*Transactions of the 17th International Conference on Structural Mechanics in Reactor Technology (*SMiRT 17)
Prague, Czech Republic, August 17 –22, 2003
Paper # G01-4

New Approaches for Evaluation of Brittle Strength of Reactor Pressure Vessels

Boris Margolin[1)], Eugene Rivkin[2)], George Karzov[1)], Victor Kostylev[1)], Alexander Gulenko[1)]

1) Central Research Institute of Structural Materials "Prometey", Saint-Petersburg, Russia
2) Research and Development Institute of Power Engineering, Moscow, Russia

ABSTRACT

Based on the Master curve conception, condition of brittle strength is formulated for heterogeneous distribution of stress intensity factor along crack front and non-monotonic, non-isothermic loading. This formulation includes the elaborated procedure for taking into account the effects of shallow cracks and biaxial loading on fracture toughness.

KEY WORDS: brittle fracture, fracture probability, fracture toughness, brittle strength of reactor pressure vessels, shallow crack effect, biaxial loading effect, weakest link model.

INTRODUCTION

At the present time when evaluating a resistance to brittle fracture for reactor pressure vessels, the following considerations are used:

- Fracture toughness K_{IC} dependent of temperature is used as a main characterictic of a material for evaluation of brittle strength.
- It is taken that brittle fracture of reactor pressure vessel does not happen if the condition

$$n \cdot K_I < K_{IC}$$

is fulfilled for each point on the crack front (where n - safety margin, K_I - stress intensity factor).

Besides, it is assumed that parameter K_{IC} is deterministic parameter and does not depend on flaw size.

At the same time it is well known that brittle fracture is a stochastic process and may be described by the weakest link model [1-5]. As a result, parameter K_{IC} depends on flaw size and, in particular, on the crack front length. It should be also taken into account that fracture toughness may vary for various load biaxialities [6-11] and for various crack depthes (a phenomen of shallow cracks) [12-14].

In the present paper, main approaches are presented on the basis of which new procedure for evaluation of brittle strength of reactor pressure vessel (RPV) have been elaborated.

ESTIMATION OF THE SHALLOW CRACK EFFECT ON FRACTURE TOUGHNESS

For plate with central crack under uniaxial tension the fracture energy density 2γ as a function of crack length a and a size of plastic zone near the crack tip is given by equation [15]:

$$2\gamma = \sigma_Y \delta_C [m \cdot \exp(1/m) \cdot \arccos(\exp(-1/m)) \cdot \sqrt{1-\exp(-2/m)} - 1], \tag{1}$$

where σ_Y - the yield strength, δ_C - the critical crack tip opening displacement,

$$m = \frac{a}{c}, \tag{2}$$

$$c = \frac{\pi E \delta_C}{8(1-\nu^2)\sigma_Y}, \tag{3}$$

ν - Poisson's ratio, E - elastic modulus, a – crack length.

As follows from Eq.(1) the fracture energy density is not a constant of a material and only for $m \to \infty$ (small loads and big crack length) the fracture energy density is a constant of a material which is equal to $\sigma_Y \delta_C$.

Taking into account that $\sigma_Y \delta_C = (1-\nu^2)\frac{K_{IC}^2}{E}$ and assuming $2\gamma = (1-\nu^2)\frac{K_C^2}{E}$, we obtain [16]:

$$K_C = \omega K_{IC}, \tag{4}$$

where K_C - fracture toughness for specimen with shallow crack, ω - coefficient which takes into account shallow crack effect: $\omega = [m \cdot \exp(1/m) \cdot \arccos(\exp(-1/m)) \cdot \sqrt{1-\exp(-2/m)} - 1]^{1/2}$. The dependence of ω on parameter m is shown in Fig.1.

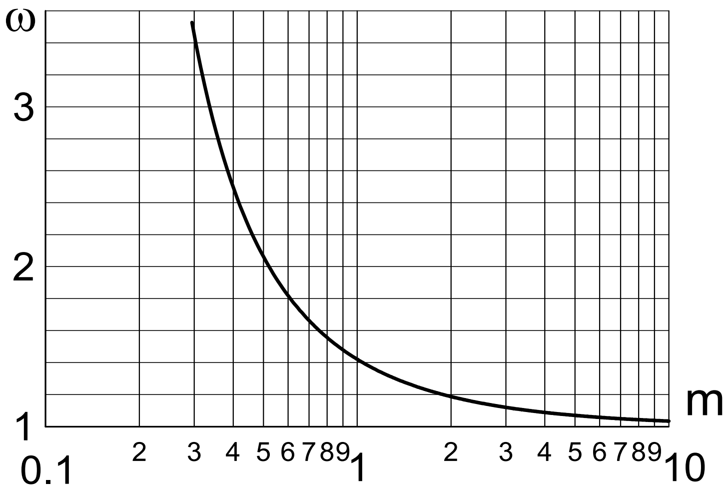

Fig.1 The dependence of parameter ω on m

According to Eqs.(2) and (3) we have:

$$m = \frac{8a}{\pi \cdot (K_{IC}/\sigma_Y)^2} \tag{5}$$

Thus, fracture toughness for shallow cracks may be calculated by Eq.(4). Applicability of Eq.(4) for RPV materials of WWER was verified by using data of CRISM "Prometey" published in [17]. In [16] 2.5Cr-Mo-V reactor pressure vessel base metal (for WWER-440) in various states was studied. Specimens with sizes W=100 mm, B=50 mm and the crack length a=10, 25 and 50 mm were tested on three-point bending, here W – specimen width, B – specimen thickness. In Fig.2 the fracture toughness versus temperature curves are presented which were obtained experimentally in [17] and calculated by Eq.(4). As seen from Fig.2 the calculated curve predicts an increase of fracture toughness values for shallow cracks, but these values are less than experimental values. Thus, in spite of clear theoretical simplifications accepted for deducing Eq.(4), this equation may be used for engineering estimation of fracture toughness for shallow cracks as it gives conservative results.

It should be also noted that according to [12-14] an increase of fracture toughness for specimens with shallow cracks is realized mainly at a/W=0.1÷0.2. Taking into account that for reactor pressure vessel with wall thickness S $W=S$, the following relation may be taken for estimation of the shallow crack effect on fracture toughness:

$$K_C = \begin{cases} K_{IC}, & \text{at } a > 0.15 \cdot S \\ \omega \cdot K_{IC}, & \text{at } a \le 0.15 \cdot S \end{cases} \tag{6}$$

ESTIMATION OF THE BIAXIAL LOADING EFFECT ON FRACTURE TOUGHNESS

It is known that under the normal operation of RPV and also under the pressurised thermal shock loading, the RPV is undergo to biaxial loading. In this connection the problem arises how to estimate the biaxial loading effect on fracture toughness. It was shown in [6-11] that biaxial loading may result in decreasing fracture toughness. This decrease is revealed for specimens with shallow cracks and large-scale yielding [6]. For small-scale yielding or deep cracks, biaxial loading does not practically affect fracture toughness [6]. Thus, the biaxial loading effect on fracture toughness has to be taken into account for shallow cracks and large-scale yielding. Hereafter procedure for determination of this parameter will be considered in detail.

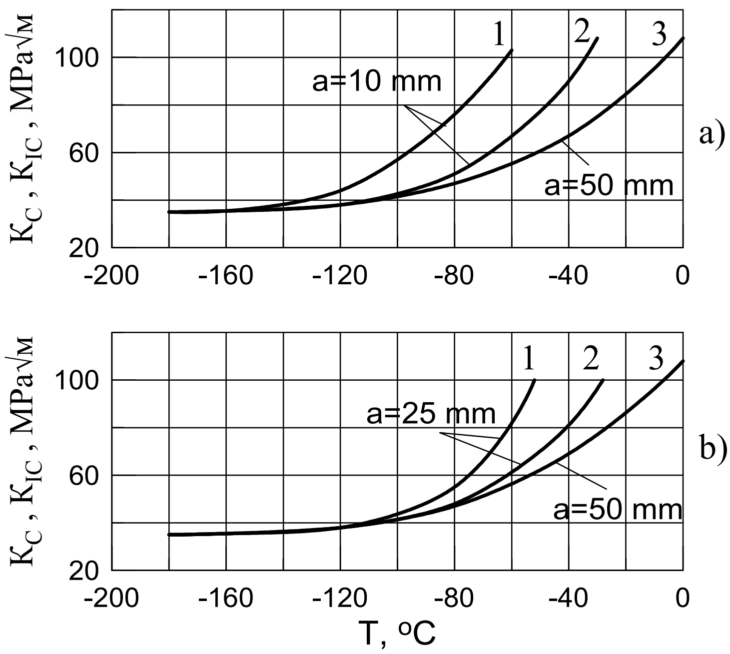

Fig.2 Comparison of the calculated and experimental data:
1,3 – treatment of experimental data (W=100 mm), 2 – calculation by Eq.(4); 1,2 – K_C, 3 – K_{IC}

The following procedure may be proposed for an estimation of the biaxial loading effect on fracture toughness [16].

For $a \le 0.15S$ and $m_\omega \le 0.7$:

$$K_C^* = \omega_b \cdot \omega \cdot K_{IC} \;, \tag{7}$$

where K_C^* - fracture toughness for specimen with regard for the biaxial loading effect, ω_b - coefficient which takes into account the biaxial loading effect on fracture toughness. Coefficient ω_b is calculated by equation:

$$\omega_b = 1\text{-}0.1\beta \,, \tag{8}$$

where β - the load biaxiality ratio, i.e. ratio of maximum nominal stress parallel to the crack plane and acting along the crack front to maximum nominal stress perpendicular to the crack plane. Eq.(7) is valid when β varies from 0 up to 2.

For $a > 0.15S$ or for $m_\omega > 0.7$, the biaxial loading effect on fracture toughness may be neglected, i.e. it may be taken $\omega_b = 1$.

Taking into account Eqs.(6)-(8) fracture toughness for biaxial loading of RPV may be determined by equation:

$$K_C^* = \begin{cases} K_{IC}, & \text{at } a > 0.15 \cdot S \\ \omega_b \omega K_{IC}, & \text{at } a \le 0.15 \cdot S \end{cases}, \tag{9}$$

$$\omega_b = \begin{cases} 1, & \text{at m}_\omega > 0.7 \\ 1 - 0.1\beta, & \text{at m}_\omega \le 0.7 \end{cases},$$

where $m_\omega = m/\omega^2$.

The proposed procedure may be justified by results obtained in [6]. These results allow one to draw the following considerations.

1. The biaxial loading effect on fracture toughness is observed for cases for which parameter β affects the dependence CTOD(J/σ_Y) (here CTOD - the crack tip opening displacement, J - J-integral). If the dependence CTOD(J/σ_Y) is invariant to β, then the biaxial loading does not affect K_{JC}. The dependences CTOD(J/σ_Y) at β=0 and β=2 are presented in Fig.3 for small and large scale yielding. As seen from Fig.3a, the dependence CTOD(J/σ_Y) at β=0 coincides with CTOD(J/σ_Y) at β=2 for small scale yielding. It follows from Fig.3a also, that for large scale yielding and deep cracks, these dependences begin to differ each from other at J/σ_Y=0.5 mm and this difference is a very small up to J/σ_Y=1.0 mm (that corresponds K_I=360 MPa√m at σ_Y=600 MPa): values of CTOD for these two curves differ less than on 4 %.

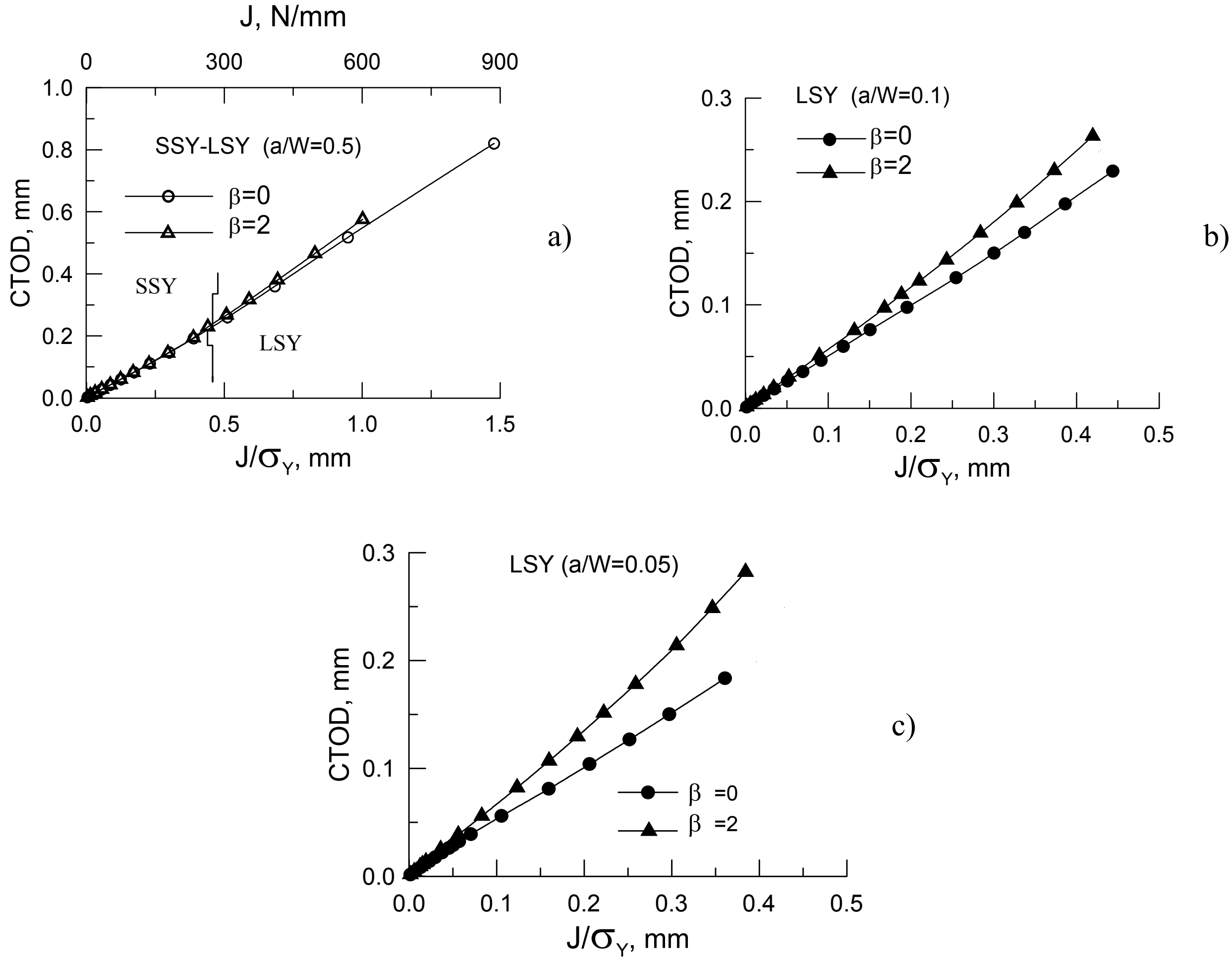

Fig.3 The dependence of CTOD on J/σ_Y for various values of β and a/W:
SSY – small-scale yielding; LSY – large-scale yielding [6]; σ_Y=600 MPa

Thus, it may be taken that the biaxial loading does not affect K_{JC} for small scale yielding and for large scale yielding and deep cracks practically.

As seen from Fig.3b and c, for shallow cracks and large scale yielding, the difference between the dependences CTOD(J/σ_Y) at β=0 and β=2 is very large and increases as the crack length decreases. Hence, for this case the biaxial loading affect K_{JC} [6].

2. Estimate a value of the parameter m_ω presented in the form similar Eq.(5):

$$m_\omega = \frac{2.55a \cdot \sigma_Y}{\dfrac{E}{1-\nu^2}\left(\dfrac{J}{\sigma_Y}\right)} \tag{10}$$

The biaxial loading effect on fracture toughness is observed for cases for which the difference between the dependences CTOD(J/σ_Y) at β=0 and β=2 is observed. It follows from Figs.3b and 3c, that at a/W=0.1 (W=100 mm, a=10 mm), these dependences begin to differ each from other at J/σ_Y=0.1 mm, and at a/W=0.05 (W=100 mm, a=5 mm) - at $J/\sigma_Y\approx$0.05 mm. Substituting a=10 mm, J/σ_Y=0.1 mm, $E=2\cdot10^5$ MPa, σ_Y=600 MPa and ν=0.3 in Eq.(10), we obtain m_ω=0.69. The same result is obtained at a=5 mm and $J/\sigma_Y\approx$0.05 mm. Thus, it may be taken that the biaxial loading effect on fracture toughness has to be taken into account at $m_\omega\le$0.7. Taking into account that for shallow cracks value of J-integral is restricted by the critical value $J_C = (1-\nu^2)\dfrac{(\omega \cdot K_{IC})^2}{E}$, we substitute value of J=J_C in Eq.(10), then value of m_ω may be calculated by the following formula:

$$m_{\omega} = \frac{8a}{\pi \cdot \left(\frac{\omega \cdot K_{IC}}{\sigma_Y} \right)^2} \tag{11}$$

Parameter m_{ω} in Eq.(11) may be presented with regard for Eqs.(5) and (11) as

$$m_{\omega} = m/\omega^2 \tag{12}$$

3. To estimate the β parameter effect on K_C^*, we use the calculated dependence of ω_b on β obtained in [6] (in [6] ω_b is designated as $\sqrt{\alpha}$ and $\sqrt{\alpha} = \frac{K_{JC|\beta \neq 0}}{K_{JC|\beta = 0}}$). The linear equation (8) may be used to approximate data presented in [6].

As parameters K_{JC} and K_C^* are identical on physical sense, the biaxial loading effect on fracture toughness of component of RPV with shallow cracks may be presented as equations (7) and (8).

ANALYSIS OF BRITTLE STRENGTH OF RPV COMPONENT WITH CRACK IN PROBABILISTIC STATEMENT

Formulation of condition of brittle strength for homogeneous distribution of stress intensity factor (SIF) along the crack front

As a condition of brittle strength it is taken condition:

$$P_f < \overline{P}_f \ , \tag{13}$$

where P_f – fracture probability, $\overline{P}_f$ - a given level of fracture probability.

For homogeneous distribution of SIF, condition (13) is equivalent the following condition:

$$K_I < K_{IC}^{B_i}\big|_{P_f = \overline{P}_f} \ , \tag{14}$$

where $K_{IC}^{B_i}(T)\big|_{P_f = \overline{P}_f}$ is fracture toughness of specimen with thickness B_i for fracture probability $\overline{P}_f$.

According to the Master curve conception [3-5] condition (14) may be calculated by the formula:

$$K_{IC}^{B_i}\big|_{P_f = \overline{P}_f} = \left(\frac{\overline{B}}{B_i} \right)^{1/4} \cdot (\overline{K}_{IC} - K_{\min}) + K_{\min} \ , \tag{15}$$

where $\overline{K}_{IC}$ is some reference value of fracture toughness for a given specimen thickness $\overline{B}$ (for example $\overline{B}$ =25mm) and a given fracture probability $\overline{P}_f$ (for example $\overline{P}_f$ ≡0.05), K_{min} – minimum fracture toughness.

If the considered crack is shallow, i.e. its depth a≤0.15S, then fracture toughness $\overline{K}_{IC}$ in Eq. (14) should be substituted by $\omega_b \cdot \omega \cdot \overline{K}_{IC}$.

Formulation of condition of brittle strength for heterogeneous distribution of SIF along the crack front

As example consider surface semi-elliptic crack. It will be taken that for each moment of a loading, $K_I(L)$, $T(L)$ (see Fig.4) are known (L – curvilinear coordinate). Consider a part of the crack front dL, on which SIF and temperature are taken to be constant and to be equal to $K_I(L)$ and $T(L)$ respectively. According to the Master curve conception [3-5]:

$$P_f^{dL} = 1 - \exp\left[-\left(\frac{K_I(L) - K_{\min}}{K_0^{dL}(L) - K_{\min}} \right)^4 \right], \tag{16}$$

where $K_0^{dL} = K_0^{dL}(T(L))$ - scale parameter for the crack front length dL and temperature $T=T(L)$.

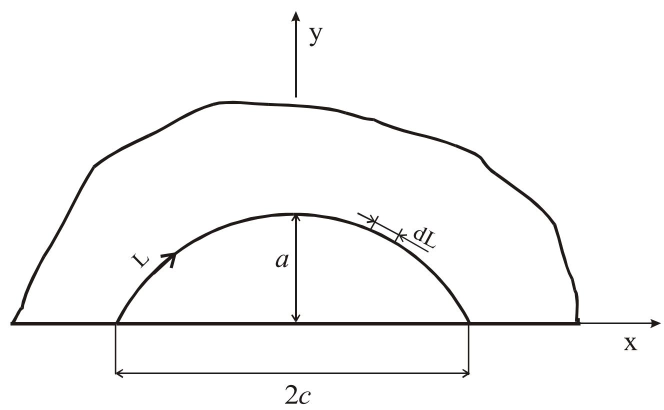

Fig.4 To the calculation brittle strength of reactor pressure vessel for heterogeneous distribution of SIF along the crack front

On the basis of [4] it may be written:

$$\frac{K_{IC}^{dL}(L)-K_{\min}}{\overline{K}_{IC}(L)-K_{\min}}=\left(\frac{\overline{B}}{dL}\right)^{1/4} \tag{17}$$

where $K_{IC}^{dL}(L)$ - fracture toughness of a specimen with thickness *dL* at $P_f=\overline{P}_f$ and temperature *T=T(L)*.

On the basis of equation from [3] it may be deduced:

$$(K_0^{dL}(L)-K_{\min})^4[-\ln(1-\overline{P}_f)]=(K_{IC}^{dL}-K_{\min})^4 \tag{18}$$

Solving Eqs.(17) and (18) simultaneously we have:

$$(K_0^{dL}(L)-K_{\min})^4[-\ln(1-\overline{P}_f)]=\left(\frac{\overline{B}}{dL}\right)(\overline{K}_{IC}(L)-K_{\min})^4 \tag{19}$$

Substituting (19) in (16) we have:

$$P_f^{dL}=1-\exp\left(\frac{[\ln(1-\overline{P}_f)](K_I(L)-K_{\min})^4\,dL}{\overline{B}(\overline{K}_{IC}(L)-K_{\min})^4}\right) \tag{20}$$

Consider various loading history for cracked component. As the loading parameter, time τ is taken that is appropriate for analysis of pressurised thermal shock condition.

As seen from Eq.(20), for values K_I and T which vary arbitrarily in time (and hence, K_{IC}), the dependence of the probability P_f^{dL} on time may be non-monotonic, that contradicts the physical meaning of cumulative probability (this parameter may increase only). Hence, Eq.(20) is valid not always and the brittle strength condition has to be reformulated allowing for this circumstance.

First of all, consider isothermal monotonic loading. For this case $\overline{K}_{IC}(L)=const=\overline{K}_{IC}$ and the parameter K_I for each part of the crack front *dL* is a monotonically increasing function of time. Then according to Eq.(20), the dependence of the probability P_f^{dL} on time is a monotonically increasing function too. Hence, Eq.(20) may be used for this case of loading. When using the weakest link theory, the probability of brittle fracture of a component with a crack of the front length *B* may be calculated by equation:

$$P_f=1-\exp\left(\frac{\ln(1-\overline{P}_f)}{\overline{B}}\int_0^B\frac{\left(K_I(L)-K_{\min}\right)^4}{\left(\overline{K}_{IC}-K_{\min}\right)^4}dL\right) \tag{21}$$

Then, on making some transformation, the brittle strength condition $P_f<\overline{P}_f$ may be written as

$$\frac{1}{\overline{B}}\int_0^B \frac{\left(K_I(L)-K_{\min}\right)^4}{\left(\overline{K}_{IC}-K_{\min}\right)^4}dL<1 \tag{22}$$

Now, consider non-isothermal non-monotonic loading. As a common case, for this loading for each part of the crack front dL, the parameter

$$\alpha=\frac{\left(K_I(L)-K_{\min}\right)^4}{\left(\overline{K}_{IC}(L)-K_{\min}\right)^4} \tag{23}$$

may vary arbitrarily. It follows from Eq.(20) that the parameter P_f^{dL} varies in the same manner.

It is clear that the cumulative probability for the considered part of crack front dL can not decrease and, at time τ, is determined by the maximum values of P_f^{dL} over time range from 0 to τ which is calculated by Eq.(20) and is denoted as max $P_f^{dL}(0,\ \tau)$. The value max $P_f^{dL}(0,\ \tau)$ is calculated by Eq.(20) as

$$\max P_f^{dL}(0,\tau)=1-\exp\left(\frac{\ln(1-\overline{P}_f)ZdL}{\overline{B}}\right), \tag{24}$$

where $Z=max\ \alpha(0,\tau)$.

When using Eq.(24) and making transformations similar ones for Eq.(22) derivation, we have the brittle strength condition for non-isothermal non-monotonic loading

$$\frac{1}{\overline{B}}\int_0^B ZdL<1 \tag{25}$$

When K_I varies non-monotonically, a phenomenon of pre-loading has place (Fig.5), which is similar to so-called warm pre-stressing (WPS). After warm pre-stressing, brittle fracture of a cracked component is known to happen at K_I never less than the value of K_I under pre-stressing, i.e. over range from τ_1 to τ_2 (see Fig.5), brittle fracture does not happen. From probabilistic point of view, over the above interval, P_f^{dL} =const.

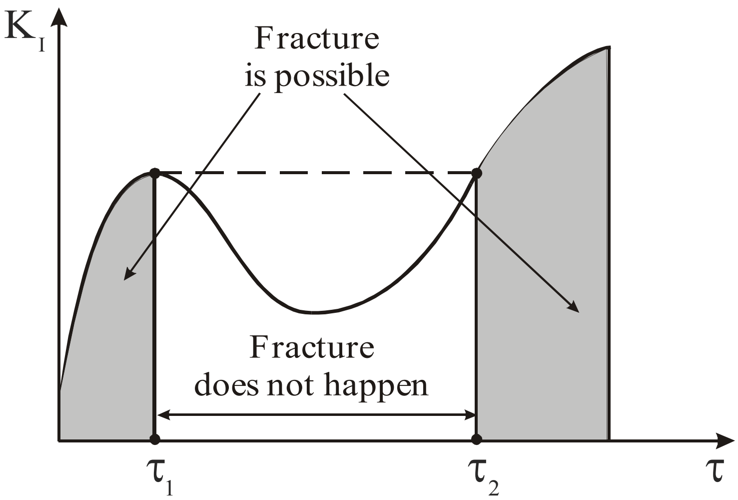

Fig.5 Possible dependence K_I on τ under non-monotonic loading and regions in which fracture is possible and impossible

In order to take into account this phenomenon, for non-isothermal non-monotonic loading, the parameter Z has to be calculated as follows: $Z=max\ \alpha(0,\tau)$, where the maximum value of α is found over time range where for each time moment τ, the condition $K_I(\tau)\geq\max K_I(0,\tau)$ is satisfied. (For illustration, in Fig.5, these time ranges are hatched.)

If it is necessary to take into account the shallow crack effect and biaxial loading effect, $\overline{K}_{IC}$ in Eqs.(22) and (23) should be substituted by $\omega_b \cdot \omega \cdot \overline{K}_{IC}$.

CONCLUSION

1. A condition of brittle strength is formulated for reactor pressure vessel with crack-like flaw in probabilistic statement. As the condition of brittle strength it is taken condition $P_f < \overline{P}_f$, where P_f – fracture probability, $\overline{P}_f$ - a given level of fracture probability. Formulation of this condition is based on the weakest link model and takes into account a variation of K_I and K_{IC} along the crack front.

2. Dependencies are proposed which allow to take into account the shallow crack effect and the biaxial loading effect on fracture toughness of reactor pressure vessel steels.

3. Using approaches presented in the present paper allows one to decrease conservatism and to increase adequacy of evaluations of brittle strength of reactor pressure vessels. Now these approaches have been included in Russian Standard on evaluation of brittle fracture of RPV of WWER type.

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