

EFFECT OF LOADING RATE ON DUCTILE-BRITTLE TRANSITION TEMPERATURE BY REFERENCE TEMPERATURE (T_0) AND MASTER CURVE APPROACH FOR A MODIFIED 9Cr-1Mo STEEL

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ABSTRACT

Towards evaluation of the ductile to brittle transition temperature (DBTT) of ferritic steels, the ASTM E 1921 based reference temperature (T_0) and the associate Master Curve approach have now been widely recognized, however, till date the standard restricts itself to static/quasi-static loading rates. It is well recognized that the flow stress of rate sensitive material increases with the strain rate and thus it is imperative that the increase in loading rate would lead to limited plasticity induced brittleness, reflected in higher T_0 . There have been efforts in literature for developing empirical correlations to derive T_0 at higher loading rates from T_0 at quasi static loading rates or vice versa. The loading rate is often expressed in terms of stress intensity factor rate (dK/dt), with unit $\text{MPa}\sqrt{\text{m}}\text{ s}^{-1}$. dK/dt is approximately equal to 10^4 - 10^5 $\text{MPa}\sqrt{\text{m}}\text{ s}^{-1}$ for low velocity tests at 1-2 ms^{-1} and $dK/dt \sim 10^6$ $\text{MPa}\sqrt{\text{m}}\text{ s}^{-1}$ for normal velocity impact tests at ~ 5 ms^{-1} . There is a need to experimentally validate the extent of change in T_0 with higher loading rates, especially for the 9Cr-1Mo family of steels, proposed to be used as wrapper material in the upcoming commercial liquid sodium cooled fast breeder reactors. The present study is directed towards determining T_0 for modified 9Cr-1Mo steel at loading rates of 1.12, 3 and 5 m/s.

The material under dynamic loading actually is subjected to a series of forward and reflective stress waves, at least in the initial part of the loading, observed as high frequency oscillations in the load-time or load-displacement traces. Thus for determining the dynamic fracture toughness of materials the inertia effect on the fracture load determination is very important in data analysis, especially for higher loading rates and/or for brittle materials. The amplitudes of the consecutive oscillations are expected to die down with time after the impact and considered negligible after a period of 2τ - 3τ , where τ is the period of inertial oscillation, a function of specimen type and geometry and speed of sound through this material. The time period, τ , is taken as 27 μs for the present campaign, implying that after 3τ i.e. 81 μs , all the measured fracture loads are free from any inertial effects. However, at a lower loading rate, i.e. ~ 1 m/s, the amplitude of the oscillations are negligible. Thus the time to fracture (t_f) with respect to the 2τ - 3τ time span is an important parameter, especially for the loading rates of 3 and 5 m/s, in ensuring the accuracy of the fracture load determination for evaluating the dynamic fracture toughness. It is observed that the time to fracture (t_f) for the loading rate of 3 m/s has exceeded the 3τ time span for all the specimens, the minimum being 100 μs , where as a few specimens for the loading rate of 5 m/s have failed to meet this criteria. Here it is observed that out of eight specimens tested at different temperatures ranging from -25 to 30 $^{\circ}\text{C}$, four specimens are at the border line of 2τ - 3τ regime and one specimen with $t_f = 10$ μs fails to qualify even for consideration. An alternative analytical technique has been used to derive the fracture load for those specimens.

T_0 values determined were 8.8 and 11.2 $^{\circ}\text{C}$ for the loading rates of 1.12 and 3 m/s respectively, and increases drastically to 26.2 $^{\circ}\text{C}$ for a loading rate of 5 m/s. This shift is little higher than that predicted empirically (~ 14 $^{\circ}\text{C}$) for conventional pressure vessel steels.

INTRODUCTION

Towards evaluation of the ductile to brittle transition temperature (DBTT) of ferritic steels, the ASTM E 1921[1] based reference temperature (T_0) and the associate Master Curve approach has now been widely recognized, however, till date the standard restricts itself within the purview of static to quasi-static loading rates. It is well recognized that the flow stress of the rate sensitive material increases with the strain rate and thus it is imperative that the increase in loading rate would lead to limited plasticity induced brittleness, reflected in higher T_0 . To this end, there have been efforts for developing empirical correlations to derive T_0 for higher loading rates from experimentally obtained T_0 at a quasi static loading rate. Wallin in 1997 [2] and Wallin and Planman in 2001 [3] had derived an effect of loading rate, expressed in terms of stress intensity factor rate. From the fracture mechanics point of view, the loading rate is often expressed in terms of stress intensity factor rate (dK/dt), with unit $\text{MPa}\sqrt{\text{m}}\text{ s}^{-1}$.

dK/dt is approximately equal to 10^4 - 10^5 MPa \sqrt{m} s $^{-1}$ for low velocity tests at 1-2 ms $^{-1}$ and $dK/dt \sim 10^6$ MPa \sqrt{m} s $^{-1}$ for normal velocity impact tests at ~ 5 ms $^{-1}$. In another effort, Yoon *et al.* in 2001 [4] also have proposed a few empirical correlations based on dK/dt . Amongst various equations proposed by Yoon *et al.*, the following represent the highest and the lowest values respectively:

$$\text{For A515 steel,} \quad T_0 = 6.1 \ln(dK/dt) - 18 \quad (1)$$

$$\text{For A533B weld,} \quad T_0 = 2.7 \ln(dK/dt) - 87 \quad (2)$$

In a more generalised form, Schindler *et al.* in 1995 [5] have proposed the following equation for the shift in T_0 , associated with the change in test velocity or the stress intensity factor rate as:

$$\Delta T \cong (22 - 0.016\sigma_y) \log \frac{\dot{K}_{ref}}{\dot{K}_{test}} \cong (22 - 0.016\sigma_y) \log \frac{\dot{V}_{ref}}{\dot{V}_{test}} \quad (3)$$

where σ_y is the yield stress at the test temperature/strain rate. Applicability of these equations in predicting T_0 at higher loading rates has been verified by Moitra in 2003 [6] and Moitra *et al.* in 2005 [7], while evaluating the DBTT for plain 9Cr-1Mo steel, with tempered martensitic microstructure. From the eqn. 1 & 2, for tests at 1.12 and 5.12 m/s, the T_0 yielded a shift of 14 and 6.2 °C respectively. However, eqn.3 showed a shift of ~ 7 °C based on the stress intensity based calculation and 7.5 °C based on velocity based estimation. However, experimental determination of the shift in T_0 with higher loading rates has been awaiting for these class of steels, proposed to be used as wrapper material in the upcoming commercial liquid sodium cooled fast breeder reactors. This effort has been directed towards filling up that gap with a Mod.9Cr-1Mo steel.

MATERIAL AND EXPERIMENTAL

The material under investigation is an Modified 9Cr-1Mo steel, with chemical composition of C-0.12, Mn-0.38, P-0.02, S-0.007, Si-0.47, Cr-9.42, Mo-1.0, Ni-0.13, Al-0.021, Nb-0.1, V-0.25, N-0.068, Fe-balance. The material in normalized and tempered condition possess a tempered martensitic structure.

The test protocol to evaluate T_0 under loading rate of 1.12 ms $^{-1}$ (referred as T_0^{dy}) using instrumented Charpy impact test with pre cracked Charpy specimens has been discussed in detail in few of the publications by Moitra *et al.* [6,7] and Sathyanarayanan *et al.* [8] for 9Cr-1Mo steel and Mod.9Cr-1Mo steel respectively. The present campaign with test velocities 3 ms $^{-1}$ and 5 ms $^{-1}$ have been carried out in the Accelerated Drop Weight Testing Machine, newly installed at IGCAR, Kalpakkam. The usual steps to derive the T_0^{dy} have been the same as followed for the loading rate of 1.12 m/s.

RESULTS AND DISCUSSION

The concept of evaluation of ductile to brittle transition temperature via reference temperature approach can be summarized as a macroscopic engineering conclusion derived from local events in microstructural scale. The significant premises on which the concept is based upon can be pointed as, firstly, it appreciates the application of fracture mechanics concepts in terms of elastic-plastic assessment of the fracture toughness of the material under investigation, secondly, it takes into account the inherent scatter in fracture toughness originating from the variation of the energy involved in the crack initiation process owing to the difference in size, shape and orientation of the randomly distributed carbides at the process zone. It is meaningfully assumed that the crack initiation leading to a cleavage crack has occurred at the weakest points, referred as the 'the weakest link' and the scatter is explained by an extreme value statistical model, the Weibull distribution.

The crack initiation process in this type of steel is a complex phenomenon with several interacting parameters like local cleavage fracture stress, matrix strain and the level of tri-axiality at the initiated crack [9, 10]. Ultimately the bonds in atomic scale has to be broken for a crack to initiate and can only be achieved by reaching the required stress level, commonly referred as the local cleavage fracture stress. On loading, at the region just ahead of the pre-existing fatigue crack of the specimen, the overall triaxiality situation limits the flow stress leading to a stress build up, often reaching the required cleavage fracture stress of the material. At this highly stressed region, commonly referred as the process zone, the initiation of the crack can occur at the carbides or by grain boundary embrittlement. For the carbides, it has been found to take place by two different ways depending on the size of the carbide and its interface strength with the matrix. In the plain 9Cr-1Mo steels with a tempered martensitic structure, the carbides are reported to be of small size where decohesion of the particle-matrix interface, instead of the breaking of the particle, has been identified as the driving mechanism [7]. Thus the local strain and strain rate in the

matrix surrounding the carbide is expected to play a dominant role in determining the fracture toughness of these classes of materials. As the loading rate increases, the local strain rate also simultaneously increases leading to a higher flow stress for a rate sensitive material. This in turn would lead to difficulties for the matrix surrounding the carbide to be strained, thus preventing the buildup stress around the carbide to be relaxed, and paving the way for an easier crack initiation by decohesion of the carbide-matrix interface. Contrary to the plain 9Cr-1Mo steel, for the modified 9Cr-1Mo under present investigation, the crack initiation mechanism has been identified as the grain boundary embrittlement by Sathyanarayanan *et al.* in 2008 [8]. Here also the restriction of local plasticity imposed by higher loading rate is expected to raise the level of local stress buildup and ease the crack initiation process by decohesion of the boundaries. In this connection it may be recalled that though there is a synergy for the influence of higher loading rate on both the crack initiation mechanisms, the overall fracture toughness at a temperature for the latter has been observed to be lower at the loading rate of 1.12 m.s^{-1} as the stress required for grain boundary decohesion ($\sim 1600\text{-}1800 \text{ MPa}$) is much lower than the stress required for carbide-matrix decohesion ($\sim 2200\text{-}2400 \text{ MPa}$). The significant increase in T_0^{dy} ($\sim 60 \text{ }^\circ\text{C}$) for the modified 9Cr-1Mo steel as compared to the plain 9Cr-1Mo steel is the reflection of this fact [10]. Overall, irrespective of the crack initiation mechanism involved in the material, the increase in loading rate actually makes the material to crack in a more brittle manner and should be reflected in the increase in the ductile brittle transition temperature. This is reflected in the T_0^{dy} values obtained for Mod.9Cr-1Mo steel in the loading rates of $1.12, 3$ and 5 m.s^{-1} , as given in Table 1, 2 and 3.

Apart from the metallurgical significance of higher loading rate on the fracture toughness, there has been a mechanistic influence on the testing protocol, which needs to be discussed. The material under dynamic loading actually undergoes a series of forward and reflective stress waves, at least in the initial part of the loading, and is observed as high frequency oscillations in the load-time or load-displacement traces. Thus in determining the dynamic fracture toughness of materials the inertia effect on the fracture load determination is very important in data analysis, especially for higher loading rates or for that of brittle materials. The amplitudes of the consecutive oscillations are expected to die down with time after the impact and considered negligible after a period of $2\tau\text{-}3\tau$ [11 Ireland], where τ is the period of inertial oscillation, a function of specimen type and geometry and speed of sound through this material. A few estimation procedure of τ is available in the literature. Earlier in 1978, Server [12] JTEV-1978] has proposed an empirical equation incorporating specimen dimensions and compliance for estimating τ for a specimen with span to width ratio (s/w) of 4 as:

$$\tau = 3.36 (w/S_0)(YBC_s)^{0.5} \quad (4)$$

where B is specimen thickness (10 mm here), C_s is the specimen compliance, Y is the Young's modulus and S_0 is the speed of sound in the specimen material. YBC_s is also referred as C_s^* , the non-dimensional specimen compliance, and expressed as a function a_0/w [12, 13]. Using this equation for the present case of deeply cracked specimen with $a_0/w \sim 0.5$ and considering velocity of sound as 6000 m/s in steel the τ is estimated to be $43 \text{ } \mu\text{s}$. Later Liu *et al.* [14] has conducted a more rigorous and analytical solution of the displacement of the three point loaded beam under dynamic loading which is proposed to be composed of two components, a vibration term and a displacement term caused by the dynamic load. In his analysis Liu has considered that the Charpy specimen undergoes a forced second grade vibration (shown in Fig. 1) in both half lengths in the same phase and calculated the period of vibration as given in eqn.5.

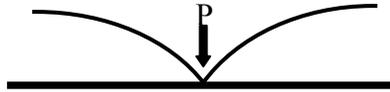


Fig.1 Forced Second Grade Vibration Mode

$$\omega = (\pi/s)^2 (YJ/\rho A)^{0.5}, \text{ and } \tau = 1/\omega \quad (5)$$

where J is the inertia moment of cracked specimen, ρ is the density of the material and A is the cross-sectional area of the specimen. With a span length of 55 mm , τ is calculated to be $32 \text{ } \mu\text{s}$, much less than Server's equation. Following another empirical correlation, similar to Server, proposed by Ireland [15, 1997] as:

$$\tau = 1.68 (l/S_0)(B/l)^{0.5}(YwC_s)^{0.5}, \text{ where } C_s = l^3/(4YwB^3) \quad (6)$$

with span as 40 mm and velocity of sound in steel as 6000 m/s , the period for vibration, τ is estimated to be $22.4 \text{ } \mu\text{s}$. For the analysis of the fracture load to determine the dynamic fracture toughness in the present campaign, it is being

thought that an analytical solution is more realistic than an empirical one and thus, the Liu’s proposition is more acceptable than the others. In the present situation the span length of 40 mm instead of 55 mm (used by Liu *et al.* [14]) would further reduce the τ . From an engineering stand point, the time period, τ , is taken as $27 \mu\text{s}$ (average of 22 and $32 \mu\text{s}$) for the present campaign, implying that after 3τ i.e $81 \mu\text{s}$, all the measured fracture loads are free from any inertial effects. One observation can be noted that in all the above treatments for evaluating the period of vibration (τ), the loading rate has never been a part of the equation. However, the loading rate does influence the amplitude of the oscillations, which increases with the loading rate. Figures 2a, 2b and 2c shows the load-displacement traces obtained from the present campaign with loading rates of 1.12, 3 and 5 m/s respectively.

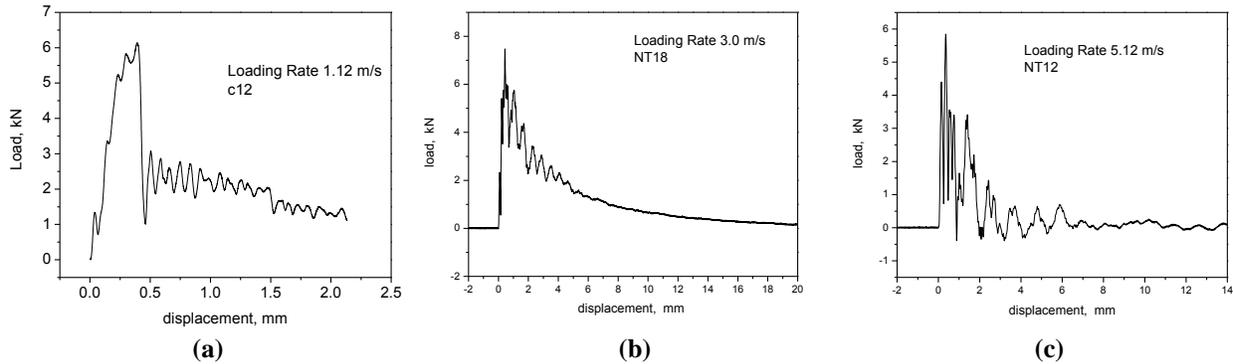


Fig.2 (a,b,c) representative load-displacement plots obtained at loading rates 1.12, 3 and 5 m/s

It can be noted that when the amplitude of vibration is negligible for the loading rate of 1.12 m/s, it is significantly present in loading rate of 3m/s and visually dominant for the loading rate of 5 m/s. Thus the time to fracture (t_f) with respect to the $2\tau - 3\tau$ time span is an important parameter, especially for the loading rates of 3 and 5 m/s, in ensuring the accuracy of the fracture load determination towards evaluating the dynamic fracture toughness. The relevant results obtained for the loading rate of 1.12 m/s and the calculated T_0^{dy} is given in Table.

Spec. No:	Test Temp. (°C)	Crack Length (a_o , mm)	K_{Jd} (MPa.m ^{0.5}) for 10 mm thickness	Validity limits (MPa.m ^{0.5}) (10 mm)	Validity Results	T_0^{dy} (°C)
C8	-30	5.30	54.92	141	Valid	8.8
C9	-30	5.40	65.93	151	Valid	
C6	-30	5.35	82.55	148	Valid	
C11	-30	5.39	61.75	148	Valid	
C12	-30	5.33	105.19	148	Valid	
C13	-30	5.31	61.40	148	Valid	
C14	-30	5.32	82.01	148	Valid	
C7	-30	5.47	65.26	150	Valid	
C10	-30	5.32	86.13	151	Valid	

Table 1. K_{Jd} of different specimens and T_0^{dy} Mod. 9Cr-1Mo steel at a loading rate of 1.12 m/s

Tables 2 summarizes the K_{Jd} , obtained from different specimens and T_0^{dy} of this steel at a loading rate of 3 m/s along with the estimated time to fracture (t_f) for each specimens. It is observed that the time to fracture (t_f) for the loading rate of 3 m/s has exceeded the 3τ i.e $67.2 \mu\text{s}$ time span for all the specimens, the minimum being $100 \mu\text{s}$ for the specimen No. NT16. Thus the loads taken for calculation of the dynamic fracture toughness in 3 m/s loading rate are considered free from inertial effects. It may be noted that due to practical reasons, the tests have been carried out at different temperatures instead of a single temperature. Thus the multi-temperature approach, as suggested in ASTM E 1921 has been adopted for evaluating T_0^{dy} .

Spec. No:	Test Temp. (°C)	Crack Length (a_0 , mm)	Time to Fracture, (t_f , μ s)	K_{Jd} ($\text{MPa}\cdot\text{m}^{0.5}$) for 10 mm thickness	Validity limits ($\text{MPa}\cdot\text{m}^{0.5}$) (10 mm)	Validity Results	T_0^{dy} (°C)
NT 16	0	5.15	100	58.39	150	Valid	11.9
NT 17	0	5.21	110	79.78	150	Valid	
NT 20	10	5.19	140	109.52	152	Valid	
NT 21	10	5.29	250	112.58	152	Valid	
NT 22	10	5.39	140	113.61	152	Valid	
NT 23	20	5.13	320	179.89	155	Invalid	
NT 24	20	5.43	350	193.54	155	Invalid	
NT 25	20	5.56	150	182.92	155	Invalid	
NT 18	20	5.24	140	110.63	155	Valid	

Table 2. K_{Jd} of different specimens and T_0^{dy} Mod. 9Cr-1Mo steel at a loading rate of 3 m/s

However, the time to fracture has been much less for loading rate of 5 m/s. Here it is observed that out of eight specimens tested at different temperatures ranging from -25 to 30 °C, four specimens (Nos. NT11, NT12, NT14, NT19) are at the border line of 2τ - 3τ regime and one specimen (NT4) with $t_f = 10 \mu\text{s}$ fails to qualify even for consideration. For the t_f in the borderline situation, there are ways have been suggested in literature for a meaningful estimation of fracture load. Initially, Turner [16], based on a beam-on-foundation model, has suggested correction factors (γ) for tup loads, and plotted with respect to the a_0/w ratio, as given in Fig. 3. In another procedure, Varga [17], proposed to derive a critical load (P_C) from traces showing failure at $t_f < 2\tau$ and given in Fig. 4. For the above four specimens, both the procedures (Turner’s and Varga’s) are applied to derive a meaningful fracture load for dynamic fracture toughness estimation.

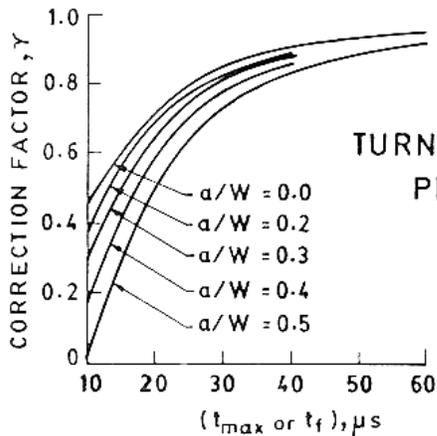


Fig.3. Turner’s procedure for fracture load estimation

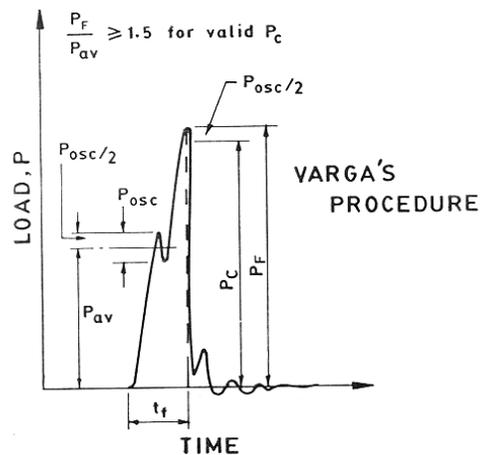


Fig.4. Varga’s procedure for fracture load estimation

The relevant results along with the revised load estimations with Turner’s and Varga’s procedure are given in Tables 3 and 4 respectively. Similar to the case of loading rate of 3 m/s, here also the multi-temperature approach, as suggested in ASTM E 1921 has been adopted for evaluating T_0^{dy} . It can be seen that the Vargas’ procedure gives slightly conservative load, and is reflected on the higher T_0^{dy} at the loading rate of 5 m/s. For a conservative DBTT estimation of this material, the higher T_0^{dy} , i.e. 26.6 °C is taken as T_0^{dy} for the loading rate of 5 m/s.

Spec. No:	Test Temp. (°C)	Crack Length (a_0) mm	Time to Fracture, (t_f , μ s)	Corrected load, Turner (kN)	K_{Jd} (MPa.m ^{0.5})	Validity limits (MPa.m ^{0.5})	Validity Results	T_0^{dy} (°C)
NT 11*	-25	5.19	60	*5.23	59.81	155	Valid	24.0
NT 12*	-20	5.38	70	*5.23	63.49	157	Valid	
NT 13	-10	5.21	110		90.51	184	Valid	
NT 14*	0	5.49	70	*6.84	85.95	183	Valid	
NT 3	21	4.97	120		83.25	182	Valid	
NT 4**	21	6.67	10	-----	-----	-----	-----	
NT 19*	21	5.22	70	*7.09	81.05	181	Valid	
NT 15	30	5.25	310		234	179	Invalid	

*Load correction for marginal cases with respect to t_f . ** Not Considered due to low t_f .

Table 3. K_{Jd} of different specimens and T_0^{dy} Mod. 9Cr-1Mo steel at a loading rate of 5 m/s using Turner’s Procedure

Spec. No:	Test Temp. (°C)	Crack Length (a_0) mm	Time to Fracture, (t_f , μ s)	Corrected load, Varga, (kN)	K_{Jd} (MPa.m ^{0.5})	Validity limits (MPa.m ^{0.5})	Validity Results	T_0^{dy} (°C)
NT 11*	-25	5.19	60	*4.1	46.57	155	Valid	26.6
NT 12*	-20	5.38	70	*4.0	48.24	157	Valid	
NT 13	-10	5.21	110		90.51	184	Valid	
NT 14*	0	5.49	70	*5.86	73.70	183	Valid	
NT 3	21	4.97	120		83.25	182	Valid	
NT 4**	21	6.67	10	-----	-----	-----	-----	
NT 19*	21	5.22	70	*6.0	68.57	181	Valid	
NT 15	30	5.25	310		234	179	Invalid	

*Load correction for marginal cases with respect to t_f . ** Not Considered due to low t_f .

Table 4. K_{Jd} of different specimens and T_0^{dy} Mod. 9Cr-1Mo steel at a loading rate of 5 m/s using Varga’s Procedure.

The variation of T_0^{dy} with the loading rate is given in Fig. 5. T_0^{dy} values determined were 8.8 and 11.2 °C for the loading rates of 1.12 and 3 m/s respectively, and increases drastically to 26.2 °C for a loading rate of 5 m/s. This shift is little higher than that predicted empirically (~14 °C) for conventional pressure vessel steels, as estimated by Yoon *et al.* in 2001 [4].

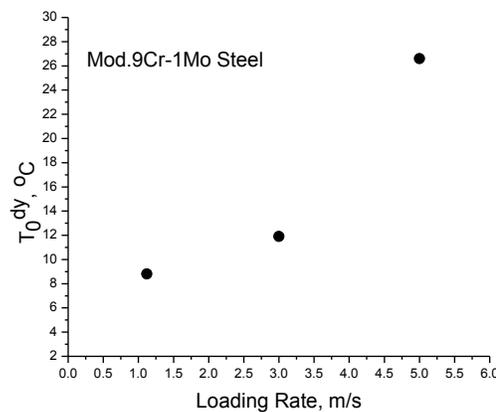


Fig. 5 Variation of T_0^{dy} with the loading rate for the Mod.9Cr-1Mo steel.

CONCLUSION

T_0^{dy} values determined were 8.8 and 11.2 °C for the loading rates of 1.12 and 3 m/s respectively, and increases drastically to 26.2 °C for a loading rate of 5 m/s. This shift is little higher than that predicted empirically (~14 °C) for conventional pressure vessel steels, as estimated by Yoon *et al.* in 2001 [4]. The above result also indicates that the stress intensity rate (dK/dt) based approaches to estimate the effect of loading rate has been more closer to the experimental observations.

REFERENCES

- [1] ASTM E 1921-05 "Test Method for Determination of Reference Temperature, T_0 , for Ferritic Steels in the Transition Range", ASTM, 2005.
- [2] Wallin K., *Advances in Fracture Research (Proc. Int. Conf. Fracture, ICF-9)*, B.L. Karihaloo, Y.W. Mai, M.I. Ripley and R.O. Ritchie (Eds.), Sydney, Australia, **Vol. 5** (1997) p.2333.
- [3] Wallin K. and Planman T., *IAEA Specialist Meeting on Master Curve Testing and Result Application.*, 17-19 September, Prague, Czech Republic, 2001.
- [4] Yoon K.K., Hall J.B., Van Der Sluys, Higuchi M and Lida K., The 2001 ASME Pressure Vessel and Piping Conference, July 23-26, Atlanta, Georgia, USA (2001) p.1.
- [5] Schindler H.J., Varga T., Njo D.H. and Prantl G., *Materials Aging and Component Life Extension*, Vol. 2, V. Bicego, A. Nitta, R. Viswanathan [Eds.], Proc. of Int. Symp. on *Material Ageing and Component Life Extension*, Milan, Italy, 10-13 Oct. (1995) p.1367.
- [6] Moitra A., 'Ductile-Brittle Transition Temperatures and Dynamic Fracture Toughness of 9Cr-1Mo Steel', PhD Thesis, Banaras Hindu University, 2003.
- [7] Moitra A., Sreenivasan P.R., Mannan S.L. and Singh V., *Metallurgical and Materials Transactions A.*, **36A** (2005) 2957.
- [8] Sathyanarayanan S., Moitra A., Samuel K. G., Sasikala G., Ray S.K., Singh V., *Mater. Sci. & Engineering -A*, **488**, Issue 1-2 (2008) p.519.
- [9] Ritchie R.O., Knott J.F. and Rice J.R., *J. Mech. Phys. Solids*. **21** (1973) p.395.
- [10] Chen J.H., Wang Q, Wang G.Z. and Li Z., *Acta Materialia*, **51** (2003) p.1841.
- [11] Ireland D.R., in *Instrumented Impact Testing*, ASTM STP 563, American Society for Testing and Materials, Philadelphia (1974) p.3.
- [12] Server W.L., *J Testing Evaluation*, **6**(1978) p.29.
- [13] Sreenivasan P.R., *Trans. Indan Inst. Met.*, **49, No.5**, (1996) p.677.
- [14] Liu Y., Zhu J., and Zhou H., *Engg. Frac. Mech*, **39**, No.6, (1991) p.955
- [15] Ireland D.R. in *Proc. Int. Conf. On Dynamic Fracture Toughness*, Cambridge, UK, The Welding Institute, (1997) p.45.
- [16] Turner C.E. in *Impact Testing of Metals*, ASTM STP 466, American Society of Testing and Materials, Philadelphia (1970) p.93.
- [17] Varga T., Njo D.H. and Prantl G., ASK-AN-425, Rev 1.(1978), in *CSNI Specialist Meeting on Instrumented Precracked Charpy Testing* (Proc, Nov 1980). Ed. R.A. Wullaert, EPRI NP-2102-LD (1981), p. 1-65.