ASSESSING THE IMPACT OF POTENTIAL REGULATORY CHANGES ON DEMONSTRATING

COMPLIANCE WITH REACTOR PRESSURE VESSEL INTEGRITY REQUIREMENTS

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A Thesis

Presented to

The Faculty of the Graduate School

At the University of Missouri-Columbia

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In Partial Fulfillment

Of the Requirements for the Degree

Master of Science

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By

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July 2016

The undersigned, appointed by the dean of the Graduate School, have examined the thesis entitled

ASSESSING THE IMPACT OF POTENTIAL REGULATORY CHANGES ON DEMONSTRATING

COMPLIANCE WITH REACTOR PRESSURE VESSEL INTEGRITY REQUIREMENTS

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ACKNOWLEDGEMENTS

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TERMS AND ABBREVIATIONS

ART	Adjusted Reference Temperature
ASME	American Society of Mechanical Engineers
ASTM	ASTM International, formerly American Society of Testing and Materials
BPVC	Boiler and Pressure Vessel Code
CF	Chemistry Factor
CFR	Code of Federal Regulations
CRP	Copper-Rich Precipitates
C_v	Charpy V-Notch
FF	Fluence Factor
K_0	Median Compact Tension Fracture Toughness Value
K_{IC}	Linear-Elastic Crack Initiation Static Fracture Toughness
K_{JC}	Elastic-Plastic Crack Initiation Static Fracture Toughness
MeV	Mega Electron-Volt
MF	Matrix Features
NDTT	Nil-Ductility Transition Temperature
NRC	Nuclear Regulatory Commission
PTLR	Pressure-Temperature Limits Report
PTS	Pressurized Thermal Shock
RPV	Reactor Pressure Vessel
RT_{T0}	Reference Fracture Toughness Temperature
RT_{NDT}	Reference Nil-Ductility Transition Temperature

T_0 100 MPa√m Fracture Toughness Temperature

T_{NDT} Nil-Ductility Transition Temperature Value

T_{cv} 50 ft-lb and 35 Mil Lateral Expansion Transition Temperature Value

TTS   Transition Temperature Shift (30 ft-lb)

USE   Upper Shelf Energy

ΔRT_{NDT} 30 ft-lb Transition Temperature Shift Value

ΔUSE Change in Upper Shelf Energy

Chapter 1: Introduction

Of all of the myriad of interconnected parts in a nuclear power plant, there is one that stands out as essential to safe operation: the reactor pressure vessel (RPV). By enclosing the reactor core, the reactor pressure vessel serves both as the central object in the reactor coolant system and as a crucial line of defense against the release of radioactive material. Because the complexity and expense involved in RPV fabrication makes it prohibitively expensive to replace, the lifetime of the RPV is often synonymous with the lifetime of the entire power facility. As a result, many millions of dollars can hinge on a utility's ability to demonstrate the continued structural integrity of a reactor pressure vessel.

Of course, the job a facility operator is tasked with is not simply reassuring itself that the systems it employs are safe, but proving to the Nuclear Regulatory Commission (NRC) and to the public that there is acceptable risk associated with continued plant operation. This places a rather extensive list of requirements on nuclear facilities, and furthermore raises the possibility that additional restrictions on operation may be implemented over time. Therefore, any ability to anticipate the actions of the NRC on the part of utilities can be beneficial.

These actions can generally be considered either *continued mandates*, *additional restrictions*, or *allowances*. A continued mandate is any existing regulation that is not expected to be completely replaced or eliminated by future regulatory actions. An additional restriction could be considered any changes in the regulatory environment that places limits on operations that were not previously in existence; these restrictions may not be explicitly stated, but could be consequences of changes that are neutral in appearance, such as an alteration to the way certain

values must be calculated. An allowance is any change or addition that allows for less restrictive operations than would otherwise be necessitated by current or future mandates. These allowances may be either broadly applicable, or expected to be applied on case-by-case basis. A single regulatory change could potentially incorporate aspects of all three of these attributes, in which case an effort must be made to determine implications for continued operation.

In order for utilities to continue to operate, they must understand the current state of regulations concerning reactor pressure vessel integrity, demonstrate compliance with existing regulatory requirements, and develop a strategy for continued assessment, monitoring and response.

Chapter 2: Current Nuclear Regulatory Commission Requirements for Integrity

The principle requirements for RPV integrity mandated by the NRC are as follows:

1. The Upper Shelf Energy of all reactor pressure vessel materials must be at least 75 ft-lbs at the beginning of the reactor lifetime, and remain above 50 ft-lbs throughout its period of operation.[1]

2. The Reference Nil-Ductility Transition Temperature (RT_{NDT}) must remain below the Pressurized Thermal Shock (PTS) screening criteria of 270°F for plates, forgings and axial weld materials, and 300°F for circumferential weld materials to continue operation without justification.[3]

3. The Pressure-Temperature Limits for reactor operation must be drawn in accordance with limits in 10 CFR 50[1] and ASME Boiler and Pressure Vessel Code (BPVC) Section XI Appendix G.[13] Changing these limits once required amendment to the operating license, but are now submitted as a separate report.[27]

4. A reactor surveillance program must be established according to the specifications of any edition of ASTM E185[4] not published after 1982 in order to verify compliance with the aforementioned[2] regulations.

5. The methods for determining the radiation-induced changes in these material properties described in Regulatory Guide 1.99 Revision 2[28] are considered acceptable to the NRC (whereas alternative methods may be acceptable given adequate justification for their use).

2.1 Background

The attribute of primary importance when evaluating the integrity of RPV material is static fracture toughness. In the early days of nuclear power, the mechanisms involved in fracture initiation and propagation were not well understood, and direct measurements of static fracture toughness of irradiated materials were prohibitively difficult as a result of the size requirements of the testing materials. Instead, extensive testing of irradiated steels was performed using methods developed at the U.S. Naval Research Laboratory for determining the likelihood of brittle failure in shipbuilding materials.[20]

The importance of avoiding conditions where brittle fracture is possible cannot be understated. In the brittle regime, small flaws can propagate very quickly under low stresses that would normally be tolerated by the material. Because a brittle material is defined by having little or no plastic deformation before failure, the energy released by crack initiation can cause continued material damage without being dissipated by inelastic forces. This can happen fast enough that no efforts can be made to avert catastrophic failure of the structure in question, and the lack of prior bulging or deformation that would occur prior to ductile failure of material means that such a breach would be extremely difficult to predict or prepare for.

Thus, the criteria for operation in early commercial facilities was relatively simple: ensure that pressure vessels operated above a maximum temperature where brittle failure of materials was possible. A nil-ductility transition temperature (NDTT) where completely brittle failure begins to occur could be somewhat easily determined from the results of drop-weight tests, which were closely correlated with the results of Charpy V-Notch (C_V) tests, the specimens for which were small enough to fit within a nuclear reactor. The NDTT + 60°F was generally

considered to be an acceptable lower bound on operation temperature by the United States Navy Bureau of Ships.[20] Although additional conservatisms for operating limits have been added since then, the testing methods required have not changed significantly.

2.2 Reference Nil-Ductility Transition Temperature

An important value used to determine the susceptibility of a material to catastrophic failure in any given application and closely related to the NDTT is the Reference Nil-Ductility Transition Temperature (RT_{NDT}). This value is now used in place of the NDTT as a zero-ductility reference point to ensure that the shape of the reference fracture toughness curve used for creating appropriate pressure and temperature limits[13] is appropriately conservative as an estimation of the real fracture toughness curve of the material. In order to establish RT_{NDT}, the following procedure[11] shall be used:

1. A temperature T_{NDT} at or above the nil-ductility transition temperature of drop-weight tests shall be selected.

2. Each specimen of the C_V test should show 35 mils (.89 mm) lateral expansion and at least 50 ft-lbs (68 J) of absorbed energy within 60°F of T_{NDT}. Provided these conditions are met, T_{NDT} is the reference temperature RT_{NDT}.

3. If these conditions are not met, additional C_V tests shall be conducted until a temperature T_{Cv} at which they are met is established. In this case the reference temperature RT_{NDT} shall be $T_{Cv} - 60°F$.

2.3 Upper Shelf Energy

The upper shelf energy (USE) can be defined as the energy absorbed by a specimen in a Charpy V-Notch test at the temperature at which the material becomes fully ductile. Because operation in this completely ductile range is preferred, the USE is representative of the maximum fracture toughness that one could expect from a particular material. The initial USE is guaranteed to be greater than 50 ft-lbs by the definition of RT_{NDT}, which requires an energy absorption of at least 50 ft-lbs within 60°F of said reference temperature. As mentioned previously, this is important for ensuring the shape of the reference fracture toughness curve is conservative.

When determining the shift in RT_{NDT}, however, the quantity of interest is the change in the 30 ft-lb transition temperature. Although this is a better way of determining the shift than a measurement of the 50 ft-lb transition temperature, one must still verify that there *is* a 50 ft-lb transition temperature to ensure conservatism. Because the upper shelf energy is typically the maximum energy that a sample will absorb at any temperature, any measurement of the USE over 50 ft-lbs guarantees that this transition temperature does in fact exist. Hence, a requirement that the USE be tested and maintained above 50 ft-lbs throughout the reactor lifetime is mandated.

2.4 Charpy V-Notch

The Charpy V-Notch test is an instrumented impact test useful for determining the bulk

mechanical behavior of a material. Although it cannot be utilized effectively to determine

fundamental physical properties, it is extremely useful for comparative analysis of materials in

engineering applications. The Charpy V-Notch (C_V) test is frequently used to measure values

relating to ductility and absorption energy to input into calculations that ensure that a given

structural material conforms to industry best-practices and has a low likelihood of sudden
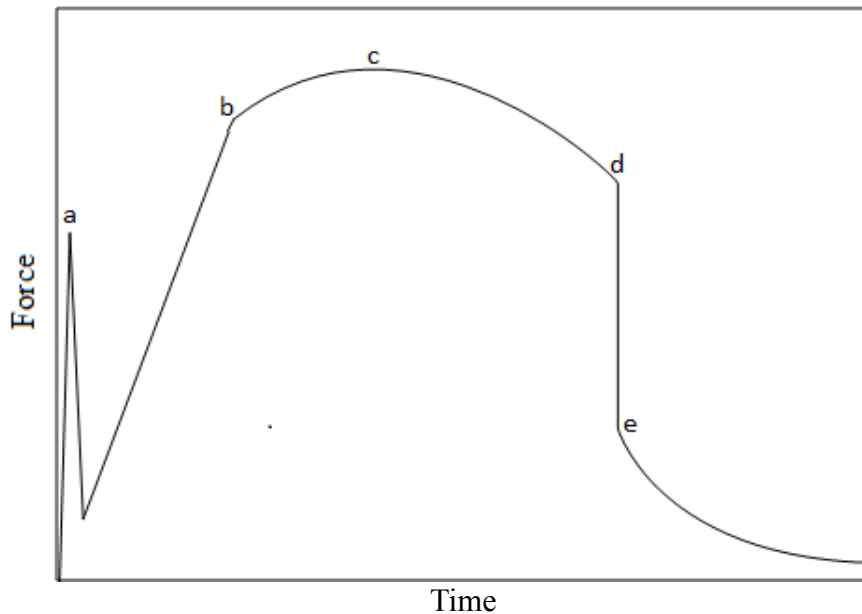
catastrophic failure.

The testing apparatus consists of a weighted pendulum, the path of which intersects the

area of a material sample held between two ends of an anvil. A standardized notched specimen [6]

is held by the anvil and broken by the falling pendulum. The energy absorbed by the material can

then be obtained by observing the height that the pendulum reaches after contact with the test

specimen in comparison to the starting height, using the equation $W = mg(h_i - h_f)$. The remains

of the specimen can be visually examined to determine the approximate percentage of ductile

fracture and the lateral expansion of the material. A clean break is indicative of completely non-

ductile fracture, while stretching and lateral distortion signify ductility. The test is only valid if

the sample is broken, so the energy of the pendulum must be high enough to ensure that this

occurs.

A modern Charpy V-Notch testing device is also outfitted with a piezoelectric sensor in

the striking location of the pendulum. The voltage output from this sensor is directly proportional

to the force exerted on the hammer by the test specimen. This force can be integrated over time

to find the velocity of the pendulum, which can again be integrated over time to obtain the distance traveled. The integral of force over the total distance traveled gives a value W for the work done by the pendulum on the test sample, which should be in relative agreement with the value obtained by observing the difference in energy of the pendulum before and after contact with the sample.[8]

The typical force-distance diagram for a C_V test on a partially ductile material would consist of an initial spike (a) when the pendulum first contacts the specimen, followed by a linear increase in force (representing the elastic expansion of the material) up to the elastic yield point (b) and subsequent non-linear increase in force as plastic expansion begins to take place. An area of peak force (c) is reached before continued plastic deformation begins to relieve forces on the specimen. This is followed by a sudden precipitous drop in applied force as the crack begins to propagate ahead of the striker (d), followed shortly by a period of steadily decreasing force as crack propagation is arrested (e) and the striker breaks its way through the rest of the specimen.

Figure 2.4 Charpy V-Notch Load-Time Curve

This sudden drop in force is indicative of a lack of ductility in the specimen, and is a more reliable way to determine the percentage of ductile shear fracture of the material than visual examination of the fractured specimen, particularly for radioactive specimens which cannot be interacted with directly. The temperature at which the drop in load disappears completely is considered a reliable indication of the onset of the upper shelf.[29] A period of crack propagation will not be present in a completely ductile material, and brittle, completely non-ductile materials such as ceramics will often exhibit crack propagation without undergoing any inelastic expansion. The percentage shear fracture is typically calculated by an equation such as *%shear = (1 − (F_d − F_e) / F_c) x 100* where F is the force at the corresponding point on a force-displacement curve.[8]

The properties of the material as determined by the C_v test may also depend on the orientation of the major working direction of the material with respect to the longitudinal axis of the specimen. Plate specimens oriented with the major working direction along the axis of the specimen are called longitudinal and have "strong" properties, whereas specimens oriented with the major working directions of the plate perpendicular to the axis of the specimen are called transverse and have "weak" properties.[7] Although the major stresses are in the strong direction for the axial flaw postulated by Section XI appendix G[13,18], ASME guidelines[11] require tests in the weak direction, so tests in both strong and weak directions are often performed.

2. 5 Drop-Weight Test

A drop-weight test is an impact test used to obtain a nil-ductility transition temperature (NDTT). The test consists of a machined specimen placed on an anvil under a suspended weight. This weight is then dropped to induce cracking in the specimen material. The temperature of the specimens is varied to determine the point of transition between temperature regimes where a particular material will or will not break.

The specimen itself must be properly machined to prevent overheating and resultant changes in the as-fabricated properties of the material. The sample is specially prepared to facilitate cracking with a weld bead that extends the entire length of the longitudinal axis of the tension surface of the specimen. The weld is then carefully notched in the transverse direction; care must be taken that the surface of the specimen is not altered during the notching of the weld.

The sample is placed on an anvil with a backstop appropriate to the specimen type (a function of the yield strength of the material). The energy of the weight also corresponds to the specimen type, but must meet additional requirements to ensure accuracy in determining NDTT.[5] Upon striking the compression surface with the free-falling weight, the specimen must show crack initiation and contact with the anvil stop on the tension surface. If either of these requirements are not met, the energy must be increased in increments appropriate to the specimen type until these requirements are met.

The selection of the specimen temperature should be such that a 10°F (5°C) interval between which the specimen transitions from a lower temperature where a break is exhibited to a higher temperature where a break is not exhibited. A break in this context should be considered

any occasion where the specimen is fractured to one or both ends of the tension surface. A test is considered no-break if the specimen is cracked but the crack does not propagate to either end of the tension surface, and no-test if the weld bead does not crack or the tension surface does not contact the anvil stop.[5]

2.6 Adjusted Reference Temperature

For applications in radiation environments, particularly in a reactor pressure vessel beltline (the cylindrical portion closest to the reactor core), the measured RT_{NDT} of structural materials at the beginning of the operational lifetime must be adjusted to account for the imbrittlement effects of high-energy neutron bombardment. In practice, the destructive nature of Charpy V-Notch and drop weight tests make it impossible to directly determine the RT_{NDT} of reactor beltline materials. The ductility properties of reactor beltline materials at a given point in the reactor lifetime must also be known well before the fact in order to determine the practical limits that these properties impose on facility operations and to ensure public safety. Thus, some form of predictive equation is needed to account for these effects.

This need is satisfied by the concept of Adjusted Reference Temperature (ART), which is given by the following equation ART = Initial $RT_{NDT} + \Delta RT_{NDT}$ + Margin. The Initial RT_{NDT} is the value of RT_{NDT} prior to irradiation determined by drop-weight and Charpy V-Notch tests as defined in Paragraph 2331 of Section III in the ASME Boiler and Pressure Vessel Code.[12] ΔRT_{NDT} is the (always positive) adjustment to the initial reference temperature; this value is typically obtained using a predictive formula calibrated to match the results of destructive testing as closely as possible. The NRC currently requires the use of methods outlined in Regulatory Guide 1.99 Revision 2.[28]

2.7 Regulatory Guide 1.99 Revision 2

Using the NRC-approved methods in Regulatory Guide 1.99 Revision 2, the ART can be calculated as a product of a chemistry factor (CF), a function of the copper and nickel content of the material, and a fluence factor (FF), a function of fluence.

The equation for fluence factor is $[f^{(0.28 - .10 \log f)}]$ where f is the fluence (10^{19} n/cm^2, E > 1 MeV) at a given depth (typically the deepest point of a ¼ thickness flaw from either side of a pressure vessel wall).[28] This semi-empirical equation was established by a combination of methods derived from the analysis of results from test capsules irradiated prior to RG 1.99 R2's release in 1988.[25, 26]

There are two ways of calculating the chemistry factor, and both must be taken into account to fulfill regulatory requirements for the monitoring of pressure vessel integrity. The value used for the chemistry factor initially is a function of the weight percent of copper and nickel in the material in question, and can be found by using the appropriate table in Regulatory Guide 1.99, Revision 2.[28] Once two or more capsules from the reactor vessel surveillance program[2, 4] have been removed,[10] a second value of CF must also be taken into consideration.

This second value is obtained by taking the measurement of ΔRT_{NDT} from irradiated specimens with a known fluence that have been removed from reactor surveillance capsules. 10 CFR 50 Appendix G[1] defines the ΔRT_{NDT} for these materials to be the increase in the 30 ft-lb (41 J) index temperatures from average Charpy V-Notch curves before and after irradiation. This value of ΔRT_{NDT} is not, however, directly used in the calculation of ART. Rather, all of the samples of a certain type (i.e., same plate or weld) in a given capsule are tested and an average

increase in 30 ft-lb temperature for each is determined.

Once there are two or more capsules, the measured ΔRT_{NDT} vs. FF is plotted on a graph. A least-squares line is then fitted to this graph, the slope of which is the chemistry factor of the cumulative surveillance data. Provided that the standard deviation from this line is less than 17°F for base metals and 28°F for welds (or equivalently, more than 68% of measured capsule results of a given type are within this distance from the value that would be predicted for that fluence using the obtained chemistry factor), the data set is considered credible and the value obtained for CF must be considered in the Pressure-Temperature Limits Report (PTLR).[28]

2.8 Pressure-Temperature Limits Report

The Nuclear Regulatory Commission requires that pressure limits on reactor operations be established in order to ensure the structural integrity of the reactor pressure vessel for any given temperature value and heatup/cooldown rate. Initially, this meant that these limits must be contained within the technical specifications of the license itself.

As of the issuance of NRC Generic Letter 96-03,[27] the NRC allows for the pressure and temperature limits established in the technical specifications of the plant operating license to be relocated to a licensee controlled Pressure-Temperature Limits Report document which may be updated for each fluence period using a methodology approved in the technical specifications. (This change was made to reduce the number of burdensome license amendment requests that must be processed by the licensee and the NRC for an essentially time-dependent set of limits.)

The methodology referenced by the standard technical specifications for Westinghouse plants and approved by the NRC for pressurized water reactors such as the one at Callaway Nuclear Generating Station is WCAP-14040-A.[15] This sequence of steps that must be taken in order to establish reliable and conservative limits on reactor operations is a synthesis of 10 CFR 50 Appendices G[1] and H,[2] Regulatory Guide 1.99 Revision 2,[28] and the technical standards that they reference, notably Section XI Appendix G of the American Society of Mechanical Engineers (ASME) Boiler and Pressure Vessel Code (BPVC).[13]

Once the fluence of all beltline materials (a somewhat complex integration of reactor operating time and the unique space-dependent characteristics of the individual reactor) has been calculated, a limiting value for ART (i.e., that of the material which would give the strictest P-T

limits) can be obtained. This is done by multiplying the aforementioned fluence factor (a function of fluence) with the chemistry factor and adding this to the initial RT_{NDT} obtained from material testing done prior to reactor initialization, along with an additional margin for error.

If credible data from two or more surveillance capsules is available, it must be determined whether the CF and margin obtained by a least-squares fit present a more limiting (higher) value of ART than would be obtained using the CF and margin from tables provided in Regulatory Guide 1.99. If surveillance data results in a higher ART for the limiting beltline material, then that ART must be used. If surveillance data results in a lower ART for the limiting material, then either ART may be used. In the instance that surveillance data are not available or the cumulative surveillance data are or become non-credible, the chemistry factor from Regulatory Guide 1.99 must be used. The end of lifetime ART (RT_{PTS}) must also stay below the Pressurized Thermal Shock screening criterion of 270°F for plates, forgings, and axial weld materials and 300°F for circumferential weld materials in order to continue operating without justification.[3]

Once a limiting ART for a given fluence period has been established, full pressure-temperature curves must be drawn in accordance with the requirements of 10 CFR 50 Appendix G using the NRC approved methodology specified in the facility operating license such as the one described in WCAP-14040-A. The requirements of 10 CFR 50 Appendix G are as follows:

Table 2.8 NRC Operating Limits

Operating condition	Vessel pressure[1]	Requirements for pressure-temperature limits	Minimum temperature requirements
1. Hydrostatic pressure and leak tests (core is not critical):			
1.a Fuel in the vessel	≤20%	ASME Appendix G Limits	[2]
1.b Fuel in the vessel	>20%	ASME Appendix G Limits	[2] +90 °F[6]
1.c No fuel in the vessel (Preservice Hydrotest Only)	ALL	(Not Applicable)	[3] +60 °F
2. Normal operation (incl. heat-up and cool-down), including anticipated operational occurrences:			
2.a Core not critical	≤20%	ASME Appendix G Limits	[2]
2.b Core not critical	>20%	ASME Appendix G Limits	[2] + 120 °F[6]
2.c Core critical	≤20%	ASME Appendix G Limits + 40 °F.	Larger of [([4])] or [([2]) + 40 °F.]
2.d Core critical	>20%	ASME Appendix G Limits + 40 °F.	Larger of [([4])] or [([2])+160 °F]
2.e Core critical for BWR ([5])	≤20%	ASME Appendix G Limits + 40 °F.	[2]+60 °F

[1] Percent of the preservice system hydrostatic test pressure.

[2] The highest reference temperature of the material in the closure flange region that is highly stressed by the bolt preload.

[3] The highest reference temperature of the vessel.

[4] The minimum permissible temperature for the inservice system hydrostatic pressure test.

[5] For boiling water reactors (BWR) with water level within the normal range for power operation.

[6] Lower temperatures are permissible if they can be justified by showing that the margins of safety of the controlling region are equivalent to those required for the beltline when it is controlling.

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Chapter 3: Potential Changes to Fracture Toughness Evaluation Procedures

3.1 Information Fusion Model

In 2002, J. A. Wang and N. S. Rao published a new approach to determining the shift in the Charpy V-Notch 30 ft-lb transition temperature.[24] The authors recognized that, of a very large number of models available for use in predicting the transition temperature shift (TTS), it is infrequent that one in particular will significantly outperform the others in reducing the uncertainty associated with projecting RPV embrittlement. These models can generally be divided into two groups: domain models and non-linear estimators.

Domain models in this case are essentially "physics" models, more or less derived from an understanding of the processes that lead to radiation embrittlement. These models typically have easily identifiable terms associated with specific contributors to embrittlement such as the formation of carbon-rich precipitates or grain boundary strengthening. Although this makes them at least partially dependent on the current state of radiation embrittlement theory, individual factors in the formula are usually weighted to reflect surveillance data results. Thus, although some aspects of the general shape of the prediction formulas are determined *a priori*, these semi-empirical equations are predictive of real embrittlement behavior.

Non-linear estimators, on the other hand, are purely empirical regression models developed by applying a particular statistical technique (Nearest-Neighbor Regression, neural networks, etc.) to a large set of reactor embrittlement data. The resulting formulas neither require nor exhibit an understanding of the underlying reasoning behind the embrittlement of materials

subjected to radiation. This makes them resistant to flaws in the current state of understanding, but less informative and more dependent on the quality of available test data.

In order to solve these problems, Wang and Rao developed a method[24] for fusing various models together. They combine these estimators linearly by giving each one a weight for a localized domain, the weight having been determined by a program that systematically reduces uncertainty for that domain by training with reactor surveillance data results. Thus, for a particular interval of each of the input variables (e.g., nickel content from .07-.08%, copper content from .04-.05%, fluence from 2.0-2.1x10^19 >1MeV), each model will be given a certain weight based on how accurately it predicts surveillance results within that domain. These weighted domains are then strung together across the entirety of transition temperature shift inputs to produce a single multidimensional curve which is guaranteed to exhibit reduced statistical uncertainty compared to any particular model.

3.2 Eason Model

One model under consideration for use in updating Regulatory Guide 1.99 was prepared by E. D. Eason, G. R. Odette, R. K. Nanstad, and T. Yamamoto for the Nuclear Regulatory Commission in 2007.[14] This model represents an attempt by the NRC to have a more physically descriptive approach to predicting the TTS than was possible in 1988 when Regulatory Guide 1.99 Revision 2 was released. Since then, the understanding of the physical mechanisms behind radiation embrittlement, while not complete, has improved considerably.

Although there are numerous processes involved in radiation embrittlement, the TTS can be accurately modeled as a summation of a term for Copper-Rich Precipitates (CRP) and a term for Matrix Features (MF). In typical reactor pressure vessel steels, residual copper from stress-relief treatment is supersaturated at plant operating temperatures. As a result, copper tends to precipitate out of the matrix into its own body-centered cubic crystal. Although this also occurs in non-irradiated steels, it tends to be very slow, as substitutional solutes depend on a vacancy-exchange for diffusion. In an irradiated steel, the proportion of vacancies per lattice atom can be significantly higher than the equilibrium number of vacancies in a non-irradiated steel. Because the number of vacancies in a steady state increases with higher flux, and the proximity to that steady state increases with increased fluence, the total number of vacancies is dependent on both factors, as well as the temperature at which they are being irradiated. Additionally, the number of solutes also influences the vacancy-interstitial recombination rate, as vacancies tend to get "trapped" in place by solute atoms, increasing the likelihood that a wandering self-interstitial

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atom will intercept it. The vacancy-trapping effects, solute interactions, and co-precipitation with other elements such as nickel, phosphorous, and manganese further complicate this situation.

The term for Matrix Features incorporates a variety of other effects known to occur in low-copper steels. Most of these can be attributed to vacancy-solute clusters and phosphorous-bonded precipitates. The dependency on flux for these tends to be similar to that for copper-rich precipitates, and the overall contribution to radiation embrittlement tends to increase with increasing solute levels. The simplified predictive equations for transition temperature shift based on these two terms are:[14]

$$TTS = MF\ term + CRP\ term$$

$$MF\ term = A(1 - 0.001718 T_i)(1 + 6.13 PMn^{2.47})\sqrt{\phi te}$$

$$A = \begin{cases} 1.140\text{x}10^{-7} & \text{for forgings} \\ 1.561\text{x}10^{-7} & \text{for plates} \\ 1.417\text{x}10^{-7} & \text{for welds} \end{cases}$$

$$CRP\ term = B\left(1 + 3.77 Ni^{1.191}\right) f\left(Cu_e, P\right) g\left(Cu_e, Ni, \phi te\right)$$

$$f(Cu_e, P) = \begin{cases} 0 & \text{for } Cu \le 0.072 \\ [Cu_e - 0.072]^{0.668} & \text{for } Cu > 0.072 \text{ and } P \le 0.008 \\ [Cu_e - 0.072 + 1.359(P - 0.008)]^{0.668} & \text{for } Cu > 0.072 \text{ and } P > 0.008 \end{cases}$$

$$g(Cu_e, Ni, \phi t_e) = \frac{1}{2} + \frac{1}{2}\tanh\left[\frac{\log_{10}(\phi t_e) + 1.139 Cu_e - 0.448 Ni - 18.120}{0.629}\right]$$

$$
B = \begin{cases}
102.3 & \text{for forgings} \\
102.5 & \text{for plates in non - CE mfg. vessels} \\
135.2 & \text{for plates in CE mfg. vessels} \\
155.0 & \text{for welds} \\
128.2 & \text{for SRM plates}
\end{cases}
$$

$$
Cu_e = \begin{cases}
0 & \text{for } Cu \leq 0.072 \text{ wt\%} \\
\min[Cu, Max(Cu_e)] & \text{for } Cu > 0.072 \text{ wt\%}
\end{cases}
$$

$$
Max(Cu_e) = \begin{cases}
0.243 & \text{for typical } (Ni > 0.5) \text{ Linde 80 welds} \\
0.301 & \text{for all other materials}
\end{cases}
$$

$$
\phi t_e = \begin{cases}
\phi t & \text{for } \phi \geq 4.39 \times 10^{10} \\
\phi t \left(\dfrac{4.39 \times 10^{10}}{\phi} \right)^{0.259} & \text{for } \phi < 4.39 \times 10^{10}
\end{cases}
$$

The units involved and range/mean of the test data (855 points) are as follows:[14]

Table 3.2 Eason Model Data Range

Variable	Description	Range	Mean
Cu	Copper content (wt %)	0.01–0.41	0.136
Mn	Manganese content (wt %)	0.58–1.96	1.300
Ni	Nickel content (wt %)	0.044–1.26	0.565
P	Phosphorous content (wt %)	0.003–0.031	0.0119
ϕt	Neutron fluence, $E > 1$ MeV (n/cm^2)	9.26×10^{15}–7.13×10^{19}	6.50×10^{18}
ϕ	Neutron flux, $E > 1$ MeV (n/cm^2/s)	1.81×10^{8}–9.71×10^{11}	5.13×10^{10}
T_i	Irradiation temperature (°F)	522–570	545

Once the transition temperature shift has been found, it can be used to find the change in upper shelf energy by the following equation: $\Delta USE = .18 * \Delta RT_{NDT}$. [21]

3.3 Master Curve

One of the major shortcomings of the 10 CFR 50 requirements is that static fracture toughness curves (the relevant property in determining the likelihood of failure in a reactor pressure vessel) are drawn using only data from dynamic fracture tests. The results of these tests are closely correlated with actual static fracture toughness properties (notably crack initiation fracture toughness K_{IC}), but must incorporate some additional requirements and conservatisms to address differences between dynamic and static conditions.

It is important to understand why Charpy V-Notch and drop-weight tests can generally be used reliably in determining K_{IC}. First, the definition of RT_{NDT} in NB-2330 [12] requires both that the temperature used must be above the nil-ductility temperature as determined by drop-weight tests AND be within 60°F of the 50 ft-lb (with 35 mm expansion) transition temperature as determined by Charpy V-Notch tests. Given these two requirements and a large of amount of historical data from C_V and static fracture toughness tests on various steels, one can be reasonably certain that the actual static fracture toughness of a material is at least as high for a given temperature as any point on the K_{IC} curve, which was established from the lower bound of static fracture test results. Because this is a correlation and not a direct measurement, some conservatisms are required; notably, the upper shelf energy as determined by C_V tests must exceed 50 ft-lbs for the entire reactor operating lifetime to maintain confidence in this correlation.

This latter requirement may pose a problem to the continued operation of some nuclear plants as they age, particularly during a period of second license renewal (>60 years of

operation), even if confidence in actual static fracture toughness properties of the reactor pressure vessel can be maintained. Thus, utilities are asking the question: why can we not use actual fracture toughness measurements and bypass this potentially onerous requirement?[23]

The problem lies with the theoretical assumptions made in the performance of static fracture toughness tests and the resulting requirements on the size of the testing material. In order for the Linear Elastic Fracture Mechanics used to calculate fracture toughness from a compact tension or three-point bend test to be valid (i. e. that "plain strain" or "brittle" properties are being tested), the size of the plastic zone must be small relative to the flaw size and specimen dimensions. Whether this is in fact the case can be determined by using the following formulas:[19]

If $2.5*(K_Q/YS)^2 \leq B$, then $K_Q = K_{IC}$

where for a compact specimen

$K_Q = P \, x \, f(a/W) \, / \, (B \, x \, W^{0.5})$

and K_Q is provisional K_{IC} value, P is load, a is crack depth, W is specimen width, B is specimen thickness, and YS is 0.2% offset yield strength.

Generally, these requirements make the necessary specimen size too large to be practical for use in testing the effects of radiation damage on fracture toughness. However, recent developments in Elastic – Plastic Fracture Mechanics have allowed this constraint to be bypassed somewhat.

The Master Curve approach uses Elastic-Plastic Fracture Mechanics to calculate a standardized K_{JC} curve for crack-initiation fracture toughness in steels instead of the Linear-Elastic Fracture Mechanics-based K_{IC} curve. Extensive testing of non-irradiated materials has shown that the K_{JC} curve has essentially the same shape for all ferritic steels[22] once they have

been indexed to a T_0, the temperature where the median fracture toughness K_0 of a specified

number of 1T (25.4mm thick) compact test specimens is equal to 100 MPa√m. Adding an

additional margin of 35°F to T_0 gives a reference temperature RT_{T0}, the K_{JC} curve for which gives

an effective lower bound on static fracture toughness.[9]

Chapter 4: Criteria for Model Evaluation

Four criteria will be used to evaluate potential model changes: data availability, data uncertainty, regulatory impact, and model versatility. A criterion for data availability is necessary because it is possible that the historical surveillance data available does not consistently include certain information that would be important for the application of any particular model. Displacements per atom (DPA) may or may not be recorded in a standardized fashion, and the neutron fluence spectrum data necessary to calculate it also may not be available. The same is true for information on the weight percent of iron alloy elements or any novel data input, the importance of which may not have been known when irradiation and ductility tests were carried out.

The criterion for data uncertainty essentially represents how accurately the proposed new model predicts long-term behavior of irradiated beltline materials and the amount by which a given sample can be expected to deviate from predicted changes in ductility. Because of the relative simplicity and data availability of the Regulatory Guide 1.99 Revision 2 standard, any new scheme must significantly improve performance over current methods before it can be considered for implementation. Some preference may be shown for models that are more closely analogous to known physical processes (for example, DPA vs. >1MeV fluence), but the most important factor is the reduction of statistical uncertainty.

A criterion for regulatory impact is important for the practical application of any standard. Although the resources of the typical nuclear utility are considerable, any additional regulatory burden would also be imposed on independent researchers, academics, and the NRC itself. The

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greater the complexity of the process required for the implementation of any standard, the more likely that mistakes may be made at some point. It would not, for example, be practical or particularly useful to require the use of computationally intensive "physical" simulations such as Molecular Dynamics, as the amount of precision needed for the inputs would likely exceed what is available to a utility and be very difficult to independently assess. Thus, the most preferable result would be a single curve and straightforward equation with a limited number of data inputs that could be easily used and understood by the average engineer and verified by the regulators.

The final criterion, model versatility is how easily the model can be adapted to improve on any of the three preceding criteria and what those adaptations might be. If any of the models examined could be improved by any modifications, it should then be shown what steps must be taken to ensure that the integrity of the model has not been compromised by these changes. The ability to adjust for reactor-specific surveillance data should also be considered.

Chapter 5: Model Evaluations

5.1 Evaluation of Information Fusion Model

Given that this model, by definition, does not use any additional data beyond what the models that it incorporates use, the lack of surveillance data is not a factor in determining whether this type of information fuser should or should not be used from a regulatory perspective. It can be assumed that all of the input models will be using surveillance information that has historically been well-recorded (i.e., fluence >1MeV rather than displacements per atom). One constraint on the use of this model, however, is that all of the models used in the fusion must have the same inputs, as it is incoherent to weight a model where one aspect of its domain is undefined against models that have a recorded value for that input. For example, if one model does not include nickel in its calculations, neither can any of the others. A decision must be made about what variables and models should be included in the information fuser before it can be used for regulatory applications. Generally, the number of inputs should be reduced as much as possible without significantly increasing the uncertainty of the model.

Because the most certain model is chosen for any particular domain, the total certainty of results in this type of information fuser model is at least as high as any of the incorporated models, and adoption of this technique will always increase predictive certainty. This allows it to serve as an effective benchmark for any particular model that one would wish to investigate. Rather than just giving an indication of overall performance or absolute performance in a particular area, information fuser models allow for close comparisons between different models

in specific domains by examination of the relative weight they are given by the fuser program. This may lead to increasingly accurate physics-based models in the future.

The main problem with this type of approach to predicting reactor pressure vessel degradation is that the model itself is analytically incomprehensible and nearly impossible to evaluate without a computer program specifically designed to do so. This presents a significant barrier to anyone wishing to independently evaluate pressure vessel integrity without institutional support, and masks any potential problems within the code of the computer program. If the NRC were to establish a rule based on an information fuser model, the only way to verify compliance with that rule would likely be to input data into the same "black box" as the utility, making it very difficult to ever tell if there were something wrong with the software. The combination of precise input requirements and model opacity make it unlikely that a regulatory body would want to use such a model directly.

Although a fuser model is extremely adaptable in that one may choose from a variety of input models to be used, the regulatory problems with information fusers are inherent. The only real way to work around this problem would be to use reduce the fused model to one that could be represented by a few continuous equations and a limited number of inputs. This would preserve the functional behavior of the model inasmuch as the complexity of the derivative model would allow, but it would render obscure the specific mechanisms involved, more or less becoming a convoluted way to obtain a completely empirical predictive equation. In this case, the specific equation must be given for a decision to be made whether or not it is appropriate for regulatory use.

5.2 Evaluation of Eason Model

All of the data required for this model to be used are available, and it has already been calibrated on a much larger data set than were the equations specified in Regulatory Guide 1.99 Revision 2. Additional inputs beyond what was required in Regulatory Guide 1.99 are necessary (specifically, average flux, irradiation temperature, manganese content, and phosphorous content), but these properties are either listed in the material/plant specifications (solute content, average nominal reactor coolant temperature) or easily calculable (average flux = fluence / full-power irradiation time).

The standard deviations of this model for low-copper (<.072%) plates and welds are 15.0 °F and 18.6 °F, respectively. It is tempting to compare this directly to the standard deviations for plates (17°F) and welds (28°F) given in Regulatory Guide 1.99 Revision 2. However, these two numbers are not directly comparable, because the more recent is based on 855 data points, while the original had only 177 data points available when it was developed. This newer model also reserved some data points for validation that were not used in calibrating it. The standard deviations for the model from calibration data and validation data were not significantly different, giving increased confidence in its predictive ability. Importantly, the Eason model incorporates an understanding of the underlying physics of radiation imbrittlement, allowing operators to have a much better idea of what is actually going on than was possible at the time Regulatory Guide 1.99 Revision 2 was developed.

Regulatory burden is not significantly increased by adopting this method of calculating transition temperature shift. There are a few more inputs (which plants should have available)

and more complex set of equations, but nothing beyond the reach of the average engineer. The results can also be independently verified without difficulty, provided one has access to the input values used. The output (TTS/ΔRT_{NDT}) is used in exactly the same way that it would be were it obtained using the methods of Regulatory Guide 1.99 Revision 2. The most significant difficulty that utilities could potentially face is that they would find that the calculated RT_{NDT} or USE for a given fluence is no longer within the realm of allowable operation as defined by the NRC. This is arguably the result of increased model accuracy; reality may be less optimistic than the predictions of Regulatory Guide 1.99 Revision 2. Therefore, utilities would face no increased burden that would not advance the interests of public safety. Some low-copper pressure vessel materials such as those at Callaway Nuclear Power Plant[17] may even have lower predicted transition temperature shifts in comparison to Regulatory Guide 1.99 Revision 2.

The model described here has already been simplified by the original authors from a slightly more physically complex version; this simplification has not significantly decreased its predictive accuracy.[14] Data from individual reactors are now considered too unreliable for determining the transition temperature shift for any particular plant,[21] so it is unlikely that the NRC will continue to require that surveillance program results be used for pressure-temperature calculations.

5.3 Evaluation of Master Curve

One of the biggest problems with the general implementation of the Master Curve is that there is not a lot of data on irradiated materials. What is available is generally from test reactors under accelerated flux conditions, although compact tension specimens were included in some reactor surveillance programs.[16,17] Extensive testing of non-irradiated metals confirms that the Master Curve approach can be applied to all non-irradiated ferritic steels with a high degree of confidence, but it is possible that some irradiated steels may not fit within this general trend. This problem is further compounded because of a general lack of a physical understanding of why the Master Curve is shaped like it is, and why an individual material may be referenced to that curve at a particular temperature. Attempts have been made, however, to put the Master Curve on a firmer theoretical footing.[23]

Because the Master Curve approach is an alternative way to quantify static fracture toughness that drops the use of upper shelf energy and RT_{NDT} entirely instead of simply changing how they are calculated, the primary uncertainty associated with it is not whether it can model the future behavior of materials under irradiation (although such a model could be incorporated within the Master Curve methodology), but how well the K_{JC} curve predicts static fracture toughness at a given temperature. Provided that the general assumptions about the shape of the curve continue to hold for irradiated materials, the uncertainty of the curve can be arbitrarily reduced by increasing the margin that separates RT_{T0} from T_0. With a margin of 35°F, 99% of materials tested fall above the K_{JC} curve in fracture toughness.

The primary motivation for the Master Curve approach is a reduction in the regulatory burden associated with requirements for maintaining a USE greater than 50 ft-lbs throughout the reactor life. Because of the extreme expense involved with replacing a reactor pressure vessel, this requirement could cause the early retirement of a number of nuclear facilities. Developing an NRC-acceptable alternative to the K_{IC} curve and the upper shelf energy requirements could allow greater operator flexible in demonstrating fracture toughness and a general increase in the effective lifetime of nuclear power plants in the United States. Because of the high confidence in current regulatory requirements for static fracture toughness (given a known RT_{NDT} and USE), it is unlikely that the use of the Master Curve approach will ever be mandated. Rather, it remains within the industry's interest to prove to the NRC that this approach can be used if necessary. Whether this approval is granted will likely be determined on a case-by-case basis until concerns about potential nonconservatisms in irradiated materials are addressed.

In order for this method to be used for currently operating plants, the Master Curve approach would need to implement a dependable model for predicting radiation-induced shifts in RT_{T0}, because compact tension specimens were not required to be included in surveillance capsules. Additionally, more data is needed to confirm that the shape of the Master Curve is radiation-independent. However, because the Master Curve is likely to be used in a case-by-case basis rather than broad prescription, there is some flexibility in how individual operators can attempt to apply it.

Chapter 6: Conclusions

Given that a draft review of the technical basis for the revision of Regulatory Guide 1.99 has already been written for the NRC [23], it is reasonable to expect that there will be substantial changes to the regulatory framework concerning the evaluation of reactor pressure vessel integrity in the near future. Although it is impossible to predict when these changes will take place, utilities should anticipate that these changes will, at the very least, be applied to some point in time within a potential period of second license renewal. Because of the substantial costs involved with repairing or replacing a reactor pressure vessel, it is important to have a sense of what the Nuclear Regulatory Commission is likely to require and the implications for what may be required of facility operators.

Because of the conservatisms inherent in and general reliability of the current requirements for Pressured Thermal Shock screening and Upper Shelf Energy maintenance, these are unlikely to change. However, the predictive equations used to find the difference in upper shelf energy and reference nil-ductility transition temperature under irradiation may be altered in significant ways. Although it is possible that these changes may allow for a greater range of operation for some facilities, it is also possible that they may cause others to be in non-compliance with existing regulations, even if continued use of Regulatory Guide 1.99 Revision 2 would have allowed operation. This should be of particular concern as reactor pressure vessel materials begin to approach the 50 ft-lb minimum Upper Shelf Energy required by 10 CFR 50 Appendix G.

Parallel developments on the Master Curve method described in ASTM E1921 may help to alleviate these concerns for utilities that are able to implement them. The NRC may allow static fracture toughness data to be used in lieu of the Upper Shelf Energy requirement if it can be shown that the Master Curve approach is as reliable as using RT_{NDT} to determine fracture toughness. This exception would likely continue to be granted on an individual licensing basis, so it is in the interest of the facility operator to determine whether the Master Curve approach may be necessary, and how it could be implemented if so.

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