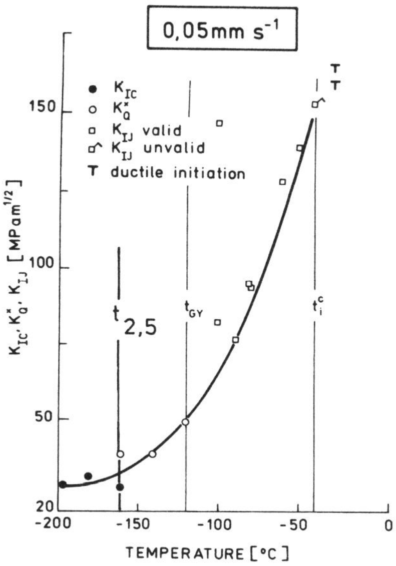
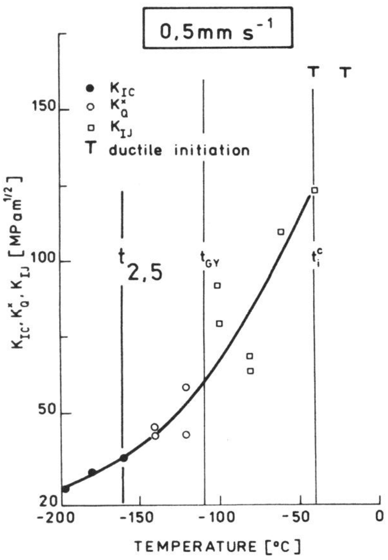


Figure 6 Cleavage fracture stress vs temperature

Figure 7a



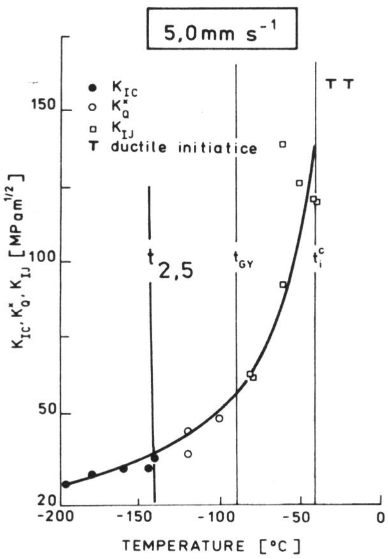


Figure 7b

Figure 7c

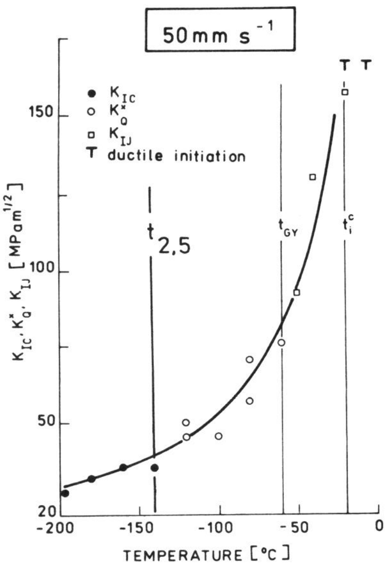


Figure 7d

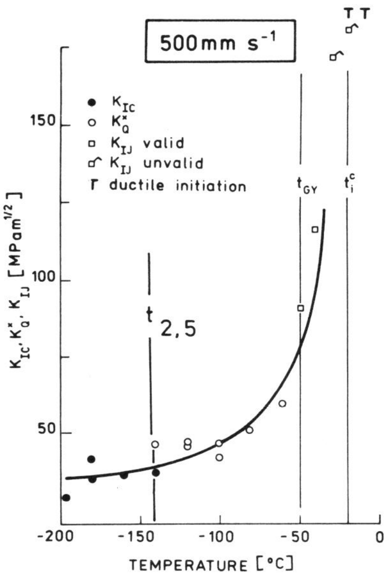


Figure 7e

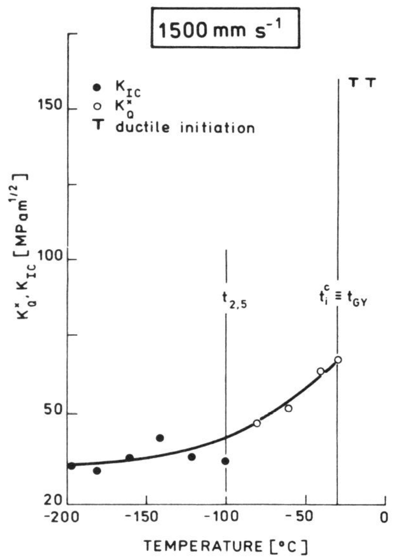


Figure 7f Fracture toughness vs temperature

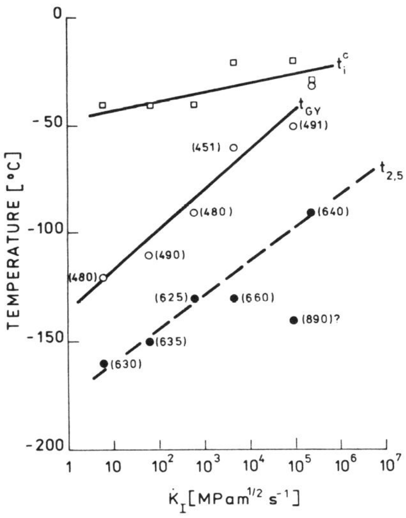


Figure 8 Transition temperatures vs loading rates

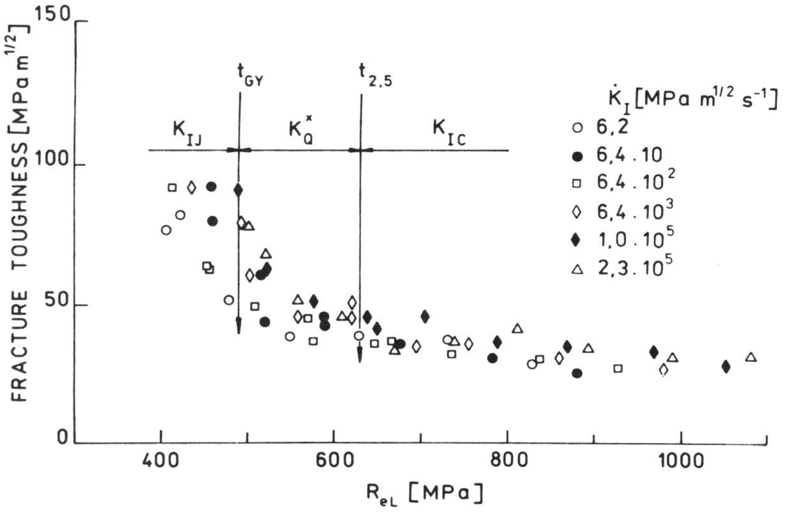


Figure 9 Dependence of fracture toughness on lower yield stress