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Fracture Toughness Prediction for RPV Steels with Various Degree of Embrittlement

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ABSTRACT

In the present report, predictions of the temperature dependence of cleavage fracture toughness are performed on the basis of the Master Curve approach and a probabilistic model named now the Prometey model. These predictions are performed for reactor pressure vessel steels in different states, the initial (as-produced), irradiated state with moderate degree of embrittlement and in the highly embrittled state. Calculations of the $K_{IC}(T)$ curves may be performed with both approaches on the basis of fracture toughness test results from pre-cracked Charpy specimens at some (one) temperature. The calculated curves are compared with test results. It is shown that the $K_{IC}(T)$ curves for the initial state calculated with the Master Curve approach and the probabilistic model show good agreement. At the same time, for highly embrittled RPV steel, the $K_{IC}(T)$ curve predicted with the Master Curve approach is not an adequate fit to the experimental data, whereas the agreement of the test results and the $K_{IC}(T)$ curve calculated with the probabilistic model is good. An analysis is performed for a possible variation of the $K_{IC}(T)$ curve shape and the scatter in K_{IC} results.

KEY WORDS: cleavage fracture toughness, Master Curve approach, local approach, Prometey probabilistic model, reactor pressure vessel steel, embrittlement.

INTRODUCTION

Fracture toughness of irradiation-embrittled reactor pressure vessel (RPV) steels is known to be experimentally determined only from small-sized specimens tested over a limited temperature range. To obtain the fracture toughness versus temperature curve, the prediction methods such as the Master Curve approach [1-4] and a probabilistic model [5-9] named now the Prometey model may be used.

At present the Master Curve approach is widely verified to predict the temperature dependence of fracture toughness for RPV steels with various degree of embrittlement. However, this approach uses the lateral temperature shift to describe the $K_{IC}(T)$ curves that may restrict its applicability for highly embrittled steels.

The Prometey model does not include any assumptions concerning the shape of the $K_{IC}(T)$ curve and the temperature lateral shift condition and provides a prediction of the $K_{IC}(T)$ curve allowing for the possibility of both a shift and a variation in shape. The probabilistic model was verified by application to RPV steels for WWER in various states [5-9].

At the same time, the Master Curve approach is more suitable for engineering application as simple method as compared with the Prometey model. Therefore it is important to know the applicability conditions for both approaches.

The present report consists of four parts: in the first one, the Prometey probabilistic model is briefly presented. In the second part, predictions of the $K_{IC}(T)$ curves for RPV materials in various states are considered on the basis of the Master Curve approach and the Prometey probabilistic model. In the third and fourth parts, analysis is given for a possible variation of the $K_{IC}(T)$ curve shape and the scatter in K_{IC} results.

PRESENTATION OF THE PROMETEY PROBABILISTIC MODEL BASED ON NEW CLEAVAGE FRACTURE CRITERION

The Local Criterion for Cleavage Fracture

The formulation of the local cleavage fracture criterion in a probabilistic manner is as follows [6, 10].

1. The polycrystalline material is viewed as an aggregate of cubic unit cells. The mechanical properties for each unit cell are taken as the average properties obtained by standard specimen testing. The size of the unit cell ρ_{uc} is never less than the average grain size. The stress and strain fields in the unit cell are assumed to be homogeneous.

2. The local criterion for cleavage fracture of a unit cell is taken as

$$\sigma_1 + m_{T\varepsilon}\sigma_{eff} \geq \sigma_d \tag{1a}$$

$$\sigma_1 \geq S_C(æ) \tag{1b}$$

where the critical brittle fracture stress, $S_C(æ)$, is calculated by

$$S_C(æ) = \left[C_1^* + C_2^* \exp(-A_d æ)\right]^{-1/2} \tag{2}$$

Here, σ_1 is the maximum principal stress, the effective stress is $\sigma_{eff} = \sigma_{eq}-\sigma_Y$, σ_{eq} is the equivalent stress, σ_Y is the yield stress, $æ = \int d\varepsilon^p_{eq}$ is Odqvist's parameter, $d\varepsilon^p_{eq}$ is the equivalent plastic strain increment, C_1^*, C_2^*, A_d are material constants, σ_d is the strength of carbides or "carbide-matrix" interfaces or other particles on which cleavage microcracks are nucleated, $m_{T\varepsilon}$ is a parameter that depends on temperature T and plastic strain and may be written as

$$m_{T\varepsilon} = m_T(T)m_\varepsilon(æ), \tag{3}$$

$$m_\varepsilon(æ) = S_0/S_C(æ), \tag{4}$$

$$m_T(T) = m_0\sigma_{Ys}(T) \tag{5}$$

where $S_0 \equiv S_C(æ=0)$, m_0 is a constant which may be experimentally determined and σ_{Ys} is the temperature-dependent component of the yield stress. Condition (1a) is the nucleation condition for cleavage microcracks. Condition (1b) is the propagation condition for cleavage microcracks.

3. To formulate criteria (1) in a probabilistic way, it is assumed that the parameter σ_d is stochastic and the remainder of the parameters controlling brittle fracture are deterministic. Such an assumption is based on analysis of the stochastic nature of various critical parameters controlling cleavage fracture of RPV steels [5].

4. To describe the distribution function for the parameter σ_d, the Weibull law is used: the minimum strength of carbides in the unit cell on which cleavage microcracks are nucleated, is assumed to obey

$$p(\sigma_d) = 1 - \exp\left[-\left(\frac{\sigma_d - \sigma_{d0}}{\tilde{\sigma}_d}\right)^\eta\right] \tag{6}$$

where $p(\sigma_d)$ is the probability of finding in each unit cell a carbide with minimum strength less than σ_d; $\tilde{\sigma}_d$, σ_{d0} and η are Weibull parameters.

5. The weakest link model is used to describe the brittle fracture of the polycrystalline material.

6. It is considered that brittle fracture may happen only in unit cells for which the conditions $\sigma_{eq} \geq \sigma_Y$ and $\sigma_1 \geq S_C(æ)$ are satisfied.

The Probabilistic Model for the $K_{IC}(T)$ Curve Prediction

The probabilistic model for fracture toughness prediction is based on the local criterion described above. The stress and strain fields near the crack tip are calculated by FEM or on the crack extension line with an approximate analytical solution [6].

The brittle fracture probability of a cracked specimen, P_f, may be presented in the form used in [11]

$$P_f = 1 - \exp\left[-\left(\frac{\sigma_w}{\tilde{\sigma}_d}\right)^\eta\right] \tag{7}$$

where the Weibull stress σ_w is

$$\sigma_w = \left[\sum_{i=1}^{k}\left(\max(S^i_{nuc}) - \sigma_{d0}\right)^\eta\right]^{1/\eta}$$

$$S^i_{nuc} \equiv \begin{cases} \sigma^i_{nuc}, \text{ if } \sigma^i_1 \geq S_C(æ_i) \text{ and } \sigma^i_{nuc} > \sigma_{d0} \\ \sigma_{d0}, \text{ if } \sigma^i_1 < S_C(æ_i) \text{ or } \sigma^i_{nuc} \leq \sigma_{d0} \end{cases} \tag{8}$$

Here $\sigma_{nuc} \equiv \sigma_1 + m_{T\varepsilon}\sigma_{eff}$; k is the number of unit cells in the plastic zone, i is the number of a unit cell. For each unit cell, the parameter $\max(S^i_{nuc})$ is the maximum value of S^i_{nuc} from the beginning of deformation up to the current moment. The above equations provide the calculation of the brittle fracture probability dependence on stress intensity factor $P_f(K_I)$ as the parameter σ_w is a function of K_I.

To predict the $K_{IC}(T)$ curve on the basis of the model proposed above, it is necessary to know the parameters $S_C(æ)$, $m_T(T)$, $\tilde{\sigma}_d$, σ_{d0} and η and also parameters describing plastic deformation to enable the stress and strain fields to be calculated. The parameters $\tilde{\sigma}_d$ and η may be determined from test results of small-sized fracture toughness specimens at one temperature. The rest of parameters are determined from uniaxial tension tests of standard cylindrical specimens.

PREDICTION OF THE K_{IC}(T) CURVE FOR RPV STEELS IN VARIOUS STATES ON THE BASIS OF THE MASTER CURVE APPROACH AND THE PROMETEY MODEL

In the present section, the temperature dependencies of fracture toughness for RPV materials in the initial and embrittled states predicted on the basis of the Master Curve approach and the Prometey model are presented and compared with experimental values of fracture toughness. For the irradiated weld, the K_{JC}(T) curves predicted with the Master Curve approach and the probabilistic model are compared.

The parameter T_0 in the Master Curve equation and the parameters $\tilde{\sigma}_d$ and η in the Prometey probabilistic model were calculated on the basis of fracture toughness test results from the same cracked specimens. The stress-strain curve parameters were determined using test results for smooth cylindrical specimens. Experimental procedures are in detail represented in [9]. The unit cell size ρ_{uc} was taken to be equal to 0.05 mm for all the investigated materials. According to [6], it was taken that m_0=0.1 MPa^{-1}.

Fracture Toughness for 2Cr-Ni-Mo-V Steel in the Initial State

Both models were calibrated using 2T-CT specimens. In the Prometey model, the parameters were calibrated from test results at T=-60°C as $\tilde{\sigma}_d$ =17323 MPa and η=6,2. By applying the Master Curve, the parameter T_0 was determined in two ways: by multi-temperature method as T_0=-84.5°C, and by single temperature method from 2T-CT specimen test results at T= -60°C as T_0=-85.8°C [8]. A comparison of the predicted curves and the experimental data is given in Fig. 1. Both methods predict the experimental behaviour very well.

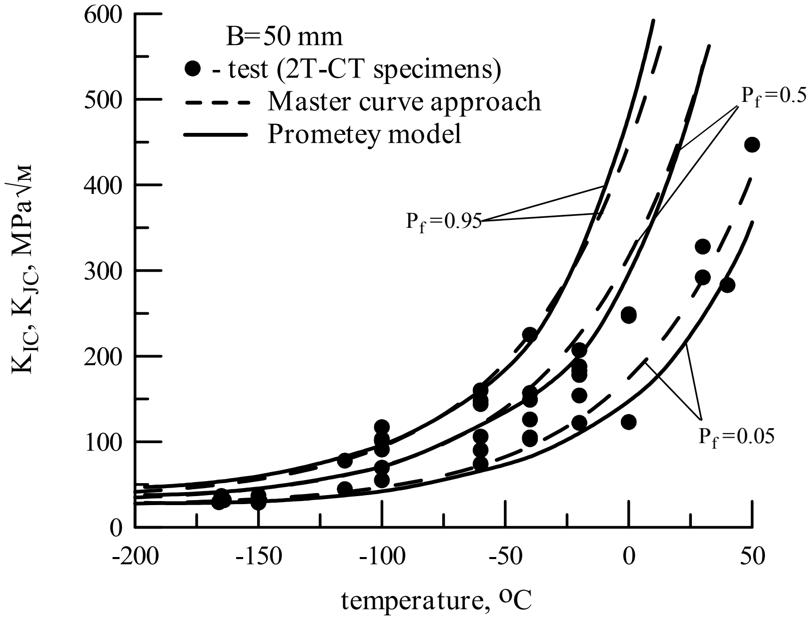

Fig. 1. Comparison of the predicted fracture toughness curves and test results for 2Cr-Ni-Mo-V steel in the initial state.

Fracture Toughness for RPV Weld Metal in the Irradiated State

RPV weld metal for WWER-1000 from surveillance capsules of the Kalinin-1 NPP was tested at Kurchatov Institute in irradiated conditions (ΔT_{41J}=69°C) [8]. Reconstituted Charpy specimens and sub-size smooth tensile specimens were used. The parameters for the Master Curve and Prometey models were calibrated on the basis of test results from pre-cracked Charpy specimens at T= -25°C (7 tests). The parameters $\tilde{\sigma}_d$ and η were determined as follows: $\tilde{\sigma}_d$=6960 MPa and η=8.0. The parameter T_0 is equal to T_0= -24.9°C [8].

The fracture toughness curves as predicted with the obtained parameters are shown in Fig. 2. The agreement between the curves predicted by the Master Curve and the Prometey model is again very good.

Fracture Toughness for 2Cr-Ni-Mo-V Steel in the Highly Embrittled State

The material under investigation is a base metal for WWER 1000 RPVs after a special thermal heat treatment (ΔT_{41J} = 180°C). The parameter T_0 in the Master Curve equation and parameters in the local criterion of cleavage fracture were determined on the basis of results from pre-cracked Charpy specimens at temperature T=30°C (14 tests). The parameters $\tilde{\sigma}_d$ and η were calculated as follows $\tilde{\sigma}_d$ = 4103 MPa and η=12. The parameter T_0 was determined as T_0= 36.8°C [9]. The predicted curves are compared with experimental values of fracture toughness for 2T-CT specimens (Fig. 3 and 4). The agreement of the calculated and test data is good for the Prometey model (Fig. 3). At the same time the experiments could not be adequately described by the Master Curve method (Fig. 4). It should be noted that the difference between the test results and the Master Curve is connected with a variation of the shape of the $K_{IC}(T)$ curve for the embrittled steel [9]. It has been shown [9] that the slope of this $K_{IC}(T)$ curve for the embrittled steel is significantly less than that used in the Master Curve.

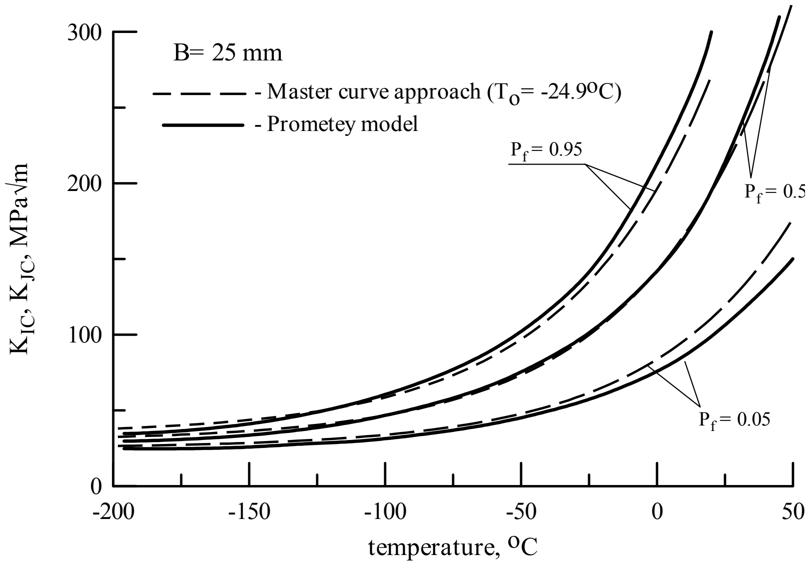

Fig. 2. Comparison of the predicted fracture toughness curves for irradiated weld.

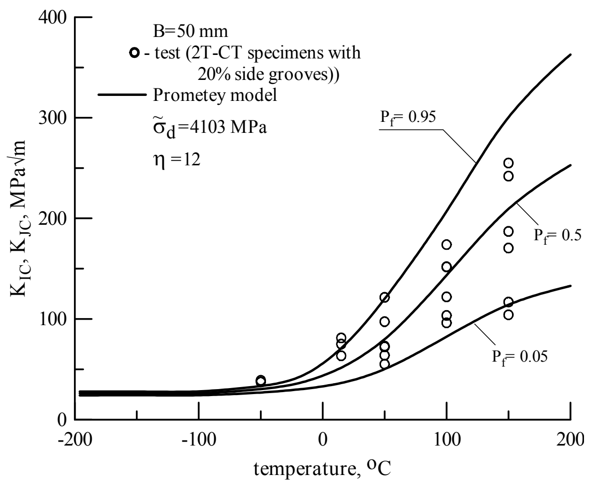

Fig. 3. Comparison of the predicted fracture toughness curves by the Prometey probabilistic model and test results (2Cr-Ni-Mo-V steel, the embrittled state).

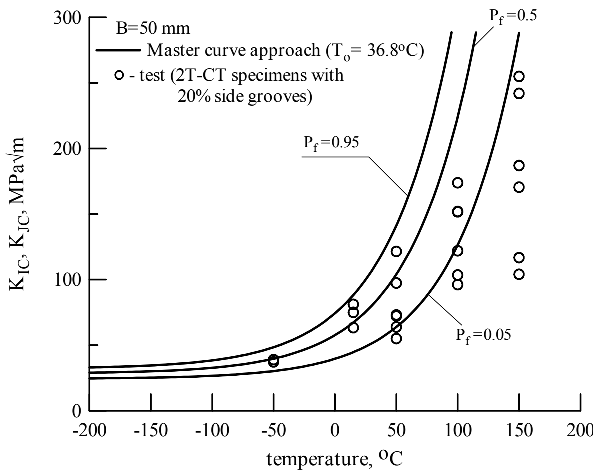

Fig. 4. Comparison of the predicted fracture toughness curves by the Master Curve approach and test results (2Cr-Ni-Mo-V steel, the embrittled state)

ANALYSIS OF THE LATERAL SHIFT CONDITION AND A POSSIBLE VARIATION OF THE $K_{IC}(T)$ CURVE SHAPE ON THE BASIS OF THE PROMETEY PROBABILISTIC MODEL

It is seen from Figs. 1 and 2 and from many other studies that the Master Curve approach provides adequate predictions for RPV materials in initial (as-produced) state and for embrittled materials with moderate degrees of embrittlement. It means that the lateral temperature shift is valid for these states. At the same time the shape of the $K_{IC}(T)$ curve for highly embrittled steel (Fig. 4) varies as compared with initial state.

From point of view of the known local cleavage fracture approaches as the Knott and Beremin models [11, 12], a shape of $K_{IC}(T)$ curve should vary for any degree of embrittlement as compared with the initial state. Indeed, in the RKR-model [12] and others, an universally accepted formulation of the local brittle fracture criterion is used

$$\sigma_{eq} \geq \sigma_Y$$
$$\sigma_1 \geq S_C,$$

Then according to this criterion, $K_{IC}(T)$=function(S_C, $\sigma_Y(T)$, strain hardening). It is known that, firstly, S_C does not depend on temperature, secondly, $\sigma_Y(T)$ is highly sensitive to temperature over low temperature range and weakly sensitive over RPV operation temperature range (20…300°C), thirdly, the strain hardening parameters are weakly sensitive to temperature over RPV operation temperature range (20…300°C) [7].

Then the conclusion should be drawn that the dependence $K_{IC}(T)$ is practically controlled by $\sigma_Y(T)$. So, two consequences follow from this – firstly, that as a degree of embrittlement increases, the $K_{IC}(T)$ curve shape changes, that is the lateral shift should not be observed and, secondly, that for high embrittlement, K_{IC} is independent on temperature practically. At the same time, applicability of the lateral shift condition was experimentally confirmed for RPV steels with low and moderate degree of embrittlement. To provide the lateral shift for $K_{IC}(T)$ on the basis of the models [11, 12] it is necessary to assume that the parameter S_C increases as temperature increases or, strain hardening decreases strongly as temperature increases that contradicts experimental data.

It is shown in Fig. 5 that the Prometey probabilistic model provides, on the one hand, the strong confirmation for the temperature lateral shift for definite conditions, and, on the other hand, predicts a variation of the $K_{IC}(T)$ curve shape for highly embrittled materials. These two possibilities are clearly deduced from Eq. (1a).

Over the temperature range where m_T is large and $\sigma_1 < m_T \cdot m_\varepsilon \cdot (\sigma_{eq} - \sigma_Y)$, i.e. the fracture condition is mainly controlled by the second term, the $K_{IC}(T)$ curve is practically controlled by $m_T(T) = const \cdot \sigma_{Ys}(T)$. Over this temperature range (low temperatures in Fig. 5), the lateral shift conception for the $K_{IC}(T)$ curve is valid. This is observed for initial state and slightly embrittled states.

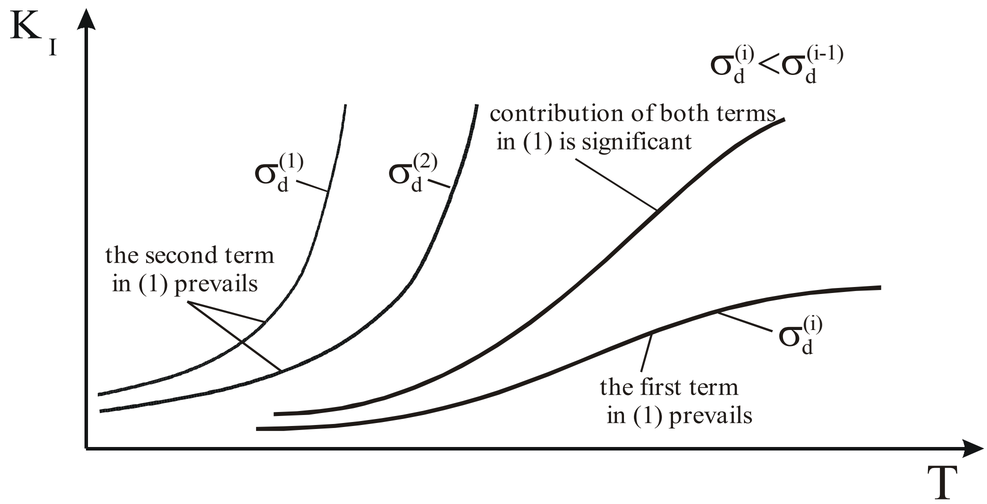

Fig. 5. The lateral shift and a possible variation of shape of the $K_{IC}(T)$ curve as predicted with the Prometey model (schematic).

As temperature increases (the room and moderate temperatures in Fig. 5), the contribution of σ_1 increases and $m_T \cdot m_\varepsilon \cdot (\sigma_{eq} - \sigma_Y)$ decreases, so that contributions of both terms in (1a) are significant. Shape of $K_{IC}(T)$ begins to change. So, over this temperature range the $K_{IC}(T)$ curve shape varies as compared with initial condition. This is predicted for moderately and highly embrittled states.

As temperature increases (elevated temperatures in Fig. 5), $\sigma_{Ys} \rightarrow 0$ and as a result $m_T \rightarrow 0$. Then the first term in (1a) becomes significantly larger than the second, $\sigma_1 >> m_T \cdot m_\varepsilon \cdot (\sigma_{eq} - \sigma_Y)$, i.e brittle fracture is mainly controlled by the first term. Taking into account that over elevated temperature range, σ_Y and strain hardening are weakly sensitive to temperature we have $K_{IC}(T) \approx const$ at $m_T \rightarrow 0$. It is quite clear that this situation may be predicted for extremely-embrittled materials.

ANALYSIS OF THE SCATTER IN K_{IC} RESULTS

It was theoretically shown in [1, 11] that the two parameter model of the Weilbull distribution, in the case of fracture toughness, may be written in the form

$$P_f = 1 - \exp\left[-\left(\frac{K_I}{K_0} \right)^b \right] \quad (9)$$

with b=4. Here K_0 is a normalization factor and b is an exponent describing the scatter in K_{IC}.

Eq. (9) may be considered as the dependence of P_f on K_I and interpreted as follows: $K_{IC} \le K_I$ with probability P_f. Experimental studies [13-15] show that the parameter b in Eq. (9) varies from 2 to 6 that depends on test temperature and material properties. It is clear that this result is in contradiction to theoretical result [1, 11] that proves that the parameter b is equal to 4, independently of local properties of a material.

In this section, an attempt is undertaken to consider various cases for which b=4 and b≠4.

The main assumptions taken for theoretical proof of b=4 in Eq. (9) may be represented as follows.

1. The probability of brittle fracture of some reference volume V_0 is given by the Weibull distribution function

$$p_f = 1 - \exp\left[-\left(\frac{\Phi}{\Phi_0}\right)^{\eta}\right]$$

where Φ_0 and η - the Weibull parameters, Φ - parameter which controls brittle fracture (for the Beremin and Wallin models $\Phi \equiv \sigma_1$, for the Prometey model $\Phi \equiv \max(\sigma_{nuc} - \sigma_{d0})$.

2. Stress and strain are inhomogeneous over the reference volume V_0.

3. Condition of the size scale similarity is satisfied over the whole range adjoining the crack tip, i.e. any parameter of the stress and strain fields near the crack tip is a function of the relative coordinate $\bar{r} = r\left(K_I/\sigma_Y\right)^{-2}$ and θ only (r and θ - the polar coordinates).

4. The brittle fracture probability of the infinite small volume $dV \ll V_0$ is given by

$$p_f^{dV} = 1 - \exp\left[-\left(\frac{\Phi}{\Phi_0}\right)^{\eta}\frac{dV}{V_0}\right]$$

It follows from 1-4 and the weakest link theory that the brittle fracture probability of cracked specimen is [1, 11]

$$P_f = 1 - \exp\left[-\frac{K_I^4 B}{\Phi_0^{\eta}\sigma_Y^4 V_0}\int_{\bar{S}_w}\left(\Phi(\theta,\bar{r})\right)^{\eta}\bar{r}\,d\theta\,d\bar{r}\right] \tag{10}$$

where $\bar{S}_w \cdot B$ is the relative volume of the plastic zone. It is assumed here that the plastic zone near the crack tip may be taken as the working volume of a material (volume in which cleavage fracture may be initiated).

As seen from Eq. (10), the terms over the integral do not depend on K_I, therefore a conclusion similar to that in [1] may be drawn that the scatter in K_{IC} does not depend on the local heterogeneity of a material and this scatter is a constant value which may be characterized by b=4 in Eq. (9).

Consider assumptions 1-4 in detail. According to local approach, polycrystalline material is viewed as a conglomerate of unit cells of finite sizes (not the infinite small volume as in consideration 4 above). Over each unit cell, strain and stress fields are averaged over the unit cell volume and are homogeneous. As a result the condition of the size scale similarity (condition 3 above) which is valid for non-structural continuum does not satisfied for unit cells nearest to the crack tip [5]. Analogy may be given for such cracked body and body with notch of the notch radius of $R=\rho_{uc}$: strain and stress fields for the unit cells nearest to the crack and notch tips are similar. It is clear that on the distance larger than 1…2ρ_{uc} from the crack tip, the size scale similarity is valid for strain and stress fields.

It is clear that if $\rho_{uc} \ll r_p$ (r_p is the plastic zone size) and the probabilities of brittle fracture of unit cells differ insignificantly, then the contribution of the unit cells for which the size scale similarity is not observed, in the fracture probability of cracked specimen, may be neglected. In [1] it is taken $\Phi \equiv \sigma_1$. As the dependence $\sigma_1(\bar{r})$ is small-gradient function, the fracture probabilities of the nearest to the crack tip unit cells do not prevail. Hence, in the model proposed in [1] the non-observance of the size scale similarity may be neglected and, as a result, b=4 independently of local heterogeneity of a material.

However, if the brittle fracture probabilities of the nearest to the crack tip unit cells are prevalent, then non-observance of the size scale similarity can not be neglected. In this case, the local properties of unit cells (namely, the parameters Φ_0 and η) affect the scatter in K_{IC}, and it may be b≠4. The cases when b≠4 are as follows.

1. The unit cell size ρ_{uc} is very close to the plastic zone size r_p. It is clear that this condition is satisfied on the lower shelf of $K_{IC}(T)$. Then the fracture probability of the unit cell nearest to the crack tip determines the fracture probability of cracked specimen, i.e. the parameter η in the Weibull distribution function for unit cell affects the scatter in K_{IC}. As known, for RPV steels $\eta \cong 6…22$ [6, 9, 11]. Therefore the conclusion may be drawn that for the lower shelf of $K_{IC}(T)$, the scatter in K_{IC} is small and the scatter parameter b>4.

2. The parameter η in the Weibull distribution function for unit cell is very large, i.e. material is close to perfectly homogeneous material. Perfectly homogeneous material is defined as a material for which the fracture probability of each unit cell, p_f, is described as Heaviside function of parameter Φ. Fracture of cracked specimen from material close

to perfectly homogeneous material is determined by the unit cell nearest to the crack tip as for other unit cells, the fracture probability is near zero. Then the scatter in K_{IC} depends on local properties of unit cells, i.e. on type of function $p_f(\sigma_d)$. For this case, for $\eta\to\infty$, $b\to\infty$. From physical point of view, this conclusion is obvious as the scatter in K_{IC} must be absent, if the scatter in properties of unit cells is absent.

3. If plastic strain have contribution in the fracture probability of unit cell then the brittle fracture probabilities of the unit cells nearest to the crack tip may be prevalent as the strain distribution near the crack tip is singular. According to the Prometey model, the relative contribution of plastic strain increases as temperature decreases. Conditions when the fracture probabilities of the unit cells nearest to the crack tip are prevalent may be analyzed as follows.

The distribution of strain and stress near the crack tip is shown in Fig. 6. According to this distribution and Eq. (8), for unit cells located over range $0 < \bar{r} < \bar{r}^*$, the fracture probability reaches its maximum value and can not increase. Then function $\Phi(\theta, \bar{r})$ in Eq. (10) according to the Prometey model is determined as

$$\Phi(\theta, \bar{r}) = \begin{cases} \Phi(\theta, \bar{r}^*) & \text{if} \quad \bar{r} \le \bar{r}^* \\ \Phi(\theta, \bar{r}) & \text{if} \quad \bar{r} > \bar{r}^* \end{cases}$$

If the condition $\rho_{uc} \ge r^*$ is satisfied then the unit cells nearest to the crack tip provide the dominant contribution in the fracture probability of cracked specimen. This condition is satisfied for not large level of K_I.

However, if $\rho_{uc} << r^*$, i.e. the range $0 < \bar{r} < \bar{r}^*$ consists of large number of unit cells with the same probability, then the unit cells nearest to the crack tip do not provide the dominant contribution in the fracture probability of specimen. For this case, we have again b=4.

Hence, for the considered cases, local properties of a material affect the scatter in K_{IC} values for relative low temperature and low K_I, i.e. for low temperature part of $K_{IC}(T)$ curve.

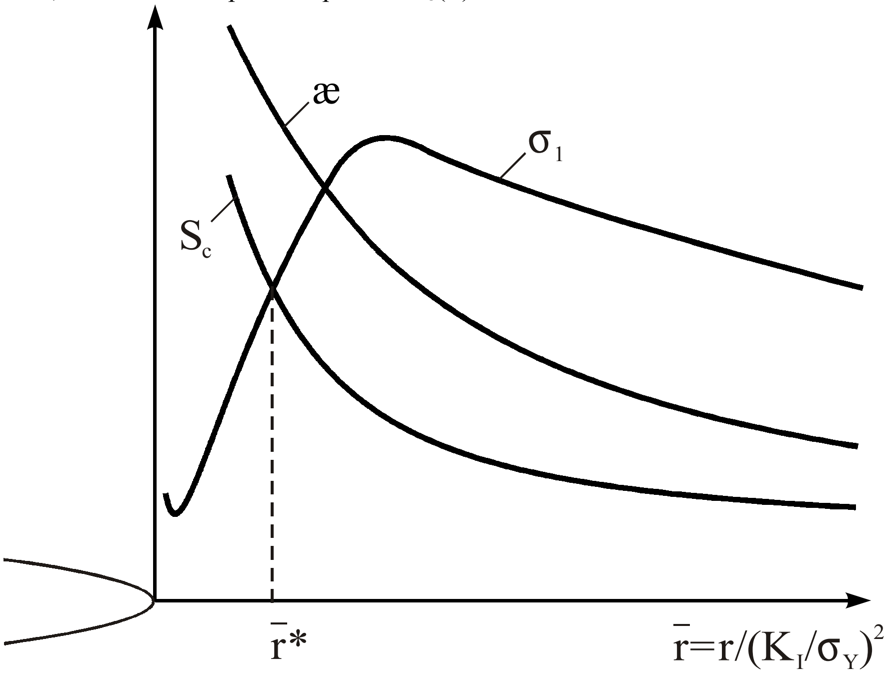

Fig. 6. The distribution of strain and stress over the plastic zone near the crack tip.

As an illustrative example for the above cases 1 and 3, the calculation results performed with the Prometey model, are shown in Fig. 7 for 2Cr-Ni-Mo-V steel both in the initial and embrittled states. Several findings may be found from these results. As seen from these results, for embrittled metal, value of b is very large and equal to ≈28 over temperature range –200…–100 °C that corresponds to the lower shelf of the K_{IC} (T) curve (Fig. 3). As temperature increases, value of b decreases and becomes equal to ≈4 at T>30 °C that corresponds to the transition temperature range (Fig. 3). For initial state, value of b is near 4 at T>-70 °C, i.e. over the transition temperature range for this state (Fig. 1), and increases and reaches b=8 as temperature decreases. It should be noted that for steel in the initial state, the lower shelf is reached at T<-200 °C (Fig. 1), and, hence, it may be believed that the parameter b may be larger than 8.

It is necessary to emphasize that using the three-parameter Weibull distribution with the parameter K_{min} instead of Eq. (9) results in good approximation of fracture toughness test results by using b=4 not only over the transition temperature range, and also over the lower shelf temperatures (see Fig. 1).

Thus, we consider various cases when b>4. It is interesting also to note that as shown in [19], for some cases, the plastic tearing before cleavage fracture may result in decreasing b<4.

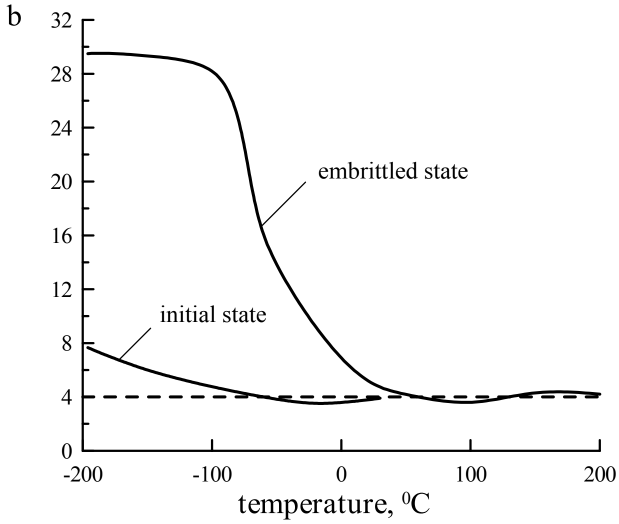

Fig. 7. The scatter parameter b as a function of temperature.

REFERENCES

1. Wallin, K., "The scatter in K_{IC} results", *Eng. Fract. Mech.*, Vol. 19, 1984, pp.1085-1093.
2. Wallin, K., "The size effect in K_{IC} results", *Eng. Fract. Mech.*, Vol. 22, 1985, pp. 149-163.
3. ASTM E 1921-02. „Standard Test Method for Determination of Reference Temperature, T_0, for Ferritic Steels in the Transition Range", *Annual Book of ASTM Standards*, Vol. 03.01, 2002.
4. Merkle, J.G., Wallin, K. and McCabe, D.E., *Technical basis for an ASTM standard on determining the reference temperature, T_0 for ferritic steels in the transition range,* Washington, U.S. NRC, NUREG/CR-5504, ORNL/TM-13631, 1999.
5. Margolin, B.Z., Gulenko, A.G. and Shvetsova, V.A., "Probabilistic model for fracture toughness prediction based on the new local fracture criteria", *Int. J. Pres. Ves. & Piping*, Vol. 75, 1998, pp. 307-320.
6. Margolin, B.Z., Gulenko, A.G. and Shvetsova, V.A., "Improved probabilistic model for fracture toughness prediction based for nuclear pressure vessel steels", *Int. J. Pres. Ves. & Piping*, Vol. 75, 1998, pp. 843-855.
7. Margolin, B.Z., Shvetsova, V.A. and Gulenko, A.G., "Radiation embrittlement modelling for reactor pressure vessel steels: I. Brittle fracture toughness prediction", *Int. J. Pres. Ves. & Piping*, Vol. 76, 1999, pp. 715-729.
8. Margolin, B.Z., Karzov, G.P., Shvetsova, V.A., Keim, E. and Chaouadi, R., "Application of local approach concept of cleavage fracture to VVER materials", *Service experience and failure assessment applications. The 2002 ASME Pressure Vessels and Piping Conference*, Vol. 437, pp. 113-120, Vancouver, BC, Canada. August 2002.
9. Margolin, B.Z., Shvetsova, V.A., Gulenko, A.G., Ilyin, A.V., Nikolaev V.A. and Smirnov V.I., "Fracture toughness prediction for a reactor pressure vessel steel in the initial and highly embrittled states with the Master Curve approach and a probabilistic model", *Int. J. Pres. Ves. & Piping*, Vol. 79, 2002, pp. 219-231.
10. Margolin, B.Z. and Shvetsova, V.A., "Brittle fracture criterion: physical and mechanical approach", *Problemy Prochnosti,* 1992, N2, pp. 3-16. (in Russian)
11. Beremin, F.M., "A local criterion for cleavage fracture of a nuclear pressure vessel steel", *Met. Trans.,* Vol. 14A, 1983, pp. 2277-87.
12. Ritchie, R.O., Knott, J.F. and Rice, J.R., "On the relation between critical tensile stress and fracture toughness in mild steel", *J. Mech. Phys. Solids,* Vol. 21, 1973, pp. 395-410.
13. Rosenfield, A. and Shetty, D., "Lower-bound fracture toughness of a reactor-pressure-vessel steel", *Eng. Fract. Mech.*, Vol. 14, 1981, pp. 833-842.
14. Landes, J. and Mc Gabe, D., *Statistical characterization of fracture in transition region.* Westinghouse Scientific Paper 79-1D3-JINTF-P4, 1979.
15. Ando, K., Magami, K. and Tuji, K., "Probabilistic aspects of cleavage crack initiation sites and fracture toughness", *Fatigue Fract. Engng. Mater. Struct.*, Vol. 15, 1992, pp. 1171-1184.
16. Margolin, B.Z., Kostylev, V.I. and Minkin, A.I., "The ductile crack growth effect on the temperature dependence of cleavage fracture toughness for RPV steel with various degree of embrittlement", *Int. J. Pres. Ves. & Piping*, Vol. 80, 2003, to be published.