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13. ABSTRACT

In the design of nuclear power plants and the selection of required structural materials, the assurance of reliability in operation is an essential consideration. The need for analytical criteria for defining the adequacy of fracture toughness specifications is particularly acute for pressure vessel materials. The 1972 revision to the ASME (American Society of Mechanical Engineers) Boiler and Pressure Vessel Code (Section III) has adopted linear-elastic fracture mechanics (LEFM) methods as a means of assuring the fracture-safe operation of nuclear vessels and components. It is noted that LEFM can be used to analyze only the behavior of metals subject to plane strain constraint (i.e., brittle behavior), while many steels, when used in structural applications, will behave in a ductile fashion. Thus, in the modernization of nuclear codes, there is additional need to include the full range of fracture mechanics options for system design, that is, to include elastic-plastic and fully plastic fracture mechanics, as well as the linear-elastic procedures. The choice of a particular toughness regime for application of the metal (e.g., plane strain or elastic-plastic) can then be made by the designer or regulatory body. It follows that this decision will have a major implication on the selection of nuclear structural materials.

This report describes recent developments in the means for defining the full range of plane strain, elastic-plastic, and plastic fracture mechanics options available to the designer. Comparisons are made between these options and the fracture toughness requirements of the ASME Nuclear Code (Section III). Existing dynamic plane strain (K_{Id}) data for structural metals are analyzed in concert with dynamic tear (DT) test trends. The limited temperature region of K_{Id} applicability for these materials is shown to presage the elastic-plastic regime, through which sharply increasing stress is required for fracture propagation until a leak-before-fail condition is ultimately attained. This phenomenon highlights the need to extend the analytical

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GENERALIZED PROCEDURES FOR THE USE OF PLANE STRAIN AND ELASTIC-PLASTIC FRACTURE MECHANICS OPTIONS IN THE MODERNIZATION OF NUCLEAR STANDARDS

INTRODUCTION

Various codes and standards are in use for the design, materials selection, and fabrication of commercial nuclear power plants. In view of the potentially serious consequences associated with the failure of a nuclear component, particularly the primary pressure vessel in water-cooled plants, these codes should offer methods for failure prevention during operation. This fact has been recognized since the inception of the nuclear power industry in the USA. Consequently, the U. S. Atomic Energy Commission (AEC) invoked the requirement that primary pressure-retaining components must be operated in the ductile regime. Fracture-safe assurance in this regime was predicated on Fracture Analysis Diagram (FAD) procedures (1) that reflected service failure correlations. At that time the ASME Boiler and Pressure Vessel Code offered essentially no guidelines with respect to fracture safety; yet it was, and still is, the most widely used code in the world for pressure vessel design.

The need to update the fracture toughness criteria for thick-section nuclear vessels became apparent as a result of AEC-sponsored research under the Heavy Section Steel Technology (HSST) Program. This research effort clarified the role of thickness in the evolvement of the constraint transition from plane strain to fully plastic behavior. Partly as a result of this effort, the ASME Code adopted rules in 1972 for fracture-safe assurance. These rules are embodied in Section III, Appendix G of the Code (2) and are based primarily on linear-elastic fracture mechanics (LEFM). It should be noted that specific criteria are presented for fracture safety during normal operations. In addition, the Code recommends that LEFM principles also be used to consider emergency conditions, but leaves the formulation of criteria for this case to the user.

The adoption of fracture toughness requirements based on LEFM principles may lead one to conclude erroneously that the general term "fracture mechanics" applies only to plane strain (brittle) metals and, furthermore, that this is the consensus among engineers in the USA. It is the intent of this report to help clarify this consideration and to emphasize the need to include, in the nuclear codes, options for design criteria that encompass the full range of fracture mechanics technology. Code requirements should include elastic-plastic and fully plastic fracture mechanics, as well as linear-elastic methods. Practical procedures for accomplishing this within the current state of the art will be described.

NECESSITY FOR FULL-RANGE TOUGHNESS CHARACTERIZATION

It has become clear that no single toughness criterion is adequate for all applications. LEFM methods can provide a quantitative assessment of the critical flaw size and stress level for fracture initiation, but this method pertains only to metals that behave in a brittle manner. On the other hand, one would generally prefer to operate critical elements of any structure, such as the primary pressure vessel of a nuclear plant, in the ductile regime since this will provide added assurance against brittle failure under unanticipated circumstances, such as accidental plastic overloading. Clearly, the designer should have the option of choosing the level of toughness deemed appropriate for a particular component; yet the current codes generally have the effect of restricting his options when economic factors are considered.

Consider next the factors associated with a general design philosophy that is predicated on the use of plane strain methods as permitted by the ASME Code. First impressions suggest that LEFM has the potential for an exact definition of the conditions for fracture instability. Upon further consideration, additional factors are highlighted that can significantly detract from the structural reliability achievable with this approach and the practicality for its implementation. These factors are

a. Large and expensive tests are required to obtain high K_{Ic} (static) or K_{Id} (dynamic) toughness values. Acceptable correlations using small specimens have not yet been perfected.

b. A standard method for measuring K_{Id} has not yet been developed.

c. Variations in K_{Id} are not highly significant to the critical size of flaws residing in regions of high stress concentration. This stems from the fact that only small flaws, on the order of 1 in. (25 mm), will remain dormant in these regions even for materials exhibiting the highest K_{Id} values that have been measured (3). In other words the significance of, say, doubling the K_{Id} of the material still results in the fact that small flaws are critical at high stress levels.

d. Engineering structures generally do contain stress concentrations of yield stress level. The small flaws which can be critical in these locations are difficult to detect and the inspection costs can add significantly to the costs of the overall structure.

e. The quantitativeness of LEFM or any other analytical technique requires definition of the minimum fracture toughness of the material in a structure, whereas the toughness trends exhibited by a few heats of a particular type of steel may define only average properties. These properties could be used to prevent, say, fracture of the average structure, but they are not sufficient to prevent the first failure. In other words, fracture prevention criteria must be based on statistical "extremal" values, not "modal" values, of the data distribution.

A comment is in order concerning the use of LEFM principles as the basis for failure prevention criteria. It is evident that the choice of a particular criterion can in no way alter the response of the metal to a given flaw and stress level. The fact that one uses an LEFM criterion does not imply that the structure itself is brittle or that it will fail in a brittle manner. The important point is that a structure whose safety is based on LEFM principles *may* fail in a brittle fashion *if* the toughness is low and the flaw sizes and stress levels are sufficiently high; that is, a vessel subject to plane strain constraint, and operating in a temperature regime where K_{Id} values can be measured using specimens of thickness equal to the vessel wall, has the propensity to fail in a brittle manner. However, proper control of the flaw size, stress level, and toughness in the LEFM regime can, at least in theory, assure fracture-safe operation. If, on the other hand, failure prevention were based on principles requiring ductile behavior (elastic-plastic or plastic), then a failure could still occur if sufficiently large flaws and high stress were present, *but* the failure would not be in a brittle mode. For an arbitrary set of conditions, the "ductile" criterion is preferred to the "brittle" criterion because (a) crack propagation under the former is more difficult, and (b) the danger of fragmentation is greatly reduced. Furthermore, errors in determining actual toughness levels, applied stresses, and existing flaw sizes are expected to have less of an influence on vessel reliability if the toughness is sufficiently high to require a certain degree of local plastic deformation before fracture.

The use of LEFM is a first line of defense in that the structure is considered lost once crack propagation is initiated. Also, it is generally conceded that structural metals are not homogeneous and isotropic and that initiation is a distinct possibility. Accordingly, other reliability criteria may be needed. With these options the designer could then select fracture-safe assurance criteria that are compatible with reliability requirements. Only after this decision has been made can the structural metal be chosen. In spite of its limitations, LEFM technology has provided valuable insight to the fracture problem, and a logical three-part toughness categorization for structural metals, namely, linear elastic, elastic-plastic, and plastic, can be defined in terms of LEFM parameters. This division of fracture behavior can be used to illustrate the different design philosophies that are appropriate for general engineering application (see Table 1):

a. Providing protection against fracture initiation for brittle metals requires control of the critical crack size and the use of LEFM principles.

b. Providing fracture control for specified nominal elastic stress levels in the presence of flaws of unspecified (large) size necessitates the use of elastic-plastic materials. An arrest philosophy is called for in this case such that the parent metal can accept local crack instabilities that continue through the section thickness without further propagation (i.e., leak-before-break).

c. If maximum protection from fracture is required for any anticipated service condition, then only metals capable of plastic deformation (in the presence of a flaw) are acceptable. Under this reliability criterion, a flaw can exhibit rapid extension solely under the action of gross plastic overloading.

The necessity to evolve quantitative assessments of the elastic-plastic and plastic regimes is clear. Unfortunately, quantitative analysis procedures, similar to LEFM, are not presently available. Research toward this goal is continuing on approaches such as the J-integral, crack-opening displacement (COD), precracked Charpy-V (C_v), and equivalent energy. However, a development effort is still required to achieve engineering application of these techniques. The dynamic tear (DT) test, on the other hand, can be used without further development work to characterize the low- and intermediate-strength steels used in nuclear construction. This test procedure is fully rationalizable in terms of section size parameters so as to define the constraint transition from linear-elastic to plastic behavior. The techniques for using the DT test as a very practical means for defining the three toughness regimes is described in the following section.

Table 1
Design Guide For Fracture Assurance Analysis

Fracture Mechanics Subdivision	Fracture Resistance Rank-Level		Structural Design Significance†	Design Principle	Fracture ‡ Mode
Plane Strain (K_{Ic} or K_{Id})	1	Highly brittle Ratio 0.1 to 0.5*	$< 0.3\ \sigma_{ys}$	Control of critical crack size	Flat
	2	Brittle Ratio 0.6 to 2	$< 0.3\ \sigma_{ys}$		Flat
Elastic-Plastic	3	Low range	0.3 to 0.5 σ_{ys}	Arrest (low stress)	Small shear lips
	4	High range	0.5 to 1.0 σ_{ys}	Arrest (high stress)	Thick shear lips
Plastic	5	Low range	$> \sigma_{ys}$	Over yield	Mixed mode
	6	High range	$>> \sigma_{ys}$		Slant

*Ratio signifies K_{Ic}/σ_{ys} or K_{Id}/σ_{yd}.
†Nominal engineering stress (σ_n) for first and continued extension of a through-thickness crack of first-penetration geometry (3-4 times the section size).
‡For section size of interest.

PROCEDURES FOR FULL-RANGE TOUGHNESS CHARACTERIZATION

L and YC Index Temperature Scales

Conservative fracture-safe design requires protection from dynamic flaw initiation, whether due to a localized flaw instability, such as a popin, or due to full-section dynamic load, e.g., impact or shock loading. The LEFM K_{Id} values have been used to achieve a flaw size/stress level relationship for plane strain metals. Figure 1 illustrates the general trend in K_{Id} (and K_{Ic}) toughness values with temperature for low- and intermediate-strength steels. Of prime importance is the sharp fracture state transition with increasing temperature that characterizes the low-alloy structural steels used in water-cooled nuclear plants. This fracture state transition is the result of constraint relaxation induced by increasing ductility on a microscale. The practical result for the structure as a whole is a large increase in deformation capability within a small temperature increment above the limiting temperature for plane strain applicability.* This behavior is reflected by the sharp increase in the large specimen DT energy curve illustrated in Fig. 1 and discussed elsewhere (4).

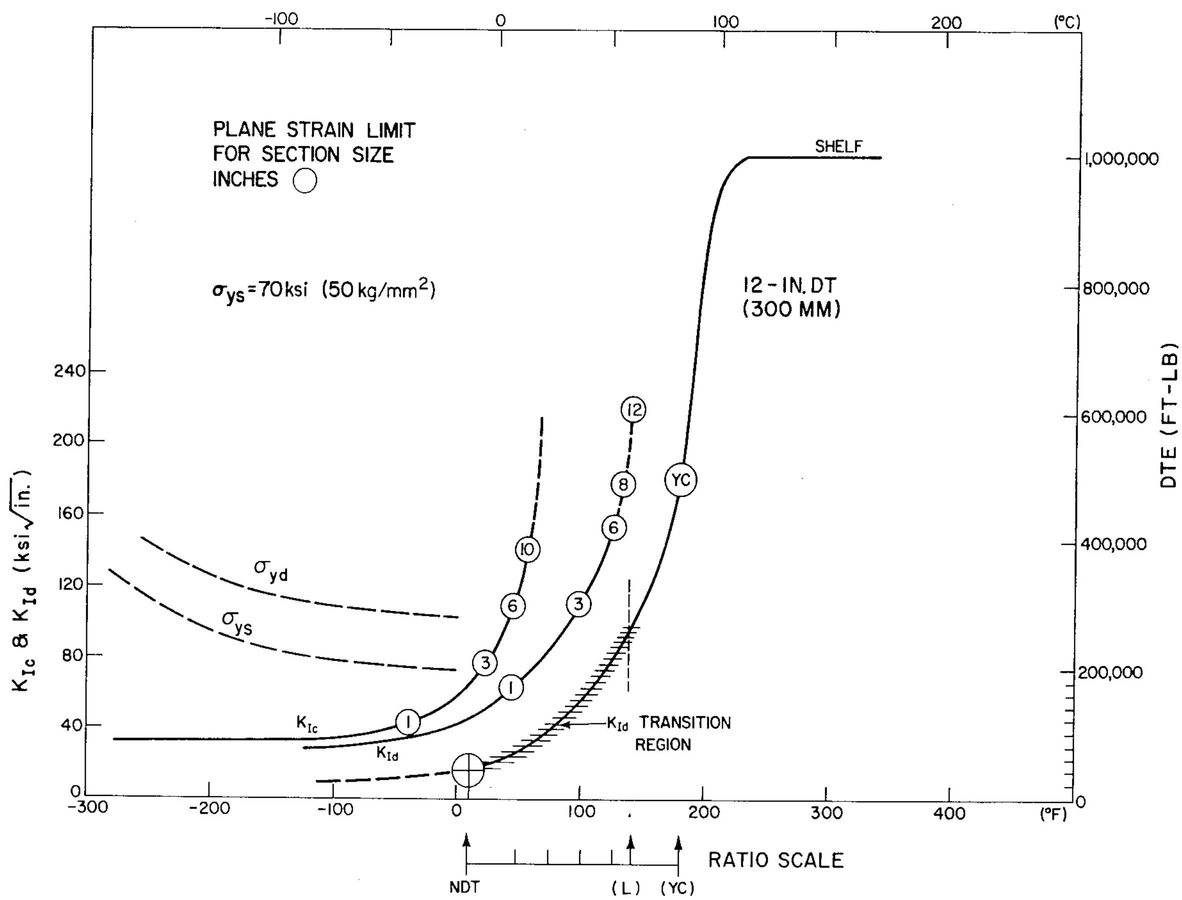

Fig. 1 — General trend of K_{Ic} and K_{Id} with temperature for A533-B steel as defined by Westinghouse Corp. data. The sharp increase in K with temperature reflects the beginning of the constraint transition, whose full extent is defined by the DT curve.

*The limit of plane strain applicability for a section of thickness B has been defined by ASTM Committee E-24 as $B \leqslant 2.5\ (K_{Ic}/\sigma_{ys})^2$ for static testing. In the absence of a standard method for dynamic testing, the same relationship, using dynamic properties (K_{Id} and σ_{yd}), is assumed to apply for dynamic loading.

From Fig. 1 it is apparent that the determination of the upper range of K_{Id} values requires large specimens. Also, these values can no longer be measured at temperatures significantly above the Drop Weight-NDT temperature. This correspondence is consistent with the sharp constraint transition from plane strain to plastic behavior. In order to give the designer the option of specifying structural reliability criteria at a toughness above the level of plane strain (K_{Id}), it is essential to develop procedures that translate the significance of the fracture state transition in terms of structural behavior. A description of this translation procedure, as based on the DT test and K_{Id} correlations, follows.

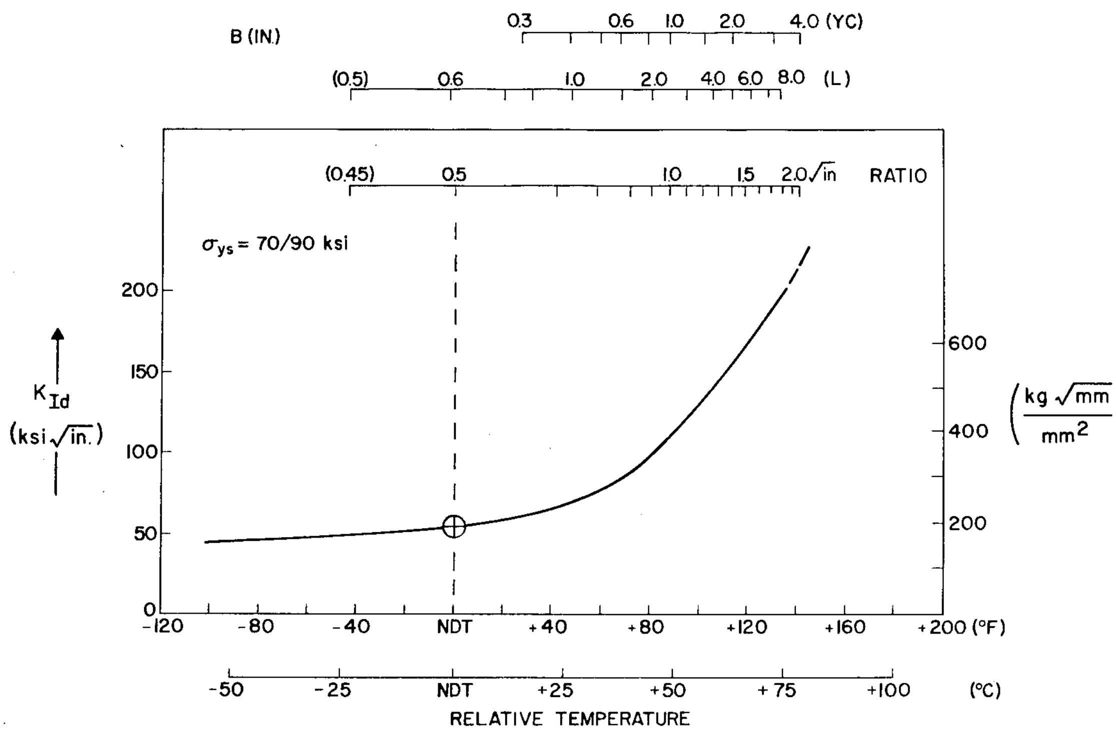

Fig. 2 — The K_{Id} vs temperature curve of Fig. 1 is used to compute the K_{Id}/σ_{yd} scale. This scale forms the basis for the L and YC scales for the given steel. Note that the values on the L and YC scales are in terms of thickness and not ratio.

An analysis of the constraint transition requires an understanding of several concepts based on LEFM behavior. The first is the sharp rise of the K_{Id} values with temperature above the NDT temperature index, as illustrated in Fig. 2. This curve is based on 8-in. (203-mm)-thick K_{Id} tests of A533-B steel conducted by Westinghouse (5), but the trend is believed to be characteristic of all low- and intermediate-strength steels. [See, for example, K_{Id} trends measured by Shoemaker and Rolfe on seven structural steels (6)]. Only Westinghouse data are described because the existence of other dynamic K_{Id} data from very thick specimens is not known. Second, note that the temperature scale in Fig. 2 can be alternatively expressed in terms of the ratio of K_{Id}/σ_{yd} (hereafter referred to simply as the "ratio" or R) and that this ratio increases sharply with increasing temperature.* Third, note that the NDT temperature may be indexed at a ratio of approximately 0.5. This index value has been justified on both theoretical and experimental grounds (6,7).

The K_{Id} curve in Fig. 2 is now used to establish two important index scales for thickness. These scales are denoted as L and YC. The L scale refers to a plane strain *limit* thickness. In other words, there exists a maximum plane strain ratio R that can be experimentally determined with a specimen thickness B as defined by ASTM Committee E-24: $B \geqslant 2.5\ R^2$. A ratio in excess of this value, for the given thickness, relates to the onset of elastic-plastic behavior. For example, to measure a ratio of $1.0 \sqrt{\text{in.}}$ (ratio scale) requires a thickness of 2.5 in. (L scale). Entering the L scale of Fig. 2 at 2.5 in. thickness translates to a temperature of about 90°F (50°C) above the NDT. In other words, plane strain measurements are not possible at temperatures higher than 90°F above NDT for a 2.5-in.-thick specimen whose material exhibits the K_{Id}-temperature trend illustrated. The YC thickness scale in Fig. 2 is used to determine the *yield criterion* for a material of thickness B that contains a sharp flaw. In this case the relationship between thickness and ratio is given by $B = 1.0\ R^2$. Thus for a 2.5-in.-thick plate (i.e., 2.5 in. on the YC scale), the yield condition is obtained at a temperature of approximately 120°F above the NDT. Consequently, a 2.5-in. section of this steel undergoes a fracture transition from plane strain (L) to plastic (YC) behavior in only 30°F (17°C).

The identification of the YC condition as $1.0\ R^2$ was first expressed by Irwin (8) as an approximation of the midrange toughness for the fracture mode transition for sheet material (i.e., Irwin's $\beta_{Ic} = R^2/B$). Later, Rolfe and Barsom (9) concluded that this criterion defines the condition at which considerable through-thickness yielding begins to occur in the notch vicinity and, further, they considered it to reasonably approximate the conditions required for leak-before-break behavior. Finally, Pellini (10) interpreted the toughness values lying between the ratios of $R = \sqrt{B/2.5}$ and $R = \sqrt{B/1.0}$ (i.e., L = $0.63\sqrt{\text{in.}}$ and YC = $1.0\sqrt{\text{in.}}$ for 1 in. thickness) as defining the limits of elastic-plastic behavior for material at the upper shelf level† toughness condition. This concept has been expressed graphically in terms of the Ratio Analysis Diagram (RAD) shown in Fig. 3 for 1 in. (25 mm) thicknesses. Here the region between the ratios of 0.63 and $1.0 \sqrt{\text{in.}}$ has been designated as elastic-plastic. The RAD is extremely useful in defining the structural reliability criterion that can be imposed with a given choice of material. For example, the choice of any 1-in.-thick steel at a strength level over 230 ksi (161 kg/mm^2) in Fig. 3 will permit only the use of a plane strain reliability criterion (see levels 1 and 2 in Table 1), whereas steels having a yield stress below 100 ksi (70 kg/mm^2) will generally exhibit plastic behavior at the shelf level toughness regardless of the reliability criterion imposed. Thus, the controlling influence of the choice of materials in relation to a desired reliability criterion can be readily visualized from the RAD.

It now appears reasonable to extend this definition of the elastic-plastic regime to encompass the fracture state transition that characterizes low- and intermediate-strength structural steels. The objective here is to relate the structural significance of a particular

*For the steel shown in Fig. 2 the dynamic yield stress σ_{yd} is approximated by the addition of 30 ksi (21 kg/mm^2) to the static yield stress σ_{ys}.

†Upper shelf level toughness refers to the temperature region corresponding to the upper plateau of the DT curve for a given thickness, as illustrated in Fig. 1.

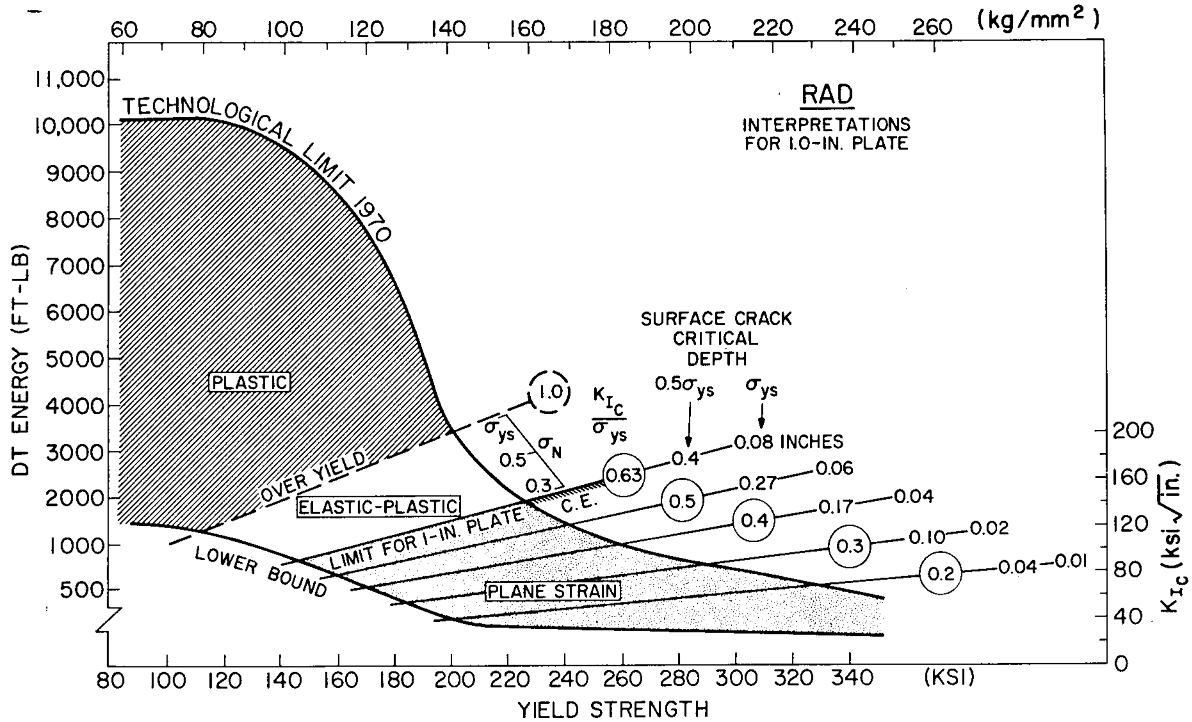

Fig. 3 — The Ratio Analysis Diagram (RAD) for 1 in. (25 mm) thicknesses is used to project fracture behavior for various yield strength steels on the basis of the upper shelf level energy or K_{Ic} level. The depth of critical surface flaws is given in terms of the nominal stress level (0.5 σ_{ys} and σ_{ys}) in a tension plate. The boundaries of the elastic-plastic region are defined by the L and YC ratios for a 1 in. thickness and the stress values here refer to a through-thickness crack.

fracture state (elastic, elastic-plastic, or plastic) to the stress level that may be imposed on the structure. This is illustrated in Fig. 4 through the use of the K_{Id} versus temperature curve for a given steel and the definitions of the L and YC index points.

The L and YC index temperatures are first entered on a K_{Id} versus temperature curve that has been obtained for the material in question (Fig. 4, bottom). The procedure is as follows:

a. Plot the ratio scale along the temperature axis by computing K_{Id}/σ_{yd} at a given temperature; assume $\sigma_{yd} = \sigma_{ys}$ + 30 ksi (21 kg/mm^2).

b. Enter the ratio scale at values of $R = (B/2.5)^{1/2} \sqrt{\text{in.}}$ and $R = (B/1.0)^{1/2} \sqrt{\text{in.}}$ and project down to the K_{Id} curve, thereby locating the respective values of L and YC. Note that these index temperatures are thickness dependent and will shift accordingly.

Next, the temperatures corresponding to L and YC are used to construct a plot of stress versus temperature (Fig. 4, top). The YC temperature is located at yield stress loading (by definition) and the L temperature is entered at 0.3 σ_{ys}. As a limit of plane strain applicability, the L index is defined as the highest temperature at which plane strain conditions apply for a through-thickness crack of length $2a$ equal to approximately three

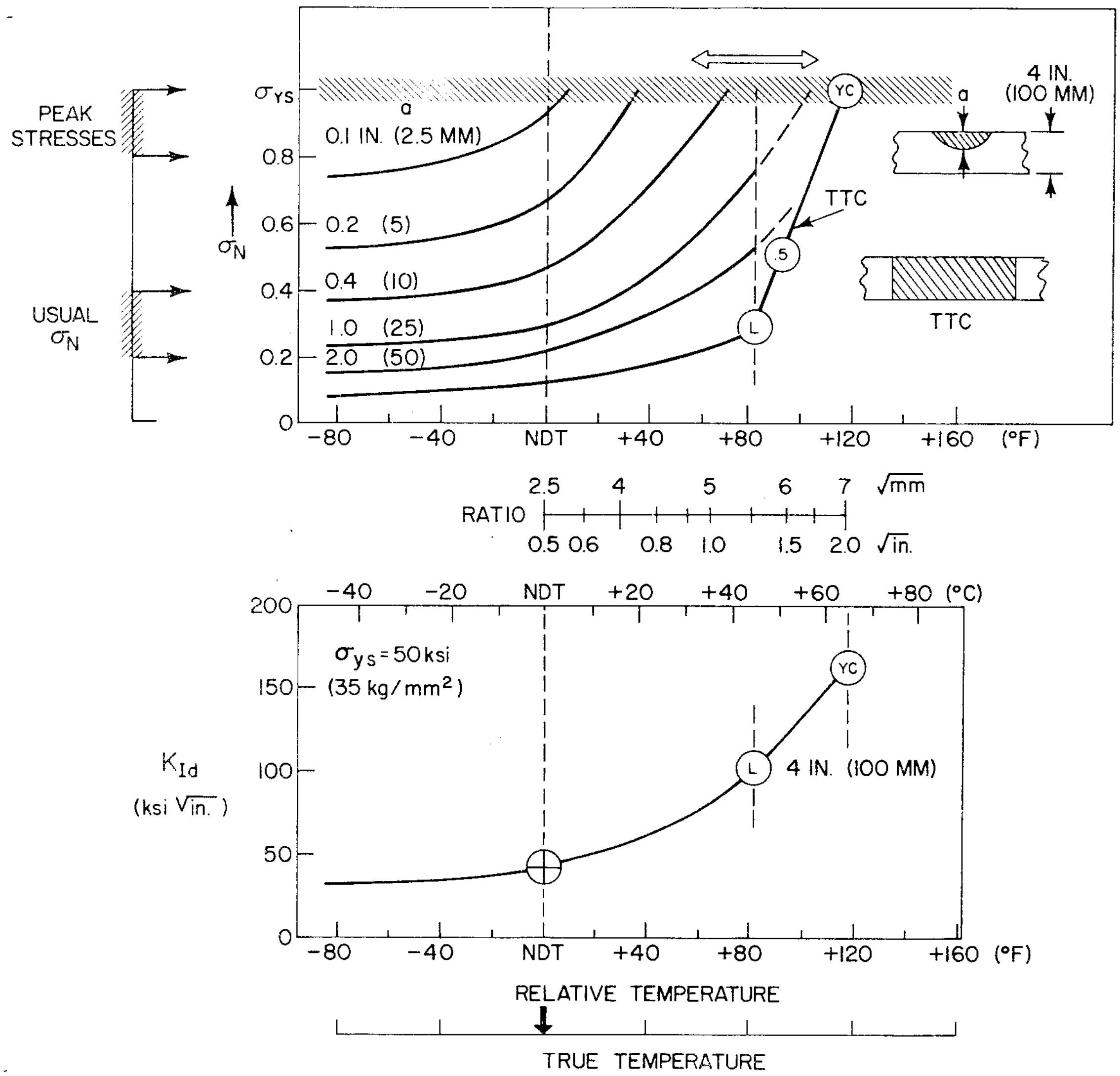

Fig. 4 — The L and YC index ratio values for a 4 in. (102 mm) thickness are used to define a stress vs temperature boundary for leak-before-fail behavior for a through-thickness crack (lowest curve, upper figure). The family of curves defines the behavior of a smaller surface crack.

times the thickness. This logic provides a conservative (highest) stress level that may be tolerated in the presence of a large flaw corresponding to leak-before-break behavior. With this flaw in a tension plate the well-known relationship

$$\frac{K_{Ic}}{\sigma_{ys}} = \frac{\sigma}{\sigma_{ys}}\sqrt{\pi a} \tag{1}$$

for a Griffith crack applies. For any thickness B that satisfies the condition for plane strain constraint, i.e., $B \geqslant 2.5\,(K_{Id}/\sigma_{yd})^2$, it can be shown that $\sigma/\sigma_{ys} > 0.3$. Thus, a conservative value of stress corresponding to the L index temperature is $0.3\,\sigma_{ys}$.

In Fig. 4, top, the allowable stress versus temperature relationship for a through-thickness flaw in a 4-in.(102 mm)-thick section is formed by connecting the L and YC points with a straight line; a second line is formed between 0.3 σ_{ys} at the L temperature and zero stress at absolute zero. To consider flaws smaller than the through-thickness flaw, the K_{Id} versus temperature relation may be used to plot a family of curves for a semielliptic surface flaw in a tension plate (Fig. 4, top). The semielliptic flaw may be treated with the equation

$$\frac{K_{Ic}}{\sigma_{ys}} = 1.1 \frac{\sigma}{\sigma_{ys}} \sqrt{\frac{\pi a}{Q}} \qquad (2)$$

where a is the flaw depth, σ is the applied stress, and Q is a dimensionless shape factor (11). Equation (2) applies only in the plane strain regime, that is, at temperatures below the L-index temperature.

Figure 4 provides a convenient analysis diagram for the designer in that it illustrates the required minimum structural temperatures that must be maintained for certain applied stress levels in the presence of a flaw. For example, consider a small surface flaw (1 in. or 25 mm deep) residing in a region of yield stress (peak) loading. For the 4-in. (102-mm) thickness illustrated in Fig. 4, it is seen that the temperature of the structure must be maintained above the L index value which, by definition, requires operation in the elastic-plastic regime. When this same flaw is loaded at the usual design stress level (i.e., 0.2 to 0.4 σ_{ys}) in uniform sections, operation below the L index temperature is possible. However, a somewhat larger flaw of 2 in. (51 mm) depth will require operation in the elastic-plastic regime.

In theory, Fig. 4 provides an ideal method to achieve fracture-safe design for a structure. Unfortunately, the required K_{Id} curve is difficult to obtain because large specimens must be tested and no standard procedure for dynamic K_{Id} measurements has been evolved. Note that the previous examples have cited a necessity to operate the structure in the elastic-plastic regime. In this regime the LEFM methods do not apply and analytical elastic-plastic approaches have not been well developed.

The DT test offers an alternative procedure which may be used by itself, or in conjunction with the K_{Id} test, to evolve Fig. 4. The philosophy behind this procedure is that the fracture state transition is associated with a change in the ratio of K_{Id}/σ_{yd}. The ratio exhibits a sharp increase with temperature above the NDT; the increase in DT energy with temperature therefore can be expressed in terms of the ratio. This analysis procedure is illustrated schematically in Fig. 5 for high shelf level steels, such as A533-B used in nuclear pressure vessel construction. For these steels it has been shown that the mid-energy of the DT curve approximates a yield condition (4). This behavior is therefore associated with the YC index temperature previously defined.

Figure 5 illustrates the correspondence of the DT and K_{Id} curves for a 1 in. (25 mm) thickness. The YC temperature from the midenergy* of the DT curve is projected downward

*The criterion of using the DT midenergy to define a YC index temperature is generally valid for steels of less than 100 ksi (70 kg/mm^2) yield strength. This is due to the high shelf level energies exhibited by these steels and the narrow temperature range corresponding to the constraint transition. A sufficient shelf energy for this purpose is 4000 ft-lb from the 1-in. RAD in Fig. 3, or 500 ft-lb using a 5/8-in. DT specimen. Steels exhibiting a shelf energy lower than this value must be treated separately. Generally, the DT energy index for these steels is determined from the lower boundary of the "plastic" region in Fig. 3.

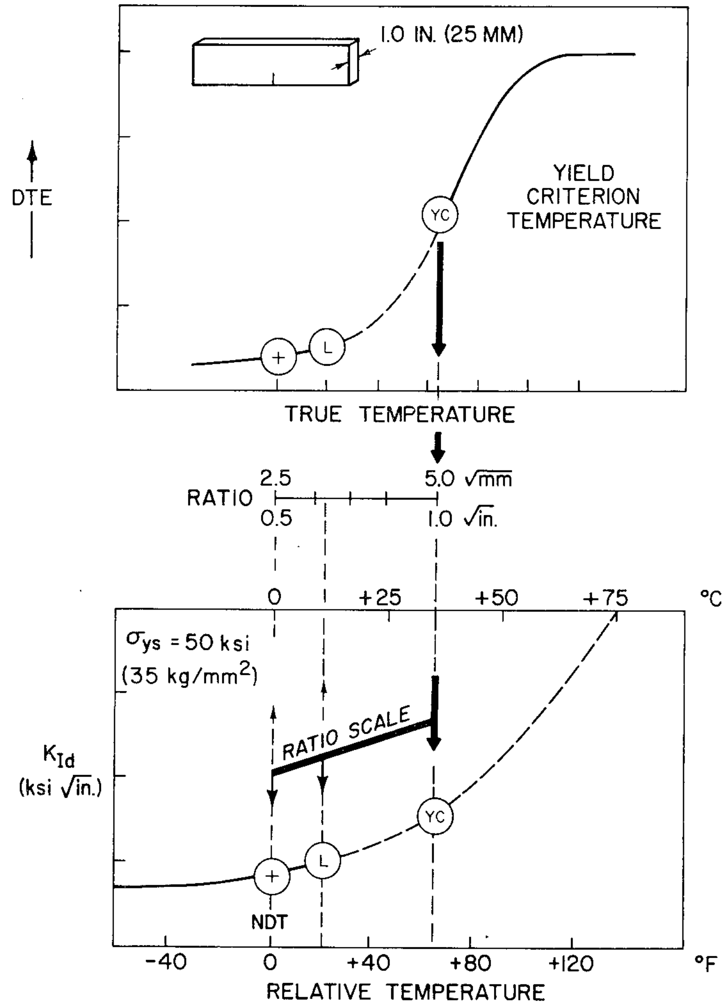

Fig. 5 — Correspondence of DT and K_{Id} curves for purposes of indexing the L and YC ratios. The midenergy of the DT curve may be used to define the YC temperature; likewise the NDT temperature corresponds to the toe region of the DT curve or can be defined with the Drop Weight Test.

to the ratio scale at $1.0\sqrt{\text{in.}}$ Likewise, the $0.5\sqrt{\text{in.}}$ ratio associated at NDT temperature* is projected from the lower toe region of the DT curve to the ratio scale. The remainder of the ratio scale between 0.5 and $1.0\sqrt{\text{in.}}$ may be interpolated without excessive error, keeping in mind the characteristic shape of the K_{Id} versus temperature curve. In this way the stress versus temperature plot of Fig. 4 is obtained in a straightforward manner.

Thick-Section Analysis Procedures

In the case of nuclear vessels, fracture-safe assurance requires consideration of thick sections. However, thick-section testing for toughness properties is expensive, and it can

*The NDT temperature may be easily determined with the Drop Weight specimen. It has also been shown (4,7,12) that the NDT temperature consistently lies at the toe region of the 5/8-in. DT curves.

be assumed that very little of this testing will ever be accomplished. Consequently, the use of small specimens to project large-specimen behavior takes on special significance. Figure 6 illustrates a way in which small DT specimens (5/8 to 1 in. thick) can be used to predict the behavior of sections an order of magnitude larger. Research conducted at NRL in conjunction with the HSST program (4) has suggested that a thick-section DT curve may be obtained by translating the smaller curve by an increment of temperature at the YC energy level as shown in Fig. 6. (Both DT curves are normalized on the basis of upper shelf energy.) The YC entry on the small-specimen curve is an index of metal quality, similar to the NDT, whereas the temperature translation of YC from the thin-section curve to the thick-section curve for high shelf level, transition temperature steels is believed to be primarily a mechanical constraint effect and should not be unique to the A533-B steel investigated.

In Fig. 6 the ratio scale has been entered using the data of Fig. 1. However, without the large specimen K_{Id} data, the location of the scale can be approximated using two YC ratio values from the small and large DT curves plus the $0.5\sqrt{\text{in.}}$ index corresponding to NDT. As long as the material exhibits a sharp constraint transition as confirmed by the thick section DT curve, this indexing procedure for the ratio scale should result in minimal error as opposed to computing the exact ratio scale using thick section K_{Id} tests. Finally, the L and YC ratio temperatures may be projected to a stress vs temperature plot (Fig. 6, bottom) to achieve the thick section analysis diagram equivalent to Fig. 4.

Examples of Indexing Procedures

Figure 7 illustrates the application of the L and YC indexing procedures to wide-plate testing conducted by Mosborg (13). This testing was accomplished by stressing the test plate to approximately 0.5 σ_{ys} and initiating a crack in a 1-ft-wide brittle crack starter plate that was welded to the test plate. Without the actual K_{Id} data for the particular ABS-C steel tested, the ratio scale was calculated from the curve of Fig. 2 using a dynamic yield stress of 80 ksi (56 kg/mm^2). Next, the L and YC values were computed, as previously described, using a thickness of 1 in. (25 mm). Connecting the L and YC stress index values results in a line that should define arrest behavior for the large through-thickness-type flaws that were presented by the brittle crack starter plate. The actual arrest/fracture data indicate a close correspondence with the predicted behavior.

Consider next the fracture behavior of the pressure vessel shown in Fig. 8. This ASTM A201 steel was part of a program conducted by the Pressure Vessel Research Committee (PVRC) of the Welding Research Council to study the growth of fatigue cracks in nozzles (1). The vessel was cycled at a shell stress of yield magnitude which resulted in 0.47% plastic strain at the nozzle. The fast fracture at 70°F (21°C) started from a fatigue crack in the nozzle which was approximately 3 in. deep and 6 in. long (76.5 × 152 mm).

The DT indexing procedures for this vessel are illustrated in Fig. 9. The 2-in. (51-mm) full-thickness DT curve exhibits a constraint elevation of 25°F (14°C) above the 5/8-in. (16-mm) DT curve at the YC or midenergy value, as expected (see Ref. 5). The ratio scale was located by computing the values of 0.8 and $1.4\sqrt{\text{in.}}$, equal to the YC values for 5/8 in. and 2 in. thickness, respectively, and plotting at the temperature corresponding to the midenergy of the DT curves. A third index point to the ratio scale was obtained from

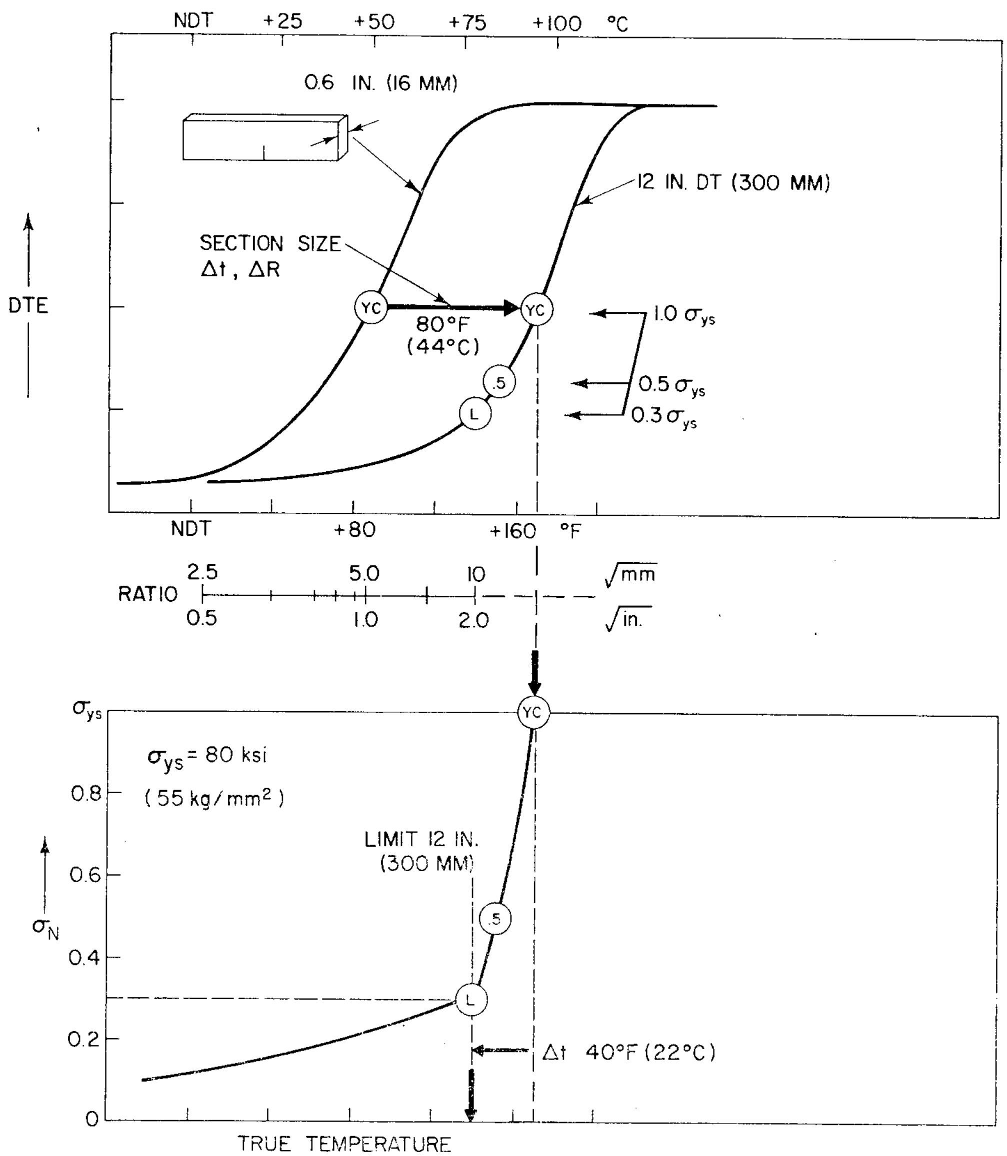

Fig. 6 — The small-size (0.6-in.) DT curve is employed to establish the YC temperature for a larger section thickness using a temperature translation of the YC index. These index values are used to construct a stress vs temperature analysis diagram for the thick section.

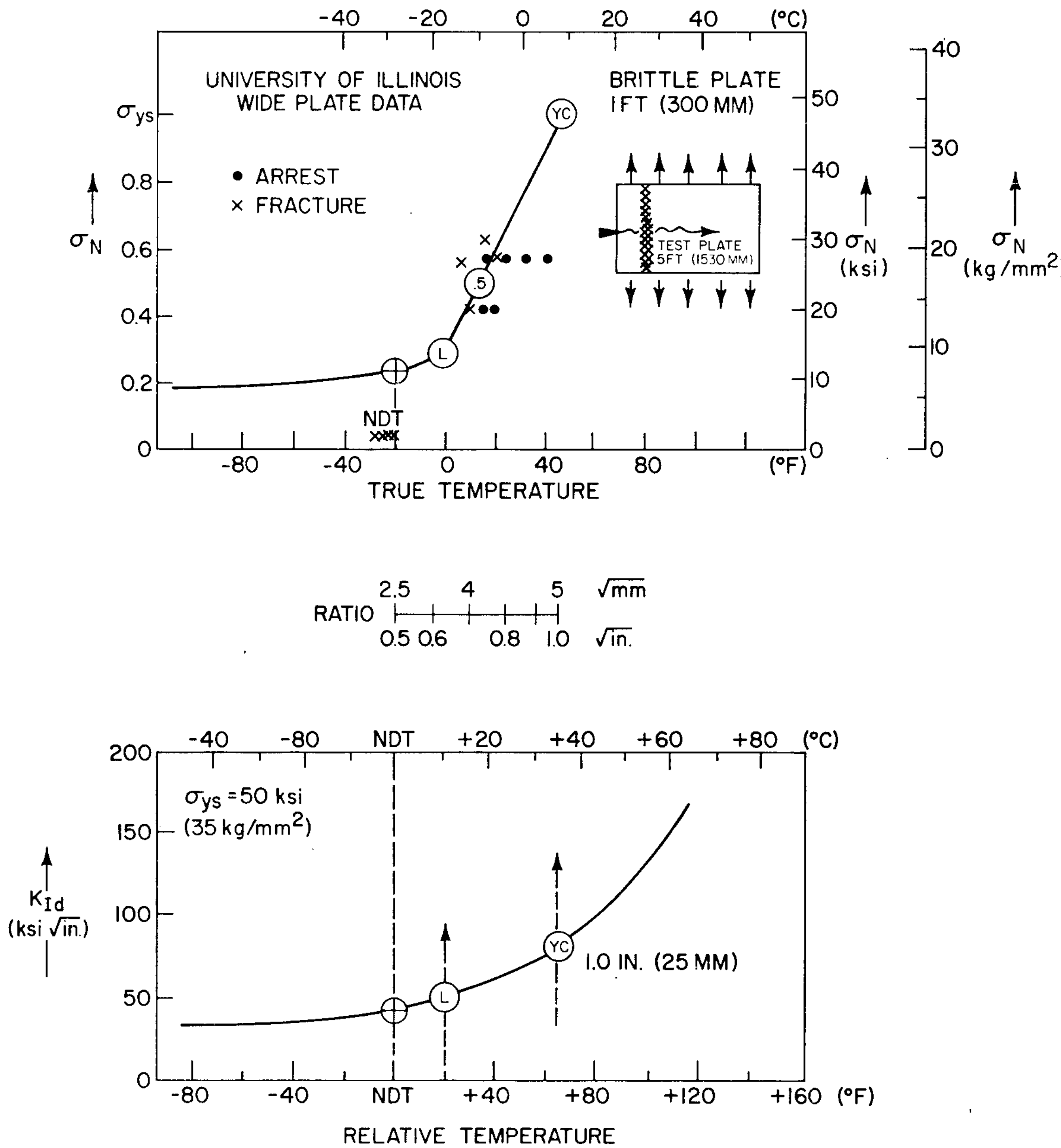

Fig. 7 — Wide plate fracture data are used to verify the arrest/fracture behavior as predicted from the L and YC indexing procedure for a 1 in. (25 mm) thickness

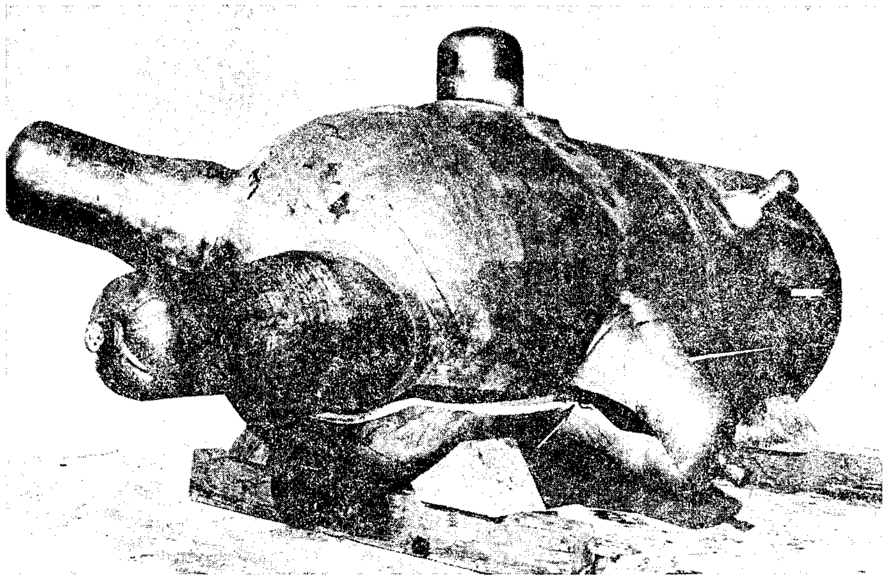

Fig. 8 — Fracture behavior of a pressure vessel that was used to study fatigue crack growth in nozzle regions. The vessel was of A201 steel having a diameter of 3 ft (914 mm) and a wall thickness of 2 in. (51 mm).

the Drop Weight-NDT temperature as illustrated. As a check, the ratio scale was also computed using the same K_{Id} curve as in Fig. 7. The correspondence of these two methods of defining the ratio scale appears to be very good. Finally, the ratio scale, as indexed by the K_{Id} scale, was used to construct the stress versus temperature graph (Fig. 9, top). The failure temperature has been entered on the graph at the yield stress level to which the shell was subjected.

The fracture behavior of this vessel is in accord with what would have been predicted from the analysis diagrams of Fig. 9. In other words, a high stress level and a large flaw were required to initiate the fracture. Additional interpretation of Fig. 9 suggests that the vessel would have exhibited a leak-before-fail behavior if the operating temperature were approximately 20°F (11°C) higher, or if the shell stress were lowered to 0.5 σ_{ys}.

COMPARISON OF CODE CRITERIA AND DT ANALYSIS PROCEDURES

Salient features of the ASME Code criteria for the coolant pressure boundary of water-cooled pressure vessels are presented here for purposes of comparison with the DT analysis procedures. The ASME criteria postulate the existence of a surface flaw having the dimensions of 0.25 B by 1.5 B (depth and length, respectively) for thicknesses of 4 to 12 in. (102 to 305 mm); a 1-in.(25-mm)-deep flaw is taken for thicknesses less than 4 in., and a 3-in.-deep flaw is taken for thicknesses greater than 12 in. The applied K_I level is assumed to consist of a component due to pressure or primary stress (K_{Ip}) and a component due to thermal stress (K_{It}). To permit vessel operation, the inequality

$$2\,K_{Ip} + K_t > K_{IR} \qquad (3)$$

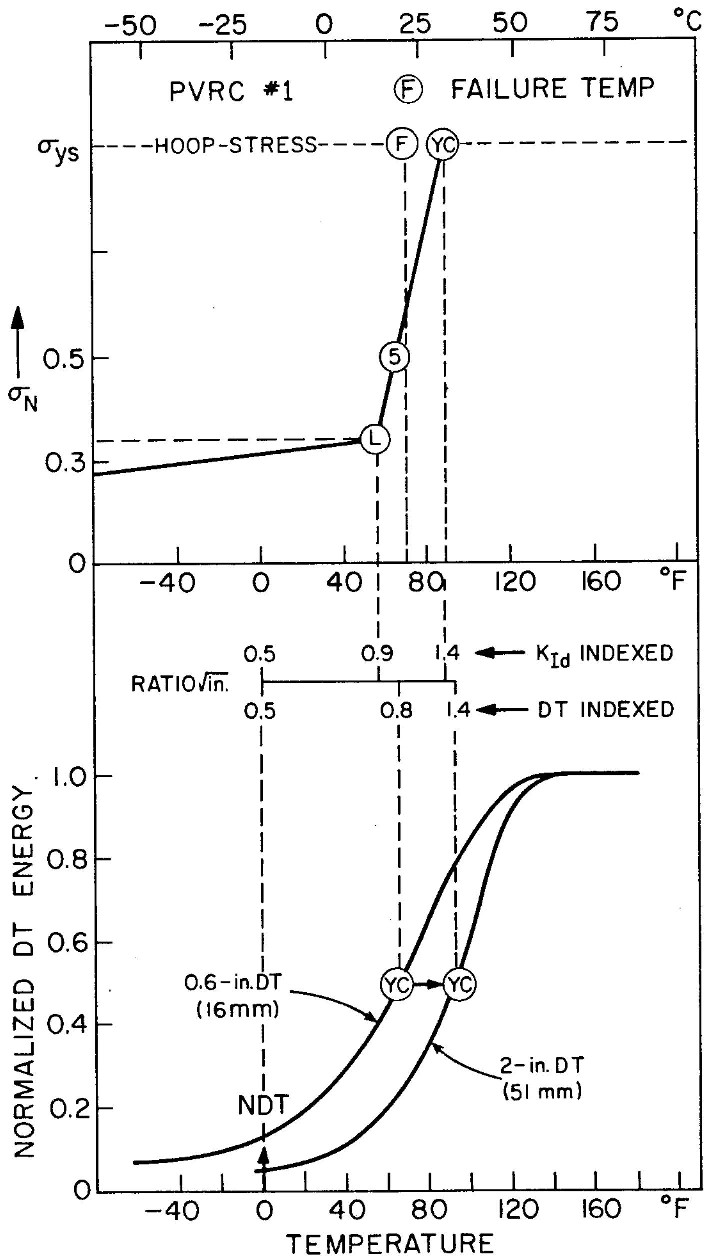

Fig. 9 — The vessel fracture illustrated in Fig. 8 is examined in terms of the L and YC indexing procedures as defined with full-thickness DT tests of the vessel material. The unstable fracture at yield stress level was expected in that the failure point lies above the line defining leak-before-break behavior.

must be satisfied, where K_{IR} is considered a lower bound curve of existing K_{Ic} and K_{Id} data for nuclear pressure vessel steels. This curve is compared with the K_{Id} curve for HSST plate* in Fig. 10, bottom, and is referenced to the NDT temperature of the particular steel. Equation (3) defines the lowest allowable temperature of operation. The actual operation of nuclear vessels, however, is governed by specific AEC criteria that are generally more conservative than the requirements of the ASME Code.

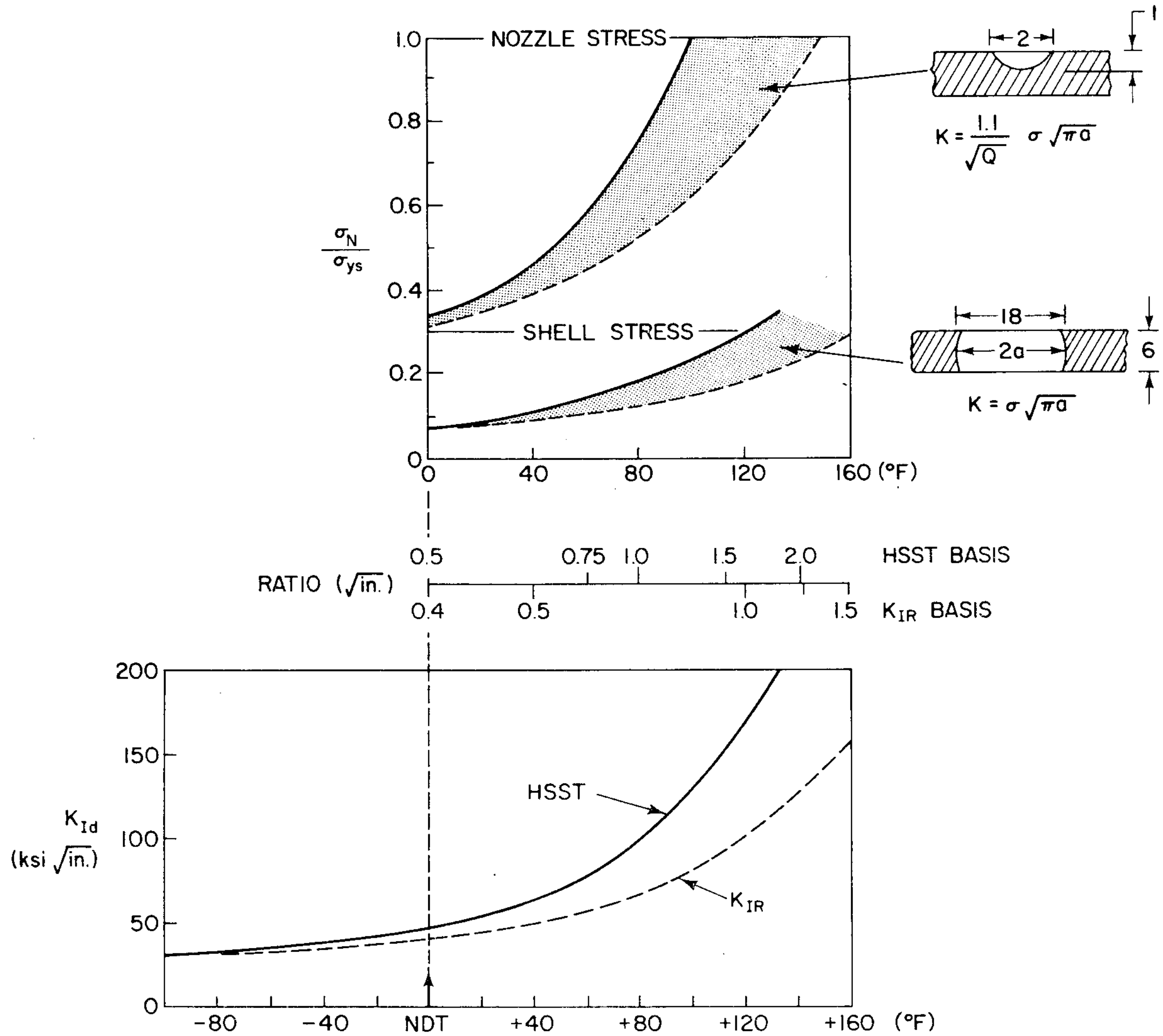

Fig. 10 — The K_{Id} curves defined by the ASME Code (K_{IR}) and by data from the HSST plate (HSST) were used to compute the critical flaw size for fracture vs temperature for two types of flaws. The ratio scale was computed by assuming a dynamic yield stress of 100 ksi (70 kg/mm^2). The intersection of the shaded area with the horizontal line (indicating the expected stress level) defines the lowest temperature permitted in the plane strain regime.

*Note that the HSST curve has been considered for all of the analysis procedures described in this paper.

Without regard to the ASME criteria, Fig. 10 illustrates the lowest permissible pressurization temperature in the LEFM regime in terms of the K_{Id} curve and the nominal stress in a vessel for two types of flaws. The top portion of the figure defines the critical stress versus temperature relation for a surface flaw in the nozzle region and for a through-thickness flaw in a 6-in.(152-mm)-thick shell region. The expected stress levels in each region during full-power operation are indicated by the horizontal lines. The curves were derived using the K_{Id} trends and the equations shown in the figure for a yield stress of 70 ksi (49 kg/mm^2). Thus for a small (1 in. or 25 mm deep) flaw in the nozzle at yield stress loading, the metal temperature during full-power operation must be maintained at approximately 150°F (83°C) above NDT* for material exhibiting a "K_{IR}-type" curve; use of a material that exhibits a K_{Id} curve labeled "HSST" permits a somewhat lower temperature. To prevent propagation of a through-thickness flaw in the shell region for K_{IR}-type metal, Fig. 10 shows that the temperature must be maintained above NDT+ 160°F (89°C) for an assumed shell stress of 0.3 σ_{ys}.

This analysis further implies that during normal plant startup or shutdown, the minimum required temperature at a given pressure could be reduced because the full operating stress level has not been achieved. However, resolution of this point is complex. As with any fracture prevention analysis, the reliability depends on accurate knowledge of the stress level, as well as on the material toughness at all points. To justify lower operating temperatures during startup, one must, for example, rationalize a fatigue crack growing into a region of low toughness (such as a slag inclusion) and triggering an unstable crack front extension such that the crack grows to critical size in the virgin metal at the reduced stress level.

Finally, the significance of temperature variations to the critical flaw size can be easily demonstrated in terms of the L and YC ratios. Using the methods described in the previous section for a 6-in. (152-mm) thickness, the computed L and YC ratios are 1.55 to 2.45 $\sqrt{\text{in.}}$, respectively. Extrapolating the ratio scale in Fig. 10 from 2.0 to 2.45, it can be seen that the L-to-YC transition evolves in less than 50°F (28°C). Because this temperature increment is so small, any margin of error in choosing an operating temperature within this increment signifies a large change in fracture toughness, and therefore a large change in fracture-safe assurance.

The sharp transition in fracture behavior with temperature is also significant to the analysis of data scatter which indicates variations in K_{Id} curves from different heats of the same material. Consider the K_{Id} curves in Fig. 10 to represent those obtained with two different heats of A533-B steel, one heat equivalent to HSST material and the other heat representing a material whose K_{Id} versus temperature curve is equivalent to the K_{IR} curve. Note that at 160°F the ratio for K_{IR} material is 1.5 $\sqrt{\text{in.}}$, whereas that for the HSST-type material is approximately 2.5 $\sqrt{\text{in.}}$ Also note that this variation in ratios at a given temperature corresponds to the L-to-YC transition (i.e., plane strain to yield performance) for a 6-in. thickness.

In view of this description of the nature of pressure vessel material, it would appear prudent to operate in a temperature regime where yielding can be tolerated. Imposition

*The equation for the nozzle in Fig. 10 is approximate; refined approximations are presented in Ref. 14.

of a failure prevention criterion that requires ductile behavior provides an almost "step-function" increase in assurance from brittle fracture, and it could help to minimize the effect of errors in computing the K levels in Eq. (3), such as determination of K_{Id} by nonstandard methods and the failure to include the localized residual stresses in welded portions of the vessel.

Within this framework, the minimum operating temperatures obtained with the ASME criteria and the DT procedures are now examined. For the ASME criteria, Eq. (3) is computed for a flaw in the cylindrical section of a 6-in.(152-mm)-thick and a 10-in.(254-mm)-thick vessel. For these vessels, respective K_{It} values of 4 and 10 ksi $\sqrt{in.}$ are assumed (14), along with a shell stress level of 20 ksi at the operating pressure.* Assuming the above stress value to apply during full-power operation, the minimum allowable temperatures using Eq. (3) and the K_{IR} curve are 112°F (62°C) and 140°F (78°C) above the NDT for wall thicknesses of 6 in. (152 mm) and 10 in. (254 mm), respectively.† It appears that the above temperatures will not exceed a YC temperature as determined from the DT test. Specifically, from the 12-in.(305-mm)-thick DT data for HSST-type material (Figs. 1 and 6), the YC temperature from the DT midrange is approximately 175°F (97°C) above NDT. Had K_{IR}-type material been tested, the DT midenergy undoubtedly would have been somewhat higher, as suggested by the shifts in the HSST and K_{IR} curves in Fig. 10, bottom. Considering the displacement of the K_{IR} curves in Fig. 10, a YC temperature for the latter steel is estimated to be 215°F (102°C), or 40°F (22°C) higher than for the HSST-type material.

To properly relate the ASME toughness requirements to the expected fracture behavior of a vessel, the actual conditions of water-cooled reactor vessel operation must be highlighted. The vessel of a pressurized water reactor may be brought to a temperature close to 500°F (260°C)‡ while the core is still subcritical; this is accomplished using the heat generated by the massive coolant pumps. The practical result if this mode of operation is adhered to is to "override" the fracture toughness requirements, and all operations with the core critical can take place above a YC temperature. Neutron bombardment of the vessel wall during service will elevate the NDT temperature, and therefore the minimum operating temperature as well as the YC temperature. However, this elevation in minimum required temperature is not expected to exceed the temperature of 500°F obtained during normal startup operations. This, however, may cause concern for some of the older plants that cannot achieve temperatures on the order of 500°F with pump heat and, at the same time, exhibit a "sensitivity" to neutron irradiation that could elevate the NDT temperature by 200 to 300°F (111-167°C).

The boiling water reactor (BWR) plants must be considered differently from the PWR types. Unfortunately, pump heat is insufficient to bring the reactor vessel to a temperature that greatly exceeds 150°F (66°C). These vessels attain operating temperatures

*This stress level has been calculated for commercial pressurized water reactors (PWR) from the tabulation of Whitman (15). The shell stress due to pressure in large PWR exceeds 22 ksi; this corresponds to 0.3 to 0.4 σ_{ys} for the unirradiated material.

†Using the ASME Code allowable membrane stress of 26.7 ksi (18.8 kg/mm^2), the minimum allowable temperatures are 138°F (59°C) and 163°F (73°C) for 6-in. and 10-in. wall thicknesses, respectively.

‡This statement applies to plants currently being built; earlier plants may not be able to exceed a temperature on the order of 350°F (177°C) using pump heat.

using nuclear heat. Fortunately, the pressure remains atmospheric in a BWR plant at temperatures less than 212°F (100°C). This means that there is negligible stress which could propagate a fracture even though the plant is being brought to temperature in a regime in which the metal could behave in a brittle manner under an emergency situation having very low probability of occurrence. In addition, existing* BWR plants are expected to experience relatively little elevation in the NDT temperature due to irradiation. Thus the startup procedures for these plants as a whole would be expected to result in the vessel being pressurized below a YC temperature, but for only a very small fraction of their total operating time.

SUMMARY

Fracture-safe operation of commercial nuclear power plants must be achieved through compliance with various codes and standards as set forth by regulatory agencies. The ASME fracture toughness requirements have recently been revised to reflect improved methods of toughness characterization. Significant portions of the current requirements are based on LEFM methods as described in the 1972 ASME Code revision. This fact presents an apparent inconsistency with the best material performance that is attainable. On the one hand it is desirable to achieve fracture safety knowing that the metal can withstand some degree of gross plastic strain in the presence of a flaw; this provides a built-in safety margin against unforeseen circumstances. Yet on the other hand, LEFM, per se, cannot provide this assurance since this method is applicable only to plane strain (brittle) metals.

Clearly, an expansion of existing codes is desirable in order to give the designer and regulatory agency the option of choosing the reliability level of the structure. Three broad reliability levels have been outlined on the basis of fracture mechanics (plane strain, elastic-plastic, and plastic) that reflect the temperature-dependent fracture state transition that characterizes nuclear structural metals. Thus, if the designer seeks the assurance of leak-before-fail behavior, a toughness level in the elastic-plastic regime is required; plane strain metals generally will not suffice. This fact emphasizes the dominant role which must be assigned to the choice of material in achieving a particular reliability level.

Modernization of nuclear codes and standards should incorporate the full range of fracture mechanics options that include elastic-plastic and fully plastic fracture mechanics, as well as linear elastic procedures. Different criteria should then be formulated for the different levels of performance desired. In this way the risks associated with the structure can be logically related to the probability and consequences of failure.

Interpretative procedures based on the DT test and the K_{Id}/σ_{yd} ratio have been illustrated to define the full range of toughness associated with the fracture state transition. Emphasis was placed on a ratio definition of the limit of plane strain applicability (L) and also a yield criterion (YC) related to the upper limit of the elastic-plastic regime. These index ratios have a definite meaning in terms of structural performance and can be defined with the DT test. It should be realized that a change in ratio bears a fixed relationship to toughness variations resulting from changes in temperature so that specification of one

*Projected BWR plants may experience greater elevations in the NDT temperature of the vessel wall than the current generation.

is identical to specification of the other. In addition, a small DT specimen (5/8 in. or 16 mm thick) is sufficient to index the metal quality; a temperature translation of this curve then can be used to define the mechanical constraint imposed by thick sections, thereby reducing the need to conduct large-size tests such as would be required to establish the curve of K_{Id} versus temperature.

The current ASME fracture toughness criteria have been examined on the basis of reliability levels they project. These criteria, as based on LEFM principles, do not consistently specify a YC type of fracture toughness. However, it is expected that YC behavior for BWR and PWR plants currently being constructed will, nevertheless, be achieved through the nature of their startup specifications.

It has been emphasized that the L-to-YC constraint transition evolves sharply within a temperature increment as small as 50°F (28°C) for 6 in. thicknesses. This temperature increment translates to a large increase in the ratio K_{Id}/σ_{yd} and thus to a large increase in reliability level. Finally, statistical (heat-to-heat) variations in toughness can have a major influence on the fracture behavior. For a given set of conditions within the temperature-constraint transition, one heat may result in plane strain behavior while another provides leak-before-fail reliability.

It appears that structures operated in accordance with the ASME fracture toughness requirements could fail in a brittle manner if subjected to unforeseen loads and flaw sizes, or if the minimum material toughness is not determined precisely. There is no commensurate ASME criteria with which the designer can impose ductile behavior when the structure is subjected to the same unforeseen circumstances.

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