Fracture Toughness of Ferritic Steels: Lower Bounds and their Implications on Testing and Application

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Abstract

For physical reasons fracture toughness $K_{lc}$ of ferritic steels in the brittle-to-ductile transition regime is affected by a pronounced scatter, which requires statistical methods to be applied for evaluation of test results as well as for application in safety analysis of structures. For this purpose the probabilistic Master-Curve-approach according to ASTM E1921 is often used. However, for engineering purposes like a screening safety analysis of a defect-containing component it is usually preferable to use a deterministic lower bound. However, within the framework of ASTM-standards there is no possibility to determine $K_{lc}$ experimentally. So $K_{lc}$ needs to be determined indirectly from the reference temperature $T_0$. In the present paper it is shown how lower bounds of fracture toughness – either plane strain $K_{lc}$ for large components or thickness-dependent $K_{jc}$ for smaller ones - can be derived from $T_0$ and how the latter can be determine with sufficient accuracy from one or a few $K_{jc}$ values.

1. Introduction

In order to assess the safety of a component in presence of crack-like defects fracture toughness in terms of $K_{lc}$, $J_{lc}$ or $K_{jc}$ is required. Consider as an example a large welded steel girder, where indications of lacking fusion all over the width of the flange were found by ultrasonic testing. Thus, the crack front can be quite long and approximately under plane strain conditions. Although there are reduced in-plane constraints – as compared to bending of deeply cracked beams - plane strain fracture toughness $K_{lc}$ is required as a basic property to assess safety of the girder with respect to brittle fracture [1]. In case of structural steel, the crucial temperature is often in the ductile to brittle transition (DBT) range where fracture toughness as a function of temperature exhibits a steep drop and a pronounced scatter of the data. Within the framework of ASTM-standards, direct experimental evaluation of $K_{lc}$ by ASTM E399 [2] or E1820 [3] is not possible for ferritic steels in this temperature range. Even $J_{lc}$-testing and...
conversion to \( K_{Jc} \) is not encouraged in [3], since the resulting toughness value may be size-sensitive. In the paragraph on significance of \( J_{Ic} \) in Appendix A.6 of [3] the user is strongly advised to apply the analysis procedure of E1921 [4] instead.

Test method ASTM E1921 [4] is based on the Master curve- (MC-) approach [5], where fracture toughness \( K_{Jc} \) of ferritic steel is considered to be governed by Weibull-statistics, which means that it depends on the probability of failure and on the size of the crack. According to [4, 5] \( K_{Jc} \) of standard 1T-specimens (i.e. thickness of \( B_{1T}=1\text{inch}=25.4\text{ mm} \)) can be expressed in terms of the cumulative probability of failure (\( p_f \)) as

\[
K_{Jc}(B_{1T}, p_f) = K_{min} + \left[\ln\left(\frac{1}{1-p_f}\right)\right]^{1/4} \left[11 + 77\exp\left(0.019 \cdot (T - T_0)\right)\right]
\]

(1)

where \( K_{min}=20 \text{ MPa}\cdot\text{m}^{0.5} \). The reference-temperature \( T_0 \) is a characteristic material property that has to be determined experimentally by a statistical analysis of a certain minimum number of \( K_{Jc}\)-data according to [4]. To predict \( K_{Jc}(p_f) \) that applies to a crack in a component, Weibull-statistics lead to

\[
K_{Jc}(B, p_f) = K_{min} + \left[K_{Jc}(B_{1T}, p_f) - K_{min}\right] \cdot \left(\frac{B_{1T}}{B}\right)^{0.25}
\]

(2)

where \( B \) denotes either the component thickness (in case of a through-crack) or the crack front length (in case of a surface crack or internal crack). An analogous relation can be applied to “normalize” \( K_{Jc}\)-data obtained from specimens of arbitrary thickness \( B_s \) to the standard thickness \( B_{1T} \).

Apart from the experimental difficulties to determine a valid \( T_0 \) and a relatively large measurement uncertainty [6] this concept leads to practical problems. For the typical situation in an engineering safety analysis - low required probabilities of failure (\( p_f \)) and large component thickness or lengths of the crack front as in the example shown in Fig. 1 - \( K_{Jc} \) predicted by (1) and (2) are questionable. Obviously, (1) and (2) do not exhibit the correct asymptotical behaviour of \( K_{Jc} \) for \( p_f \to 0 \) and for \( B \to \infty \), since both of them predict \( K_{Jc} \) to approach \( K_{min}=20 \text{ MPa}\cdot\text{m}^{0.5} \), which is an auxiliary number that serves well to evaluate test-data, but not to predict fracture toughness values for low \( p_f \) and high \( B \). Instead, for physical reasons a lower-bound of fracture toughness that depends on temperature and yield strength is expected to exist [7]. In fact, there is experimental evidence that \( K_{Jc}(B) \) does not follow (2) for \( p_f < 0.025 \); it rather approaches a well-defined lower bound \( K_{Jc(LB)} \) for \( p_f \to 0.05 \) [8]. For \( p_f \) approaching zero as well as for increasing crack front lengths they tend to \( K_{min} = 20 \text{ MPa} \cdot \text{m}^{0.5} \), which is not a physical lower bound but just a statistical fitting parameter. In fact, as shown in [8], (1) does not deliver accurate \( K_{Jc}\)-values for \( p_f < 0.025 \). Often the 5%-tolerance bound (i.e. \( p_f=0.05 \)) is regarded as a “lower bound”. However, e.g. in a law suit, a safety of only 95% against brittle fracture is hard to defend. As another drawback of the concept, validity of (1) and (2) is restricted to \( T_0-50K < T_0 < T_0+50K \) [4], so the upper transition regime, which often is an important one in a safety analysis, is not covered. Thus, all in all, the MC-concept as represented by [4] is well suited to evaluate the reference temperature \( T_0 \) from a number of tests, but not to predict fracture toughness to be used in an engineering safety analysis of a defect-containing structural component.

An engineering fracture mechanics analysis is usually performed stepwise, starting with a screening based on simple conservative models and stepwise refinements of the models where necessary. For such purposes, using well-founded deterministic lower bounds of fracture toughness are recommended rather than sophisticated probabilistic approaches. For reactor-pressure vessel (RPV-) steels a well known lower bound of \( K_{Jc} \) is provided by the ASME reference curve [9]. According to [10, 11] it can be expressed in terms of \( T_0 \) as follows:

\[
K_{Jc(ref)}(T) = 36.5 + 22.8 \cdot \exp\left[0.036 \cdot (T - T_0 - 19.4K)\right]
\]

(3)

The drawback of this lower bound is its excessive conservatism for thin components, i.e. for thicknesses that do not meet the condition of plane strain at the crack front. Furthermore, being an empirically lower envelope of numerous experimental data \( K_{Jc}\)-data for the A533 grade B, class 1 steel, its reliability is unclear if it is applied to other types of structural steel.
The present paper deals with these open questions. As recently shown by the author [12] the thickness-dependence of \( K_{Jc} \) resulting from the statistical weakest-link effect seems to saturate at a certain limiting thickness that corresponds to plane strain behaviour. This allows plane strain fracture toughness \( K_{Ic} \) to be estimated from \( K_{Jc} \). In reverse, a lower bound for limited component thickness can be obtained from a lower-bound \( K_{Ic} \) such as (3). The derivation and the underlying models are recapitulated and discussed in the first part of the present paper. One of the main problems in using (3) and related lower bounds is that the required \( T_0 \) is often not available and its determination by the standard procedure described in [4] is often not possible due to a lack of test material, time or testing budget. As shown below, the existence of a thickness- and temperature-dependent lower bound as mentioned above can be used to simplify the evaluation procedure of \( T_0 \).

2. Lower bound \( K_{Jc} \) as a Function of Thickness

According to Weibull-statistics, the cumulative probability of failure is a function of the fracture-controlling volume \( V_c \) next to the crack-front, where the stresses are high enough to initiate cleavage. Whether or not an unstable crack extension is triggered depends on the presence of a local weakness such as a micro-crack or a brittle particle in this volume, which is a matter of probability. The in-plane dimensions of \( V_c \) are known to be in the order of the crack-tip opening displacement (CTOD), which is proportional to \( K_{Ic}^2 \), so \( V_c \) is proportional to \( K_{Ic}^{-4} B \) for a component of thickness \( B \) [13]. For a 2-parameter Weibull-distribution this leads to the following dependence of \( K_{Jc} \) on the component thickness \( B \):

\[
K_{Jc}(B, p_f) = K_{Jc}(B_T, p_f) \left( \frac{B}{B_T} \right)^{0.25} \quad \text{for} \ B < B_{sat} \tag{4}
\]

Obviously, the asymptotical behaviour of (4) for \( B \to \infty \) is not correct, since for physical reasons a certain limiting value, which may depend on temperature and be related to planes strain fracture toughness \( K_{Ic} \), is approached [7]. Therefore, in [12] the authors postulated that a saturation of Weibull-statistics occurs at a certain thickness \( B_{sat} \), which means that for \( B > B_{sat} \) \( K_{Jc}(B) \) is no longer decreasing, but remains constant at the level \( K_{sat} \), as sketched in Fig. 2, thus

\[
K_{Jc}(B, p_f) = K_{sat} \quad \text{for} \ B > B_{sat} \tag{5}
\]

Physically, the postulated saturation of the weakest-link-effect is due to the extreme slenderness of the critical volume \( V_c \). Its width is the specimen thickness \( B \) or the length of the crack front, whereas its in-plane dimension is only in the order of the crack-tip opening displacement (CTOD), thus two or three orders of magnitude...
smaller. A saturation of the effect of the thickness is assumed to occur for

\[ B > B_{\text{sat}} = \beta_{\text{sat}} \cdot \frac{K_{Jc}^2}{E \cdot R_p} \]

where \( \beta_{\text{sat}} \) is a constant in the order of \( 10^2 - 10^3 \), left open to be determined experimentally.

On the other hand, as mentioned above, a thickness- and temperature-dependent lower bound of \( K_{Jc} \) is expected to exist for physical reasons [7]. The expected probability distribution is sketched in Fig. 3. Experimental evidence for this behaviour is found in [8], indicating that there is a certain value of \( K_{Jc} \) associated with \( p_f=0 \). Eqs. (3) – (6) should include this special case as well, which means that \( K_{\text{sat}} \) for \( p_f=0 \) represents the lower bound of plane-strain fracture toughness \( K_{Ic} \). In mathematical terms:

\[ K_{Jc}(B > B_{\text{sat}}, p_f = 0) = K_{Ic/LB} \]

Combining (4), (6) and (7) leads to

\[ K_{Jc}(B, p_f = 0) = \left( \frac{\beta_{\text{sat}}}{R_p \cdot E \cdot B} \right)^{\frac{1}{4}} \cdot K_{Ic/LB}^{\frac{3}{2}} \]

By eq. (8) a lower bound of \( K_{Jc} \) for \( B < B_{\text{sat}} \) can be determined if the lower bound of \( K_{Ic} \) is known. For reactor pressure vessel steels (RPV-steels) the latter is assumed to be the empirical lower bound (3), thus

\[ K_{Ic/LB} = 36.5 + 22.8 \cdot \exp\left[0.036 \cdot (T - T_0 - 19.4K)\right] \]

Fig. 2: Postulated dependence of fracture toughness on thickness: Saturation of weakest-link effect at \( B = B_{\text{sat}} \). \( B = B_{p0} \) denotes the transition to upper-shelf behaviour

Fig. 3: Dependence of cumulative probability of failure on \( K_{Jc} \) (schematic).
From comparison of (8) and (9) with the experimentally determined lower envelope of numerous $K_{Jc}$-data measured for the RPV-steel 22NiMoCr 3-7 $\beta_{sat}$ was found to be about 1150 [12]. With $\beta_{sat}=1150$ and the ratio $E/R_p$ for the considered steel (as given in eq. (14) below) inserted in eq. (6), this condition turns out to coincide roughly with the condition for plane strain fracture toughness according to [2, 3], i.e.

$$B > B_{sat} = \frac{2.5 \cdot K_{Jc}^2}{R_p^2} = B_{pE}$$  \hspace{1cm} (10)

This finding confirms the assumption made by Merkle et al. in [14], where they attempted to unite $K_{Jc}$ and plane strain fracture toughness $K_{Ic}$. It indicates that saturation of the thickness-effect occurs if both conditions (6) and (10) are fulfilled, the latter being apparently the crucial one in the present case. Correspondingly, from (4) and (10) the following relation between the lower bounds of $K_{Jc}(B)$ and $K_{Ic}$ is obtained:

$$K_{Jc}(B, p_f) = \frac{1.257}{B^{0.25}} \cdot \sqrt{R_p} \cdot K_{Jc}^{3/2}$$  \hspace{1cm} (11)

$$K_{Jc}(B, p_f = 0) = \frac{1.257}{B^{0.25}} \cdot \sqrt{R_p} \cdot K_{Jc/LB}^{3/2}$$  \hspace{1cm} (11a)

3. Generalisation of Lower Bound $K_{Ic}$

From (4) and (10) one obtains a simple relation to determine an equivalent plane strain fracture toughness value $K_{Ic}(p_f)$ from a $K_{Jc}$ value measured on a specimen of thickness $B_s$.

$$K_{Ic}(T, B, p_f) = 0.858 \cdot R_p^{1/3} \cdot B_s^{1/6} \cdot K_{Jc}^{2/3}(B_s, p_f)$$  \hspace{1cm} (12)

This relation holds for lower bounds ($p_f=0$) as well. As shown in [121], applying (12) on the experimentally determined lower envelope of the $K_{Ic}(T)$ data for the RPV steel 22NiMoCr 3-7 resulted in a curve that agreed well with (3). This is remarkable, since it means the empirical relation (3) could be obtained independently from relatively few $K_{Jc}$ of an arbitrarily chosen steel measured by relatively small specimens. It strongly indicates - or even proves - that (3) holds for other ferritic-bainitic steels than RPV-steels as well.

However, the steel 22NiMoCr 3-7 used for comparison in [12] happened to have about the same yield strength as A533 grade B, class 1, so the effect of the yield stress on the lower bound needs to be considered. Denoting the yield stress of the test-material 22NiMoCr 3-7 used in [12] as $R_{p(test)}$, application of (11) on (3) leads to the following lower bound of $K_{Jc}$ as a function of the thickness $B$:

$$K_{Jc}(T, B, p_f = 0) = \frac{1.257}{B \cdot \sqrt{R_{p(test)}}(T)} \cdot \left\{36.5 + 22.8 \cdot \exp[0.036 \cdot (T - T_0 - 19.4K)]\right\}^{3/2}$$  \hspace{1cm} (13)

for

$$B < \frac{2.5 \cdot K_{Jc}^2}{R_{p(test)}^2}$$  \hspace{1cm} (13a)

According to the MC-concept, $K_{Jc}$ of ferritic-bainitic steels with yield strengths at room temperature in the range of 275 MPa < $R_p$ < 825 MPa is governed just by one parameter, $T_0$. Consequently, the lower bound (13), which represents the special case $p_f=0$ of $K_{Jc}(T, B, p_f)$, is supposed to be governed just by $T_0$ as well. Thus, inserting in eq. (13) the yield stress of the test material used in [12],

$$R_{p(test)}(T) = 349 + 86.7 \cdot \exp(-0.00691 \cdot T) \hspace{1cm} (in \hspace{0.1cm} MPa)$$  \hspace{1cm} (14)
results in a lower bound that should hold for any steel covered by E1921. Correspondingly, (12) applied on (13) delivers the lower bound plane strain fracture toughness $K_{lc}$ of an arbitrary steel with the yield stress $R_p$ as follows:

$$K_{lc/LB}(T) = \left( \frac{R_p(T)}{R_p(test)(T)} \right)^{1/3} \cdot K_{lc(ref)}(T).$$  \hspace{1cm} (15)

For the sake of simplicity the term in brackets can be approximated as $R_p/R_p(test)$ at room temperature, since this ratio to the power of $1/3$ is not much temperature dependent. With $R_p(test) = 425$ MPa at room temperature (from (14)) and $K_{lc(ref)}$ from (3) the following generalized lower bound of plane-strain fracture toughness is obtained:

$$K_{lc/LB} = \left( \frac{R_p^{RT}[MPa]}{425} \right)^{1/3} \cdot \{36.5 + 22.8 \cdot \exp[0.036 \cdot (T - T_0 - 19.4K)]\}.$$  \hspace{1cm} (16)

Eq. (16) is expected to hold for any ferritic-bainitic steel covered by E1921 [4].

4. Upper Bound and Scatter Band

In a safety analysis the lower bound of fracture toughness is of prime importance. However, the upper bound may also be of interest with respect to material testing, as shown in section 5. Therefore, we consider in the following the behaviour of the scatter band as a function of the thickness and the temperature.

![Upper and lower bound as a function of the thickness at T=T0 (a) and T=T0+50K (b) for the case of the RPV steel 22NiMoCr 3-7](image)

Fig. 4: Upper and lower bound as a function of the thickness at $T=T_0$ (a) and $T=T_0+50K$ (b) for the case of the RPV steel 22NiMoCr 3-7.
Fig. 5: Crescent scatter band between lower and upper bound of $K_{Jc}(1T)(T)$

According to [4] $K_{Jc}$-values of a test series to determine $T_0$ should not exceed the 0.98%-tolerance bound, otherwise the material is considered as inhomogeneous. This is confirmed by the data presented in [17, 18], where nearly all of the more than 100 data are enveloped by the 98%-tolerance bound. Thus, pragmatically, eq. (1) with $p_f = 98\%$ can be considered as an upper bound for a homogeneous material. For $K_{Jc} > K_{Jc,UB}$ it is likely that the weakest-link-effect does not saturate for $B > B_p$, so the upper bound is expected to keep following eq. (4) for $B > B_p$, i.e. in the region of valid $K_{Jc}$. The corresponding behaviour of the upper and lower bound is shown in Fig. 4 for two exemplary temperatures. The theoretical scatter band is given by the area bounded by these two curves. It explains the experimental observation that the width of the scatter band decreases with increasing thickness of the specimens [8]. As one can see from Fig. 4 even plane-strain $K_{Ic}$-values (i.e. $K_{Jc}$ below the plane-strain boundary) still exhibit a significant scatter. However, $p_f$ of $K_{Jc}$ is not expected to exhibit a normal but rather a log-normal distribution.

The upper and lower bounds for the standard specimen thickness $B = B_{1T} = 0.0254$ m can be obtained by (13) and (1), respectively, in terms of $T$ as follows:

- Lower bound (eq. (13) with $B=0.0254$ m and $R_{p,ref}(T)$ from eq. (14)):

$$K_{Jc/LB}(T) = 36.5 + 11.56 \cdot \exp\left[0.0475 \cdot (T - T_0)\right]$$

(17a)

- Upper bound (eq. (1) with $p_f=0.98$ and $B=0.0254$ m):

$$K_{Jc/UB}(T) = 35.47 + 108.3 \cdot \exp\left[0.019 \cdot (T - T_0)\right]$$

(17b)

The curves (17a) and (17b) are shown in Fig. 5. The crescent area between them represents the theoretical scatter band of experimental $K_{Jc}$-data for a specimen thickness of 1 inch.

5. Estimation of $T_0$ by the CF- Method

In order to determine the lower bound $K_{Ic}$ or $K_{Jc}$ by (13) or (16), respectively, the reference temperature $T_0$ has to be known. This material property is usually not available for structural steels, and its evaluation by the procedure of E1921 is often beyond the possibilities within an engineering safety analysis. If there are no fracture toughness data available, then $T_0$ can be estimated from the Charpy transition temperature $T_{28J}$ by the correlation [15]

$$T_0 = T_{28J} - 18 \text{ K}$$

(18)

According to experience this empirical relation works surprisingly well. However, $T_0$ determined from experimental $K_{Jc}$-data is still preferable. In the following a simple but efficient method is suggested to determine $T_0$. As shown below, this method - called the “crescent-fit” (CF-) method – is much less demanding than E1921 concerning number of specimens and validity criteria.

From the theoretical lower and upper bounds of $K_{Jc}$ given in (17) in terms of $T_0$ an upper and lower limit of $T_0$, denoted as $T_{0/UB}$ and $T_{0/LB}$, respectively, can be determined from just one experimental $K_{Jc}$-value measured at a temperature $T_{test}$, as indicated in Fig. 5. From eqs. (17 a) and (17b) these limits are found to be

$$T_{0/L} = T_{test} - \frac{1}{0.0475} \cdot \ln \left[ \frac{K_{Jc(1T)} - 36.5}{11.56} \right]$$

(19a)

$$T_{0/U} = T_{test} - \frac{1}{0.019} \cdot \ln \left[ \frac{K_{Jc(1T)} - 35.47}{108.3} \right]$$

(19b)
so $T_0$ is likely to be

$$T_0 = T_{0/M} \pm \frac{T_{0/U} - T_{0/L}}{2} \quad (20)$$

where $T_{0/M}$ denotes the mean value of $T_{0/U}$ and $T_{0/L}$. If there are two or more $K_{IC}$-data available, then the accuracy of $T_0$ is increased by averaging. It is plain to see from Fig. 5 that the higher $K_{IC(1T)}$, the lower the span between $T_{0/U}$ and $T_{0/L}$ – which means the lower the uncertainty of $T_0$. Therefore, in the averaging procedure the individual $T_{0/M}$ should be weighted by the inverse of the span between $T_{0/U}$ and $T_{0/L}$, thus

$$T_0 = \left\{ \frac{T_{0/M(i)}}{W(i)} \right\}_{m} \quad (21)$$

where $\{X(i)\}_m$ denotes the mean value of $i$ individual values of $X$, and $W(i)$ the weight of each individual $T_{0/M(i)}$, defined as

$$W = (T_{0/U} - T_{0/L})^{-1} \quad (21a)$$

The uncertainty of the resulting $T_0$ can be obtained simply from the combination of the individual upper and lower bounds of $T_0$ as given by (19), which means that the maximum $T_{0/L}$ represents the lower limit and the minimum $T_{0/U}$ the upper limit of $T_0$. Compared with the standard procedure of E1921 [4], the CF-method as explained above is able to deliver relatively accurate $T_0$ from only a few specimens, as shown by some examples in the next section.

6. Verification by Experimental Data

Fig. 6 shows a series of $K_{IC}$-data that are obtained from 1.6T SEB-specimens ($W=B=40$) of the RPV-steel 22NiMoCr 3-7. For the experimental details we refer to [17, 18]. From 1T-CT-specimens $T_0$ was determined to be -71°C. The procedure of ASTM E 1921 [4] applied on the data shown in Fig. 6 resulted in $T_0 = -75.2$ °C. As one can see from Fig. 6, three of the data – those tested at -20°C, -10°C and 0°C - are invalid, so they have to be considered as “non-tests” according to [4]. Using these three $K_{IC}$ (which actually should be denoted as $K_{IQ}$ according to [4]) in (21) leads to $T_0 = -75.5$ °C, with an uncertainty of ±1.3 K. This is in nearly perfect agreement with $T_0$ from E1921. It is remarkable that even each single invalid $K_{IC}$ delivers nearly the same $T_0$ by application of (19):

- $T_{test} = -20°C$: $T_0 = -74.8°C \pm 10.4$ K
- $T_{test} = -10°C$: $T_0 = -75.1°C \pm 5.7$ K
- $T_{test} = 0°C$: $T_0 = -75.7°C \pm 1.3$ K

In the same way, the test series performed in [17, 18] on SEB-specimens of different sizes were evaluated. The comparison between the standard $T_0$ and $T_0$ determined from the two highest invalid $K_{IC}$ by the CF-method is shown in Fig. 7. The agreement is within the scatter band of the standard $T_0$. Actually it seems that $T_0$ from small SEB-specimens determined by the CF-method is even less biased than the standard $T_0$.

Fig. 8 shows test data obtained from 1T-CT-specimens of a girder made of structural steel S355 J2. This steel has a nominal yield stress of 355 MPa at RT and a Charpy energy of 27J at -20°C. From the single test at 20°C one obtains by (19) $T_0 = -31.7 \pm 11.4$ °C. The corresponding upper and lower bound is shown to envelope the experimental data at -20°C and -30°C. Using all 5 values shown in Fig. 8 delivers $T_0 = -38°C (+6.6/-5.1K)$, which is in good agreement with the estimation from the Charpy energy by (18).
Another interesting and advantageous feature of the CF-method is its insensitivity with respect to crack-tip sharpness. 1T-specimens of the steel 22NiMoCr 3-7 were prepared with a sharp notch made by EDM-cutting instead of a fatigue crack [16]. Two of the specimens were tested at -26°C. The resulting $K_J$ (denoted as $K_{Jn}$ to indicate the sharp notch) were 196.8 MPa $m^{0.5}$ and 267.2 MPa $m^{0.5}$. From these values eq. (21) delivers $T_0 = -72.2 °C +6/-9K$.

The surprisingly good agreement with standard $T_0$ (see Fig. 7) can be explained by the relatively small effect of the notch sharpness on $K_J$ in the upper transition regime [16] and the relatively steep slope of the upper and lower boundaries.
bounds in this range. It indicates that the CF-method is not much demanding with respect to the quality of the crack-front (sharpness, straightness, etc.).

7. Discussion and Conclusions

For an engineering safety analysis deterministic lower bounds of $K_{lc}$ are usually better suited than statistical $K_{lc}$-data based on MC-approach. However, for ferritic steels in the ductile-to brittle temperature range $K_{lc}$-values are not directly measurable, since [2] and [3] are not applicable to them. Thus, $K_{lc}$ has to be determined indirectly. In the present paper it is shown how reliable and traceable lower bounds of fracture toughness – either in terms of $K_{lc}$ for large components or $K_{lc}$ for smaller ones - can be derived from $T_0$, and how the latter can be determined from a few, possibly invalid experimental $K_{lc}$ values. The suggested method to estimate $T_0$ is based on the theoretical thickness-dependent upper and lower bounds of $K_{lc}$, which define upper and lower limits of $T_0$. Since the slope of the upper and lower bound is increasing and the width of the scatter band between them decreases with increasing temperature, it is recommended to perform the tests not near $T_0$ but in the upper transition range. Even one or two $K_{lc}$ values determined near upper-shelf can be sufficient. Attention should be given to the loading rate, since it can affect the slope of the bounds [19]. The equations given above are applicable only to quasi-static testing ($dK_{lc}/dt < 1 \text{ MPa m}^{0.5}/\text{s}$)

Summarizing, the CF-method exhibits several advantageous features, compared with the standard method according to E1921: Firstly, the number of required specimens is reduced. Even one test can deliver useful bounds of $T_0$. Secondly, validity of the individual $K_{lc}$-data is not a big issue. $K_{lc}$ can be used without censoring. If they are censored, then rather conservative $T_0$ will result. Thirdly, there is no restriction concerning the test temperature. Actually, it is advantageous and lowers the uncertainty of $T_0$ if test are performed in the upper transition range, even if cleavage is preceded by some amount of tearing crack extension. These highly “invalid” data tend to increase $T_0$, which is conservative. Fourthly, the crack-tip quality has not a big effect, if tests are performed in the upper transition range, near upper shelf. Good approximations of $T_0$ are obtained even from sharply notched specimens.

Considering these advantages, the CF-method is expected to serve particularly well in cases of inhomogeneous materials such as welds, where the data-sets are often not sufficient for a valid $T_0$ according to [4]. It enables upper and lower limits of $T_0$ to be identified and quantified from just a few specimens.

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