Uncertainties in T_0 and Corresponding Safety Margins in Lower Bounds of K_{Ic} in the Ductile-to-Brittle Transition Regime of Ferritic Steels

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Abstract. The reference temperature T_0 according to ASTM E1921 can serve to determine a lower bound of fracture toughness of RPV-steel in the ductile-to-brittle transition range by a correlation between T_0 and RT_{NDT}. However, T_0 is known to be affected by a bias related to specimen size, which leads to some uncertainties. Therefore, questions concerning transferability of T_0 to large structures and adequate safety margins arise. These and related issues are explored in the present paper experimentally and theoretically. On a typical RPV-steel the effects of loading rate, specimen size and shape, crack depth and loading rate were investigated experimentally. It was found that the shape of the MC is sensitive to the loading rate, even in the range of quasi-static loading, but even more pronounced at higher loading rates. This effect leads to a dependence of T_0 on the test temperature. Furthermore, T_0 turned out to be size-dependent to some degree, which indicates that the requirements of ASTM E1921 are not restrictive enough to guarantee size-independence. Based on these findings a modified formula to determine RT_{NDT} from T_0 is suggested.

Introduction

Transferability of fracture toughness from laboratory specimens to engineering components is one of the key issues in engineering fracture mechanics, particularly in the ductile-to-brittle transition (DBT) regime of ferritic steel. In this regime fracture toughness is known to be affected by an inherent scatter. Moreover, it is very sensitive to various influencing factors, including temperature, specimen or component size, loading rate, crack-tip constraints and material inhomogeneity, which tend to increase the apparent scatter of experimental data. There is no doubt that experimental characterization of fracture toughness in the DBT-regime requires statistical approaches. A widely used evaluation method is the master-curve (MC) approach, which is standardized in ASTM E 1921 [1]. According to the MC-approach, $K_{Jc}(T)$ is characterized by just one parameter, the reference temperature T_0. In principle, T_0 enables fracture toughness to be evaluated as a function of temperature for any given component and at any desired probability of failure (pof).

However, there are some concerns about the transferability of the corresponding fracture toughness to larger structural components. It is well known that T_0 determined in accordance with [1] depends – to some unknown degree - on the used specimens (see [1] for references). In particular, T_0 from SEB specimens (pre-cracked Charpy) may deviate significantly from those obtained by standard CT-specimens. In [1] it is noted that *"…On average, T_0 values obtained from C(T) specimens are higher than T_0 values obtained from SE(B) specimens. Best estimate comparison indicates that the average difference between C(T) and SE(B)-derived to values is approximately 10 °C. However, individual C(T) and SE(B) datasets may show much larger T_0 differences, or the SE(B) T_0 values may be higher than the C(T) values …"* Such significant deviations between mechanically rather

similar crack configurations raise questions on the reliability and transferability of K_{Jc} determined by the MC-concept. Unfortunately, a rational explanation for this so-called bias is given neither in [1] nor in the open literature.

An alternative, more pragmatic approach is the determination of a lower bound $K_{Ic}(T)$-curve by means of an empirical correlation between T_0 and RT_{NDT} [2, 3]. Although mainly empirical, this approach is well-suited for conservative deterministic safety analysis practical application. However, the questions of the influencing factors and their effect on T_0 remain.

From the perspective of a regulator the main problem is the lacking explanation of the above mentioned bias, which can significantly affect a safety analysis. In order to set adequate margins, the phenomenon has to be understood. In order to explore this bias and further uncertainties a research project sponsored by the Swiss Federal Nuclear Safety Inspectorate (ENSI) was conducted, where the toughness behavior of a common reactor pressure vessel (RPV)-steel in the DBT-range was investigated experimentally and theoretically. The experimental part is documented in detail in [4, 5]. Subsequently, the data were analyzed, generalized and interpreted based on theoretical relations and principles [6, 7]. As an outcome a modified formula to determine RT_{NDT} from T_0 is suggested, which is included in a new ENSI-guideline for ageing surveillance [8]. The present paper explains its background. Furthermore, it gives an overview on the main findings in general and lessons learned concerning transferability and uncertainty of T_0.

Evaluation of Fracture Toughness from T_0

As mentioned in the introduction, there are two common ways to derive fracture toughness values $K_{Ic}(T)$ from T_0: i) consider a certain tolerance bound (e.g. 5% pof) as a technically lower bound [9, 10], ii) use a correlation between T_0 and RT_{NDT} according to the ASME-lower-bound-concept [2, 3]. These approaches are briefly reviewed and discussed in the following.

MC-Tolerance Bounds. By the reference temperature T_0, fracture toughness of ferritic steel in the BDT-range can be determined from statistical relations for any desired probability of failure (pof). According to [1] K_{Jc} at an arbitrary temperature in the range T_0-50K < T < T_0+50K is obtained by

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

with K_{min} =20 MPa·m$^{0.5}$. Eq. (1) holds for specimens or components of a thickness B_{1T}=25.4 mm. For other thicknesses B the equation has to be corrected by

$$K_{Jc(pof,B)}(T) = K_{\min} + \left[K_{Jc(pof,1T)} - K_{\min}\right]\cdot\left(\frac{B_{1T}}{B}\right)^{0.25} \qquad (2)$$

With (1) and (2), K_{Ic} can be determined for any given thickness B. In case of a crack with 3D-characteristics, such as a surface crack, B can be the length of the crack front. Besides the uncertainty of T_0 stated in the introduction, there are some major drawbacks in this approach, such as:

- In a safety analysis of a real component the required pof is usually difficult to be defined. A deterministic lower bound is often preferred and more suitable.

- For small pof (< 0.02) that are required to assure safety of a real component, eq. (1) is inaccurate, since it approaches K_{min}, which is not a physical quantity, but an auxiliary statistical number. It does not account for the fact that there is a deterministic lower bound of K_{Ic}.
- To determine T_0, only data from the temperature range T_0-50° < T < T_0+50° can be used. Correspondingly, eq. (1) is valid with the same restrictions on T. Thus, the upper transition range is not covered by (1)
- For cracks with a long crack front, i.e. a surface crack along a girth weld, (2) may become very conservative.
- The bias in T_0 mentioned above indicates that there might be an additional, constraint-dependent size effect not accounted for in (2), which only covers the statistical size effect.

For these reasons, MC tolerance bounds are often not suitable in an engineering safety analysis.

Correlation with RT_{NDT}. According to [2], RPV-steels in the DBT-regime exhibit a lower bound of fracture toughness given by

$$K_{LB(ASME)}(T) = 36.5 + 22.8 \cdot \exp[0.036 \cdot (T - RT_{NDT})] \quad (3)$$

where RT_{NDT} is the so-called nil-ductility reference temperature, which shall be determined by Charpy and Pellini tests as described in [2]. The main drawbacks of (3) are its purely empirical foundation and the involvement of Pellini tests, which are difficult to be performed and hard to interpret in terms of fracture mechanics. Alternatively, RT_{NDT} can be determined from T_0 by the following correlation [3]:

$$RT_{NDT} = RT_{T0} = T_0[^oC] + 19.4K \quad (4)$$

This makes (3) to be a practical concept. If fracture toughness can be characterized by just one parameter, as postulated by the MC-concept as well as in eq. (3), then the existence of a reliable relation between RT_{NDT} and T_0 is plausible. However, the relation does not explain the above mentioned bias in T_0. A possible improvement is suggested in the next section.

Margins According to ENSI Guideline B1

The discussion above leads to the conclusion that lower-bound K_{Ic}(T) according to Eq. (3) and (4) are better suited than (1) and (2) to demonstrate safety and defect tolerance of nuclear power plants within a surveillance plan. However, although this approach is well accepted and widely used, there are still some open questions and unresolved issues. As mentioned in the introduction T_0 can differ by 15K or even more between different standard test specimens. This result should be accounted for in a correlation such as (4). Furthermore, uncertainties and required safety margins are not outlined in [3]. Adequate safety margins are a key issue when safety of a real component has to be assessed based on scattering data. One of the crucial questions is, if and how much of a safety margin is already implicitly contained in (4).

ENSI recently released a guideline for ageing surveillance of RPVs [8] which accounts for the bias und the main uncertainties of T_0 by corresponding shifts and margins, as explained below. It requires K_{Ic}(T) to be determined by (3), with RT_{NDT} to be calculated by

$$RT_{NDT} = RT_{ref} = T_0 + 12.4K + \Delta T_s + \sqrt{\frac{\beta^2}{n} + \sigma_{\exp}^2 + \Delta T_M^2 + \Delta T_T^2} \quad (5)$$

In (5) n is the number of valid tests to determine T_0, β and σ_{exp} are defined and explained in [1]. According to [1] reasonable values are β=18K and σ_{exp}=4K. The additional terms have the following meaning:

- ΔT_s is the systematic (non-stochastic) part of the specimen size effect. It shall be chosen as ΔT_s =0, if T_0 was evaluated from tests on 1T- C(T)-specimens, and ΔT_s =10K in case of pre-cracked Charpy specimens, respectively.
- ΔT_M accounts for the inhomogeneity of the test material. It shall be chosen as ΔT_M =0 for base material and ΔT_M =6K in case of weld material. These values are tentatively taken from [7]. Currently, a research project is going on to investigate this issue for a typical weldment of an RPV, so eventually this number will be adjusted.
- ΔT_T represents the statistical part of the specimen size effect. It shall be chosen as ΔT_T= 0 for 1T-CT-specimens and as ΔT_T= 5K for pre-racked Charpy specimens, respectively.

The quantities under the square root represent effects on T_0 with stochastic nature, those outside the root represent deterministic effects. Eq. (5) is based on the assumption that in the hypothetical case of homogeneous material, a very large number n of 1T-CT-specimens and a very accurate experimental measurement set-up to determine T_0 leads to $RT_{NDT} = T_0 + 12.4K$. Correspondingly, 12.4K represents the systematic (i.e. non-stochastic) part of the shift according to eq. (4), 19.4 K. The value 12.4K is verified by experimental data, as shown below.

Experimental Background

A number of CT- and SEB-specimens of different sizes were tested in the DBT-range according to [1]. As a representative test material RPV-steel 22NiMoCr 3-7 was chosen. Standard 1T-CT-specimens (B = 25.4 mm) and 3-point bending specimens (SEB) of different thicknesses from 0.4T (B=W=10 mm) up to 3.2T (B=W=80 mm) were used. The test procedure and the detailed results are documented in [4, 5]. Fig. 1 shows the obtained K_{Jc}-values for all CT- and SEB-specimens. The multi-temperature option of [1] was applied to evaluate T_0 for each specimen type and size. The resulting T_0-values are given in Table 1. As expected and in accordance with the note in [1], T_0 from the 0.4T-SEB-specimens are significantly lower than T_0 from CT-specimens. The latter can be regarded as a reference value, since special care was taken to determine T_0 from CT-specimens: A relatively large number of CT-specimens (21) were tested with a good spacing of the test temperatures on both sides of T_0. Moreover, T_{100} (see definition in eq. (6) below) agreed well with T_0, which indicates that the corresponding T_0 can be considered as a value with little uncertainty.

Table 1: T_0-values of the used specimens

	1T-CT	3.2T-SEB	1.6T-SEB	0.8T-SEB	0.4T-SEB
Thickness [mm]	25.4	80	40	20	10
T_0 [°C]	-71	n.a.	-75.2	-85.8	-86.1

The lower bound curve delivered by (3) and (4) with T_0 from the 1T-CT-specimens (T_0=-71°C) captures all experimental data with a certain margin, whereas the curve based on T_0 from the 0.4T-SEB-specimen tends to be non-conservative. Replacing 19.4 by 12.4 in eq. (4) (corresponding to eq. (5)) the lower bound curve still envelops all data points, but with a reduced margin (red full line in Fig. 1).

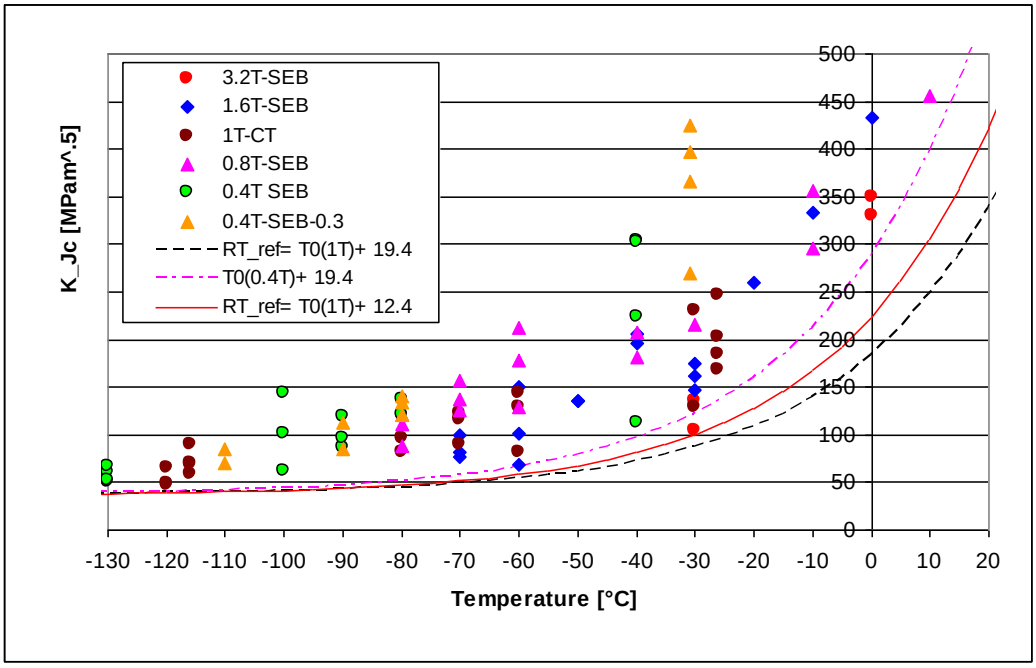

Fig. 1: K_{Jc} obtained from specimens of different thicknesses (1T=25.4mm) as a function of temperature, in comparison with the lower bounds given by (3), with different T_0 used in (4).

Sources of Inaccuracies and Biases in T_0

Besides the unavoidable experimental uncertainty quantified in (6) by β and σ_{exp}, the main sources of deviations or biases in T_0 are found to be the test temperature T_{test} (relative to T_0), the specimen size and the loading rate. These effects are briefly discussed below. For more details we refer to [6, 7].

Effect of Test Temperature. It is obvious that maximum accuracy of T_0 is obtained if the tests are performed right at the reference temperature, i.e. $T_{test} = T_0$. However, since T_0 is usually not known in advance, T_{test} in general deviates from T_0, which affects the accuracy of T_0. The evaluation according to [1] is based on the empirical finding that the median of $K_{Jc}(T)$ follows the curve

$$K_{JCmed}(T) = 30 + 70 \cdot \exp[0.019 \cdot (T - T_0)] \qquad (6)$$

Since (6) is essentially empirically founded it is expected to hold only as an approximation. Correspondingly, any deviation of the actual median curve from (6) leads to errors in T_0, if the test temperature is away from T_0. The corresponding effect on T_0 was explored by generalizing (6) to

$$K_{JCmed}(T) = 30 + 70 \cdot \exp[p \cdot (T - T_{100})] \qquad (7)$$

T_0 and T_{100} have the same physical meaning. If p=0.019, then T_0 and T_{100} coincide. If p deviates from 0.019, then T_{100} deviate from T_0. The difference increases with increasing difference in p and in the difference between T_{test} and T_0. If T_0 and T_{100} differ from each other, then T_0 is expected to be inaccurate. Since T_{100} is less restricted than T_0, it can come closer to the physical "correct" reference temperature corresponding to K_{Jc}=100 MPa·m$^{0.5}$ (provided the experimental data-set is large enough and well distributed on the temperature axis for the corresponding regression).

T_{100} and p can be evaluated from a series of tests at different tests temperatures by linear regression in a logarithmic scale of the ordinate. The results are given in Table 2. Apparently, p is higher than 0.019 in most cases. Therefore, T_0 can deviate from T_{100} depending on the test temperature. From (6) and (7) the difference is easily obtained in mathematical terms as follows [6, 7]:

$$T_0 - T_{100} = (T_{test} - T_{100}) \cdot \left(1 - \frac{p}{0.019}\right) \tag{8}$$

Eq. (8) applies to the single-temperature option of [1]. In case of the multi-temperature option, the average T_{test} can be inserted. As discussed above, T_{100} is expected to be closer to the physical reality, so T_0 - T_{100} quantifies a possible error in T_0. According to (8) it mainly depends on the differences T_0 - T_{test} and p - 0.019.

Table 2: Parameters defined in eq. (7) with corresponding loading rate (in MPa√m/s)

specimen	1T-CT	3.2T-SEB	1.6T-SEB	0.8T-SEB	0.4T-SEB	0.4T-SEB
p	0.0180	n.a.	0.0251	0.0196	0.0273	0.0264
T_{100} [°C]	-72.0	-58.0[1)]	-68.0	-85.2	-82.7	-82.4
Loading rate	0.316	0.318	0.838	0.611	0.923	0.766

[1)] estimated from 2 valid specimens and assumption p=0.019

[2)] mean value of dK_J/dt in the range $0 < K_J < 30$ MPa·m$^{0.5}$.

Effect of loading rate. The data shown in Table 2 and Fig. 2 indicate that p correlates with the loading rate. It tends to increase with increasing loading rates for $dK_J/dt > 0.6$ MPa·m$^{0.5}$/s. This can be explained by local adiabatic heating in the fracture process zone due to the increase of loading rates. It leads to locally increased temperatures, which promote loss of constraints. Additional tests at much higher rates (up to impact loading by a Charpy pendulum hammer) confirm this trend [6, 7]. For impact loading the p-values were found to be in the range of 0.035 – 0.04. It seems that there is a saturation of the rate-effect on p at this level.

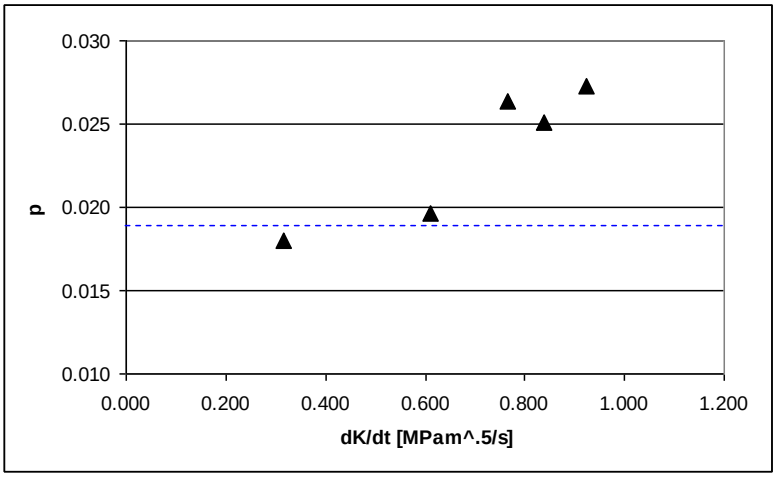

Fig. 2: Values of coefficient p (see eq. (7)) as a function of the loading rate,

According to [1], loading rates should be in the range 0.1 MPa·m$^{0.5}$/s < dK_J/dt < 2 MPa·m$^{0.5}$/s for quasi-static testing. The present results indicate that the requirement should rather be 0.1 MPa·m$^{0.5}$/s < dK_J/dt < 0.6 MPa·m$^{0.5}$/s.

Effect of Specimen Thickness. To investigate the effect of the specimen size on T_0, it is advantageous to consider the behaviour of T_{100} rather than T_0, since – as discussed above - the former is less affected by additional influencing factors than T_0. Fig. 3 shows T_{100} as a function of the thickness of the specimen. There is a clear trend to increased T_0 with increasing thickness. It is interesting to note that the difference between B=10mm and B=25.4 mm is just about 10K, i.e. the value that is mentioned in [1] as the average difference between CT- and pre-cracked SEB-specimens.

Tentatively – although the present database is actually too small for this purpose it – the following correlation formula is derived from the data shown in Fig. 3.

$$\Delta T_{100}(B) = T_{100}(B) - T_{100}(B_T) = 10.769 \cdot \ln(B/mm) - 34.835K \qquad (9)$$

B_T denotes the thickness of a standard 1T-specimen, thus B_T=24.5 mm. For physical reasons it is expected that $\Delta T_{100}(B)$ reaches a saturation at about ±15K. Since T_{100} and T_0 have essentially the same physical meaning, $\Delta T_{100}(B) \approx \Delta T_0(B)$, so eq. (9) can be applied to T_0-values as well. For example the equation can be applied to correct T_0 from small test specimens to the standard thickness, or to adjust T_0 in (1) if applied to larger components.

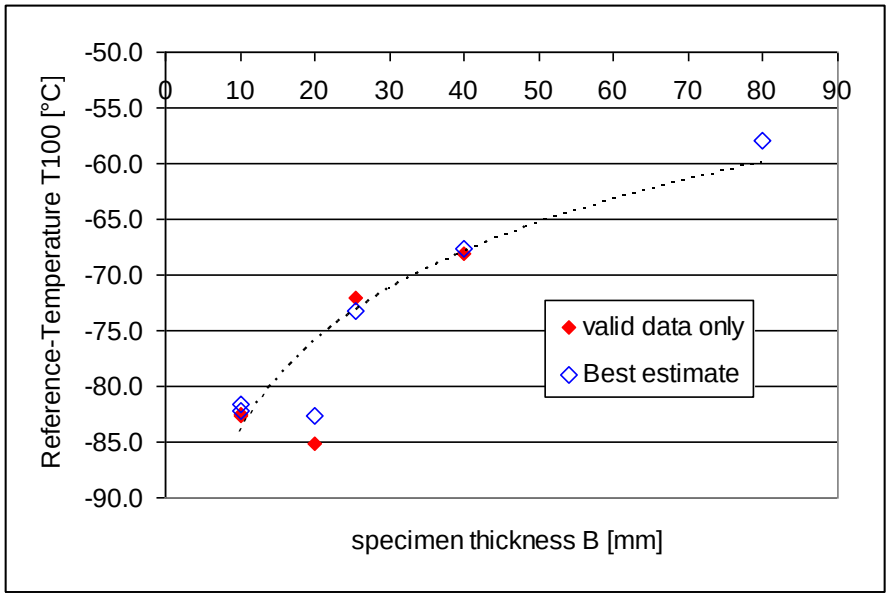

Fig. 3: T_{100} as a function of specimen thickness

Discussion and Conclusions.

According to the literature (see [1] for further references) the difference between T_0 determined by CT- and pre-cracked SEB- specimens can vary between zero and more than 20°K. The presented experimental data confirm this general trend by a difference of about 15K. The reason for this bias can be explained with the results shown and discussed above. The analysis reveals that it mainly results from a combination of deterministic size effect and a stochastic part, which depends on the test temperature and the loading rate. The observed size effect is probably due to a plasticity-induced loss of constraints. Thus, it seems that the size requirement of ASTM E1921 is not restrictive enough to guarantee size-independence of J_c and correspondingly of T_0. Furthermore, it was found that T_0 can be influenced by the test temperature, particularly if the single temperature

method is used at a temperature far away from T_0. Therefore, it is recommended to apply the multi-temperature option of [1] whenever it is possible, and to strive for testing close to T_0. In existing T_0-data, the loading rate and the individual test temperatures are usually not reported. Therefore, the corresponding effect has to be regarded as stochastic.

If T_0 is used to determine fracture toughness of a real component, either by (1) and (2) or by (3), these effects have to be accounted for. A formula to determine RT_{NDT} is suggested by eq. (6). Its structure reflects the discussed deterministic and stochastic influences. The values of the various parameters are chosen on the basis of available data from the literature and own research.

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