NRC Review of the Technical Basis for Use of the Master Curve in Evaluation of Reactor Pressure Vessel Integrity

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BACKGROUND

Fracture Toughness Characterization

The fracture toughness of the reactor pressure vessel (RPV) steel in a nuclear plant provides a key input to calculations that commercial licensees perform to demonstrate the fracture integrity of the vessel during both normal operations and postulated accident conditions (e.g. pressurized thermal shock, or PTS). Currently, the ASME K_{IC} and K_{IR} curves, indexed to the RT_{NDT} of the material, describe the fracture toughness of the RPV and its variance with temperature. These curves were adopted in 1972 as a lower bound representation to a set of 173 linear elastic fracture toughness (K_{IC}) values and 50 linear elastic arrest toughness (K_{IA}) values for 11 heats of RPV steel. The use of RT_{NDT} to normalize temperature was intended to account for the heat-to-heat differences in fracture toughness transition temperature, thereby collapsing the fracture toughness data onto a single curve. However, RT_{NDT} is not always successful in this regard, often providing a conservative characterization of fracture toughness.

Developments since 1972 set the scene for substantial improvements to the K_{IC} / RT_{NDT} characterization of fracture toughness. In 1980 Landes and Schaffer noticed a weakest link size effect for specimens failing by transgranular cleavage. They demonstrated that larger specimens fail at lower toughness values, even when the severe size requirements of linear elastic fracture mechanics (LEFM) are satisfied. Beginning in 1984, Wallin and co-workers from VTT in Finland combined this weakest link size effect with micro-mechanical models of cleavage fracture. Wallin developed a model that accounts successfully for size effects, and provides a means to calculate statistical confidence bounds on cleavage fracture toughness data. These concepts, combined with the observation that ferritic steels exhibit a common variation of cleavage fracture toughness with temperature, gave birth to the notion of a “master” fracture toughness transition curve for all ferritic steels.

Recently Master Curve technology has been incorporated into ASTM and ASME codes and standards. In 1997 ASTM adopted standard E1921 that describes how to measure an index temperature for the Master Curve, T_o. T_o locates the Master Curve on the temperature axis for the steel of interest. E1921 incorporates a modern understanding of elastic-*plastic* fracture mechanics, and so permits determination of T_o using specimens as small as a precracked CVN. In 1998 ASME published Code Cases N-629 and N-631. These Code Cases permit use of a Master Curve-based index temperature ($RT_{To} \equiv T_o + 35°$ F) as an alternative to RT_{NDT}. Because RT_{To} is calculated from fracture toughness data, it consistently positions bounding K_{IC} and K_{IR} curves relative to fracture toughness data for all material and irradiation conditions encountered in nuclear RPV service. Such consistency cannot be achieved via the correlative RT_{NDT} techniques used currently.

Motivation for a Improved Accuracy

Price deregulation of the electric power industry in the United States fundamentally changes the economics of continued of nuclear power plant (NPP) operation. Before deregulation NPPs,

which provide primarily baseload, were paid based on capacity. Now NPPs must compete with other energy sources, so utility executives are considering new operational scenarios, some of which were unheard of as little as five years ago: extending the licensed life of the plant beyond 40 years, removal of flux reduction, up-rating of the reactor, etc. These actions all increase the rate of embrittlement, causing current licensing limits to be approached at an earlier date. Also, the lead time needed to bring replacement power sources (e.g. gas turbines, coal, or license renewal of the NPP for an additional 20 years) on-line push back by nearly a decade from EOL the date on which utilities, and consequently the NRC, must make the decisions and do the analysis that decide the future of a NPP. In combination, these factors suggest that the fate of nearly 30% of currently operating pressurized water reactors (PWRs) will be decided between 2005 and 2010. Consequently, both the industry and the NRC are now considering refinement of the procedures used to estimate of RT_{NDT} at EOL with an eye to reducing known over-conservatisms while adequately protecting the public safety. Use of the Master Curve is but one of these refinements

In addition to these economic motivations for change, regulatory motivations exist as well. The perception, based on RT_{NDT}, of a lower toughness RPV steel than actually exists can unnecessarily restrict the permissible pressure-temperature (P-T) envelope for routine heat-up and cool-down operations, which can reduce overall plant safety. For example, an unnecessarily narrow P-T envelope increases the possibility of damaging pump seals due to insufficient cooling water pressure. Considering that pump seal failure produces a small-break loss of coolant accident (a potential pressurized thermal shock initiator), this situation is clearly undesirable. Additionally, the current perception of low RPV toughness (based on high RT_{NDT} values) produces the need for flux suppression systems to maintain an acceptable P-T envelope. Flux suppression produces higher fuel peaking and, consequently, less margin against fuel damage if an accident were to occur. The risk to the plant and the public associated with these situations can be mitigated by replacing the conservative RT_{NDT}-based characterization of fracture toughness with the more accurate characterization provided by the Master Curve.

The use of Master curve-based approaches, is consistent with the NRC's goal of moving toward a risk informed framework for rule and decision-making. This framework, and the probabilistic risk assessment (PRA) methodologies that support it, require the use of best estimate values rather than bounding values whenever possible. The Master Curve provides best estimates of fracture toughness, along with the explicit consideration of uncertainty. Conversely, RT_{NDT} technology provides bounding values, suggesting that the Master Curve fits better within a risk informed framework than does RT_{NDT}.

OBJECTIVE

In a recent NUREG, the Staff examined the technical basis for both the Master Curve itself, and for its application to the assessment of nuclear RPV integrity against fracture [Kirk 00e]. Here we focus attention on the application issues that need to be addressed to transition from the current bounding approach to toughness estimation toward a best-estimate approach that is more consistent with a risk-informed decision making process. To establish the baseline against which progress to this goal is measured, we begin by reviewing the origin of conservatisms inherent to the current RT_{NDT} / K_{IC} procedures for fracture toughness characterization.

CURRENT PROCEDURE TO ESTIMATE THE FRACTURE TOUGHNESS OF NUCLEAR RPV STEELS

Procedure Description

In all calculations to assess the integrity of a nuclear RPV against fracture, an estimate of the fracture toughness of the vessel after neutron embrittlement is needed. Practical limitations regarding the volume of material that can be irradiated as part of a surveillance program restrict both the quantity and size of the material samples used to obtain this estimate. Currently the fracture toughness of an RPV steel is estimated as follows:

1. The transition temperature of the material before irradiation ($RT_{NDT(u)}$) is determined using either ASME NB-2331 procedures [ASME NB2331], or alternative procedures intended to be conservative to NB-2331 [NRC MTEB5.2].
2. $RT_{NDT(u)}$ is shifted to account for the effects of neutron irradiation. The shift added is the difference in the CVN 30 ft-lb transition temperature (ΔT_{30}) before and after irradiation. ΔT_{30} may be either based on shift measurements (from a ASTM E185 qualified surveillance program) or on shifts calculated from chemical composition using an embrittlement trend curve [NRC RG199R2].
3. Margins are added to account for uncertainties in the state of knowledge of the material, and for uncertainties in the calculational process [NRC RG199R2, Randall 87].
4. The estimated transition temperature of the vessel after some amount of neutron irradiation (now $RT_{NDT(u)} + \Delta T_{30} +$ Margin) is used as an index temperature for the ASME K_{IC} and/or K_{IR} curves, thus establishing the lower bound above which the actual fracture toughness of the material is expected to lie.

It should be noted that nowhere in this process is the fracture toughness of the material actually measured, rather it is inferred through a series of correlations. The components of this procedure began to be established as early as 1972, and the procedure was solidified in concept as early as 1977 (NRC RG199R1). Two state-of-knowledge limitations that existed in this timeframe necessitated adoption of a correlative approach to toughness estimation:

1. <u>Linear Elastic Characterization of Fracture Behavior</u>: Between 1972 and 1977, the only mathematical description of fracture behavior sufficiently well developed for ASME codification was one premised on a linear elastic characterization of material constitutive behavior. At temperatures in fracture mode transition, large fracture toughness specimens (minimum lineal dimension of $\approx$ 2in.) of nuclear RPV steels need to be tested to meet the validity requirements of a linear elastic fracture theory [ASTM E399]. It is not practical to use specimens of this size as part of a surveillance program.
2. <u>Need to Determine the Entire Transition Curve</u>: Calculations of the fracture integrity of a nuclear RPV require as input the complete variation of toughness with temperature through transition, not just the toughness at a fixed temperature. Between 1972 and 1977 there was no procedure available from which such a comprehensive description of transition fracture toughness behavior could be inferred based on tests of a limited number of specimens.

While approximate, the K_{IC} / RT_{NDT} procedure is believed to be, and indeed *must* be, conservative (i.e. always underestimate the measured fracture toughness of the material in question) due to the factors discussed further in the following section.

Conservatism of Procedure

Due to the LEFM Representation of Fracture Toughness

In 1972, ASME adopted the K_{IC} and K_{IR} curves to describe the variation with temperature of the static and dynamic (respectively) fracture toughness of nuclear RPV steels [WRC 175, Marston 87]. These curves were hand-drawn as lower bounds to a set of fracture toughness data valid according to the LEFM requirements of ASTM E399 [ASTM E399].

ASTM E399 places severe restrictions on the size of the plastic zone at fracture relative to the overall size of the specimen to ensure that a linear elastic description of material flow behavior is not violated in a significant way. The E399 size requirement is as follows:

$$a, b, B \geq 2.5\left[\frac{K_I}{\sigma_y}\right]^2 \qquad (1)$$

where a is the crack length, b is the length of the uncracked ligament, B is the specimen thickness, K_q is the stress intensity factor at fracture, and σ_y is the yield strength at the test temperature. Considering that the diameter of the plastic zone ahead of a deforming crack in a thick structure can be expressed as follows:

$$d_{plastic} = \frac{1}{3\pi}\left[\frac{K_I}{\sigma_y}\right]^2 \qquad (2)$$

one concludes that E399 requires that the smallest length scale in the specimen (a, b, or B) must exceed the size of the plastic zone by a factor of approximately 25 (=2.5· 3· π). This restriction invariably admits only the lowest part of the population of cleavage fracture toughness values to further analysis, as illustrated in Fig. 1. Since the K_{IC} and K_{IR} curves were based exclusively on these low fracture toughness values, it is clear that the requirement for LEFM validity forces establishment of a low bounding curve.

Due to the Use of RT_{NDT} to Normalize Temperature

When using fracture toughness data to establish the bounding K_{IC} and K_{IR} curves, the fracture toughness values were not plotted *vs*. temperature, but rather *vs*. the difference between the test temperature and an index temperature called RT_{NDT} [WRC 175, Marston 78]. RT_{NDT} is determined from Charpy V-Notch (CVN) and nil-ductility temperature (NDT) data as per ASME NB-2331, as follows:

$$RT_{NDT} = MAX\{T_{NDT}, T_{35/50} - 60\} \quad \text{(in °F)} \qquad (3)$$

where T_{NDT} is the nil-ductility temperature determined by testing NDT specimens as per ASTM E208, and $T_{35,50}$ is the transition temperature at which Charpy-V notch (CVN) specimens tested as per ASTM E23 exhibit at least 35 mills lateral expansion and 50 ft-lbs absorbed energy. RT_{NDT} is intended to account for the heat-to-heat differences in fracture toughness transition temperature, and thereby collapse all of the transition toughness curves for specific heats of steel onto a single curve [ASME NB2331, ASTM E208, ASTM E23]. This procedure of using RT_{NDT} to normalize temperature conservatively places the K_{IC} curve relative to measured fracture toughness data for the following reasons:

1. <u>The NB-2331 Procedure for Determining RT_{NDT}</u>: This procedure requires first that T_{NDT} be established, and that that three CVN tests be conducted at 60° F above T_{NDT} to demonstrate that the minimum CVN energy exceeds 50 ft-lbs, and that the minimum lateral expansion exceeds 0.035-in. NB-2331 does not require the user to either bracket the NDT temperature (i.e. achieve both break and no-break results), nor does it require determination of the temperature at which the 50 ft-lbs / 35 mil criteria is just exceeded.

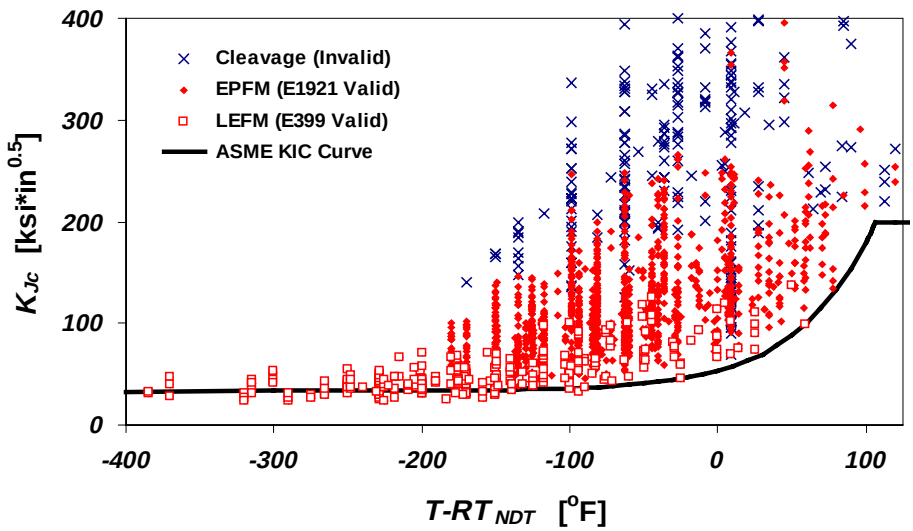

Figure 1. Placement of LEFM (ASTM E399) valid data relative to the overall population of cleavage fracture toughness data for nuclear RPV steels. All values are plotted as-measured, and are normalized relative to an ASME NB-2331 value of RT_{NDT}.

Consequently, the NB-2331 procedure forces reported values of RT_{NDT} toward the upper end of all RT_{NDT} values for a particular heat of steel.

2. <u>The Procedure by which the Relationship Between the ASME K_{IC} curve and RT_{NDT} was Established:</u> In the early 1970's an ASME task group established the following relationship between RT_{NDT} and the K_{IC} curve:

$$K_{IC} = 33.2 + 2.81 \cdot \exp[0.0198 \cdot (T - RT_{NDT} + 100)] \qquad (K \text{ in ksi}\sqrt{\text{in}},\ T \text{ in °F}) \qquad (4)$$

This equation (a hand-drawn curve at the time) was constructed in 1972 such that no existing measured K_{IC} value in transition (i.e. at T-RT_{NDT} > 100° F) fell below the K_{IC} curve*. This empirical approach to developing a transition toughness curve was needed because at the time no theoretical basis existed to account for the differences in loading, loading rate, crack geometry, and specimen thickness between NDT and CVN tests and the conditions of interest in nuclear RPV service (i.e. a sharp crack in a thick structure).

The substantial collection of fracture toughness data available today (Fig. 1) testifies to the bounding characteristics achieved through the use of the ASME NB-2331 definition of RT_{NDT} along with the ASME K_{IC} curve†. It is important to recognize that the *combined* effects of these

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* The ASME committee did not enforce this bounding requirement on the lower shelf, as evidenced by the considerable number of K_{IC} values that fall below the 33.2 ksi√in asymptote in Fig. 2(a).

† Only one K_{IC} value falls below the K_{IC} curve in transition. A K_{IC} value of 98¼ ksi√in measured using a 6T C(T) of HSST Weld 72W falls 0.9 ksi√in below the ASME K_{IC} curve at T-RT_{NDT} = +59.4° F.

two factors produce a bounding curve. Neither the ASME NB 2331 definition of RT_{NDT} nor the ASME K_{IC} equation acting individually ensures bounding.

Quantification of Conservatism

Because the index temperature RT_{NDT} is determined with complete independence from the fracture toughness data it represents through its use with the ASME K_{IC} curve (eq. (4)), there is no guarantee that, for example, a K_{IC} curve positioned with respect to RT_{NDT} will always under-estimate K_{IC} data by the same amount. In fact, quite the contrary is true, as illustrated in Fig. 2. Recently, Bass et al. [Bass 00] quantified the range of possible conservatism inherent to a K_{IC} curve positioned using RT_{NDT} by the procedure illustrated in Fig. 3. Fig. 4 shows the results of this analysis, which demonstrate that a definition of the transition temperature that consistently positions a bounding curve relative to fracture toughness data can fall below RT_{NDT} by up to 200° F, illustrating the conservatism inherent to the RT_{NDT} process.

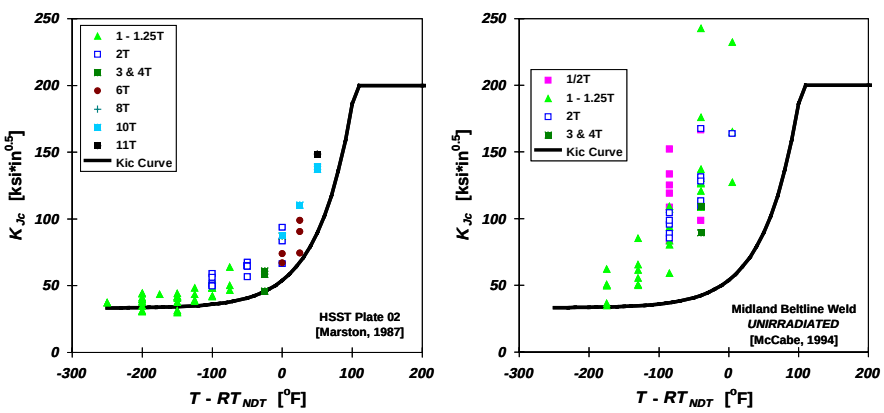

Figure 2. Illustration of the inconsistency with which RT_{NDT} positions the K_{IC} curve relative to as-measured fracture toughness data.

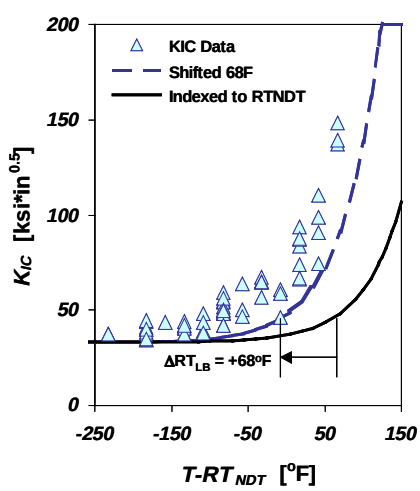

Figure 3. Procedure for defining the conservatism inherent to a K_{IC} curve located based on RT_{NDT} relative to measured K_{IC} data for the same steel [Bass 00]. The RT_{NDT}-located K_{IC} curve is translated toward the dataset until it intersects the first K_{IC} value in transition. The amount of translation defined ΔRT_{LB}.

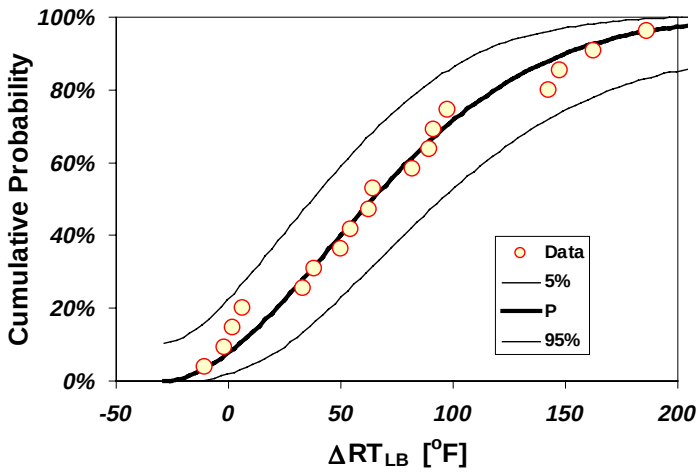

Figure 4. Conservatism inherent to a K_{IC} curve located based on RT_{NDT} quantified by applying the procedure illustrated in Fig. 3 [Bass 00] to and expanded set of LEFM valid data assembled by the Oak Ridge National Laboratory (ORNL) [Bowman 00].

APPLICATION OF THE MASTER CURVE IN RPV INTEGRITY ASSESSMENT

Table 1 summarizes the codes, standards, and regulations that concern estimation of fracture toughness values used in nuclear RPV integrity calculations. The first two steps identified in Table 1 include a standard to measure toughness, and a procedure that uses this information to position a reference toughness curve on the temperature axis. ASTM E1921-97 and ASME Code Cases N-629 and N-631 fulfill these needs for the Master Curve. Questions raised previously by the Staff regarding the use of Master Curve technology in these codes and standards [Mayfield 97, Kirk 00a] have received considerable attention over the past few years, and are now largely resolved [Kirk 00e]. These questions, and the resolution status of each, are as follows:

1. ASTM E1921-97
    a. Is the single temperature dependence of the Master Curve appropriate for all RPV steels of interest, even after irradiation?: On-going research activities performed by both Natishan (and co-workers) [Natishan 98, Natishan 99a, Natishan 99b, Wagenhofer 00a, Wagenhofer 00b, Kirk 00b] and Odette (and co-workers) [Odette 00] provide encouraging evidence that questions regarding the theoretical limits on the universal Master Curve shape will soon be resolved. These results provide guidance on two related questions:
        i. Breadth of Applicability: Research focused on establishing the physical basis for a universal Master Curve shape reveals that the lattice structure alone controls the temperature dependence of fracture toughness. Thus, the Master Curve will model well the temperature dependence of fracture toughness for all pressure vessels steels of any product form both before and after irradiation because all of these steels have a BCC matrix phase lattice structure.
        ii. Effect of Test Temperature: T_o values determined as per E1921-97 do not show a systematic bias or trend with test temperature, nor is this expected due to the common dependence of fracture toughness on temperature for all ferritic steels. Revisions to E1921-97 propose further

restriction to the range of temperatures within which one is permitted to perform toughness tests to estimate T_o. Available empirical evidence suggests that this additional restriction is not necessary.

b. Does the ¼-power scaling rule adopted within the Master Curve reflect appropriately the effect of specimen size on fracture toughness?: Provided the material has a random distribution of cleavage initiations sites spread homogeneously throughout its volume, the Weibull model of cleavage fracture toughness in transition relies only on the existence of a state of small scale yielding to ensure its theoretical applicability. As the micro-scale inhomogeniety needed to violate the assumption of a random distribution of cleavage initiation sites is not characteristic of RPV steels, applicability of the Master Curve statistical fracture model can be assessed based on a calculation of the deformation state at fracture. Under small scale yielding conditions, fracture toughness will scale with thickness raised to the ¼-power. This result is anticipated theoretically and is well confirmed experimentally.

c. Are T_o values determined using precracked CVN specimens equivalent to T_o values determined using larger specimens?: T_o values determined using precracked CVN specimens show a systematic bias relative to T_o values determined using physically larger samples. This bias depends on the deformation level at fracture. Information is presented herein that can be used to correct for this bias. It is important that such a correction be reviewed and balloted by ASTM committee E08 due to the interest of nuclear licensees in using precracked CVN specimens removed from surveillance to estimate T_o.

2. ASME Code Cases N-629 and N-631

a. Will K_{IC} and K_{IR} curves indexed using T_o provide an equivalent implicit margin to current approaches?: These Code Cases provide a Master Curve-based index temperature for the K_{IC} and K_{IR} curves that produce implicit margins functionally equivalent to those historically accepted for RT_{NDT}. The relationship between RT_{To} and T_o, i.e. $RT_{To} \equiv T_o + 35^o\,\mathrm{F}$, is defensible as it bounds a reasonable percentage of all fracture toughness data now available (97.5%) for a crack front length (2.1-in.) that exceeds the great majority of flaws found in RPV fabrication.

In contrast to this substantial progress, Steps 3 and 4 in Table 1 have received little focus to date. Nevertheless, plant-specific Master Curve submittals have moved / are moving forward. In the next section we summarize these submittals, and discusses how each submittal has addressed Steps 3 and 4 in Table 1, both of which go beyond the scope of ASTM and ASME codes and standards. This discussion is followed by a section concerning the essential characteristics a general framework to estimate the fracture toughness at EOL. Finally, we discuss recent progress, or lack thereof, toward developing the various components of such a general framework.

Plant-Specific Applications of Master Curve Technology

To date the commercial nuclear power industry has brought two submittals before the NRC that use the Master Curve to estimate the vessel fracture toughness at EOL and assess compliance with 10CFR50.61 (i.e., with the PTS Rule). These submittals concerned / concern the licenses of the Zion [Yoon 95] and Kewaunee [Lott 99, Lott 00, Server 00] NPPs[‡]:

[‡] Since the NRC's response to the Kewaunee submittal is still pending, a detailed discussion is not appropriate at this time. Consequently, reference is made only to information presented at ASME conferences concerning the Kewaunee submittal.

- Zion: In the Zion submittal the licensee sought to use Master Curve technology and fracture toughness data on the limiting vessel material (Linde 80 weld WF-70) to establish a new un-irradiated value of RT_{NDT} [Yoon 95]. The protocols of 10CFR50.61 were then used to estimate the effects of both irradiation and uncertainties on this value, and to establish a PTS screening criteria to compare this value to. The Zion submittal did not modify 10CFR50.61 protocols to account for the use of Master Curve technology to estimate RT_{NDT}.
- Kewaunee: In a series of papers concerning the Kewaunee submittal, Lott, et al. outline several strategies to use measured values of T_o, both un-irradiated and irradiated, to estimate a RT_{NDT}-like quantity at EOL [Lott 99, Lott 00, Server 00]. In developing these estimation strategies, the authors sought to use T_o to estimate a RT_{NDT}-like quantity in a manner that parallels and satisfies the intent of current regulations (i.e. 10CFR50.61). In the Kewaunee submittal this RT_{NDT}-like quantity was compared to the current PTS screening criteria [10CFR50.61]

In summary, lacking any established alternative approach, the Zion and Kewaunee submittals both align closely with current procedures to estimate the toughness for some future irradiation condition, and to assess the adequacy of this toughness during a postulated PTS event. This approach invariably leads to assignment of burdensome margins to account for mis-fits, both real and perceived, between Master Curve technology and the 10CFR50.61 framework. We examine the potential for moving away from this paradigm in the next section.

Table 1. Codes, Standards, and Regulations that Govern the Assessment of Fracture Toughness for Use in a PTS Analysis.

Step		**Current Technology**	**Master Curve Technology**
1	**Measure a Material Property**	*CVN*: ASTM E23 *NDT*: ASTM E208	T_o: ASTM E1921
2	**Establish an Index Temperature and Define a Reference Toughness Curve**	RT_{NDT} ASME NB-2331	RT_{To} ASME N-629 and N-631
3	**Estimate the Toughness of Some Future Irradiation Condition (e.g., at EOL)**	Expressed in: 10CFR50.61, 10CFR50 APPG, ASME XI-G Based on: SECY 82-465, NRC MTEB5.2, NRC MEMO 82, Randall 87	Not Yet Established
4	**Establish a Screening Criteria for PTS**	Expressed in: 10CFR50.61 Based on: SECY 82-465	Not Yet Established

Progress Toward a Generic Master Curve Methodology

The information presented in Table 1 points out that factors exist beyond those considered thus far by ASTM and ASME that need to be addressed to bring Master Curve technology to the point that it can be applied routinely to assess nuclear RPV integrity:

1. Procedures to estimate the toughness at EOL: These procedures would predict T_o and/or RT_{To} for future irradiation conditions from available information (i.e. mechanical properties, chemical properties, fluence), and adjust these estimates to account for various uncertainties. Toughness is determined through the association of these index temperatures with fracture toughness transition curves. Reg. Guide 1.99 Rev. 2 describes the procedures used currently to this end [NRC RG199R2][§]. No parallel rule or guidance exists currently for Master Curve-based methodologies.
2. A PTS screening criteria: This would be a value / values to which a Master Curve-based estimate of T_o and/or RT_{To} at EOL would be compared to assess the suitability of the reactor for operation through EOL. SECY-82-465 establishes the technical basis for the current criteria (300° F for circumferential welds, 270° F for longitudinal welds, plates, and forgings) of 10CFR50.61 [SECY 82465, 10CFR5061]. No parallel rule or guidance exists for the Master Curve.

In this section we examine the current RT_{NDT}-based procedure to estimate the fracture toughness at EOL and discuss its role in establishing the current PTS screening criteria. This discussion provides a perspective on the obstacles that plant specific Master Curve applications have encountered in attempts to parallel current procedures. In the following sections we turn attention toward the future research and development achievements needed to eliminate these obstacles.

The model used to estimate toughness in the PFM calculations that established the current PTS screening criteria is as follows [SECY 82465]:

$$RT_{NDT(f)} = RT_{NDT(u)} + \Re \cdot \Delta RT_{NDT(f)} \tag{5}$$

where

- $RT_{NDT(f)}$ is the estimated RT_{NDT} of the vessel material after irradiation to the fluence *f*. Toughness is determined from $RT_{NDT(f)}$ through its use as an index temperature for the K_{IC} and K_{IR} curves
- $RT_{NDT(u)}$ can represent ***either*** of the following values:
    - A value of RT_{NDT} in the unirradiated condition based on testing a specific vessel material in accordance with ASME NB-2331, ***or***, if such measurements are unavailable,
        - For Welds: A generic mean value determined from a data set relevant to the material class of interest. Currently accepted generic mean values include -56° F for welds made with Linde 0091, 1092, 0124, and ARCOS B-5 welding fluxes, and -5° F for welds made with Linde 80 flux.
        - For Plates: If only CVN data are available, as is sometimes the case for plate materials, MTEB-5.2 provides procedures to estimate RT_{NDT} values that are intended to be conservative to (i.e. higher than) RT_{NDT} values determined using ASME NB-2331 [NRC MTEB52].

[§] These procedures find their origins in the work that led up to and provided the technical basis for the current PTS screening criteria [NRC MTEB5.2, NRC MEMO 82, Randall 87, SECY 82-456]. Nevertheless, the procedures are *applied* to estimate toughness not only for use in a PTS assessment (where 10CFR50.61 adopts Reg. Guide 1.99 Rev. 2 procedures and applies them at EOL fluence), but also as part of the calculations that establish heat-up and cool-down limits for routine operation [10CFR50 APPG, ASME XI-G].

- $\Delta RT_{NDT(f)}$ is the mean value of the irradiation induced transition temperature shift, and is calculated as follows:

$$\Delta RT_{NDT(f)} = (CF)\, f^{(0.28-0.1\log f)} \tag{6}$$

  $\Delta RT_{NDT(f)}$ can represent ***either*** of the following values:
  - It is the mean value of the of this shift for the material samples tested as part of the credible surveillance program, ***or***, if the surveillance data is not deemed to be credible,
  - It is the mean value of this shift for a material having the composition (Cu and Ni) corresponding to the heat average for the entire heat of material in question.

  In the former case, when credible surveillance data is used to establish ΔRT_{PTS}, the value $\Re$ adjusts ΔRT_{PTS} to account for differences between the chemical composition of the surveillance material and the heat average chemical composition. $\Re$ represents the "ratio procedure" as described in 10CFR50.61(2)(ii)(B). $\Re$ is defined as the chemistry factor (*CF*) for the best estimate composition of the heat divided by the chemistry factor for the specific composition of the surveillance weld. Tables in 10CFR50.61 define chemistry factors based on material product form, Cu, and Ni.

Natishan and co-workers have recently developed a diagrammatic representation of eq. (5), Fig. 5, which illustrates how the value of an input parameter (e.g. Cu, Ni, ϕt, CVN, NDT, etc.) "flows" through eq. (5) to produce an estimate of the value of RT_{NDT} after irradiation to EOL fluence [Li 00]. Thus, in addition to its use in determining the PTS screening criteria, eq. (5) also establishes the variability in estimates of $RT_{NDT(f)}$ that are compared to this screening criteria. This amount of variability, often called a "Margin," is traditionally added to the estimate of $RT_{NDT(f)}$ as follows [NRC RG199R2]:

$$RT_{NDT(f)} = RT_{NDT(u)} + \Re \cdot \Delta RT_{NDT(f)} + M \tag{7}$$

$$M = 2\sqrt{\sigma_I^2 + \sigma_\Delta^2} \tag{8}$$

where

- σ_I is the standard deviation in the value of $RT_{NDT(u)}$. It can represent ***either*** of the following values:
  - σ_I is *"determined from the precision of the test method"* if $RT_{NDT(u)}$ is established either (a) by testing the specific vessel material in accordance with ASME NB-2331, or (b) by MTEB-5.2 procedures. While not explicitly stated in 10CFR50.61, a value of σ_I = 0°F is used in this situation.
  - If a measured value of $RT_{NDT(u)}$ is not available, σ_I is the standard deviation of the data set used to establish the generic mean value of $RT_{NDT(u)}$. The most common value of in this situation is 17°F [NRC MEMO 82]. This value applies to welds made with Linde 0091, 1092, 0124, ARCOS B-5, and Linde 80 welding fluxes. Other values, like 26.9°F for B&W plate materials have also been established and are recorded in RVID.

  In both cases the sum $\{RT_{NDT(u)} + 2\sigma_I\}$ represents a bounding value of RT_{NDT} before irradiation. When RT_{NDT} is determined according to ASME NB-2331 or MTEB-5.2, these protocols produce a bounding estimate, so σ_I can be zero. However, when a mean

value of RT_{NDT} is used then 2σ =34°F needs to be added to produce a bounding estimate.

- σ_Δ is the standard deviation in the value of ΔRT_{PTS}. It can represent ***either*** of the following values:
    - If credible surveillance data **is not** available, the σ_Δ values are 28°F for welds and 17°F for plates
    - If credible surveillance data **is** available, the σ_Δ values are 14°F for welds and 8.5°F for plates.

These observations illustrate that the main difficulty faced by plant specific Master Curve applications has been the lack of an accepted framework by which to estimate the irradiated fracture toughness of the vessel from T_o data (i.e. a version of eq. (5) for T_o), and the fact that this framework was never used to establish a PTS screening criteria for T_o. Consequently, there is currently no T_o-based PTS screening criteria, and there is no T_o-based margin term (i.e. an eq. (8) for T_o) based on uncertainty in the input variables. Beyond these general difficulties, the Zion and Kewaunee submittals have encountered certain specific concerns in their attempts to parallel eqs. (7) and (8), as follows:

1. Zion: If an un-irradiated T_o is used and shifted using the Reg. Guide 1.99 Rev. 2 fluence function, concerns have arisen regarding the appropriateness of applying a CVN-based shift to fracture toughness data.
2. Kewaunee: With the current methodology, the toughness after irradiation can only be estimated from the sum of an un-irradiated reference temperature and an irradiation-induced shift in the reference temperature. Direct measurement of the irradiated transition temperature was not considered when the calculations that support the current PTS rule were adopted. Consequently, this approach currently lacks an established basis to account for differences between the composition of the surveillance samples and the composition of the material in the vessel. The existing Ratio procedure operates on the irradiation-induced shift in the transition temperature, not on its absolute value, making the proper application of this procedure to an irradiated transition temperature unclear.

Ultimately there is the nagging concern that forcing Master Curve-technology into the current, non-Master Curve, framework may produce systemic “lack of fit” uncertainties, thereby resulting in the need for higher margins. The only way to alleviate this concern is to establish a Master Curve framework to estimate toughness at EOL, and use this framework as part of the PFM calculations to establish a PTS screening criteria applicable specifically to Master Curve-based estimates of fracture toughness. Work on the development of such a framework for the Master Curve has only recently begun [Natishan 00]. In the following sections we review recent progress in the developing some of the components of such a framework, including:

1. Generic values of T_o for use when plant specific data is unavailable
2. Irradiation damage effects on T_o (Irradiation trend curves)
3. Treatment of the newly recognized linkage between fracture toughness and crack front length.
4. Treatment of the loading rate effect on fracture toughness to establish the position of the crack arrest curves used in establishing the PTS screening criteria.

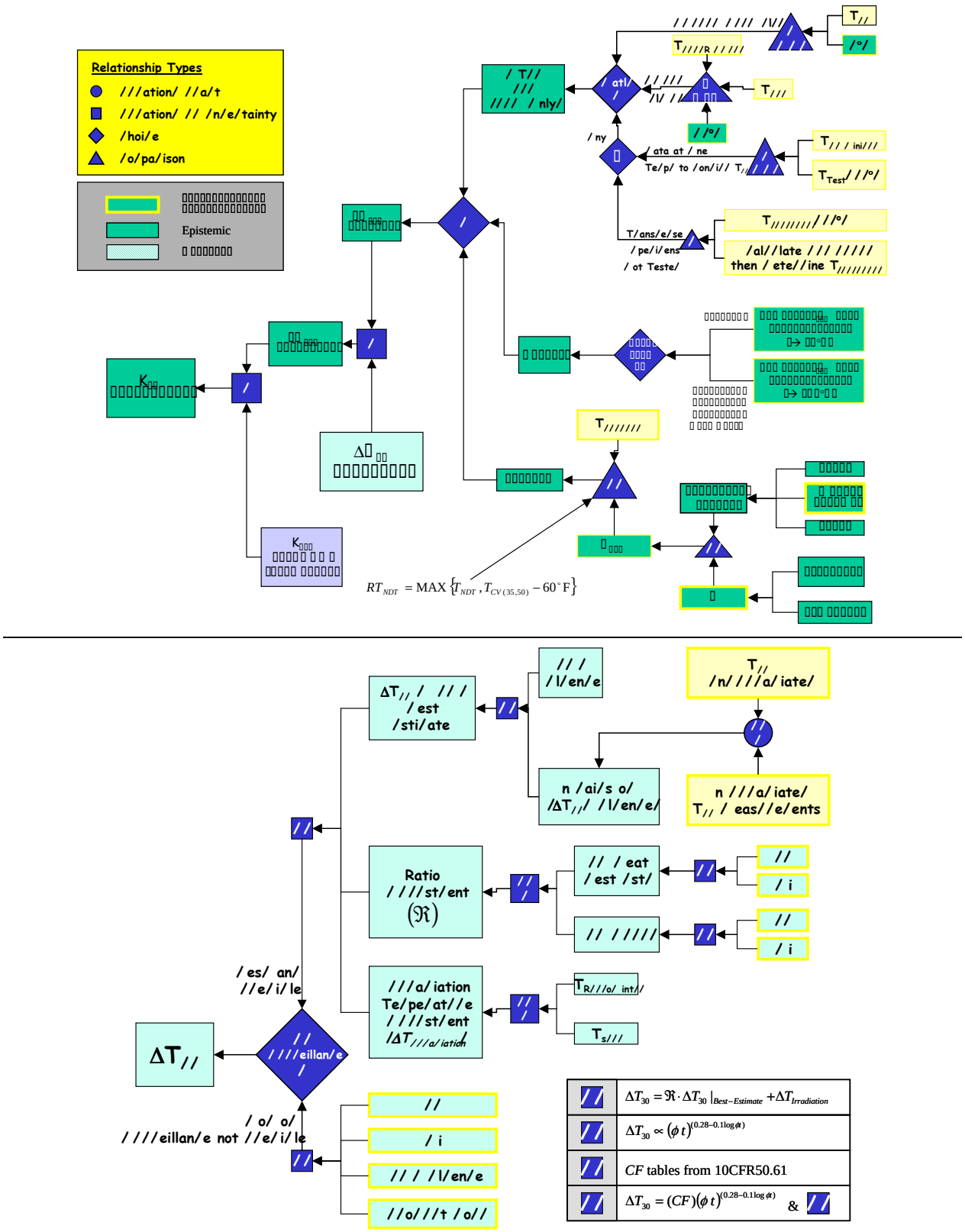

Figure 5. Root cause diagram illustrating the methodology used currently to estimate the fracture toughness of a reactor pressure vessel steel after irradiation [Li 00].

Generic Values of T_o

Current RT_{NDT}-based procedures provide generic values of un-irradiated RT_{NDT} for use when material specific information is not available. Similar generic values of RT_{To} will most likely be needed as part of a Master Curve methodology that is usable by all plants. Here we use a large collection of fracture toughness values [Rosinski 99] to establish candidate generic RT_{To} values by the following procedure:

1. The database is queried to identify all fracture toughness data available for a particular class of RPV materials. Here we consider classes defined by flux type (for welds) and by ASTM material specification (for plates and forgings).
2. The fracture toughness values are normalized to a 2.1-in. thickness using the following weakest-link relationship included in ASTM E1921-97:

$$K_{Jc(2.1T)} = K_{\min} + \left(K_{Jc(measured)} - K_{\min}\right)\left(\frac{B}{2.1}\right)^{1/4} \qquad (9)$$

   A "size" of 2.1-in. is selected to maintain consistency with the average size associated with the original K_{IC} database used to establish the relationship between K_{IC} data and RT_{NDT} for the current ASME K_{IC} curve [Marston 87].
3. These size-normalized fracture toughness values are plotted *vs.* test temperature. A K_{IC} curve, i.e.

$$K_{IC} = 33.2 + 2.81 \cdot \exp\left[0.0198 \cdot \left(T - RT_{To(generic)} + 100\right)\right], \quad (K \text{ in ksi}\sqrt{\text{in}},\ T \text{ in } ^\circ\text{F}) \qquad (10)$$

is then plotted, and the value of $RT_{To(generic)}$ is adjusted position the curve so that it bounds 97.5% of the fracture toughness values in fracture mode transition. While in principal any tolerance bound can be selected, we selected a 97.5% value to maintain consistency with how a RT_{To} positioned K_{IC} curve bounds the original K_{IC} data set [Wallin 97].

Fig. 6 illustrates this procedure for A533B Cl. 1 plate and for Linde 80 welds, while Table 2 summarizes $RT_{To(generic)}$ values for the different RPV material classes. This procedure to establish generic values of RT_{To} incorporates the material uncertainty within the class into the value of $RT_{To(generic)}$ by basing the position of the 97.5% tolerance bound curve on fracture toughness data for a number of different heats from the same material class. Consequently, if these values of $RT_{To(generic)}$ are used in a plant assessment, a non-zero uncertainty term (equivaluent to σ_I in the current methodology) should ***not*** be used.

Table 2. Generic RT_{To} values for different classes of nuclear RPV materials

Material Class	$RT_{To(Generic)}$ [°F]	Total Number of K_{Jc} Values	Number of K_{Jc} Values not Bounded	% Bounded
A508 Cl. 2	-14	38	0	100.0%
A508 Cl. 3	-42	606	15	97.5%
A302B	14	58	1	98.3%
A302B Mod.	-39	26	0	100.0%
A533B Cl. 1	18	1481	36	97.6%
Linde 0091	2	71	1	98.6%
Linde 0124	-25	178	4	97.8%
Linde 1092	-151	148	3	98.0%
Linde 80	-34	213	5	97.7%

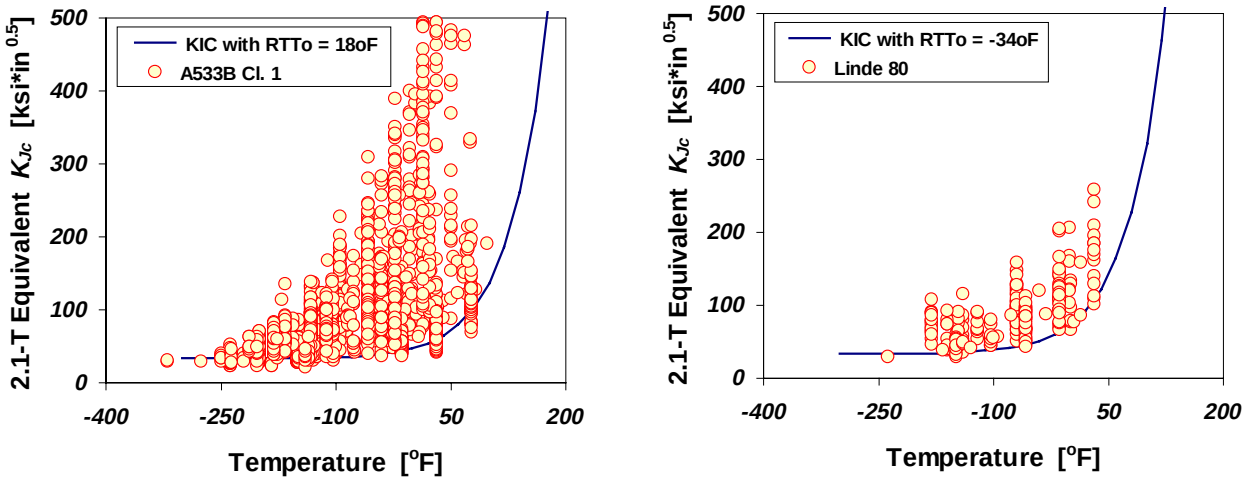

Figure 6. Use of fracture toughness data for A533B Cl. 1 (left) and for Linde 80 (right) to establish generic values of RT_{To}.

Estimate of Irradiation Damage Effects on T_o

As expressed by eq. (5), the current technique for estimating the transition temperature after irradiation is to add an irradiation shift to an un-irradiated transition temperature value. The shift in the CVN transition temperature at 30 ft-lbs is currently calculated from fluence and composition using the following formula [NRC RG199R2]:

$$\Delta T_{30} = (CF)\, f^{(0.28-0.1\log f)} \tag{11}$$

Here the *CF* (chemistry factor) expresses the aggregate effect of Cu, Ni, and product form on irradiation sensitivity. Reg. Guide 1.99 Rev. 2 includes tables of *CF* values for a range of compositions. The form of the fluence function in eq. (11), i.e. $f^{(0.28-0.1\log f)}$, was established by curve-fitting a database of 177 ΔT_{30} values [Randall 87]. In Master Curve-based applications, a question arises regarding the appropriate form of the shift equation for T_o. Since the irradiation shifts in both Charpy and fracture toughness transitions are largely controlled by increases of material flow strength produced by irradiation, it seems reasonable that the fluence function for shifts of Charpy transition temperature might model shifts in the fracture toughness transition temperature (i.e. T_o) as well. Sokolov and Nanstad compared irradiation shifts of both CVN energy and fracture toughness transition [Sokolov 96]. This comparison (see Fig. 7) showed a 1:1 correlation for welds (42 data points). Conversely, examination of 47 plate materials shows that irradiation shifts the fracture toughness transition temperature 16% more than it does the CVN transition temperature. In both cases the relationship between the two transition temperatures was linear. More recently, Onizawa and Suzuki presented results demonstrating a nearly 1:1 correlation ($\Delta T_o = 1.03 \cdot \Delta T_{30}$) for 4 extensively characterized plates [Onizawa 00]. Also Kirk et al. compared available data on T_o shifts produced by irradiation to the functional form of eq. (11) (see Fig. 8). Figures 7 and 8 both suggest that the Reg. Guide 1.99 (Rev. 2) fluence function provides a reasonable description of the shift in T_o produced by irradiation.

These results are encouraging. However, the high cost of irradiated material testing will likely preclude development of a sufficiently well populated database of T_o shift values to either directly develop a T_o-based irradiation trend curve, or even to test empirically the

appropriateness of eq. (11) for the conditions of interest. Consequently, resolution of this issue could rest with establishing a sound basis for why T_o and CVN shifts should be the same, or at least related. Existence of such a rationale, which is not currently being investigated, would pave the way for establishing the appropriate functional form for T_o shifts based on extensive databases of CVN shifts values that are now available [Eason 98].

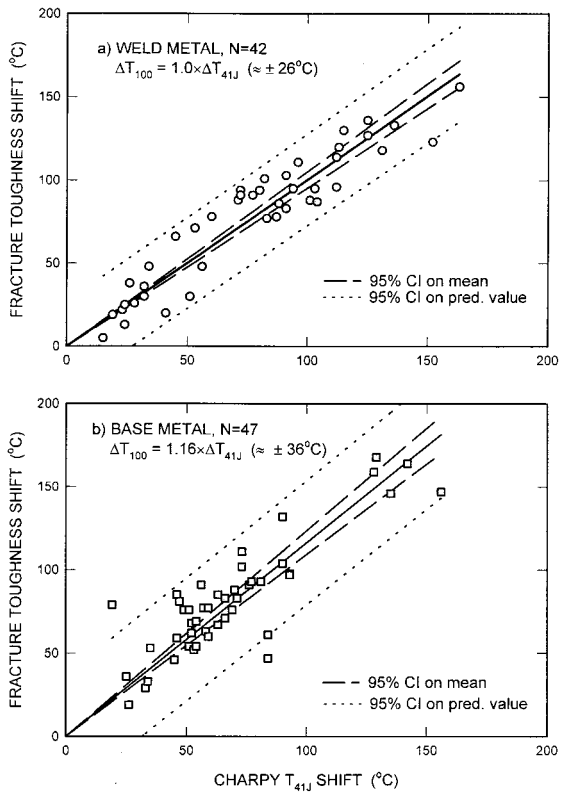

Figure 7. Comparison of irradiation induced CVN and T_o shifts for nuclear RPV welds and base materials [Sokolov 96].

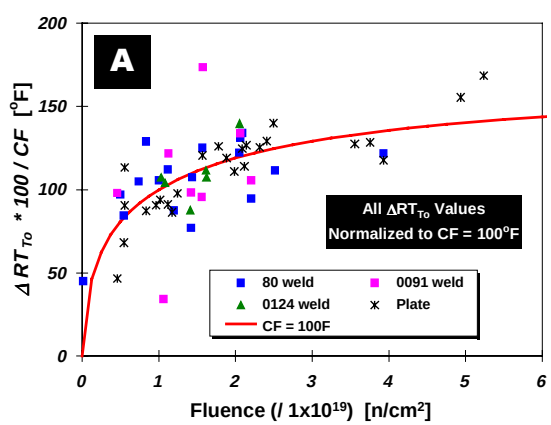

Figure 8. Variation of ΔRT_{To} with fluence, and comparison with Reg. Guide 1.99 Rev. 2 fluence function developed to describe trends in Charpy V-Notch data [Kirk 99].

The Effect of Crack Front Length on T_o

The Master Curve incorporates the following relationship between fracture toughness and the length of the crack front based on a weakest-link model of cleavage fracture under small scale yielding conditions:

$$K_{Jc(Size1)} = K_{\min} + \left(K_{Jc(Size2)} - K_{\min}\right)\left(\frac{B_{Size2}}{B_{Size1}}\right)^{1/4} \qquad (12)$$

Here, K_{min} = 20 MPa√m and represents the value of applied K_I below which cleavage fracture is not possible. The subscripts "Size1" and "Size2" refer to toughness (K) or thickness (B) values for two different specimen thicknesses. Eq. 12 applies to straight-fronted cracks in a state of small scale yielding. It predicts a decline in fracture toughness with increasing crack front length, a prediction in accord with considerable experimental evidence for fracture test specimens [Kirk 98a, Rathbun 00]. Eq. (12) represents a significant departure from current ASME code practice that treats toughness and crack front length as independent variables.

The practice of positioning a bounding curve relative to fracture toughness data addresses the effect of crack front length on toughness. This practice implicitly links to the bounding curve the crack front length(s) characteristic of the fracture toughness data used to establish its position. Thus, both RT_{NDT} and RT_{To} indexed K_{IC} curves have the same implied crack front length because the original K_{IC} data set [Marston 87] provided the basis for positioning both curves. However, once a fracture assessment methodology used the Master Curve directly rather than just using T_o to position a bounding curve, explicit procedures to determine the effect of crack front length on fracture toughness will be needed. Since vessels contain either embedded elliptical flaws or semi-elliptical surface breaking flaws, this methodology will need to treat crack front length effects, and address their interaction with loss of constraint effects, for non-straight fronted cracks. Fig. 9(a) compares cleavage fracture toughness data for semi-elliptical surface cracks in A515 steel with a Master Curve for this material [Joyce 97b, Porr 95]. This comparison illustrates that the relationship between crack front length and fracture toughness that works so well for straight fronted cracks in fracture toughness specimens, eq. (12), places data for part-through surface cracks too high relative to the standard Master Curve. In Fig. 9(b) these data are brought into agreement with the Master Curve by using only 20% of the total crack front length of the past-through surface cracks when calculating their equivalent 1T fracture toughness.

The analysis presented in Fig. 9 is a very rudimentary. It fails to discriminate between the variable-K_I field around the crack front or loss of full constraint where the crack front intersects the free surface as the cause of this change in the scaling relationship. Nevertheless, the analysis does suggest that, whatever the cause, only a small fraction of the crack front length in a non-straight fronted crack contributes significantly to the probability of cleavage fracture. To enable application of the Master Curve to structures, a form of eq. (12) that addresses non-straight fronted fatigue cracks, and can treat both statistical size effects and constraint effects, is needed. Several on-going research programs address this goal using Weibull models coupled with 3D elastic-plastic finite element analysis to predict fracture the conditions for crack initiation from semi-elliptical surface cracks [Gao 99, Bass 00b]. Ultimately, these efforts will need to both provide a predictive model and assess the breadth of material / irradiation / loading conditions to which the model applies.

Non-straight fronted cracks are considered in reactor pressure vessel integrity analysis in the following three areas:

1. Flaw specific-assessments performed according to ASME Section XI (IWB-3500, IWB-3600),
2. PTS analysis as described in 10CFR50.61 and performed in accordance with Regulatory Guide 1.154, and
3. Calculation of permissible limits on heat-up and cool-down performed in accordance with ASME Section XI Appendix G.

In the first two cases, the flaws used in the calculations represent flaws that exist, or could exist in an operating RPV. Thus, a technical resolution of the effect of crack front length on fracture toughness should provide an appropriate analysis methodology for these calculations. Conversely, heat-up and cool-down curves are calculated for a postulated flaw that penetrates one-quarter of the way through the reactor pressure vessel wall and has a 6:1 ratio of surface breaking length to depth. This size of this flaw exceeds considerably that observed in any operating RPV (an 8-in. thick vessel this flaw would have a crack front length of 14-in.), making the flaw size a conservatism implicit to this analysis methodology. Thus, before the Master Curve can be used for Appendix G analyses, a reconciliation of the ¼-T flaw methodology and the Master Curve approach is needed.

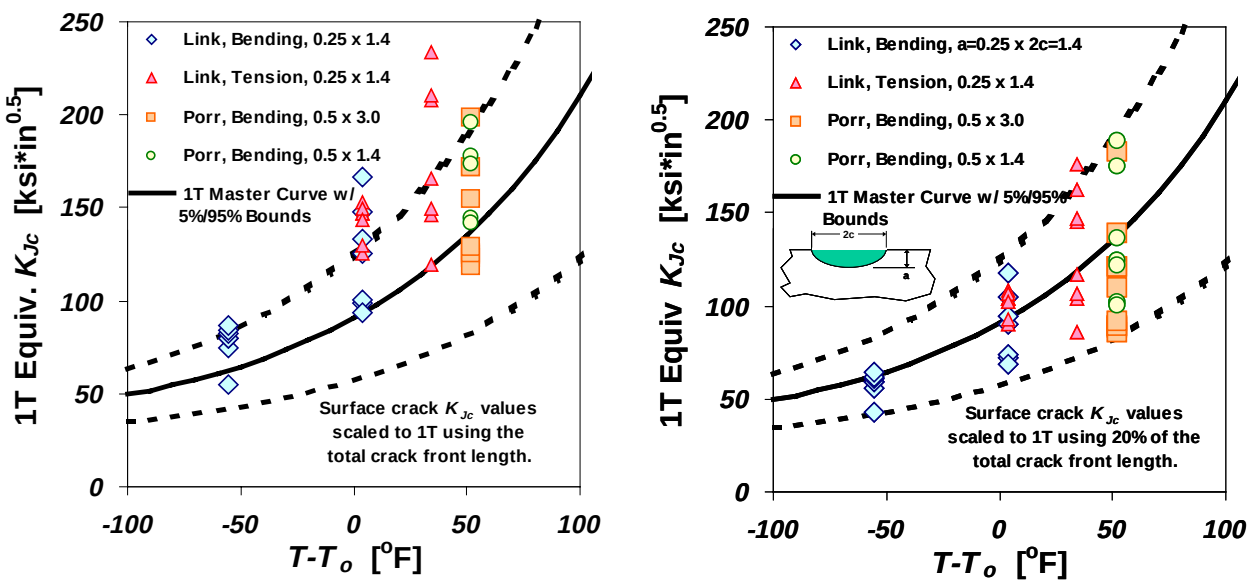

Figure 9. Comparison of data for part through surface cracks to a 1T equivalent Master Curve [Joyce 97b, Porr 95]

<u>The Effect of Loading Rate on T_o</u>

Because the postulated failure of a RPV would likely involve a rapidly propagating crack, the fracture integrity assessment methodology needs to account for the effect of loading rate on fracture toughness. Rate effects enter the methodology via the separation between the static and dynamic fracture toughness curves. This separation is currently fixed irrespective of either the loading rate differential between the two curves, or the strength level / degree of irradiation of the material in question [Yoon 99]. Nevertheless, empirical evidence abounds that both loading rate and material strength influence the fracture toughness transition temperature [Barsom 87].

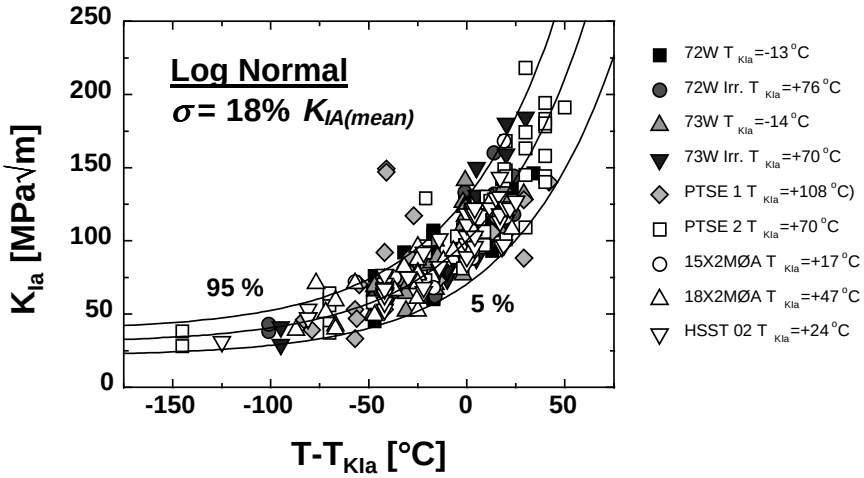

Figure 10. Crack arrest Master Curve proposed by Wallin [Wallin 98b].

Currently the ASME K_{IR} curve represents the lower-bound toughness for both crack initiation at an elevated loading rate, and for crack arrest. In a PTS calculation, a vessel is not considered to have "failed" unless an initiated crack cannot be arrested [Dickson 95]. Absent a change in this definition of vessel failure, treatment of crack arrest will be part of any comprehensive RPV integrity assessment strategy. While crack initiation at elevated loading rates fits well within the Master Curve framework, the same weakest link model used to characterize crack initiation clearly cannot describe crack arrest. Crack arrest will not occur until the local driving force for continued crack propagation falls below the local material arrest toughness over a significant portion of the propagating crack front [Wallin 98b]. The requirements for crack arrest are therefore controlled by a distributed process on the micro-scale, in contrast to crack initiation, which is controlled by local properties. This simple model suggests that the scatter in crack arrest toughness values should be less than for crack initiation toughness values, and that crack arrest toughness should not exhibit a statistical size effect. A recent analysis by Wallin bears out these expectations. In an examination of nine different sets of crack arrest data (seven drawn from HSST/HSSI program records) Wallin demonstrated that crack arrest data are distributed log-normally about a mean curve that has the same temperature dependence as the Master Curve (see Fig. 10).

This similarity between the temperature dependence of initiation and arrest toughness suggests the possibility of describing the position of the arrest toughness curve in terms of a shift from the position of static initiation toughness curve, e.g. as a shift relative to T_o. Wallin examined this possibility using 55 sets of data for ferritic steels that included a variety of product forms, strength grades, and irradiation conditions [Wallin 98b]. Based on a statistical analysis of these data Wallin developed the following shift equation:

$$T_{o(arrest)} - T_{o(static)} = \Delta T_{o(arrest)} = \exp\left\{4.98 - \left(\frac{T_0 + 273}{119.2}\right)^{0.915} + \left(\frac{\sigma_y}{572.4}\right)^{0.868}\right\} \tag{13}$$

where T_o is in °C and σ_y is the static room temperature yield strength in MPa**.

** Wallin has also published a correlation, based on analysis of 59 data sets, that permits estimation of the temperature shift between a static and dynamic crack *initiation* toughness curves [Wallin 97b]:

ASME Code Cases N-629 and N-631 propose using RT_{To} as an index temperature for both the K_{IC} and K_{IR} curves, thereby maintaining the traditional fixed separation between these curves. Fig. 11 demonstrates that this procedure will produce a bounding estimate of crack arrest toughness provided the separation between the median curves for static initiation and crack arrest toughness falls below 95° F. In Fig. 12 we use eq. (13) determine the conditions for which separations of less than 95° F occur. This comparison is made over the range of T_o values observed for irradiated and un-irradiated RPV steels using mean yield strength values for these conditions (un-irradiated = 69 ksi, irradiated = 90 ksi) taken from the database (Appendix A). While only cursory in nature, this analysis suggests that the Code Case N-629 proposal provides a bounding curve for plants approaching their end of license (i.e. T_o > 140° F). Thus, the Code Case proposal appears to provide an adequate approach for assessment of EOL conditions (and thereby PTS).

While the correlations presented in this section provide a useful summary of the trends exhibited by available data, they cannot replace a more fundamental, physically based, understanding of why such trends should occur. The absence of such an understanding raises questions regarding the limits of applicability of these relationships, thereby impeding progress in the application of Master Curve concepts in nuclear RPV integrity assessment.

SUMMARY AND CONCLUSIONS

The information provided in this paper demonstrates that substantial progress has been made recently concerning the adoption of a Master Curve testing standard, and the use of the T_o-index temperature measured by this standard to position bounding fracture toughness curves for use in vessel integrity calculations. Questions raised previously by the Staff regarding the use of Master Curve technology in these codes and standards are now largely resolved. The main difficulty faced when using the Master Curve to assess RPV integrity is now the lack of an accepted framework by which to estimate the irradiated fracture toughness of the vessel from T_o data, and the fact that this framework was never used to determine a PTS screening criteria for T_o. Consequently, there is currently no T_o-based PTS screening criteria, and there is no T_o-based margin term to account for uncertainty in the input variables. Ultimately these deficiencies fuel a concern that forcing Master Curve-technology into the current, non-Master Curve, framework may produce systemic “lack of fit” uncertainties, thereby resulting in the need for higher margins. The only way to alleviate this concern is to establish a Master Curve framework to estimate toughness at EOL, and use this framework as part of PFM calculations to establish a PTS screening criteria applicable specifically to Master Curve-based estimates of fracture toughness. Work on the development of such a framework for the Master Curve has only recently begun. In this paper we reviewed recent progress in the developing some of the components of such a framework, including the following:

$$\Delta T_{o(dynamic)} = \frac{T_{o(static)} \cdot \ln(\dot{K}_I)}{\Gamma - \ln(\dot{K}_I)}, \quad \Gamma = 9.9 \cdot \exp\left\{\left(\frac{T_{o(static)} + 273}{190}\right)^{1.66} + \left(\frac{\sigma_y}{722}\right)^{1.09}\right\}$$

Differences in strain rate between crack initiation and crack arrest suggest that $\Delta T_{o(arrest)}$ will always exceed $\Delta T_{o(dynamic)}$. Furthermore, PTS events do not usually produce rapid mechanical loading rates. Consequently, we focus exclusively on crack arrest in this discussion.

1. Generic values of RT_{To} are provided for use when plant- or material-specific values of RT_{To} are not available.
2. Data is provided that demonstrates a 1:1 correlation between the irradiation shift of the Charpy-V and T_o transition temperatures. This information suggests the possibility of applying embrittlement trend curves developed from CVN data to estimate the effect of irradiation on T_o.
3. Available data suggests that weakest link scaling models developed for straight fronted cracks in test specimens systematically under-predict the fracture resistance of the semi-elliptical and buried cracks found in reactor pressure vessel service.
4. ASME Code Case N-629 uses RT_{To} to position both the K_{IC} and K_{IR} curves with a fixed temperature separation between them. Information presented in this paper demonstrates that this fixed separation under-estimates the crack arrest toughness of RPV steels in some circumstances, and over estimates it in others. This finding suggests that a revision of the Code Case is needed to ensure that the K_{IR} curve provides an appropriate degree of bounding to crack arrest data for all material conditions of interest.

These findings provide cause for optimism that the issues surrounding application of Master Curve-based methodologies to the assessment of nuclear reactor vessel safety can be favorably resolved providing focused efforts continue in a number of key areas.

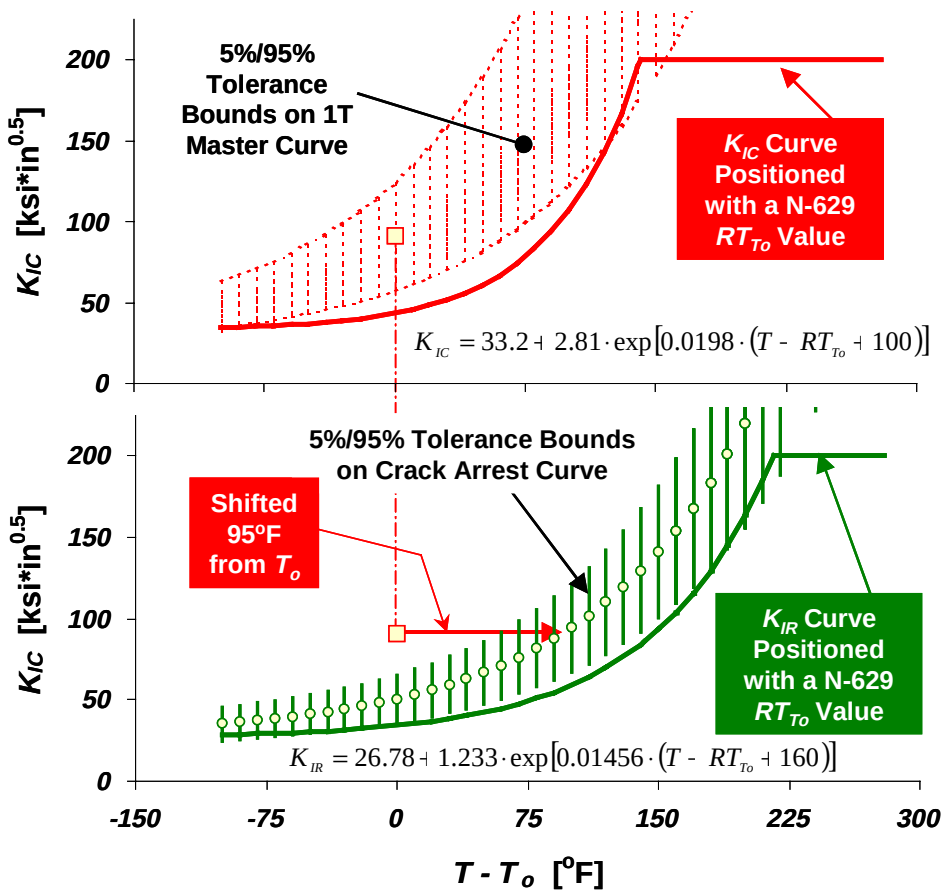

Figure 11. Illustration of the largest shift between a static initiation toughness curve (Master Curve) and a crack arrest toughness curve that will be bounded by a K_{IR} curve located using RT_{To}.

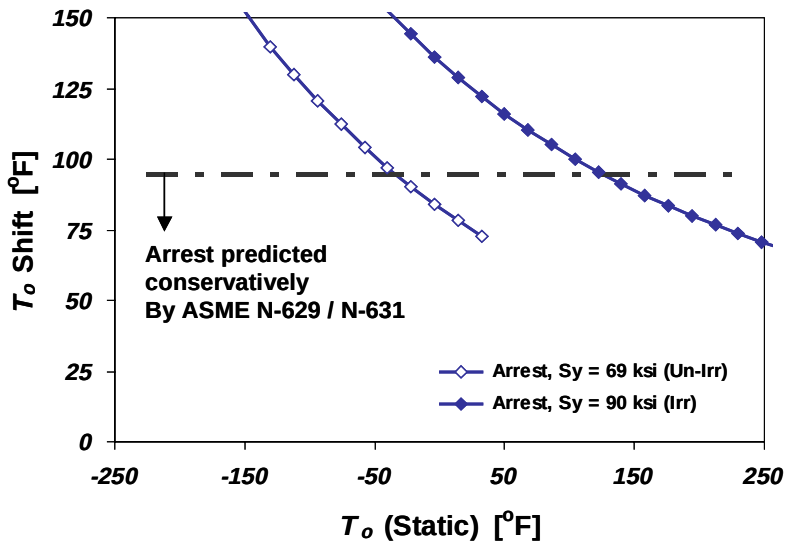

Figure 12. The shift in transition temperature between a static initiation toughness curve (Master Curve) and a crack arrest toughness curve [Wallin 98b].

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