SCK • CEN

STUDIECENTRUM VOOR KERNENERGIE
CENTRE D'ÉTUDE DE L'ÉNERGIE NUCLÉAIRE

NUCLEAR REACTOR PRESSURE VESSEL INTEGRITY INSURANCE BY CRACK ARRESTABILITY EVALUATION USING LOADS FROM INSTRUMENTED CVN TESTS.

A. Fabry

SCK•CEN, Boeretang 200,
B-2400 Mol, Belgium

October 15, 1997

BLG-750

29 - 24

NUCLEAR REACTOR PRESSURE VESSEL INTEGRITY INSURANCE BY CRACK ARRESTABILITY EVALUATION USING LOADS FROM INSTRUMENTED CVN TESTS.

A. Fabry

SCK•CEN, Boeretang 200,
B-2400 Mol, Belgium

October 15, 1997

BLG-750

NUCLEAR REACTOR PRESSURE VESSEL INTEGRITY INSURANCE BY CRACK ARRESTABILITY EVALUATION USING LOADS FROM INSTRUMENTED CVN TESTS.

Albert Fabry

SYNOPSIS

This paper highlights some R&D accomplishments related to a series of contractual reports to Tractebel Energy Engineering, prepared in support of plant life management (PLIM) activities for the Doel-I and Doel-II nuclear power facilities.

It is contended that the early concepts of fracture analysis diagram and of crack arrest temperature, independently pioneered by Pellini and by Robertson in the fifties, can advantageously be applied to insure *appropriately conservative* safety margins for *ageing* reactor pressure vessels- this while keeping abreast with state-of-the-art fracture mechanics technology. This can be done by advanced analysis of the crack arrestability features embedded in the response of the Charpy V-notch impact test routinely used for commercial vessel surveillance.

TABLE OF CONTENTS.

NUCLEAR REACTOR PRESSURE VESSEL INTEGRITY INSURANCE BY CRACK ARRESTABILITY EVALUATION USING LOADS FROM INSTRUMENTED CVN TESTS.

Figure Captions.

NUCLEAR REACTOR PRESSURE VESSEL INTEGRITY INSURANCE BY CRACK ARRESTABILITY EVALUATION USING LOADS FROM INSTRUMENTED CVN TESTS.

Albert Fabry
SCK-CEN, Boeretang 200, B-2400 Mol, Belgium

October 15, 1997

ABSTRACT: *This work, undertaken in the context of nuclear reactor pressure vessel (RPV) surveillance, aims at re-visiting the crack arrest approach to structural integrity insurance. This approach, mandatorily used under normal plant operation conditions, can also offer an attractive alternative to the crack initiation philosophy promoted for accident analysis. To this end, an appropriately conservative, cost effective and robust methodology is forwarded and demonstrated; it makes use of the crack arrest information contained in the instrumented Charpy V-notch impact test and/or in the shear fracture appearance of broken samples. Particular attention is paid at the appraisal of uncertainties and the related safety margins. The resulting capability is placed in perspective with the state-of-the-art crack initiation methodology based on the slow bend testing of precracked specimens, presently under standardization world-wide. The investigation leads to highlight three conceptual weaknesses of current engineering and regulatory practices. Improved crack arrestability evaluation emerges as an optimal approach to insure safe PWR operation up to design end-of-life and beyond.*

KEYWORDS: Reactor pressure vessel steel, safety, regulation, surveillance, fracture toughness, crack arrest.

FOREWORD.

The strategical importance of permanently insuring the integrity of nuclear reactor pressure vessels (RPVs) has always been acknowledged. The emphasis has increased as reactors age, because the interest has raised into the feasibility of safe and economically balanced plant operation beyond design life, even if at the cost of license renewal in some countries, of vessel anneal in others,... The central concern is prevention of the propagation of cleavage cracks through the vessel, possibly leading to its rupture; this could be triggered by a host of conceivable accidental pressure-temperature transients, if the steel fracture toughness was allowed to degrade exaggerately. Another, less publicized concern entails the cycle operation cost, whose increase upon service depends also on vessel embrittlement margins through start-up and shutdown pressure-temperature limitations.

Pressure vessel surveillance aims at anticipating the behavior of the relevant materials, by exposing them in capsules near the vessel inner wall. Due, in particular, to space limitations within these capsules, the specimens cannot simulate properly the stress-strain field conditions of the vessel. Correlation methods are thus used to convert their response into vessel toughness. Intrinsically, such correlations entail significant uncertainties which must be conservatively accounted for by appropriate margins. Fracture toughness can refer to the material resistance to crack initiation -K_{Ic}, K_{Id}-, or to crack propagation/arrest -K_{Ia}-, to static or to dynamic loading. Charpy V-notch impact (CVN) surveillance specimens are sensitive to a mix of initiation and propagation effects, complicated by shear lip formation and by a three-dimensional evolution of the stress-strain field as deformation proceeds. For fracture mechanics RPV evaluation, the *static* toughness K_{Ic} and the *dynamic* toughness K_{Ia} are empirically linked to this intricate, *dynamic* CVN response, without any attempt to separate its components nor to address the service-induced modification of strain rate effects. Yet, it has long been known that such improvements are possible if use is made of the load-time signals recorded by strain gages on the Charpy hammer. The implications to RPV safety margins are considerable for a number of PWR plants.

1. OVERVIEW OF BASIC CONCEPTS AND CHALLENGES.

The concept of temperature of Nil-Ductility Transition, abbreviated here as ***Nil-Ductility Temperature (NDT)***, has been forwarded by Pellini in 1953 during the development of the explosion bulge test at the U.S. Naval Research Laboratories. By definition, NDT is the largest temperature at which a crack, initiated by explosion loading of a *large scale, structure-representative* test plate containing a *small* flaw, will propagate under purely elastic conditions (i.e., plate remains flat at fracture). Investigation of *service failures* showed that these could be prevented by requesting to operate the structure at temperatures exceeding NDT by a fixed, minimum amount. This approach has been quantified by the well known "Fracture Analysis Diagram" (FAD) (Pellini, 1963). The diagram also refers to the concept of ***Crack Arrest Temperature (CAT)***, a concept specifically demonstrated by the development and application of the Robertson large-scale crack arrest test (Robertson, 1953). The CAT is the temperature above which unstable propagation of an "infinitely" long crack becomes impossible for a given applied stress; in a fracture mechanics perspective, this can also be seen as the temperature above which the crack arrest toughness exceeds a given magnitude, 100-200 MPa$\sqrt{m}$ for stresses of relevant engineering concern. Above the NDT, the stress associated to the CAT, i.e. the CAT curve, increases sharply with increasing temperature and reaches the yield stress at the so-called ***Fracture-Transition Elastic (FTE) temperature***.

For mild steel structures of thickness about 50 cm, FTE≅ NDT+33°C (At higher stress levels, the structure would fail by general plastic collapse). ***The FTE can thus be seen as a safety criterion where the margins are provided by temperature alone, irrespective of the size of postulated flaws.*** Furthermore, neither CAT nor FTE are affected by test specimen orientation nor by the specimen width along the crack path, but both depend on thickness, and to less an extent on the yield stress (and the applied stress in the CAT case). This is quantified by the full curves of Figure 1, constructed using a recent literature correlation based on the compilation of an extensive experimental data base (Wiesner, 1996) [for comparison, the dashed lines refer to the CAT and to the small flaw configurations as evaluated in the original Pellini FAD diagram (Pellini, 1963)].

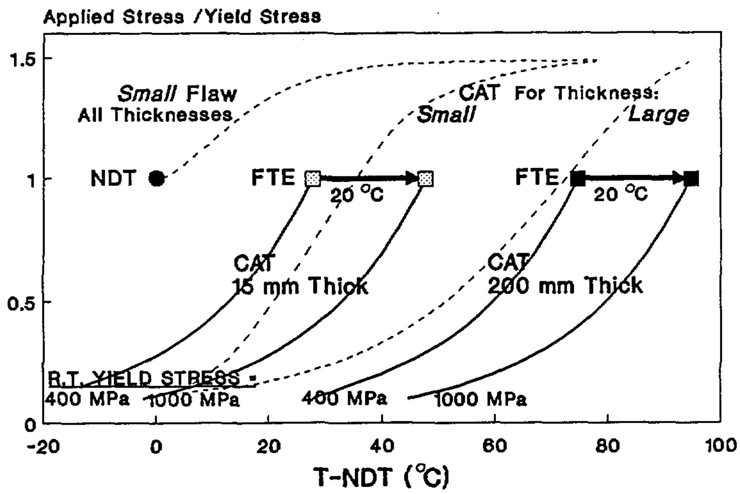


Figure 1. Influence of Stress and Thickness on Crack Arrest.

It is seen that the difference FTE-NDT increases by only 20°C for an increase of the yield stress from 400 to 1000 MPa - a range essentially encompassing the spectrum of possibilities for PWR vessels- and furthermore, this 20° increase does not depend on thickness. For a 200 mm thick PWR vessel, on average: FTE= NDT+85°C, with a maximum variation of ± 10°C for the considered yield strength range. This decreases to NDT+ 80°C (± 10°C) for a thinner vessel of 150 mm, and the thickness effect is thus small as well as easily accountable for. This effect is not linear at smaller thickness: for a hypothetized 10mm thick specimen, one would have: FTE= NDT+29°C, calling for a penalty of +56°C if transposing to the 200 mm thick vessel.

The ***Drop Weight ("Pellini") Test*** was developed (Puzak, 1952) and eventually standardized (ASTM E208, 1963) in order to allow NDT determinations with relatively small-scale specimens. For this three-point bending impact test, the specimen is a rectangular coupon (of minimum thickness 16 mm x minimum width 51mm x 127 mm); a brittle weld bead, containing a notch, and deposited at the center of one face, serves as crack starter; the amount of deflection is controlled by a stop so as not to exceed a few mm.; NDT is the maximum temperature at which the specimen breaks- an event defined to occur whenever the brittle crack extends to just reach the edges of one (or both) of the tensile surfaces on either side of the weld bead. Basically, the drop weight test measures the resistance against continued crack propagation, which is equivalent to crack arrestability. Thus, NDT has initially been used to index the arrest fracture toughness K_{Ia} of ferritic steels: more specifically, the lower bound K_{Ia} curve has been considered to be an unique function of the "reduced" temperature T-NDT. This is an approximation: fracture mechanics interpretations of the drop weight test suggest that NDT better correlates to K_{Ia} *normalized* by the dynamic yield stress (e.g. Irwin, 1967, Shoemaker, 1971, Sumpter, 1986).

Subsequently, significant **conceptual mismatch** has been introduced by the fact that the ASME Code indexes the fracture toughness curves (K_{Ia} and K_{Id}, but also static K_{Ic}) to T-RT_{NDT}; here, the ***Reference Temperature for Nil Ductility Transition RT_{NDT}*** is defined as the lowest of: 1) NDT, and of: 2) T_{CV}-33°C, where T_{CV} is the temperature at which each of three CVN impact specimens exhibits at least 68J absorbed energy and 0.89mm lateral expansion. This modification was done with the *intent* to expediently provide some insurance against possible tearing instability for materials displaying low CVN upper shelf energies. The mismatch is double:

1) One ignores recent, albeit well established technical and regulatory progress (see for instance USNRC Regulatory Guide 1.161, June 1995): rather than adding some margin to brittle initiation and arrest curves, any potential tearing instability concern can and should be separately evaluated on basis of the ductile initiation fracture toughness and of J-resistance curves;

2) Static toughness is indexed to dynamic quantities; this neglects the known fact that the strain rate effect on toughness depends on the yield stress.

What actually seems to transpire here is some amount of confusion between the ***crack initiation and crack propagation approaches to safety***. Indeed, for normal operation of a PWR reactor pressure vessel, the ASME Code Section III Appendix G procedure defines pressure-temperature limitations with reference to the K_{IR} curve, which is essentially equivalent to the lower bound K_{Ia} curve; the philosophy for brittle fracture prevention is crack arrest and the onset of upper shelf temperature is defined in relation to the K_{Ia} curve. If the CVN upper shelf energy falls below the 68J. regulatory limit, a tearing instability analysis is nowadays required and therefore, the rationale having led to replace NDT by RT_{NDT} becomes obsolete. On another hand, under accident conditions, the current nuclear philosophy for brittle fracture prevention as implemented for instance by the July 1991 U.S. Code of Federal

Regulation, Title 10, Part 50, Article 61 - the pressurized thermal shock (PTS) rule - is primarily crack initiation; however, indexing the K_{Ic} curve to RT_{NDT} , as done in this context, is technically incorrect. It is generally believed that the crack arrest approach is more conservative than the initiation one. By contrast, it has been found (Fabry, 1997.a) that, for irradiated reactor pressure vessels, the K_{Ic} curve tends towards the K_{Ia} curve as a result of service-induced steel strengthening: the two philosophies thus tend to become equivalent. This important finding, substantiated further in § 5, is consistent with earlier studies entailing K_{Id} versus K_{Ic} patterns for unirradiated steels spanning a large range of strengths (Barsom, 1970). The existence of this effect- not acknowledged by current nuclear regulation- actually means that the arrest fracture prevention approach is ultimately not penalizing to the nuclear industry, yet provides the most secure safety margins, given that crack initiation can never be guaranteed in an absolute sense (e.g., local brittle zones, undetected weld flaws, residual stresses, ...).

Aside of the above NDT/RT_{NDT} mismatch in assessing beginning-of-life (baseline) vessel condition, another, most significant source of uncertainty and of unwarranted conservatism stems from the current practice to assume that service-induced shifts of fracture toughness curves to higher temperatures are uniquely proportional to the upward shift of the temperature at which the CVN impact energy reaches an arbitrary level, chosen for instance as 41 J. in the USNRC regulation. The eventual biases stem from the fact that the CVN impact energies, not only are of a dynamic nature unsuitable to static toughness indexing (see above), but still more importantly, the biases are inherent to the fact that these energies are a mix of crack front formation and initiation energy, crack propagation energy, plastic deformation energy and shear lip formation energy. The relevance of ***CVN energy partitioning*** has been qualitatively grasped ever since unexpected structural engineering failures have been clarified by the discovery of the ductile-brittle transition temperature (DBTT) phenomenon in the fourties.

Not until the early sixties however could an experimental separation of these energy contributions be attempted: this was then made possible by the successful development of strain gage instrumented Charpy tups, which allow the on-line recording of load-time (deflection) signals during the entire impact event. In particular, the load at arrest of a brittle crack can be detected in function of the test temperature, and the post-arrest energy (associated to shear lip formation) can be derived as well. This is used to define a characteristic temperature at which a reference level of the considered parameter is reached and to look for correlations of this temperature with either CAT (Fearnehough, 1973, Hagedorn, 1983) or NDT (Berger, 1979, Ahlf, 1986, Schmitt, 1990, Pachur, 1994). Good to excellent correlations have been reported; most importantly, while arrest-related parameters were always found, as expected, to increase *linearly* with CAT or NDT, this was not always the case for the corresponding 41J. temperatures: larger increases are observed in a number of cases; the reasons for such "outlier" behavior have been quantified in detail (Fabry, 1996.a). More recently, excellent correlations between the CVN arrest load and the crack arrest fracture toughness K_{Ia} have also been documented (Wallin, 1995, Nanstad, 1996, Iskander, 1997). The evidence supporting the feasibility to use CVN arrest loads in the frame of the crack arrest fracture prevention philosophy is altogether compelling, but the many disparate details/ resp. the sometimes apparent divergences of published correlations need to be unified/ resp. clarified. This can be and is done herein by: 1) Addressing the relevant physical and statistical aspects of the CVN test; 2) Considering the relationship between CVN load signal and shear fracture appearance (SFA), along a previously outlined formulation (Fabry, 1996.b). This second line of inquiry was encouraged by extensive literature evidence regarding SFA correlations to crack arrest [see for example the references mentionned at the beginning of § 3].

In summary, the outlined CAT/FTE approach is attractive in the nuclear reactor pressure vessel safety context for the following, inter-related reasons:

1) The approach allows to establish the most secure, yet not necessarily penalizing safety margins for vessel operation under the life management (or life extension) conditions prevailing for all plants once in full-fledged service regime; furthermore, the approach is often less unduly conservative than the crack initiation approach as applied to-day for accident analysis

2) The correlation CAT/FTE to NDT is sufficiently well established (Wiesner, 1996) and is sufficiently insensitive to postulated flaw size, to stress, to constraint, to service strengthening effects,... so as to make one able to *guarantee confidence* in *predicting crack arrest* behavior of actual reactor pressure vessels *from the knowledge of NDT only*

3) Furthermore, as elaborated hereafter, NDT can be reliably correlated to the shear fracture appearance and to the crack arrest load of the instrumented CVN impact test, as routinely used in surveillance programs; this confers to the approach the *needed practicability for straightforward use by the engineering community*- while not requiring any extra testing

4) Finally, as illustrated in § 4.1, the safety margins predicted by this approach are consistent with the ones resulting from the application of state-of-the-art fracture mechanics.

An important comment is called for by the fourth item. That fracture mechanics applies to crack arrest at the NDT is clear, but that it may apply also to crack arrest at the FTE can another hand be questionned on fundamental grounds; in particular, this should not be so if the FTE actually represents the temperature at which arrest stems from complete redistribution of the applied stress among the unbroken, plastically reinforced ligaments left in the wake of the fast running crack (Cottrell, 1995). In any case, the CAT/FTE/NDT correlation approach itself would of course remain applicable; FTE, primarily a temperature-based, rather than a stress-based criterion, is probably also the most restrictive, safety-wise (Smith, 1997).

2. FORMULATION OF "MASTER CURVE" FOR CVN ARREST LOAD CORRELATIONS.

2.1 Uncertainties of Drop Weight Test.

In order to judge the performance of the semi-empirical formulation proposed below, it is necessary to first develop some estimate of the accuracy with which the quantity to be predicted is itself usually defined. In this section, this quantity is chozen as the drop weight nil-ductility temperature, but it must be kept in mind that the ultimate objective entails the previously outlined CAT/FTE/NDT approach. Thus, at this stage, the issue is to decide whether or not *unbiased* NDT estimates can be obtained from CVN arrest loads within confidence intervals comparable to the ones of the "Pellini" test. Only subsequently will one address the final stage of accuracy for structural integrity application (§ 4.1).

The drop weight testing conditions may affect NDT determinations for nuclear pressure vessel steels, leading in some cases to unconservative evaluations (Holt, 1986). Of primary importance are the crack starter bead welding and specimen cooling rate conditions; in particular, two-pass bead application (ASTM Method E208-81) tends to cause high toughness of the heat-affected zone (HAZ), as well as some quenching and tempering of the specimen; excessive HAZ hardness acts as a barrier against crack propagation, i.e. lowers the observed NDT with respect to its unperturbed value. Additional uncertainties are associated to the material variability as well as to the testing temperature steps (usually 5 or 10 °C). It is difficult to assess the exact magnitude of all these uncertainties when establishing CVN to NDT correlations, because these depend on the considered experiment and steel. Many of the input data for the present study have been collected from literature. An across-the-board estimate of the overall NDT uncertainty has been attempted by examining a large set of data (71 tests) compiled by EPRI for unirradiated plate HSST-02 (Server, 1978). This is shown by Figure 2. The median NDT value is -28 °C, with a 2σ confidence interval of ± 15 °C. Two separate determinations, one at ORNL (Childress, 1971) and one by the Belgian program, give respectively -18 and -11 °C. The value stemming from the correlation presented below is equal to -11 °C. The correlation is actually consistent with a *2σ **NDT uncertainty of** ± 15 °C*, and this estimate is accepted. It is nevertheless recognized that state-of-the-art drop weight tests are probably more accurate, with a 2σ confidence interval somewhere in the range ± 8-15 °C.

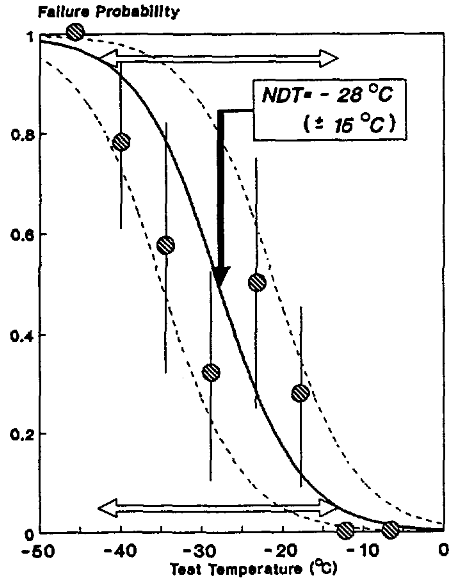


Figure 2. Uncertainties of Drop Weight Test.

2.2 Uncertainties of CVN Arrest Load Determinations.

The statistical distribution of the CVN arrest load F_a has been found to be representable by a three-parameter Weibull function with temperature-dependent threshold f_a (T) and shape parameter m=2. The scale parameter, or median, is denoted $\overline{F}_a(T)$. Considering a given temperature T, i.e. dropping the T dependency, one can write the cumulative distribution function as

$$\psi(F_a) = 1 - \exp(-\left[(F_a - f_a)/(\overline{F}_a - f_a)\right]^m) \qquad (1)$$

The mean and standard deviations are respectively equal to

$$\langle F_a \rangle = \left[0.886(\overline{F}_a - f_a)\right] + f_a \qquad (2)$$

$$\sigma = 0.463\overline{F}_a \qquad (3)$$

Figure 3.a compares this distribution to the best two-parameter Weibull fit (m= 4). The 61 tests used for this evaluation are relative to the low upper shelf Linde-80 weld selected for the ASTM round robin on CVN reconstitution (Onizawa, 1997); all specimens have been broken at ORNL on a same machine; the exercise entailed the comparison of ASTM and DIN tups, and statistical evaluation showed that no tup influence could be detected for the arrest load. The existence of a

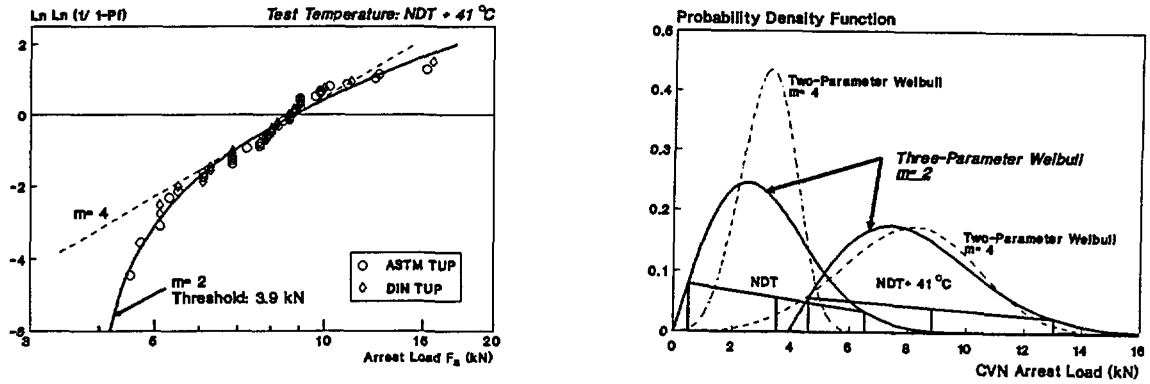


Figure 3. Statistical Distribution of CVN Arrest Load

a) ASTM Round Robin (Linde 80 Weld)

b) Three-Parameter Weibull with Temperature-Dependent Threshold vs. Two-Parameter Weibull

temperature-dependent arrest load threshold is to be expected on straigthforward physical grounds: indeed, there is always a temperature above which brittle crack propagation cannot take place, irrespective of the applied stress (upper bound of upper shelf onset temperature) while on another hand, at decreasing temperature, the arrest load tends towards zero. Actually, and as independently confirmed in a subsequent section, it is reasonable to define NDT as the temperature below which the arrest load **threshold** is zero and above which it departs from zero. This is not without reminding one procedure adopted for direct correlation of CVN arrest loads to the thickness-corrected CAT (Hagedorn, 1983); yet the referred correlation - CAT= temperature where F_a drops to zero- seems to conflict with a previous study suggesting NDT to be equal to the temperature at which the average CVN arrest load is 5 kN (Berger, 1979). It could be concluded, and has sometimes been considered (Ahlf, 1986), that the F_a level corresponding to NDT depends on the steel. These apparent disparities are resolved by the present results.

Indeed, a reliable definition of the statistical distribution of F_a in function of temperature is essential before any generalized correlation be seriously attempted. As Figure 3.b illustrates, the present Weibull distribution is significantly wider at NDT than for instance the m=4 two-parameter Weibull distribution; this in turn affects the definition of the best median $\overline{F}_a(T)$ relationship and therefore of the median $\overline{F}_a$ level for correlation to NDT. Many experiments that would appear inconsistent with one another for the [m=4, f=0] distribution are recounciled with the [m=2, f(T)] statistics.

Two other important considerations entail ***precision*** and ***validity limits.*** This can be briefly discussed at the light of Figure 4, where arrest loads for unirradiated plate HSST-03 have been analyzed using the "master curve" formulation further described below.

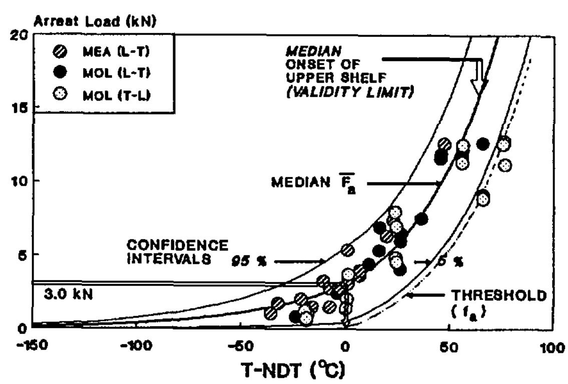


Figure 4. "Master Curve" Formulation for CVN Crack Arrest Load

Incidentally, it can be seen that no orientation effect (L-T versus T-L) is detectable, and that tests by two independent laboratories are in good agreement; these two observations are general and have been confirmed by examination of a large data bank relative to many pressure vessel steels tested at a significant number of laboratories. Figure 4 indicates that the optimal testing temperature range for sufficiently precise application of the approach extends approximately from NDT-15°C to NDT+60°C. At lower temperatures, the master curve is too flat and, as already suggested by Figure 3, its confidence interval is too large. But at higher temperatures, validity questions arise, requiring some amount of data rejection. More exactly, rejection is called for whenever the test temperature exceeds the *median* onset temperature T_O of the CVN upper shelf: this characteristic temperature is defined as the median temperature at which the brittle crack initiation load F_u equals the arrest load F_a and the median shear fracture appearance thus reaches 100%. The procedure to determine T_O is illustrated in a subsequent section. From a "physical" viewpoint, the need for such rejection criterion stems from the fact that at or above T_O, plastic deformation of the specimen becomes excessive: the near- plane strain conditions prevailing at NDT, and which constitute one crucial facet of the rationale under the CVN-drop weight correlation, are violated. Strictly speaking, such "violation" may already occur at T_N, the median temperature above which $F_u < F_m$, where F_m is the maximum load: significant ductile stable crack growth then precedes the brittle initiation. However, extensive evaluation has shown that a T_N rejection criterion would be unnecessarily severe. Along the same lines, it has been found that, in case of multiple brittle initiation and arrest, the first arrest point must be used. All in all, the presently accumulated experience indicates that, in order to achieve an unbiased, confident application of the proposed correlation, a *minimum of four tests within the temperature range NDT-15°C to NDT+60°C are needed.* Such requirement is generally fulfilled by routine surveillance testing, and if not the case, CVN reconstitution (van Walle, 1996) can often provide the necessary remedy.

2.3 "Master Curve" Formulation.

The CVN arrest load "master curve" formulation illustrated by Figure 4 is specified as an increasing exponential function of the "reduced" temperature T-NDT, with a modest slope increase at the NDT. The associated statistical distribution is the three-parameter Weibull function with temperature-dependent threshold.This formulation is expressed by the following series of simple equations:

$$\overline{F}_a(T) = 3.00\exp(\alpha T^*) \qquad T^* = T - NDT \qquad (4)$$

$$\alpha = 0.026 \quad for \quad T^* > 0$$
$$\alpha = 0.020 \quad for \quad T^* < 0$$

(Load in kN, Temperature in °C), with the 2σ confidence interval given by

$$\overline{F}_a \pm \Delta = \left[(\overline{F}_a - f_a)x(1 \pm 0.762)\right] + f_a \qquad (5)$$

where the temperature-dependent threshold is

$$f_a(T^*) = 2.05\left[\exp(0.026T^*) - 1\right] \qquad \text{for } T^*>0 \qquad (6.a)$$
$$= 0 \qquad \text{for } T^*<0 \qquad (6.b)$$

For this F_a master curve formulation, the median nil-ductility temperature NDT corresponds to a median CVN arrest load indexing level of 3.00 kN.

A qualitative physical argument has been previously given for the existence of the temperature-dependent threshold. That this threshold would depart from zero exactly at the NDT and increase according to equation (6) was an initial working assumption, but it has been subsequently confirmed by the evaluation of a substantial arrest load data bank, and by the statistical features of fracture appearance data, as reviewed in a forthcoming section. The selection of the exponential increase of the threshold above NDT, which implicates a corresponding median exponential behavior (but not necessarily of the same slope), was inspired by the ASME representation of the lower bound K_{Ia} curve. Whenever linear elastic fracture mechanics is adequate to describe crack arrest, the assumption of proportionality of K_I to stress, thus of K_{Ia} to arrest load, is indeed reasonable insofar as plane strain conditions are nearly respected: this should be the case for CVN specimens broken at temperatures up to at least the ductility temperature T_N (§ 2.2). A change of the slope α of the exponential behavior was intuitively expected to occur at T_N, a temperature at which a fully formed ductile *crack front* begins to propagate; the change was found instead to take place already at NDT, which may suggest concurrence with the median temperature for onset of *local* ductile initiation.

2.4 "Master Curve " Validation.

The formulation of § 2.3, applied with due attention to the precision requirements and validity limits outlined in § 2.2, does adequately meet its purpose in providing a one-to-one correspondance between the drop weight NDT and the temperature at a median CVN crack arrest load of 3 kN. This is demonstrated by Figure 5 and Table 1. Basically, the "CVN- predicted" NDT values are consistent with the drop weight values to within the 2σ confidence interval of ± 15°C estimated in § 2.1. It is reasonable to conservatively *assign a 2σ confidence interval of ± 22°C to the median NDT values projected through the present "master curve" correlation.*

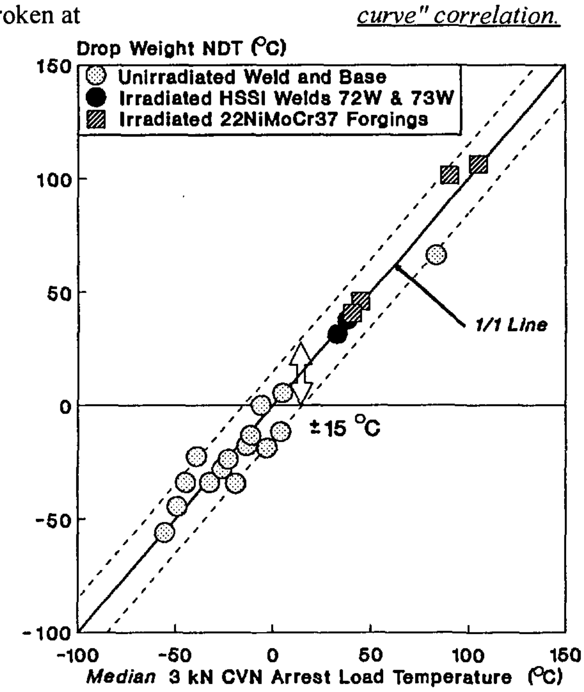


Figure 5. NDT Corresponds to Temperature at Median CVN Arrest Load of 3 kN.

TABLE 1
Comparison between NDT Determinations by Drop Weight Test
and by CVN Arrest Load Correlation

Steel and Condition	*NDT: Nil Ductility Temperature (°C)*	
	Drop Weight	CVN Correlation
Unirradiated (Baseline)		
A302-B Reference Plate	-12 to -23	-14
A533-B Plate HSST-02	-11 to -28	-11
A533-B Plate HSST-03	-12	+4
KS-01 22 NiMoCr 37 Forging	+5	+5
KS-02 22 NiMoCr 37 Forging	0	-6
A508 Cl.2 Forging (PTSE-1)	+66	+83
Doel-I,-II Surveillance Forgings	-24	-24
Doel-IV A508 Cl.3 Surveillance Forging	-35	-19
22 NiMoCr 37 Surveillance Forging	-20	-3
HSSI Weld 72W	-27	-27
HSSI Weld 73W	-34	-34
ASTM Round Robin Linde-80 Weld	-23	-39
Quad Cities-2 Surveillance Weld	-46	-50
Doel-I,-II Surveillance Welds	-30	-30 to -45
22 NiMoCr 37 Surveillance Weld	-35	-45
Midland Vessel Weld	-55	-55
Irradiated		
KS-01 22 NiMoCr 37 Forging		
2 E19 cm^{-2}	+45	+45
7 E19 cm^{-2}	+100	+90
KS-02 22 NiMoCr 37 Forging		
2 E19 cm^{-2}	+40	+40
8 E19 cm^{-2}	+105	+105
HSSI Weld 72W 1.5-2 E19 cm^{-2}	+34	+34
HSSI Weld 73W 1.5-2 E19 cm^{-2}	+38	+38

Figures 6, a to d, offer four selected illustrations relative to unirradiated pressure vessel steels: a) ASTM reference A302-B plate; b) German 22 NiMoCr 37 weld and forging; c) two U.S. Linde-80 welds; d) Belgian A508 Cl. 3 forging.
The suggestion (Ahlf, 1986) that the F_a indexing level may depend on the steel is examined by reevaluation of the considered experiments on Figures 7, a and b: the published index is 3.3 kN for the first case (KS-01 forging) and 2.3 kN for the second case (KS-02 forging); all these data and their scatter are also accounted for by the present "master curve" formulation, which, despite its unique 3.0 kN index, allows to predict NDT to

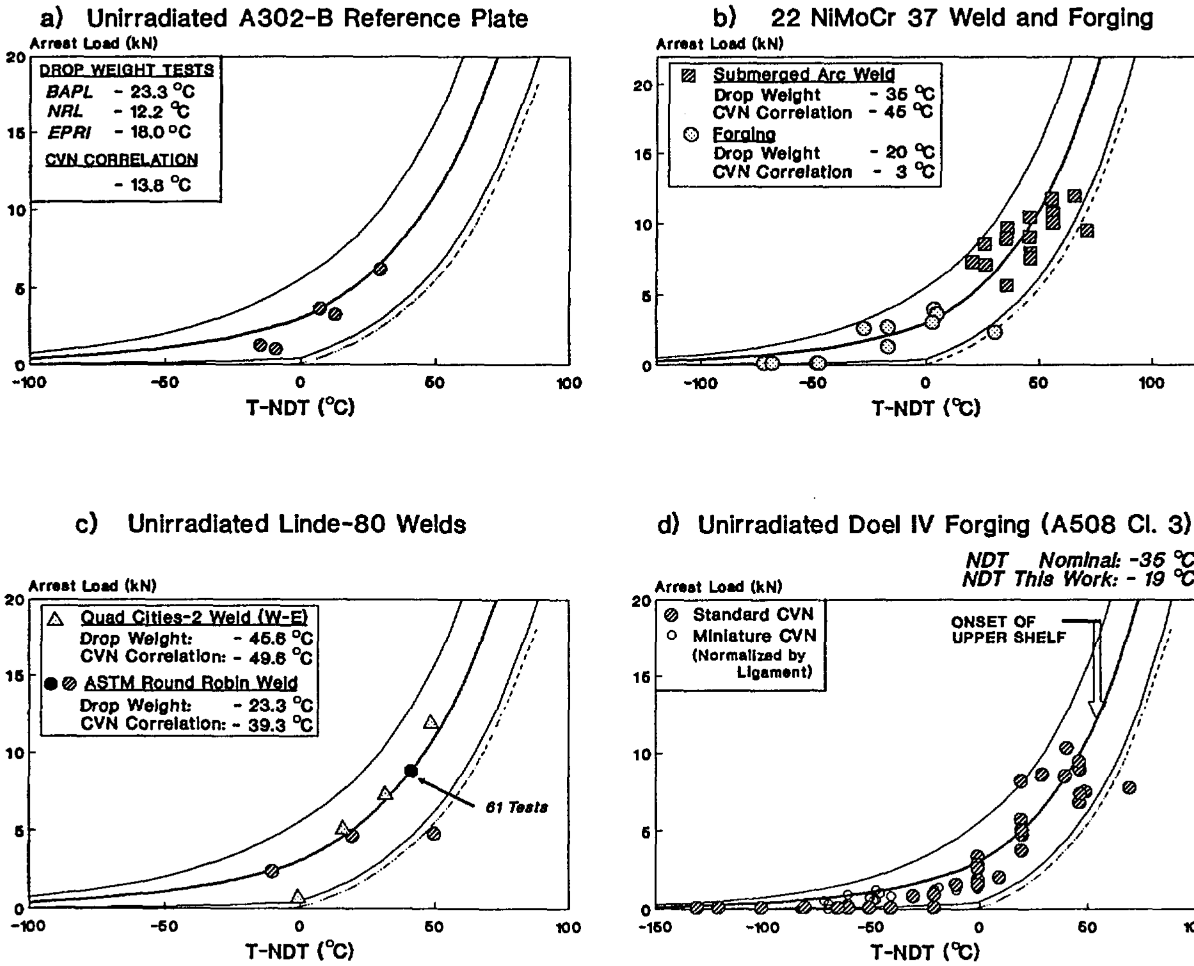


Figure 6. Validation of "Master Curve" for CVN Arrest Load Correlations

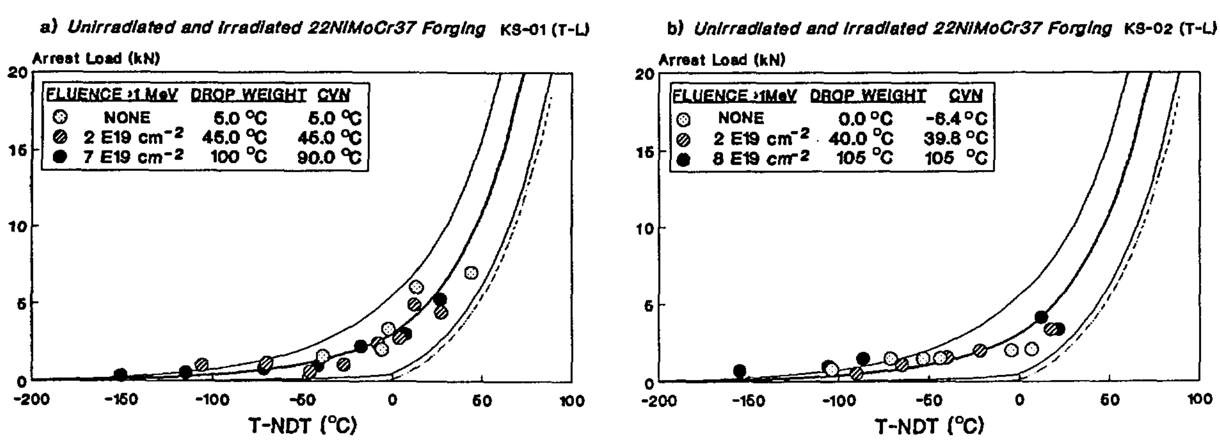


Figure 7. CVN Arrest Load Tracks Well Drop Weight NDT

within better than ±5°C, except in one case (-10°C). Clearly, the differences of interpretation among various authors primarily stem from the formulation (or lack of it) adopted to represent the temperature dependency of the arrest load, and in particular to assess precision, validity limits and confidence intervals. To this respect, it is finally relevant to mention here that the parameters adopted to describe the *shape* of the median and threshold curves in equations (4) and (6) account also for a number of experiments in which no NDT, CAT,....measurements were performed, but which entailed rather extensive CVN arrest load and fracture appearance determinations at numerous temperatures within the transition.

2.5 "Master Curve" Relationship to Crack Arrest Fracture Toughness.

The formulation has been applied to some experiments performed at Oak Ridge National Laboratories (ORNL) and analyzed elsewhere with purposes similar to the present ones (Wallin, 1995, Nanstad, 1996, Iskander, 1997). No major discrepancy should be expected (nor is observed) beween the referred publications, insofar as the input data bases tend to significantly overlap. The major interest however in re-examining these experiments is that, in addition to drop weight tests, they also encompass fracture mechanics K_{Ia} tests, from which one can, in particular, derive the median temperature at a reference level of 100 MPa$\sqrt{m}$, noted T_A herein. Figure 8 gathers the results. On the left part, the excellent performance of the present formulation can once more be appreciated; note that for weld 73W, the ORNL F_a data have been augmented by tests done at Mol using specimens reconstituted from the ORNL broken compact tension remnants; also, the outlier point (ORNL) does not meet the T_O validity criterion. The right part of Figure 8 has been constructed by simply shifting, by 20°C along the abscissa, the F_a median, threshold and confidence interval master curves from the left Figure. The point to emphasize is that this shift is *unique* for the five experiments.

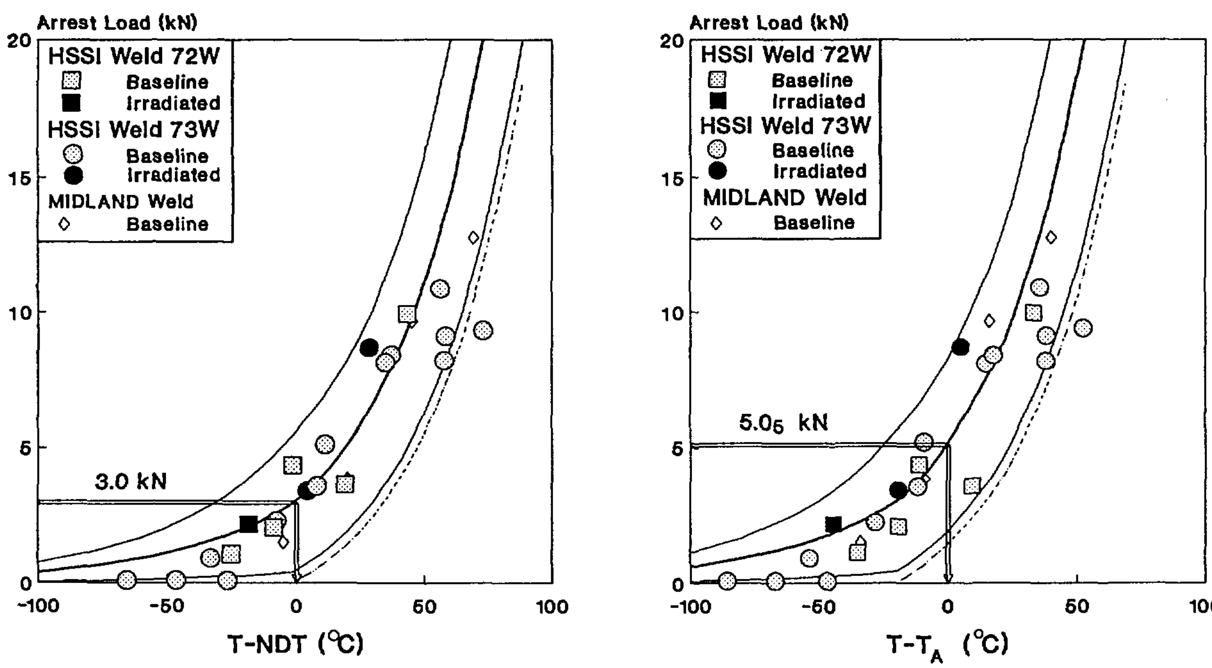


Figure 8. Indexing Crack Arrest Fracture Toughness to Arrest Load of Instrumented CVN Test : T_A (Temperature at Median K_{Ia} of 100mpa$\sqrt{m}$) = "Pellini"NDT+20°C.

For the present F_a master curve formulation, the temperature T_A at which the median fracture toughness K_{Ia} is equal to 100 MPa$\sqrt{m}$ corresponds to a CVN arrest load indexing level of 5 kN (exactly: 5.05 kN); furthermore:

$$T_A = NDT + 20°C \qquad (7)$$

with a 2σ uncertainty of $\pm$ 12°C.

This compares favourably with a recent correlation (Wallin, 1995): TFa4= T_A-10°C (2σ = $\pm$ 27°C) where TFa4 is the temperature at which the median CVN arrest load is 4 kN; indeed, this transforms into T_A= NDT+21°C upon consideration of equation (4) for the master curve. Good concurrence is also found with the work of the two other previously mentionned authors (Nanstad, 1996, Iskander, 1997).

3. LINK OF CVN LOAD-TEMPERATURE DIAGRAM AND FRACTURE APPEARANCE.

3.1 Formulation.

As already mentionned in § 1, brittle crack arrestability has, since the early sixties, been shown by many studies to approximately correlate with CVN shear fracture appearance (SFA): namely, CAT is linearly linked to the temperature at which SFA reaches a given level, such as 25% (e.g., Nichols, 1965, Cowan, 1967, Fearnehough, 1973, Kussmaul, 1977) or 50% (e.g., Hagedorn, 1971, Akiyama, 1985, Smedley, 1989, Wiesner, 1993). These correlations differ somewhat from one another and their scatter is usually large. Nevertheless, it has been proposed, in the context of nuclear power operation, that the ***50% Fracture Appearance Transition Temperature (FATT)*** allows to better quantify service embrittlement than the temperature at which the CVN absorbed energy reaches "conventional" energy levels such as 41 J., etc...: namely, FATT shifts are less biased, less scattered and often less conservative than energy shifts (Fabry, 1996.a and b, McElroy, 1996). This issue is re-visited now by further consideration of the physically-grounded link between SFA and the instrumented CVN load diagram (Fabry,1996.b):

$$SFA\ (\%) = [1-(F_u-F_a)/\{F_m + k(F_m-F_y)\}] \times 100 \qquad (8)$$

where F_y, F_m, F_u and F_a are respectively the general yield load, the maximum load, the brittle initiation load and the arrest load, while the parameter k has been introduced to quantify the occurence of local ductile initiation

k=0	Initiation at maximum load
k=1	Initiation at general yield load
k=0.5	Initiation at load equal to $(F_y+F_m)/2$

Figure 9 illustrates application of this model to the unirradiated A533-B reference plate HSST-02 in

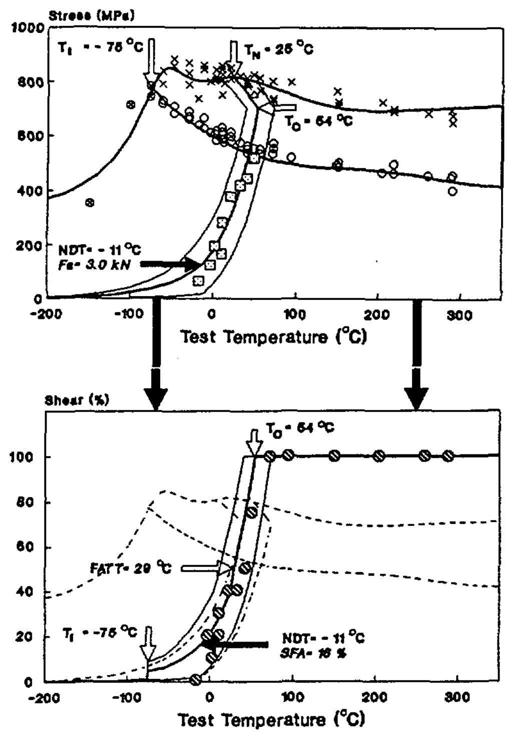


Figure 9. Link of CVN Load-Temperature Diagram and Shear Fracture Appearance.

the L-T orientation. Also indicated are all the characteristic temperatures for the CVN test: T_N, T_O, NDT, FATT, as previously defined, and T_I, the "brittleness" temperature, in general equal to the median temperature at which SFA equals zero. Note that k=0.5 for $T>T_N$ and k=0 for $T<T_N$. It has been easy to establish the SFA confidence intervals from equation (8). Indeed, the scatter associated to F_y and F_m plays a negligible role (the two-parameter Weibull distribution has been found applicable, with a slope parameter m always in excess of 35): this is because these loads reflect the material flow properties and are not affected by the inhomogeneity of the distribution of fracture triggering particles. As the top part of Figure 9 shows, the role of F_u is minor for the considered steel. In general terms, it has been found that **the scatter of the arrest load governs the SFA scatter.** Therefore, the SFA scatter can be modelled from equation (8) if one describes the F_a scatter by equations (5) and (6). The bottom part of Figure 9 has been derived in this way. The validity of this statistical model is confirmed by the extensive SFA data bank available for this plate (Server, 1978, Stallmann, 1987), see Figure 10 (this excludes the data of Figure 9, which are the only ones for which load-time traces could be located). Statistical tests do not reveal any orientation effects here, in line with a previous investigation of plate HSST-03 (Vandermeulen, 1993); this observation is in general true for reactor pressure vessel steels. It is now possible to scrutinize further the fundamental features outlined in § 2.2. The Weibull evaluation of all data on Figure 10 is partially illustrated by Figure 11 and the results are summarized by Figure 12. It is found that the SFA scatter trends change drastically at the NDT, left part of the Figure: the slope parameter m suddenly increases if the two parameter Weibull description is adopted. However, the alternate use of the three parameter Weibull function, with constant slope (m=2), leads to the **departure from zero at NDT of the temperature-dependent threshold**, right part of Figure 12. This second interpretation is recommended: indeed, it hinges upon the qualitative, yet physically- defendable grounds discussed in § 2.2,; this argumentation is clearly vindicated by the present statistical analysis.

The ***recommended SFA representation*** is therefore given by ***equation (8)***, complemented, from the statistical point of view, by the ***three-parameter Weibull distribution with constant slope m equal to 2 and with temperature-dependent threshold:***

$$sfa\,(T^*)\,[\%] = 12\,[\exp(0.026\,T^*)-1] \qquad (9)$$
$$0 \le T^* \le 85°C$$

where, as before, T*= T-NDT (°C).

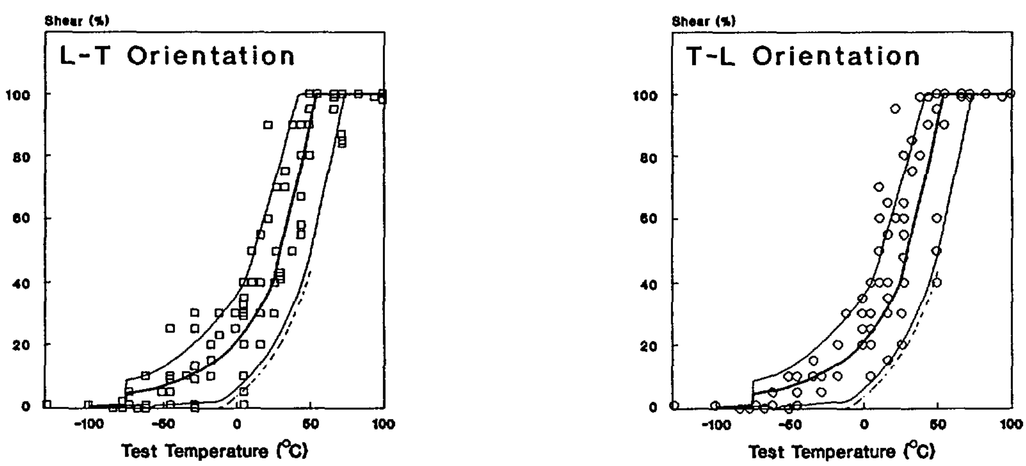


Figure 10. SFA Data Base for Unirradiated Plate HSST-02

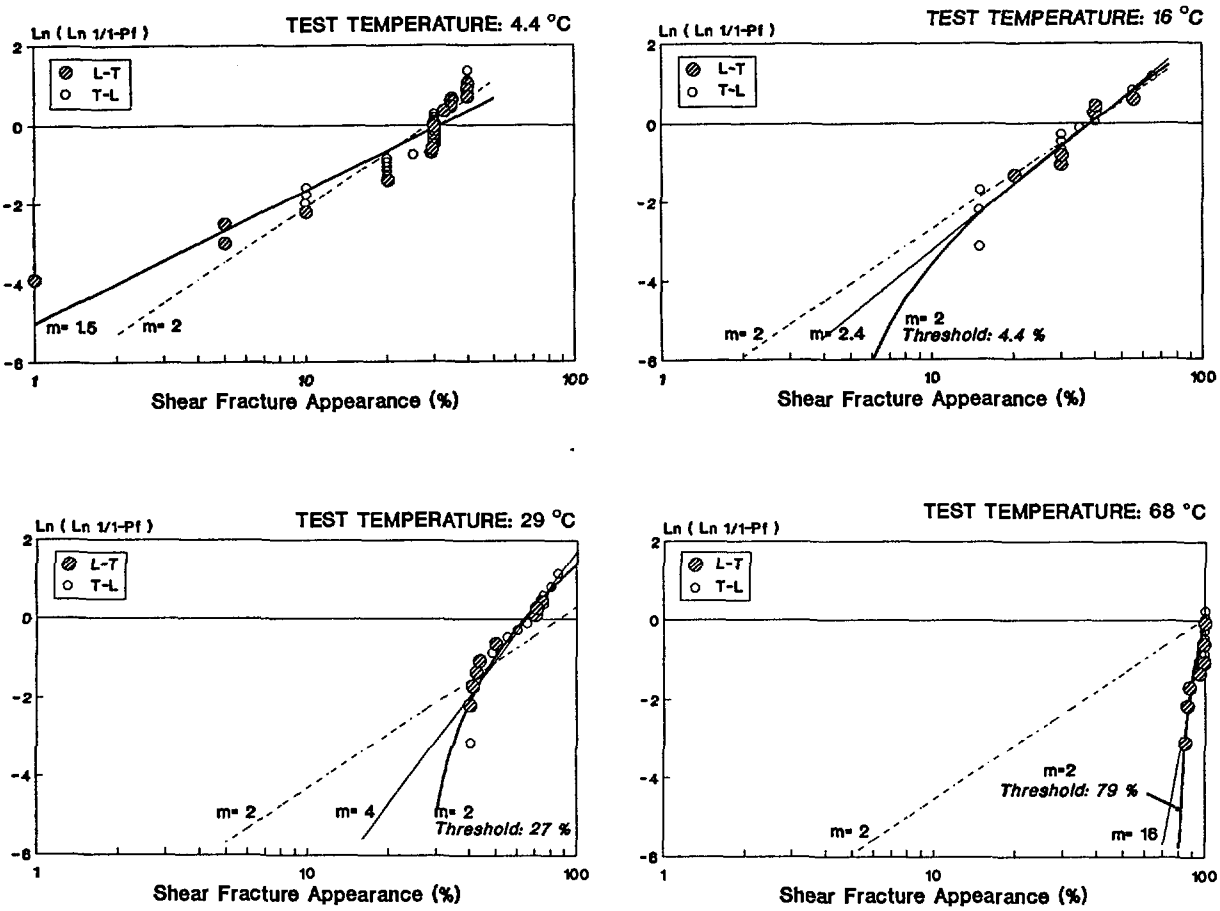


Figure 11. Weibull Evaluation of CVN Fracture Appearance

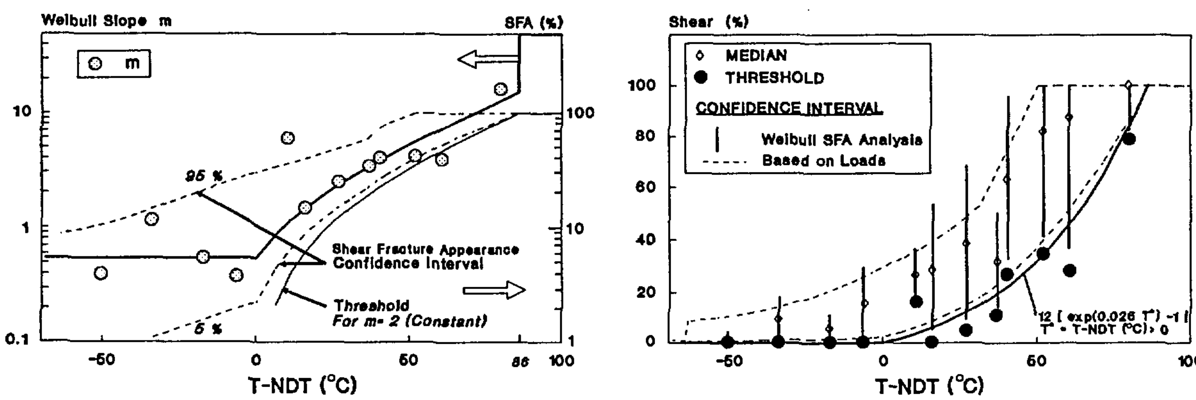


Figure 12. Scatter Trends of CVN Fracture Appearance Change Drastically at NDT

3.2 **Systematics and Limitations.**

The results obtained above for unirradiated plate HSST-02 can be summarized by the following Table 2.

TABLE 2
Systematics of CVN Crack Arrest Indicators

Index	Typical Temperature of CVN Crack Arrest Indicator (°C)		
	Median	Confidence Intervals(2σ)	
		95%	5%
NDT	-11	-42	+19
T_A	+9	-4	+29
FATT	+29	+16	+49
T_O	+54	+40	+75

It is seen that the confidence interval for NDT is almost the double of the ones obtained for the three other crack arrest indicators. This of course is true for all steels and conditions, because simply reflecting in all generality the statistical behavior of CVN arrest loads. For this material, FATT (median)= NDT(median) + 40°C, while T_O (upper bound)= NDT (median) + 86°C, and FTE≅NDT (median)+ 85°C.

The correct description of the confidence intervals of the arrest correlation indicators (e.g. Table 2) is crucial for the least squares definition of their best median values. The application margins however are not equal to these confidence intervals; this is briefly discussed in § 4.1.

It may also be wondered how far these indicators are equivalent in terms of structural application. It will subsequently be shown in § 4.1 that NDT and T_A are indeed equivalent. By contrast, using FATT or T_O for nuclear applications is not necessarily equivalent to using NDT or T_A. It is interesting to briefly discuss the FATT case. A compilation of in-service observations for three different types of reactor pressure vessel steels is presented on Figure 13.

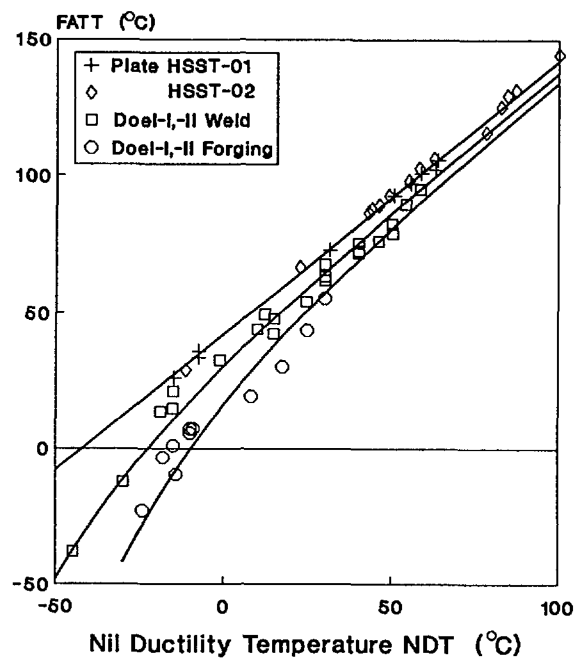


Figure 13. In Service CVN-FATT Shift is Upper Bound for NDT-Shift

For plates HSST-01 and HSST-02 in all considered surveillance capsules whatever their exposure, an unique, "best" relationship FATT= NDT+42°C holds and, within its 2σ confidence interval of ±

5°C, is in agreement with the unirradiated HSST-02 results of Figure 9; NDT does approximately correspond to a constant SFA level of 16%. For the unirradiated condition of the other two steels however, NDT≅ FATT, and this 50% equivalence level tends to decrease towards 16% with increasing embrittlement: the **in-service FATT-shift is an upper bound for the NDT shift.** Quite generally speaking, it has been found that

---

$$\textbf{FATT} \leq \textbf{NDT} + \textbf{50°C} \quad (10)$$

The use of T_O entails even more conservatism, although never as much as when using the 41J. indexing.

One can qualitatively grasp the patterns of Figure 13 at the light of equation (8). For a given test temperature, any service-induced plasticity decrease tends to decrease the difference F_m-F_u between the maximum load and the brittle initiation load F_u: namely, the amount of ductile stable crack growth decreases, thus F_u increases and may even reach F_m at the limit of extremely large embrittlement (there is then essentially no ductile stable crack growth anymore, but shear lip formation without brittle initiation at T_O). So, the less plasticity, the larger F_u for a given arrest load F_a and the lower SFA: FATT increases relative to NDT. Conversely, another simple way to put it is that, the more ductile the steel, the larger the fraction of shear to be expected at the NDT.

These considerations explain why literature correlations of CVN-SFA to crack arrest temperatures can only be approximative: the SFA "indexing" level cannot be unique- yet, paraphrasing, it is "more unique than others", -such as the 41J. energy level. In absence of instrumented information, the 50% FATT index can be used to conservatively measure embrittlement shifts, while avoiding the possible biases of energy/lateral expansion indexing. More generally, the combination of CVN loads and fracture appearances (e.g. Figure 9) allows to minimize uncertainties and to detect eventual anomalies.

4. PERFORMANCE OF "MASTER CURVE" FOR CVN ARREST LOAD CORRELATIONS.

4.1 Application to a Commercial PWR Vessel.

It is instructive to compare three "recipes" to define crack arrestability in a reactor pressure vessel. As an example, this is done on Figure 14 for the critical, circumferential weld of a commercial, 180 mm thick vessel in the beginning-of-life (BOL) condition as well as in an extended end-of-life (E-EOL) condition, well beyond the plant design life: this corresponds to a maximum 290°C neutronic exposure of 5.0 E19 cm^{-2} (>1MeV). Here, the unirradiated R.T. yield strength of 428 MPa was found to have increased in service up to an upper bound value of 570 MPa, while the unirradiated NDT of **-30°C** (identical for drop weight and F_a master curve method) was found to have increased to a conservatively estimated value of +73°C, with a 2σ upper bound of **+95°C** (T_A= +115°C). Note that the median NDT increase of 103°C is at least 40% less in this case than the 41 J. CVN shift.

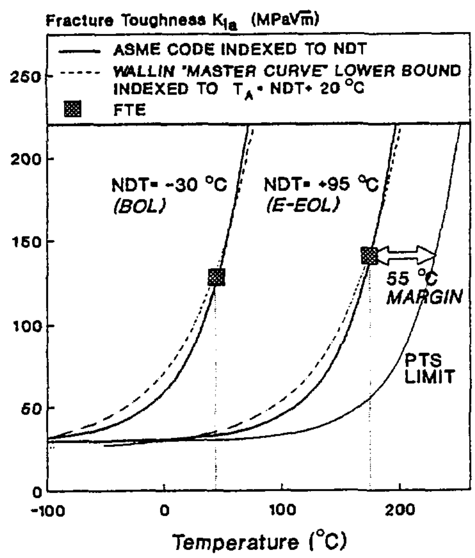
The three "recipes" are as follows:

1) Use the ASME Code K_{IR} curve, where RT_{NDT} is replaced by NDT as defined by the CVN arrest load

2) Use Wallin's 2σ lower bound K_{Ia} "master curve" where the indexing temperature is taken equal to the T_A value defined by the CVN arrest load

3) Determine FTE for the considered vessel thickness, for the relevant R.T. uniaxial yield strength and for the relevant NDT defined by the CVN arrest load.

The *three approaches are consistent* with one another and indicate a *substantial safety margin of*



VESSEL CHARACTERISTICS

Thickness	**180 mm**
Diameter	**3330 mm**
Operating Pressure	**16 MPa**
Membrane Stress *(Normal Operation)*	**155 MPa**

Figure 14. Evaluation of Margin Against Brittle Fracture for Circumferential Weld of Commercial PWR Vessel Operated Well Beyond Plant Design Life.

55°C for the E-EOL condition. By contrast, application of the *current regulatory concepts* to this vessel leaves *almost no margin.*

It is not too surprising that the first two "recipes" above agree so well: the shapes of the K_{IR} curve, of Wallin's lower bound K_{Ia} curve and of the present F_a "master curve" are all close to exponential functions with quite similar slopes; the agreement, or lack thereof, depends only on the accuracy of the difference T_A-NDT (only 20°C, with an uncertainty less than 15°C). Concurrence with the third "recipe" is gratifying, given that the FTE/NDT relationship is based on correlations *independently* developed for non-nuclear steels, and from experiments seldomly involving thicknesses as large as the ones of ASME-III designed reactor pressure vessels.

It must be noted that the bounding rather than the best estimate value has been used for the single, experimentally defined input parameter (NDT or T_A) needed to apply the "recipes". This 2σ upper bound is distinct from the one considered in § 3.2- which characterizes the material scatter at the relatively local scale of a CVN specimen. Any large, non-arresting crack in a vessel would necessarily sample a large volume of material and reflect its median properties (Cottrell, 1995). But the median property here is derived through a correlation (Fig. 5). The 2σ confidence interval associated to the prediction has been estimated as ± 22°C (§ 2.4); this has been found consistent with the NDT embrittlement trends and uncertainties for the considered weld, as revealed by the data from up to six surveillance capsules and ten material conditions (i.e. including baseline, ageing and annealing at two temperatures).

The view discussed in § 1 that the ASME K_{IR} curve should always be indexed to NDT rather than to RT_{NDT} can now be further justified. Asking for additional protection against low upper shelf toughness, especially in the case of an accidental pressure-temperature transient, amounts to ask whether the arrested crack could reinitiate in a ductile mode (in a regulatory perspective, brittle re-initiation is excluded because FTE at E-EOL is significantly less than the value corresponding to the PTS screening criterion). In the example of Figure 14, the FTE occurs at NDT+80°C and corresponds to a toughness K_{Ia} of 140 MPa√m for a stress of almost four times the estimated membrane stress in normal operation. In theory, and in the most unlikely event that the applied stresses reach yield magnitude, ductile re-initiation would thus be possible if the K_{Jc} level corresponding to J_{Ic} is less than 140 MPa$\sqrt{m}$; the tearing modulus would furthermore have to be quite low for such crack to propagate to any significant degree. Let now ask what happens if FTE has been underestimated. It is obvious from Figure 14 that the critical K_{Jc} level for ductile re-initiation after arrest will change little (at E-EOL, it is only 14 MPa $\sqrt{m}$ larger than at BOL) and the same is true for the corresponding steel J-R curve. Thus, correcting (i.e. increasing) FTE would not affect the margin against ductile re-initiation, and this in turn illustrates well that replacing NDT by RT_{NDT} does not meet its intent of providing protection against tearing instability for steels with low CVN upper shelf. The only sound way to address ductile initiation or post-arrest re-initiation is through a fracture mechanics tearing evaluation, as already stated (§ 1).

Finally, it is useful to emphasize that the present FTE/CAT/NDT approach applies to any service condition. Insofar as its associated margins are conservatively assessed, it is an *absolute formulation* rather than a formulation based on differences between E-EOL and BOL (baseline) conditions; this is in contrast to current regulation and may prove helpful for oldier vintage vessels whose unirradiated mechanical properties are ill-defined, with no archive material left for an improved characterization.

4.2 Conservatism of Current Regulatory Approach.

The practical implications of the present concepts can be considerable when assessing safety margins for PWR pressure vessels. The case of the vessel discussed in § 4.1 is by far not unique. That the current regulatory approach may be unduly conservative has been known for some time (Fabry 96.a,b) and significant work is in progress with a view to promote future regulatory acceptance of an enhanced RPV surveillance methodology. In particular, it has been shown by sampling the Belgian BR3 vessel that the margins estimated in the past for the base metal of this vessel are much larger than indicated by the prevailing engineering methods (Fabry 97.b): neither was the wet anneal of this vessel in 1984 necessary, nor was the 1987 plant shutdown (a decision largely due to the cost of answering new requests to perform additional, extensive probabilistic PTS studies). Actually, the fracture toughness of the material has been adequate all along. The same is true for the vertical Linde-80 weld. Investigation of a representative, commercial surrogate weld irradiated in a test reactor at the plant operation temperature shows that service-induced transition temperature shifts are exceedingly less than according to regulatory expectations. The relevant comparison for BR3 surrogate weld and plate materials is illustrated by Figure 15 (note that the plate orientation is T-L).

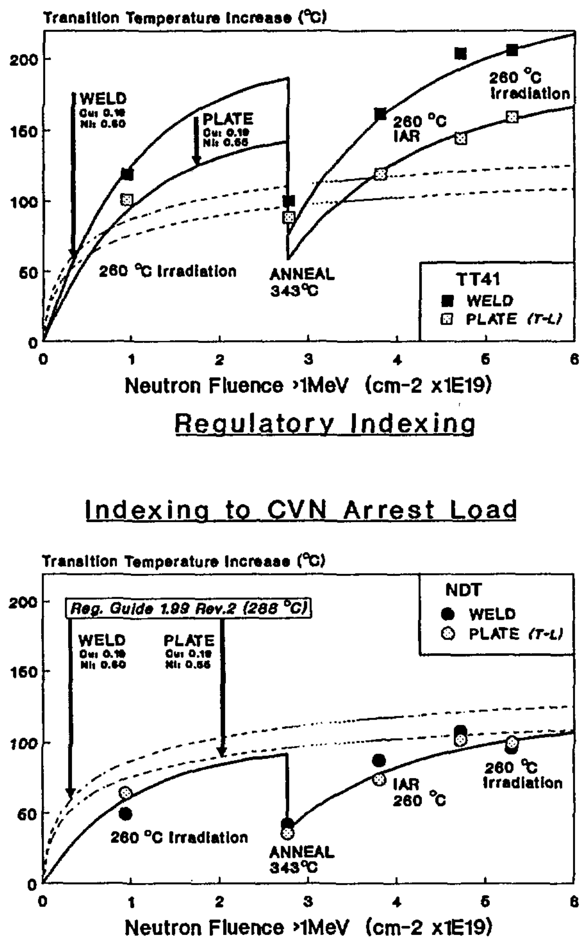


Figure 15. Irradiation/Anneal/Re-Irradiation Path Depends on Fracture Toughness Indexing Concept

At 260°C irradiation temperature, the NDT shifts as determined according to the present approach are significantly less than the corresponding 41J. CVN shifts and are even below the predictions of the USNRC Regulatory Guide 1.99 Rev.2 for the quite less detrimential irradiation temperature of 288°C (according to the Guide, correction to 260°C calls for an additional penalty of +28°C). It is encouraging that a crack arrest philosophy can offer such benefit, not only in terms of pressure-temperature limitations during normal plant operation, but also for plant management decisions with respect to the avoidance of accidental threats beyond vessel design life.

5. MERGING OF CRACK ARREST AND CRACK INITIATION APPROACHES.

Another direction of recent experimental and theoretical efforts to modernize RPV surveillance centers on the crack initiation approach. It is common knowledge that prohibitively large specimens, incompatible with insertion in a commercial surveillance capsule, are needed for the direct measurement of valid static initiation fracture toughness K_{Ic} in the transition temperature range - i.e., vadid in the sense of linear elastic fracture mechanics (LEFM). However, elastic-plastic fracture mechanics (EPFM) indicates that the space compatibility requirements can be met by J-integral testing. The temperature range for LEFM-valid K_{Ic} measurements or for EPFM-valid K_{Jc} determinations depends on the yield strength of the steel and on the specimen size, but the size restrictions are relaxed if some amount of plasticity can be accomodated, as feasible with the J integral concept. For RPV steels in general, constraint corrections remain small or negligible for median K_{Jc} levels up to $\cong$ 100-120 MPa$\sqrt{m}$, even for the deeply-precracked Charpy-V geometry tested under three-point slow bend (Anderson, 1993). Thickness corrections are easily applied if one accounts for the fact that, in the transition range, cleavage statistics is governed by the weakest link model. A "master curve" procedure has been developed to incorporate these advances (Wallin, 1991) and is presently being standardized by ASTM. The median K_{Jc} (T) curve is represented by a constant term to which is added an exponential function of the reduced temperature T- $T_{KJc}{}^0$ where $T_{KJc}{}^0$ is the temperature at a reference level of 100 MPa$\sqrt{m}$. By convention, $T_{KJc}{}^0$ is usually quoted so as to correspond to a specimen thickness of 25.4 mm (one inch). The scatter is described by a Weibull three-parameter distribution of slope m=4 and of constant threshold of 20 MPa$\sqrt{m}$. The only parameter to be experimentally determined is $T_{KJc}{}^0$ and in principle, this can be done to within a 2σ confidence interval of $\pm$ 15-20°C by testing a minimum of six Charpy-size (or larger) samples at a properly selected temperature. A particularly useful feature of this approach in the present context is that reconstitution technology allows to fabricate precracked Charpy-V (PCCV) specimens from the existing inventory of broken surveillance remnants (van Walle, 1996).

Wallin's K_{Jc} evaluation procedure has been systematically applied to surveillance and test reactor irradiation results for which extensive experimental documentation was available, together with the CVN load-time traces needed for crack arrest analysis by the present formulation. As typical illustration, Figure 16 shows the application of Wallin's procedure to the USNRC-sponsored investigation of dose rate effects for the A302-B reference plate (Hawthorne, 1990) while Figure 17 condenses the companion F_a master curve evaluation. The R.T. static yield strengths have also been carefully assessed, among other mechanical properties not considered herein. The major outcome of this study in the present context is presented on Figure 18.

Figure 18 clearly confirms that RPV steel strengthening tends to remove the strain rate effect on fracture toughness: T_A= $T_{KJc}{}^0$ for yield strength $\geq$ 600 MPa. This figure is very comparable to similar displays of $T_{KJd}{}^0$ -$T_{KJc}{}^0$ (dynamic versus static initiation) in function of the R.T. static yield strength (Barsom, 1970, Fabry, 1996.b). This should be so insofar as crack arrest obeys a stress-related cleavage criterion, i.e. at NDT and at T_A: the strain rate corresponding to K_{Ia} is larger than for the usual K_{Id} tests, but this obviously does not

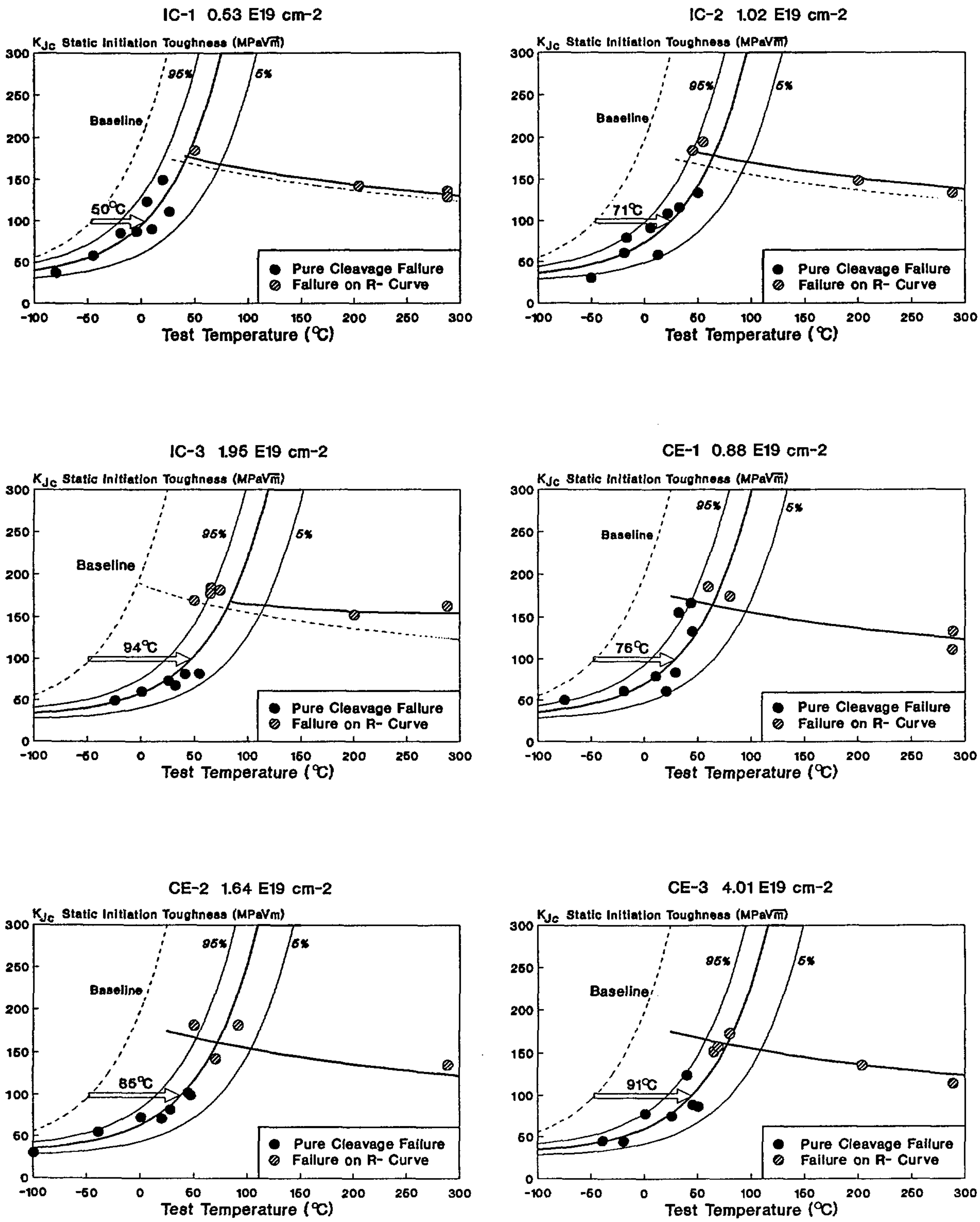


Figure 16. Kjc- Shift for A302-B Reference Plate (Half Thickness, L-T)

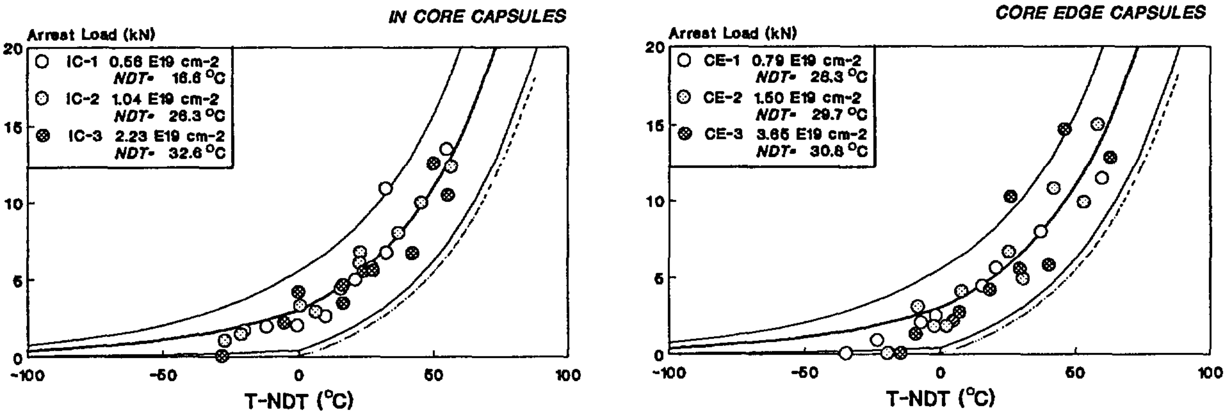


Figure 17. CVN Arrest Loads for A302-B Reference Plate.

affect the nature of the physical effect responsible for the trends. A simple physical rationalization has been previously outlined to explain the disappearance of the strain rate effect on crack initiation at increasing strength, and applies here as well. Basically, strengthening entails an increase of the athermal contribution to the flow stress and this shifts upwards the temperature range relevant to initiation (and arrest), bringing it increasingly closier to the cut-off temperature above which the thermally activated component of the flow stress vanishes; once this cut-off is exceeded, all static and dynamic properties become equivalent.

Note that "blue brittleness" can complicate this simple picture in some cases. Much more importantly, it must be stressed that the ideal, physically-grounded correlation parameter for strain rate effects is not the yield stress, but the ratio of the yield stress to the microcleavage fracture stress (Fabry, 1997.a). The simplified representation adopted here does not fundamentally affect the engineering and regulatory implications of the present findings. The application details however should not be based on the crude, indicative linear fit of Figure 18.

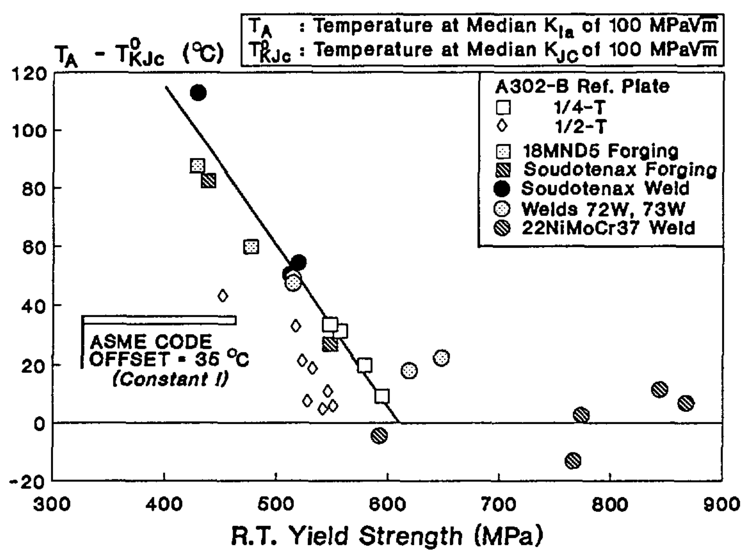


Figure 18. Strain Rate Effect on RPV Steel Fracture Toughness

By contrast to the above results, the ASME Code and its nuclear application are based on the assumption of a *constant* offset: T_A-T_{KJc}^0 ≅35°C. Strictly speaking, this regulatory fix holds for lower bound toughness curves while Figure 18 pertains to median "indexing" temperatures, but this distinction is primarily of academic nature; it is clear that the Code does not properly account for the experimental observations. Why does the Code entertain such bias, especially if one considers that such type of observations and some grasp at their physical underpinnings have existed since the seventies ? At least two main factors have presumably contributed. First, sustained interest in the crack initiation approach is relatively recent: this seems traceable to the realization that design basis accidents did not comprehensively address the whole spectrum of hypothetical pressurized thermal shock transients. As long as crack arrest remained the linchpin of regulation, the strain rate effect did not matter much in practice: dynamic properties are the relevant ones and it certainly seems a-priori convenient to "index" K_{Ia} to the dynamic CVN test. Second, observations that the K_{Ic}/K_{Jc} shift can for some melts exceed the 41J. CVN shift are also somewhat recent (Hiser, 1985). Furthermore, this effect seems to mostly affect plates and forgings, by contrast to welds- which, more often than not, govern safety margins. The reason for such patterns (Fabry, 1996.b) is that the strain rate effect on fracture toughness tends to be compensated and obscured by biases stemming from the CVN energy/lateral expansion indexing procedure. The balance between the two sources of bias depends on the steel. If the 41J. level is always "triggered" by the initiation-linked energy fraction, as often happens for base metals in the L-T orientation, there is no CVN indexing bias for service-induced shifts, and the K_{Ic}/K_{Jc} shift exceeds the CVN shift. At the opposite end of possibilities, if control of the 41J. level upon service moves from the initiation-linked to the post-arrest energy fraction, the 41J.-shift will be excessive and may even overcompensate the strain rate effect: this happens for instance with Linde-80 flux welds.

It is clear from Figure 18 that, at yield strengths of ≅ 600 MPa and above, the present, F_a-based crack arrest formulation is entirely equivalent to the T_{KJc}^0 initiation approach based on the slow bend testing of precracked specimens; considering uncertainties, the "break-even" may even hold at lower service-induced strengthening. From a cycle-to-cycle economy viewpoint, pressure-temperature limitations are the paramount consideration and these are dictated by K_{Ia} . On another hand, margins against accident govern any concern or decision relative to end-of-life, extended end-of-life, license renewal, ...; these margins are usually calculated in terms of K_{Ic}/K_{Jc} . Thus, the two approaches to safety seem complementary, but this work indicates that they also are mutually reinforcing to a degree not always sufficiently comprehended. When moving from the BOL to the EOL condition in most PWRs however, it is believed that the "last word" remains with crack arrest, whether in normal operation or under hypothetized accident regimes.

6. CONCLUSIONS.

The results of this work lead to the view that current regulation and engineering practices in the field of nuclear reactor pressure vessel integrity insurance are affected by some conceptual flaws, and appear to deserve improvement in three main, interrelated respects:

1) The influence of service exposure on the strain rate sensitivity of fracture toughness is, and should not be, neglected

2) The 'physics' of the CVN impact test- the test central to the presently accepted toughness indexing procedure- is, and should not be, ignored (separation should be done between the roles of crack initiation and propagation, shear lip formation,..; the same integral features should not be used irrespective of whether one wants to monitor service effects on initiation or on arrest fracture toughness)

3) Unwarranted and unnecessary mismatch is introduced between brittle cleavage and ductile tearing fracture modes when replacing NDT by RT_{NDT} for the (ill-served) purpose of trying to

incorporate some protection against low CVN upper shelf through the indexing of the unirradiated cleavage fracture toughness.

These simplifications can cause significant biases- *unacceptably* overconservative ones sometimes- and seem to have obscured the cost/benefit appraisal of the relationship between the crack initiation and crack arrest philosophies to assess RPV safety margins against brittle fracture.

It is contended that the crack arrest approach, mandatory under normal and upset plant operation conditions, is also, *if properly implemented*, the most attractive and efficient approach for faulted and accident regimes at ageing plants. A simple, reliable implementation strategy using the load signals of the instrumented CVN test has been formulated. A number of reasons behind the stated preference have been advanced and justified, the two main ones being:

1) Less conservatism in defining heat-up and cooldown operation limits

2) Greatest safety guarantee against accidents at no obvious economic penalty when approaching end-of-life or extended end-of-life.

7. ACKNOWLEDGMENTS.

This study has been jointly sponsored by SCK-CEN and by Tractebel. Special thanks are expressed to Robert Gérard, Tractebel project manager, for his competent support. The author is also indebted to his colleagues, in particular Eric van Walle, Rachid Chaouadi, José Van de Velde and Alfons Verstrepen, for their help and interest, to Kunio Onizawa (JAERI), who provided the data from the ASTM round robin on CVN reconstitution, to Fred Stallmann (ORNL) for making available the CVN load-time traces for the A302-B reference plate and to Mike Sokolov and Shafik Iskander (ORNL) for kindly forwarding advance copies of work to be published.

8. REFERENCES.

Ahlf,J., Bellmann,D., Föhl, J., Hebenbrock, H.D., Schmitt, F.J., and Spalthoff,W., "Irradiation Behavior of Reactor Pressure Vessel Steels from the Research Program on the Integrity of Components", *Radiation Embrittlement of Nuclear Reactor Pressure Vessel Steels: An International Review (Second Volume)*, ASTM STP 909, L.E. Steele, Ed., American Society for Testing and Materials, Philadelphia, pp. 34-51 (1986).

Akiyama, T., Hirase, Y. and Urabe, N., "Development of simple test method to evaluate crack arrest property", Trans. ISIJ, Vol. 25, pp. 1267-1275 (1985).

Anderson, T.L., and Dodds, R.H. Jr., "Simple Constraint Corrections for Subsize Fracture Toughness Specimens", *Small Specimen Test Techniques Applied to Nuclear Reactor Vessel Thermal Annealing and Plant Life Extension*, ASTM STP 1204, W. R. Corwin, F.M. Haggag, and W.L. Server, Eds., American Society for Testing and Materials, Philadelphia, pp. 93-105 (1993).

Barsom, J.M., and Rolfe, S.T.,"Correlations between K_{Ic} and Charpy-V notch test results in the transition-temperature range", *Impact Testing of Metals*, ASTM STP 466, American Society for Testing and Materials, Philadelphia, USA, pp. 281-302 (1970).

Berger, C., Ewald, J., Wiemann, W. and Wojaczyk, H.G., "Ermittlung von Sprödbruch und Bruchmechanischen Kennwerten mittels Kerbschlagproben", Proc. Conf. Sitzung DVM AK Bruchvorgänge, pp. 182-195, DVM (1979).

Childress, C.E., "Manual for ASTM A-533 Grade B Class 1 Steel (HSST Plate 03) Provided to the International Atomic Energy Agency", Report ORNL-TM-3193, March (1971).

Cottrell, A., "Keynote address: Introductory Comments on Crack Arrest", in Proceedings of the *TWI-HSE Seminar on "Crack Arrest Concepts for Failure Prevention and Life Extension"*, TWI, Abington, Cambridge, UK, 27 September (1995).

Cowan, A., Fearnehough, G.D., Gilles, J., and Lees, G.M., "Irradiation damage in mild steel: a comparison of crack arrest tests and Charpy criteria and influence of neutron spectrum", ASTM STP 426, pp. 107-134, American Society for Testing and Materials, Philadelphia (1967).

Fabry, A., Van de Velde, J., Puzzolante, J.L., Van Ransbeeck, T., Verstrepen, A., Biemiller, E.C., Carter R.G., and Petrova, T., "Research to Understand the Embrittlement Behavior of Yankee/BR3 Surveillance Plate and Other Outlier RPV Steels", *Effects of Radiation on Materials: 17th International Symposium*, ASTM STP 1270, D.S. Gelles, R.K. Nanstad, A.S. Kumar, and E.A. Little, Eds., American Society for Testing and Materials, Philadelphia, pp.138-187 (1996.a).

Fabry,A., van Walle, E., Van de Velde, J., Chaouadi, R., Puzzolante, J.L., van Ransbeeck, Th., and Verstrepen, A., "On the Use of the Instrumented Charpy 'V' Impact Signal for Assessment of RPVS Embrittlement", *Evaluating Material Properties by Dynamic Testing*, ESIS 20 (Edited by E. van Walle), Mechanical Engineering Publications, London, pp. 59-78 (1996.b).

Fabry, A, "Characterization by Notched and Precracked Charpy Tests of the In-service Degradation of Reactor Pressure Vessel Steel Fracture Toughness", *Small Specimen Test Techniques*, ASTM STP 1329, W.R. Corwin, S.T. Rosinski and E. van Walle, Eds., American Society for Testing and Materials (1997.a).

Fabry, A., Chaouadi, R., Puzzolante, J.L., Van de Velde, J., Biemiller, E.C., Rosinski, S.T., and Carter, R.G., "Embrittlement and Annealing of Reactor Pressure Vessel Steels: Comparison of BR3 Surveillance and Vessel Plates to the Surrogate Plates Representative of the Yankee Rowe Vessel", *Effects of Radiation on Materials: 18th International Symposium*, ASTM STP 1325, Randy K. Nanstad, Ed., American Society for Testing and Materials, Philadelphia (1997.b).

Fearnehough, G.D., "The small-scale test and its application to fracture propagation problems", Proc. Conf. '*Dynamic Crack Propagation*', pp. 77-102, Noordhoff Publishing (1973).

Hagedorn, K.E., Kochendörfer, A., "Measurement of the crack arrest temperature in the test according to T.S. Robertson and comparison with the transition temperatures as measured in the notch bend test", Arch. Eisenhüttenwes., Vol. 42, pp. 39-47 (1971).

Hagedorn, K.E., "Comparison of transition temperatures from instrumented impact tests with the Robertson crack-arrest temperature", Arch. Eisenhüttenwes. 54, N 6, pp. 239-244 (1983).

Hawthorne, J.R. and Hiser, A.L., "Influence of Fluence Rate on Radiation-Induced Mechanical Property Changes in Reactor Pressure Vessel Steels", Report NUREG/CR-5493, MEA-2376 (1990).

Hiser, A.L., "Correlation of Cv and KIc/KJc Transition Temperature Increases Due to Irradiation", Report NUREG/CR-4395, MEA-2088 (1985).

Holt, J.M., and Puzak, P.P., "*Drop-Weight Test for Determination of Nil-Ductility Transition Temperature: User's Experience with ASTM Method E 208*", ASTM STP 919, American Society for Testing and Materials, Philadelphia, (1986).

Irwin, G.R., Kraftt, J.M., Paris, P.C., and Wells, A.A., "Basic Aspects of Crack Growth and Fracture", NRL Report 6598, Naval Research Laboratory, Washington, D.C. (1967).

Iskander, S.K., Nanstad, R.K., Sokolov, M.A., McCabe, D.E., Hutton, J.T., and Thomas, D.L., "Use of Forces from Instrumented Charpy V-Notch Testing to Determine Crack-Arrest Toughness", *Effects of Radiation on Materials: 18th International Symposium*, ASTM STP 1325, R.K. Nanstad, M.L. Hamilton, F.A. Garner, and A.S. Kumar, Eds., American Soiety for Testing and Materials, (1997).

Kussmaul, K., Krägeloh, E., Kochendörfer, A., Hagedorn, E., "The Relationship between Notched Bar Impact Bending Test, Robertson Test and Drop-Weight Test", Nuclear Engineering and Design, 43, pp. 203-217 (1977).

McElroy, R.J., and Lowe, Jr., A.L., "Irradiation Embrittlement Modeling of Linde 80 Weld Metals", *Effects of Radiation on Materials: 17th International Symposium*, ASTM STP 1270, D.S. Gelles, R.K. Nanstad, A.S. Kumar, and E.A. Little, Eds., American Society for Testing and Materials, Philadelphia, pp.68-85 (1996).

Nanstad R.K., Sokolov, M.A., and McCabe, D.E.,"Fracture Toughness Curve Shift Method", Heavy-Section Steel Irradiation Program, Progress Report, NUREG/CR-5591, ORNL/TM-11568, Vol.7, N 1,pp. 10-1 to 10-4 (1996).

Nichols,R.W., "Fast and brittle fracture studies related to steel pressure vessels", Proc. Roy. Soc. A, Vol. 285, pp.104-119 (1965).

Onizawa, K., van Walle, E., Nanstad, R., Sokolov, M., Pavinich, and W., "Critical Analysis of the ASTM

Round-Robin on Reconstitution", *Small Specimen Test Techniques*, ASTM STP 1329, W.R. Corwin, S.T. Rosinski and E. van Walle, Eds., American Society for Testing and Materials (1997).

Pachur, D., "Comparison of Drop-Weight and Instrumented Charpy Impact Test Results for Irradiated RPV Steels", *Effects of Radiation on Materials: 16th International Symposium*, ASTM STP 1175, A.S. Kumar, D.S. Gelles, R.K. Nanstad, and E.A. Little, Eds., American Society for Testing and Materials, Philadelphia, pp.195-210 (1994).

Pellini, W.S. and Puzak, P.P., "Fracture analysis diagram procedures for the fracture-safe design of steel structures", Welding Res. Counc. Bull., n 88 (May 1963).

Puzak, P.P., Eschbacher E.W. and Pellini, W.S., "Initiation and propagation of brittle fracture in structural steels", Weld. J. Vol. 31, pp. 561-s to 581-s (Dec. 1952).

Robertson, T.S., "Propagation of brittle fracture in steel", Journ. of the Iron and Steel Institute, Vol. 175, pp. 361-374 (1953).

Schmitt, F.J., "Correlation of C_v and Drop-Weight Transition Temperature Increase Caused by Irradiation", *Effects of Radiation on Materials: 14th International Symposium (Volume II)*, ASTM STP 1046, N.H. Packan, R.E. Stoller, and A.S. Kumar, Eds., American Society for Testing and Materials, Philadelphia, pp. 373-384 (1990).

Server, W.L., and Oldfield, W., "Nuclear Pressure Vessel Steel Data Base", Report EPRI NP 933 (1978).

Shoemaker, A.K. and Rolfe, S.T., "The Static and Dynamic Low-Temperature Crack Toughness of Seven Structural Steels", Engineering Fracture Mechanics, Vol.2, N 4, pp. 319-340 (1971).

Smedley, G.P., "Prediction and specification of crack arrest properties of steel plate", Int. J. Pres. Vess. & Piping, Vol. 40, pp. 279-302 (1989).

Smith, E., "The criteria for the arrest of a crack in a pressure vessel", Int. J. Pres. Ves. & Piping, 71, pp. 55-60 (1997).

Stallmann, F.W., "Analysis of the A302B and A533B Standard Reference Materials in Surveillance Capsules of Commercial Power Reactors", Report NUREG/CR-4947, ORNL/TM-10459 (1987).

Sumpter D.G., "Fracture Mechanics Interpretation of the Drop Weight NDT Temperature", *Drop- Weight Test for Determination of Nil-Ductility Transition Temperature: User's Experience with ASTM Method E208*, ASTM STP 919, J.M. Holt and P.P. Puzak, Eds., American Society for Testing and Materials, Philadelphia, pp. 142-160 (1986).

Vandermeulen, W., Fabry, A., Puzzolante, J.L., Van de Velde, J., Van Ransbeeck, Th., and van Walle, E., "Charpy Specimen Reconstitution as a Means of Providing Data for PLEX Licensing Purposes", Int. J. Pres. Ves. & Piping, Vol. 54, pp. 89-98 (1993).

van Walle, E., "Reconstitution: Where Do We Stand ?" *Effects of Radiation on Materials: 17th International Symposium*, ASTM STP 1270, D.S. Gelles, R.K. Nanstad, A.S. Kumar, and E.A. Little, Eds., American Society for Testing and Materials, Philadelphia, pp.138-187 (1996).

Wallin, K., "Statistical modelling of fracture in the ductile-to-brittle transition region", *Defect Assessment in Components- Fundamentals and Applications*, ESIS/EGF9, Edited by J.G. Blauel and K.-H. Schwalbe, Mechanical Enginering Publications, London, pp. 415-445 (1991).

Wallin, K., Planman T., and Rintamaa, R., " Comparison of some indirect measures of crack arrest", Proceedings of the TWI-TSE Seminar on "Crack arrest concepts for failure prevention and life extension", Abington, Cambridge,UK, 27 September (1995).

Wiesner, C.S., Hayes, B. and Willoughby, A.A., "Crack Arrest in Modern Steels and their Weldments-Comparison between Small and Large Scale Experiments", Int. J. Pres. Ves. & Piping, Vol. 56, pp. 369-385 (1993).

Wiesner, C.S., "Predicting structural crack arrest behaviour using small-scale material characterization tests", Int. J. Pres. Ves. & Piping, 69, pp. 185-196 (1996)