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Generic Evaluation of Environmentally Assisted Fatigue in BWR and PWR Reactors: A Feasibility Study

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Generic Evaluation of Environmentally Assisted Fatigue in BWR and PWR Reactors: A Feasibility Study

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EPRI Project Managers
K. Wolfe
D. Steininger

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ELECTRIC POWER RESEARCH INSTITUTE
3420 Hillview Avenue, Palo Alto, California 94304-1338 ▪ PO Box 10412, Palo Alto, California 94303-0813 ▪ USA
800.313.3774 ▪ 650.855.2121 ▪ askepri@epri.com ▪ www.epri.com

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THE FOLLOWING ORGANIZATIONS PREPARED THIS REPORT:

XGEN Engineering

Structural Integrity Associates

Electric Power Research Institute (EPRI)

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The following organizations prepared this report:

XGEN Engineering
7173 Queensbridge Way
San Jose, CA 95120

Principal Investigator
S. Ranganath

Structural Integrity Associates, Inc.
5215 Hellyer Avenue Suite 201
San Jose, CA 9518

Principal Investigator
T. Gilman

Electric Power Research Institute (EPRI)
3420 Hillview Avenue
Palo Alto, CA 94304-1338

Principal Investigators
D. Steininger
K. Wolfe

SIMRAND, LLC
5339 Romford Dr.
San Jose, CA 95124

Principal Investigator
Y. Garud

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PRODUCT DESCRIPTION

The Nuclear Regulatory Commission (NRC) requires utilities to perform fatigue calculations for primary system components. While straightforward, these calculations, which apply environmental multipliers to original design fatigue calculations, often result in cumulative usage factors (CUF) greater than 1.0. However, reducing the excessive conservatism of the original design calculations almost always yields an acceptable CUF. This study evaluated whether generic calculations could be performed to address many fatigue locations that must be evaluated in BWR and PWR reactors.

Background

The NRC is requiring utilities to demonstrate that the CUF of major Class 1 components is not exceeded even when accounting for environmental effects that were not considered during the initial design of the plant. In many cases, inclusion of the environmental effects results in a CUF significantly greater than 1.0 that would imply that fatigue cracks should be occurring frequently in operating plants. Since such damage has not been observed, it is believed that the methodology used to account for the environmental effects must be excessively conservative. Much of this conservatism lies in the fatigue calculations performed as part of the original plant design.

Objective

To determine whether a generic approach can be used to demonstrate that environmental effects are bounded by conservatisms in the original plant design fatigue calculation

Approach

The project team first performed a sample calculation for a typical feedwater nozzle in a boiling water reactor (BWR). The team performed two analyses. The first considered the fatigue analysis that was performed during the original plant design. This design analysis used a very severe thermal transient and resulted in a relatively high CUF even before considering environmental effects. When the team included environmental effects using the methodology of NUREG/CR-6909, the resulting CUF was significantly greater than 1.0. The second analysis repeated the original design calculation, but used a more realistic thermal transient. When the team included environmental effects in this analysis, the CUF was significantly less than 1.0, which demonstrated that, at least for one component, conservatism in the original design calculation bounded the additional usage due to the environment. Since the feedwater nozzle has one of the higher CUF values in a BWR, it was expected that other components would show similar results and that a generic methodology was possible.

In order for generic analyses to be performed that encompass a majority of components for which calculations are required, component configurations and thermal conditions must be similar in most plants. If, for example, the feedwater nozzles in each plant are physically different and are exposed to unique transients, the number of calculations required would be intractable. The team performed an evaluation for both BWR and PWR reactors to determine the number of different calculations that would be required to generically address the required components.

Results

The evaluation demonstrated that all of the components in BWRs for which environmental calculations are required could be addressed by a small number of generic calculations. For PWRs, many, but not all components could be addressed. Overall, there was a high level of confidence that the project objectives could be largely met. However, the cost of performing the calculations was higher than initially anticipated. When the cost was evaluated in comparison with the aggregate utility need, it was determined that the project would not be pursued.

Applications, Values, and Use

This project has demonstrated, as have projects before it, that fatigue design calculations typically contain a significant amount of conservatism such that when environmental effects are added, the resulting fatigue usage is often unacceptable. However, if the conservatism is removed, either generically or on a plant specific basis, acceptable results can almost always be obtained. A significant challenge going forward will be to determine where additional work should be performed to generically reduce such conservatism in a manner that best serves utility needs.

Keywords

Fatigue
Environmentally assisted fatigue

ABSTRACT

Utilities are required by the Nuclear Regulatory Commission to perform fatigue calculations for primary system components. These calculations must address environmental effects. While straightforward, the calculations that apply environmental multipliers to original design fatigue calculations often result in cumulative usage factors (CUF) greater than 1.0. However, if conservatism in the original design calculations is reduced, the CUF is almost always shown to be acceptable.

A study was performed to evaluate whether generic calculations could be performed to show that environmental effects are bounded by conservatisms in the original calculations. It was determined that, while such an approach has merit and would be able to address many fatigue locations that must be evaluated in BWR and PWR reactors, the current benefit to industry did not warrant the relatively high projected cost of performing such calculations.

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INTRODUCTION

1.1 Introduction

The Nuclear Regulatory Commission (NRC) is requiring utilities to demonstrate that the cumulative fatigue usage (CUF) of major Class 1 components is not exceeded even when accounting for environmental effects that were not considered during the initial design of the plant. Simplistically, these environmentally assisted fatigue (EAF) effects are incorporated by multiplying the fatigue usage factor calculated with air fatigue curves by a correction factor, F_{en}. The F_{en} values are based on laboratory test data and are a function of the material chemistry, the reactor water oxygen content, temperature and strain rate. Values range from 1 to over 10, so in some cases the correction for environmental effects changes the calculated fatigue usage significantly and often results in a CUF greater than 1.0.

The fact that these predicted fatigue failures are not seen to occur in operating plants has led industry to the conclusion that the overall methodology includes a significant level of conservatism. The conservatism may be introduced by the characteristics of the test data that were used to establish the F_{en} factors, by the analytical methodology used to perform the calculations, or a combination of both. In 2012, EPRI published a report [1] that identified gaps in the current state of understanding of EAF phenomena. The report identified research that could be undertaken to enhance the currently available test data and to reduce conservatism in the analytical methodology.

In looking at the analytical methodology, it quickly becomes apparent that the original design fatigue calculations are in many cases the source of considerable conservatism. These calculations, many of which were done in the 1970s and 80s, often assumed simplified thermal transients (e.g., step changes in temperature), conservatively enveloped load pairs by selecting the worst case pair and utilized relatively unsophisticated stress analysis techniques. Provided the resulting CUF was less than 1.0, further refinement of the calculations was not required. However, when the fatigue analysis is performed in this way and then used in an EAF calculation, the resulting CUF is often unacceptable. If, however, a more realistic definition of the thermal transient is used and modern methods of stress analysis are employed, the calculated usage factor is decreased by a significant amount. In fact, in cases where an EAF calculation based on the original design fatigue calculation results in a CUF greater than one, it has almost always been possible to show an acceptable value of usage when an improved methodology is employed and realistic transients are considered.

The fact that reducing the conservatism in the design calculation almost universally results in an acceptable EAF analysis implies that it may be possible to demonstrate generically that the fatigue usage based on the design analysis includes enough conservatism to bound environmental effects. If successful, the results could be used by utilities to demonstrate acceptable fatigue usage without the need to perform detailed calculations. The success of such an approach depends in part on whether the mechanical design of the reactor components, the form of the design transients and the form of the actual transients are similar enough across the entire fleet to allow a manageable number of calculations to be performed in demonstrating the generic result. Clearly, if a large number of plant specific component configurations exist or if design and/or actual transients vary significantly from plant to plant, the number of required calculations could become untenable. However, if component designs are relatively uniform and if transients are similar or can be bounded in an acceptable manner, a specific reactor component (e.g., BWR feedwater nozzles) could be addressed with one or two variations of a calculation.

This reports documents the results of a study to determine the feasibility of such an approach. Section 2 provides historical information and further background on the subject. Section 3 presents a sample analysis demonstrating the sources and magnitude of the conservatism in a typical application. Sections 4 and 5 present the results of the feasibility study for BWRs and PWRs respectively. Section 6 reviews statistical methods that were developed to quantify uncertainty in EAF calculations, and overall conclusions are discussed in Section 7.

2 BACKGROUND: ENVIRONMENTALLY ASSISTED FATIGUE STRATEGY

2.1 Introduction

This chapter reviews work that has been performed on environmental effects on fatigue beginning in the 90's through to the present.

Portions of the history of industry efforts related to environmentally assisted fatigue described below are taken from Reference 2.

2.2 Discussion

Industry and the U.S. Nuclear Regulatory Commission (NRC) have been working on the issue of environmental fatigue of piping (and applicable to other components as well) for many years. NRC research on the subject of reactor water environmental effects on fatigue was first published in the early 1990s. Research was motivated by acknowledgement that the fatigue curves in Section III (applies to plant construction) of the ASME Boiler and Pressure Vessel Code were based on testing in air, primarily at room temperature, adjusted by adding margin, in part to compensate for temperature and "industrial" environments (e.g., a testing laboratory environment), whereas LWR pressure boundary components contact reactor water at elevated temperatures. This issue is described in NUREG/CR-5704. In 1993, proposed "interim" fatigue curves that accounted for environmental effects for carbon, low alloy and stainless steels were published in NUREG/CR-5999. These curves were based on test results. The testing was conducted at Argonne National Laboratory (ANL) and was the first in a series of tests and accompanying publications on environmental fatigue.

To better understand the impact the interim fatigue curves presented in NUREG/CR-5999 would have on fatigue life predictions for operating nuclear plants up to a lifetime of 60 years, Idaho National Engineering Laboratories (INEL) evaluated fatigue sensitive locations in all four nuclear steam supply system (NSSS) designs and documented the results in NUREG/CR-6260. The methodology for predicting fatigue life used information from existing plants' ASME Code required stress reports, supplemented by additional calculations, to obtain cumulative usage factors (CUF) for each reactor design. The results indicated that some locations exhibited a calculated CUF greater than 1. The report showed also that for many of these sensitive locations the CUF would be less than 1 if excessive conservatisms were removed from the evaluations.

Work on quantifying the effects of the environment on the fatigue of primary system components continued through the 1990s as funded by NRC. This work was documented in NUREG/CR-6583 which provided fatigue correlations for carbon and low-alloy steel and in NUREG/CR-5704 that provides correlations applicable to austenitic stainless steels. In these documents a new approach to quantifying the effect of metal fatigue was presented and consisted of defining an environmental factor, F_{en}, that is

$$F_{en} = N_{air}/N_{water} \qquad \textbf{Eq. 2-1}$$

where

F_{en} = environmental fatigue multiplier,

N_{air} = fatigue life (number of cycles) in air at room temperature, and

N_{water} = fatigue life (number of cycles) in water at temperature.

Therefore, the fatigue usage derived from air curves, such as that presented in the ASME Code, is multiplied by F_{en} to obtain the fatigue usage in the applicable environment.

During the above developments industry analysts started applying the new correlations to fatigue sensitive locations in the main piping of nuclear power plant designs. They soon discovered that using past analysis techniques and associated assumptions, along with the new environmental fatigue methodology of the F_{en} approach, resulted in CUFs significantly greater than 1 for many locations. In Reference 3, DOE and EPRI identified analytical conservatisms associated with the implementation of ASME Code, Section III fatigue evaluations (ASME NB 3200 and NB 3600). Additionally, the document presents the effect of the LWR environment on two typical components using both design transients and that obtained from plant historical data. It was found in this investigation that fatigue usage for some actual plant transients obtained from in-plant monitoring systems was significantly less than that calculated using design-basis transients. The report's conclusion is that the reductions in margin due to environmental effects as presented by the ANL fatigue data or correlations are more than offset by conservatisms found in typical ASME Code fatigue evaluations.

Additional work initiated by NRC and documented in NUREG/CR-6674 concluded that the environmental effects of reactor water on ASME Section III fatigue curves had an insignificant contribution to core damage frequency. However, as a result of potential fatigue crack initiation, the frequency of primary coolant leakage did increase and becomes a significant concern for a plant's licensing period beyond 40 years (i.e., in the so called license renewal period). This conclusion raised industry awareness to the possibility that nuclear plants may not be able to show that certain fatigue sensitive locations in the primary coolant system may have associated with them a CUF less than 1, especially during a plant's license renewal period (40 to 60 years). This issue of environmental fatigue of primary system components and subsequent leakage during a plant's license renewal period was addressed by the NRC. Since NUREG/CR-6674 indicated such leakage can occur during a plant's license renewal period, the NRC requires that utilities applying for license renewal address the effects of primary coolant on fatigue usage in selected examples of affected components on a plant-specific basis.

In 2007, the NRC published Regulatory Guide 1.207, "Guidelines for Evaluating Fatigue Analysis Incorporating the Life Reduction of Metal Components Due to the Effects of the Light-Water Reactor Environment for New Reactors." The Regulatory Guide references NUREG/CR-6909 which provides revised expressions for F_{en} factors for carbon and low-alloy steels, austenitic stainless steels, and nickel alloys, as well as revised fatigue design air curves to be used with the F_{en} factors. Currently, NRC is developing a revision to this guideline that will reference a revised NUREG/CR-6909 that includes new data (i.e., a substantial amount of Japanese data was added) in support of the F_{en} factors and may have a new title indicating that the Regulatory Guide is applicable to new and operating reactors. Additionally, NUREG-1801, Rev. 2 (the latest "GALL" Report) requires that the sample locations from NUREG/CR-6260 be analyzed for environmentally-assisted fatigue in addition to other "sentinel" locations that may be more limiting. An industry attempt at applying this sentinel location concept to a particular plant is documented in Reference 4.

Some early guidance for evaluating environmental fatigue for plant license renewal is provided in the original NUREG-1801, the NRC Generic Aging Lessons Learned (GALL) Report. This report allows use of some prior NUREG reports (i.e., predecessors to NUREG/CR-6909) containing environmental fatigue graphs and correlations or the updated methodology documented in NUREG/CR-6909 with its F_{en} approach. While these reports provide the methodology associated with development and application of the F_{en} factors, they do not provide guidance on several application details of the methodology. Because of this lack of guidance, evaluation of environmental fatigue using the F_{en} approach produces results that are dependent on an analyst's assumptions, assigned arbitrary margins, and interpretations. To reduce inconsistency in results between practitioners of fatigue analysis that occurs because of this lack of guidance, industry established several initiatives defining a more unified approach when using the F_{en} methodology.

EPRI and General Electric applied a F_{en} type of approach (Reference 5) in which parameters affecting fatigue life as identified in NUREG/CR-6260 and/or NUREG/CR-5999, such as strain rate, temperature, etc were selectively applied in a fatigue analysis where it was shown that a simple additive combination of these parameters is overly conservative. The F_{en} equations were derived from the statistical characterizations in NUREG/CR-6335. This F_{en} approach was used to support a site specific submittal by Calvert Cliffs in support of their license renewal initiative concerning thermal fatigue (see Reference 6). Additionally, this F_{en} approach was used in several EPRI projects to evaluate fatigue sensitive component locations in four types of nuclear power plants: an early-vintage Combustion Engineering (CE) PWR , an early-vintage Westinghouse PWR , and both late vintage and early vintage GE BWRs. Component locations similar to the 6 locations evaluated in NUREG/CR-6260 were evaluated. The intent of these investigations was to demonstrate that enough conservatism existed in the underlying component fatigue design per ASME Code, Section III requirements such that evaluation for fatigue environmental effects in remaining plants of the fleet is not necessary. The NRC did not accept this conclusion. NRC did not agree that the results contained enough conservative margin to allow the results to be applied generically. The primary reason the NRC did not approve the approach was because the studies were done without using the most up to date environmental fatigue data or correlations that were soon to be published in NUREG/CR-6583 and NUREG/CR-5704.

The Pressure Vessel Research Council (PVRC) Steering Committee on Cyclic Life and Environmental Effects (CLEE) (Reference 7) reviewed published environmental fatigue test data and the F_{en} methodology used by EPRI and NRC. The committee adopted the F_{en} approach but modified it by defining threshold values for the pertinent variables above which environmental effects must be taken into consideration. As noted above, NUREG/CR-5999 uses an additive combination of parameters to incorporate the effects of the environment on the fatigue life of a component. This is judged to be overly conservative. As noted by the PVRC, "It has been observed that in order to have a large effect of the environment on the S-N fatigue life, a critical combination of conditions is necessary. If any one of the conditions is missing, the effect of the environment on the fatigue life will be moderate." Additionally, the PVRC recognized that the F_{en} equations do not collapse to the appropriate fatigue air curve when no environmental conditions are operating, instead the equations contain an environmental shift even in the absence of environmental effects. For example, the equation that fits reactor water environmental fatigue data for stainless steels predicts an asymptotic environmental shift of 2.55, even for temperatures below the environmental threshold. At the threshold values F_{en} should equal 1. Therefore, to compensate for this shift and define "moderate environmental effects" above threshold the PVRC adjusts F_{en} by what is referred to as a "Z factor." The F_{en} factor is divided by the Z factor when applying the F_{en} approach defined by NRC. The Z factor is considered to be available margin that can be used in the determination of the acceptability of fatigue evaluations that are calculated using environmentally adjusted fatigue curves appropriate for the material and strain rate of actual plant components. The NRC has not generally accepted this concept of a moderate environmental condition and the use of the Z factor although one utility has received a plant specific SER that provided some relief based on this approach. The NRC justified its position based on the large data scatter associated with laboratory testing. The NRC relied on arguments presented in NUREG/CR-6583 to justify its disapproval.

ASME initiated several Code Cases (some are in draft form at this time) after Regulatory Guide 1.207 and the technical basis document, NUREG/CR-6909, were published. To date the Code Cases have not been approved by NRC for licensee use. One particular Code Case that is presently in draft form provides rules for evaluating environmental effects using a flaw tolerance approach for situations where the fatigue usage factor exceeds 1.0. The Flaw Tolerance Code Case Draft uses an approach similar to that in ASME Code Section XI, Appendix L. The NRC has yet to support a flaw tolerance approach for use in plant design. The NRC has to date taken the position that new plant design and construction philosophy applies to plants entering license renewal and therefore must not allow the potential existence of growing fatigue cracks as implied by a fatigue usage factor greater than 1.0. No distinction is made between fatigue cracks of an engineering size detectable by NDE and microcracks that develop during the initiation phase (i.e., long before the CUF equals 1.0) of fatigue cracking. (Note that one plant has been successful in obtaining plant specific NRC approval for use of a flaw tolerance approach; whether the NRC will grant approval for additional plants is uncertain.)

In parallel with the development of the Code Cases mentioned above, EPRI defined two so called "sample piping or nozzle problems" that were evaluated by a number of industry volunteers for environmental fatigue using NUREG/CR-6909 data. The purpose of this effort was to identify areas needing either improvement or clarification in the Code Cases or additional guidance from industry. The effort also attempted to address a number of evaluation issues that were identified in MRP-47: Revision 1, "Guidelines for Addressing Fatigue Environmental Effects in a License Renewal Application." Information and conclusions developed from these two efforts are contained in Reference 2. Additionally, because of industry concerns that plant's would not achieve a CUF = 1.0, excessive conservatism required by ASME Code, Section III, NB-3680 on stress indices applied to standard pipe fittings was identified for possible reduction in the future [8].

Because of Regulatory Guide 1.207 and its requirement to evaluate new plant designs against the effects of environmental fatigue, EPRI initiated work in simplifying some of the analysis that would be involved in this type of evaluation. Specifically, an attempt was made to develop a solution that identifies bounding plant transients among the population of potential design transients when environmental fatigue requirements are considered. Achieving this objective is somewhat difficult since the F_{en} multiplier depends on the strain rate associated with the transient. Typical fatigue analysis before NUREG/CR-6909 would assume a temperature change for example to a pipe wall to be instantaneous. This maximizes the stress response. But such a change would cause the applied F_{en} to be of minimum value because the associated strain rate is high. But it has also been determined that the product of F_{en} and usage is a maximum at a small but non-zero ramp time. Therefore, it is necessary to consider real ramp times associated with plant transients.

At the design stage of a plant, identifying transients with anticipated ramp times and evaluating all transients against environmental fatigue effects is difficult. It is desirable to identify bounding transients and remove the potential need to apply administrative controls on the plant to meet certain operational requirements and maintain a CUF < 1.0. Reference (9) defines these bounding ramp times for a variety of geometry and material configurations. The report provides the necessary tools that a plant designer can use to identify limiting ramp times using a table look-up scheme for these configurations. Such a process avoids an iterative process that would be used to identify the bounding conditions when F_{en} requirements are considered.

In the 2012 time frame, industry became very concerned that plants going into license renewal were not going to be able to show fatigue usage factors less than 1.0 for this period of time (i.e., 40 to 60 years) when the analysis accounts for environmental effects on fatigue as given by NUREG/CR-6909 fatigue correlations. Most utilities received approval for license renewal with the stipulation that they would perform the necessary fatigue analysis according to industry requirements before entering the license renewal period. Some industry representatives wanted to continue to refute the reasonableness of the correlations presented in NUREG/CR-6909. The MRP was directed to develop an overall plan that would ameliorate this issue at a reasonable cost to the industry. In developing a plan, MRP reviewed much of the work that had been performed on the topic of environmental fatigue, much of which is discussed above. Additionally, a number of experts or practitioners of fatigue analysis within the industry were consulted.

The review arrived at the following two conclusions. First, it was apparent that the NRC was steadfast in their position that NUREG/CR-6909 environmental fatigue correlations (or other NRC-approved correlations) must be used in any fatigue analysis submitted to them for review and approval unless alternate representative, environmental fatigue data was provided and approved. It was acknowledged that the best laboratory environmental fatigue data applicable to a nuclear plant's primary side piping and vessel components were provided by the correlations presented in the NUREG/CR-6909. It was apparent that developing new and convincing environmental fatigue data that could receive NRC approval for use by industry instead of those presented in NUREG/CR-6909 was many years in the future. Therefore, a plan was defined assuming these NUREG correlations would be used without modification in any analysis to be submitted to the NRC. Secondly, it was recommended that development of alternate environmental fatigue test data be pursued over the long term. It was recognized that such testing would be very expensive. The details of this plan are provided below.

NUREG/CR-6260 and Reference 3 indicates that there is a considerable amount of conservatism incorporated in much of the fatigue analysis performed for the design of BWR and PWR plants. For example, in many cases design transients used in the analysis are developed using simple concepts, such as step functions for temperature variations. This kind of easy approximation was made as long as the fatigue usage factor was calculated to be less than 1.0. During the design stage of a nuclear plant there are many tradeoffs allowing analysis as dictated by ASME Code requirements to be as simple as possible. This usually results in input values to the fatigue analysis taking on conservative values because they are easy to define or determine. Additionally, the required ASME fatigue analysis as represented by for example, the prescription specified in NB-3600, is itself conservative. It is conservative because it is based on the concept of "design by formula" and is intended to be relatively easy to implement; therefore it incorporates inherent conservatism to allow the development of simplified equations for use.

There may be a need to modify this section of the Code to effectively incorporate the environmental effects on fatigue presented by the correlations in NUREG/CR-6909.

Another area of conservatism that was identified during the effort documented in this report is the penalty associated with the calculation of K_e (elastic-plastic penalty factor). There is a good discussion on K_e and its associated conservatism in reference (3). As noted in this reference K_e is a significant area of conservatism in the Section III fatigue evaluation process. If any Class 1 component has a high fatigue usage factor, such as a value close to 1.0 it is generally determined that K_e was calculated to be greater than 1.0 for at least one load pair set. The factor K_e is used to factor the alternating stress intensity amplitudes (S_{alt}) when using the ASME fatigue design curves. Because the fatigue curves use the logarithmic scale, a relatively small K_e can have a significant effect on the fatigue usage factor. K_e is essentially a peak strain concentration factor accounting for the effects of localized plastic strain. It is a correction factor to the elastic stress concentration factor. K_e comes into effect when $S_n > 3S_m$. K_e is equivalent to the actual peak strain divided by the peak strain calculated for completely elastic behavior. In many cases the reduction of the maximum value of K_e calculated through analysis can be significant when compared to the Code mandated value. This effect on component fatigue is illustrated in the analysis performed on a BWR feedwater nozzle presented in Section 4 and further discussed in Section 5.

Further evidence of the inherent conservatism in a plant's fatigue analysis is provided by analysis such as the example presented in Section 3 of this report. In this analysis, fatigue calculations for a typical BWR feedwater nozzle using design and actual transient information obtained from a BWR's plant computer are compared. The results show that conservatism introduced by use of the design transient more than compensates for environmental effects prescribed by NUREG/CR-6909 in a calculation where a more realistic transient is used. Therefore, it appears that some utilities, if not all BWR utilities, can incorporate environmental effects on fatigue as prescribed by NUREG/CR-6909 and obtain a cumulative usage factor less than 1.0 as long as excessive conservatism, previously applied in such analysis, is removed from the calculation.

The amount of unnecessary conservatism identified by previous researchers should be reduced when performing environmental fatigue analysis. To effectively deal with plants that have not obtained NRC approval for their license renewal period or for plants with NRC license renewal period approval, but who have not yet met their license renewal commitment on fatigue evaluation for the extended operational period, a generic approach for this evaluation was identified as a potentially workable plan. This generic approach would identify fatigue sensitive locations (at least it would address those locations identified in NUREG/CR-6260) in the remaining PWRs and BWRs. Generic analysis would be done for these locations using one plant design. The approach's objective is to bracket all other plants with a generic calculation done once. The environmental fatigue data presented in NUREG/CR-6909, latest edition, would be used in the generic, bounding analysis without modification. Industry would accept in the short term the validity of the environmental effects on fatigue as presented in NUREG/CR-6909.

Further investigation into the feasibility of the above generic approach led to two reports, one dealing with BWRs and the other focused on PWRs, which are summarized in Sections 4 and 5. The BWR report concluded that the generic approach could be done without much difficulty and there would be few outliers needing additional plant specific analysis. It appeared that the effort would be valuable and useful to the BWR fleet. The PWR report was not as encouraging, but did conclude that a generic approach would work for some of the fatigue sensitive locations. The PWR report indicated also there would be several outliers because there are numerous different

PWR designs in the field for some of these fatigue sensitive locations (e.g., pressurizer surge line nozzle). Therefore it was concluded that a generic, bounding approach for environmental fatigue evaluation for the license renewal period is an effective approach to resolve the EAF issue in the short term.

Since it appeared that the generic approach would help plants meet their piping fatigue license renewal commitment, an effort was initiated to determine how many BWR and PWR nuclear plants would actually be helped by the generic effort. It was assumed up to this point in time, because of the concerned expressed by some industry personnel, that numerous plants would not meet their licensed renewal commitment on piping fatigue. Westinghouse was asked to determine how many plants still needed to perform work necessary to satisfy their license renewal commitment concerning environmental fatigue. This request was specific to PWRs. Structural Integrity Associates was asked the same question and the request was focused on BWRs. The investigation by these two contractors determined that there was a relatively low percentage (approximately 15 %) of plants, both BWRs and PWRs, that still need to perform analysis to satisfy their licensed renewal commitment. Therefore, it was determined that generic analysis does not offer much value to the nuclear plant fleet especially since the cost of such analysis is relatively high. The generic, bounding approach to environmental fatigue evaluation was canceled.

What is most notable about the fact that so few utilities still need to satisfy their license renewal commitment is how the other utilities met their requirement. These utilities have spent the money and analyzed the problem using more complex analysis techniques (e.g., 3-D finite element) and reduced conservatisms. These plants and their contractors have learned where the conservatism lies through their own analytical investigations or by reading the many industry references (some of which are referenced in this report) that have addressed this issue during the last 15 years. Additionally, it is probably reasonable to conclude that plants and their contractors have learned how to perform the analysis in a consistent and appropriately conservative manner by reviewing the work that went into the two sample problems conducted by EPRI's MRP (Reference 2) and its utility and vendor participants.

One outcome of the industry effort to resolve the environmental fatigue issue was the transformation of EPRI committees involved with addressing the fatigue issue to a revised committee structure and objectives. Two fatigue committees were created. A "short term" committee composed of utility personnel and contractors was created to oversee EPRI efforts to address what was originally believed to be an immediate issue of utilities not being able to demonstrate a CUF of 1.0 during the license renewal period of operation. This committee ultimately made the decision that the generic, bounding approach to fatigue evaluation should not go forward because it was not necessary. In the future this committee will oversee focused efforts by EPRI in support of initiatives taken by the ASME Section III, Subgroup on Fatigue Strength and/or NRC.

A "long term" committee was also formed overseeing R&D that could help resolve the issue of plants not achieving a CUF of less than 1.0 for beyond a 60 year operation. It is generally agreed within industry that after all analytical pencil sharpening has been used to obtain a CUF less than 1.0 for plants operating in their license renewal period, there was nothing left in the analytical world to manipulate and keep this factor less than 1.0 in the 60 to 100 year time frame when accounting for environmental effects as defined by NUREG/CR-6909. To allow plant operation in this time frame with acceptable fatigue characteristics requires more realistic environmental fatigue data than that presented by NUREG/CR-6909. It is generally believed that such experimental data can only be obtained through testing of prototypical components. NUREG/CR-6909 contains environmental fatigue correlations using data obtained from classical fatigue type testing of small specimens undergoing uniaxial, membrane only stress cycling in an appropriate environment at constant temperature. Only prototypical testing of environmental effects has a chance of being valid and reasonable to the extent that it could substitute for the testing used to develop the correlations provided in NUREG/CR-6909. Until such time that these data become available the NRC methodology of incorporating environmental effects on fatigue as provided in NUREG/CR-6909 and referred to in Regulatory Guide 1.207 must be followed.

It appears that analytical approaches to fatigue evaluation can be performed without incorporating unnecessary conservatism, but with environmental effects taken into account with a resulting CUF less than 1.0. A majority of plants have successfully accomplished this evaluation and therefore met their license renewal commitment associated with environmental fatigue. But this conclusion probably doesn't hold for plants seeking Long Term Operation (LTO) involving operational lifetimes in the 60 to 80 year time frame.

Finally, there is presently a fatigue related issue that industry should be made aware and was not addressed by the efforts discussed in this report. The issue concerns Leak-Before- Break regulation. The NRC requirements concerning the identification of postulated pipe rupture (break) locations and the required dynamic/pipe whip analysis are specified in Section 3.6.2 of NUREG-0800 and Branch Technical Position 3-4 [USNRC 2007a]. When this regulation is applied to the pipe damage mechanism of fatigue the NRC requires that a pipe rupture must be postulated at any reactor pressure boundary location where the cumulative fatigue usage factor, CUF, exceeds 0.1 as calculated by the design-by-analysis rules and procedure defined in Section III of the ASME Code, specifically NB-3200. It is anticipated that when such fatigue analysis is performed under these rules, environmental effects on fatigue as prescribed by NUREG/CR-6909 will be incorporated, although this approach has not been formally acknowledged by NRC to date as a requirement. In any event, such analysis with a limit of CUF equal to 0.1 will result in a significant increase in the number of postulated break locations in the reactor pressure boundary. It is generally believed, at least by this author, that the appropriate way to handle this issue is by application of a risk informed break location that is supported by a technical position involving: 1. Low break probability, 2. High damage tolerance, and 3. Low risk of core damage and large early release. Analysis addressing these issues is provided in References 10 and 11. It is anticipated that the xLPR effort undertaken by NRC will address this issue in this manner and additional work on this issue is not warranted.

2.3 Conclusion

A considerable amount of industry work has gone into developing alternatives to the environmental fatigue correlations developed by NRC over the course of about 15 years. This effort was motivated by a concern that plant piping and associated components would not exhibit a CUF less than 1.0 for a plant's license renewal period when accounting for the effects of the environment as expressed by the correlations and accompanying methodology published in NUREG/CR-6909. NRC requires that this evaluation be performed using these correlations. Industry has also performed considerable work identifying guidance on how this methodology can be applied in practice and without incorporating unnecessary conservatism in the analysis. It appears that the majority of plants have successfully met their license renewal commitment concerning environmental fatigue by following this guidance and reducing unnecessary conservatism. Therefore, no additional short term effort, such as a generic, bounding approach to environmental fatigue evaluation capturing a large number of plants, is required, although for the short term, there may be a need to refine the correlations or methodology for easier application or refine ASME Code's fatigue methodology to more effectively use the correlations. A long term effort to produce more accurate environmental fatigue data may be required over the long term to support plants that would operate in the 60 to 100 year time frame. But even in this case, an alternate approach to testing, such as a flaw tolerance technique for evaluating fatigue damage and acceptance, may prove to be a more effective, less costly approach.

3
EXAMPLE ANALYSIS – BWR FEEDWATER NOZZLE

As an example of how conservatism in the original fatigue design calculation can be removed, an analysis of a BWR feedwater (FW) nozzle is discussed in this section. A review of the stress analysis for the FW nozzle shows two areas of conservatism that result in the high fatigue usage. First, the largest stress range comes from the combination of step change down in temperature during turbine roll and the step up in temperature during hot standby and loss of FW pumps. The assumption of a step change results in very high stresses. Even a small ramp rate of say, 30 minutes (i.e. the temperature drops from 550°F to 50°F in 30 minutes rather than instantaneously) can reduce the stress range significantly. This has two important benefits, it reduces the P+Q stress range for fatigue, but an even greater benefit comes from the reduction in K_e from simplified elastic-plastic analysis. Analysis has shown that a modest change in the ramp time can reduce the fatigue usage significantly. To evaluate the conservatism resulting from the step change assumption in the limiting transients, a finite element analysis of the feedwater nozzle was developed and stress analysis was performed for the original transient with the step change and the same transient with a ramp of 30 minutes as shown in Figure 3-1. Comparison with actual plant transient data shows that the 30-minute ramp is reasonable and conservative. Figure 3-2 shows the comparison of the stresses at the end of the temperature drop with the step change and with the 30 minute ramp. As expected, the stress with the ramp drop in temperature is much lower than that with the step change. The peak stress at the end of the temperature drop is 92 ksi for step change and 50 ksi at the end of the 30-minute ramp. Figure 3-3 shows the fatigue usage based on the original analysis with the step change and the usage based on the 30-minute ramp assumption. CUF decreases from 0.95 to 0.102. The decrease in CUF is due to two reasons: i) reduced stress due to the ramp time assumption; ii) the reduction in K_e from around 1.8 to 1.0 since the P+Q range with the ramp assumption will be less than 3 Sm.

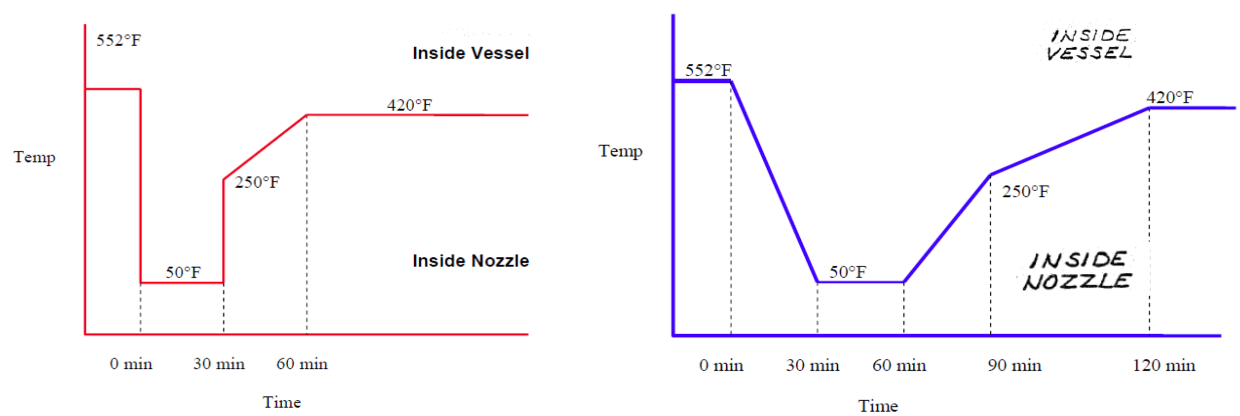
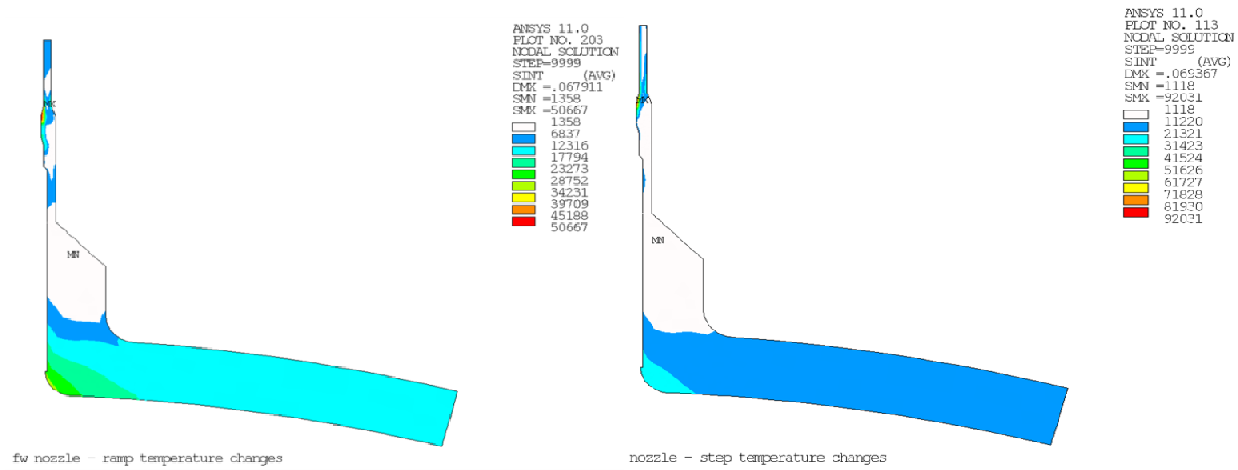


Figure 3-1
Original Design Transient (left) and Transient with Ramp Time



Revised Ramp Transient 30.45 min **Original Design Transient 0.45 min**

Figure 3-2
Finite Element Model for the FW Nozzle

An alternate approach may be to retain the step change assumption (i.e. no ramp assumption), but address only the part related to simplified elastic plastic analysis. The use of the ramp assumption requires justification based on plant monitoring which may be difficult. By assuming the step change based on the original plant design basis, the need for plant specific temperature monitoring is avoided. Figure 3-4 shows the ASME Code K_e factor as a function of the P+Q range. It is seen that the factor can be as high as 3.33 for stainless steel and 5 for carbon steel. It is well known that the K_e factor in the ASME Code is extremely conservative. Elastic-plastic finite element analysis has shown that the actual K_e factor may be one half the values specified in NB-3228 of Section III, ASME Code. Figure 3-4 also shows a proposed factor of 2 reduction in the maximum K_e value that may be justified with sufficient analysis. The impact on CUF of reducing the K_e value by a factor of 2 is shown in Figure 3-5. The original CUF with the Code K_e values (and the step change assumption) is 0.95. The CUF for the assumed factor 2 reduction in the maximum K_e (but retaining the step change in temperature) is seen to be 0.328. This reduction in CUF may be sufficient to account for the EAF effect. While the K_e based approach has less benefit than the previous ramp time based analysis, it has the advantage that actual plant transients are not needed to justify the higher ramp time. Thus, it has higher generic acceptability.

For BWRs, the feedwater nozzle typically has a relatively high CUF. The fact that the analytical techniques described above are able to render the CUF (including environmental effects) less than 1.0 for this component gives confidence that similar results will be obtainable for other BWR locations.

Original Design Basis Transient

Transient Pair (See Fig 4-4)	K_e	Sa ksi	No. of Allowable Cycles (1980 Code)	No. of Design Cycles	Fatigue Usage
HS LOFP (CD)	1.816	145	375	56	0.149
TR - HS (AB)	1.746	141	440	260	0.591
HS-HS (BC)	1.345	94	1600	188	0.118
Other Cycles					0.092

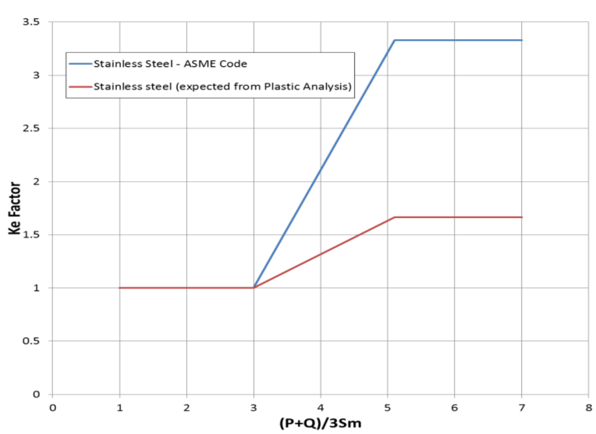
Original Design 40-yr CUF=0.95

Revised Transient with Ramp Time

Transient Pair (See Fig 4-4)	K_e	Sa ksi	No. of Allowable Cycles (1980 Code)	No. of Design Cycles	Fatigue Usage
HS-LOFP (CD)	1.0	44.8	38365	56	0.0015
TR-HS (AB)	1.0	44.4	40134	260	0.0065
HS-HS (BC)	1.0	38.8	82801	188	0.0023
Other Cycles					0.092

Revised Design 40-yr CUF=0.102

Figure 3-3
Effect of Ramp Time on FW Nozzle CUF



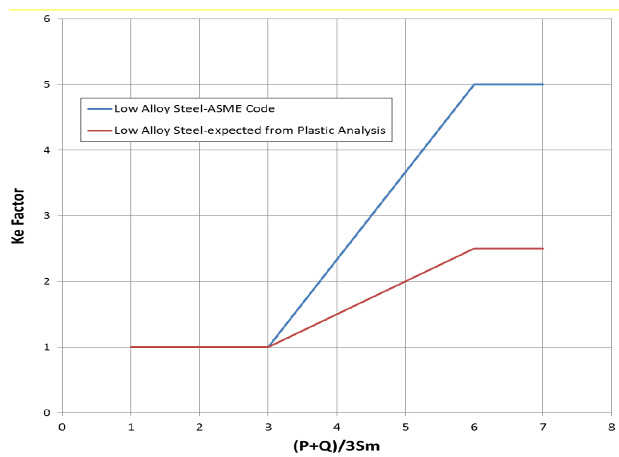


Figure 3-4
K_e values for Stainless Steel and Low Alloy Steel (from NB-3228.3) and Factor of 2 Reduction Assumption

Original Analysis based on Code K_e Factors

Transient Pair (See Fig 4-4)	K_e	Sa ksi	No. of Allowable Cycles	No. of Design Cycles	Fatigue Usage
HS LOFP (CD)	1.816	145	375	56	0.149
TR - HS (AB)	1.746	141	440	260	0.591
HS-HS (BC)	1.345	94	1600	188	0.118
Other Cycles					0.092

Total Design 40-yr CUF=0.95

Analysis based on Updated K_e Factors

Transient Pair (See Fig 4-4)	K_e	Sa ksi	No. of Allowable Cycles	No. of Design Cycles	Fatigue Usage
HS-LOFP (CD)	1.233	98.4	1632	56	0.0343
TR-HS (AB)	1.213	98.0	1661	260	0.1565
HS-HS (BC)	1.098	76.8	4118	188	0.0457
Other Cycles					0.0920

Updated K_e based 40-yr CUF =0.328

Figure 3-5
Effect of the Reduction in K_e on the Calculated Fatigue Usage

4 FEASIBILITY STUDY RESULTS - BWRS

For BWR plants, NUREG/CR-6260 identified six locations that were expected to be representative of components that had higher CUFs and/or were important from a risk perspective. Those locations are:

1. Reactor vessel feedwater nozzle
2. Reactor recirculation piping (including inlet and outlet nozzles)
3. Core spray line reactor vessel nozzle and associated Class 1 piping
4. Feedwater line Class I piping
5. Residual heat removal (RHR) return line Class 1 piping
6. Reactor vessel shell and lower head (CRDM nozzle)

A discussion of the feasibility of performing generic calculations for each of these locations is discussed below.

4.1 Feedwater Nozzle

The feedwater nozzle and safe end represent the most vulnerable component from the viewpoint of fatigue and EAF. Any generic assessment of EAF in BWRs must therefore start with the feedwater nozzle. Because of the background of cracking in the FW nozzle, several design modifications were made. The background and the reasoning for the design modifications are provided in the next subsection.

4.1.1 Feedwater Nozzle Background

The feedwater nozzle is used to inject feedwater to the vessel and this in turn produces steam. After plant heat-up, when it is time to start making steam, the feedwater is injected into the nozzle. The feedwater is initially cold (prior to FW heaters being effective) and can result in thermal shock on the nozzle. The thermal sleeve separates the nozzle (which is hot after heat-up) from the cold feedwater and essentially protects the nozzle. Early designs of the BWR thermal sleeves were of the slip fit or interference fit design (Figure 4-1). Use of the slip or interference fit designs was to allow easy disassembly and removal of the thermal sleeve. This design led to two significant problems in BWRs: i) temperature cycling and ii) flow induced vibration (FIV). The slip fit design (and the interference fit which tended to lose the interference over time due to creep) resulted in leakage past the seal allowing the cold feedwater to mix with the high temperature downcomer flow surrounding the nozzle. The turbulent mixing of the cold and hot water led to high frequency (0.1 to 1.0 Hertz) temperature cycling. The cracking seen in BWR FW nozzle was attributed to fatigue initiation from thermal fatigue. The fact that the nozzles were clad with stainless steel further exacerbated the problem since the thermal stresses were higher in the clad due to higher thermal expansion coefficient when compared to that of the low alloy steel. Once cracks initiated due to thermal fatigue, the stresses due to system cycling during plant operation led to subsequent fatigue crack growth in the nozzle blend radius and the nozzle annulus region. Crack depths ranging from 0.5 to 1.5 inches were observed in several BWR plants.

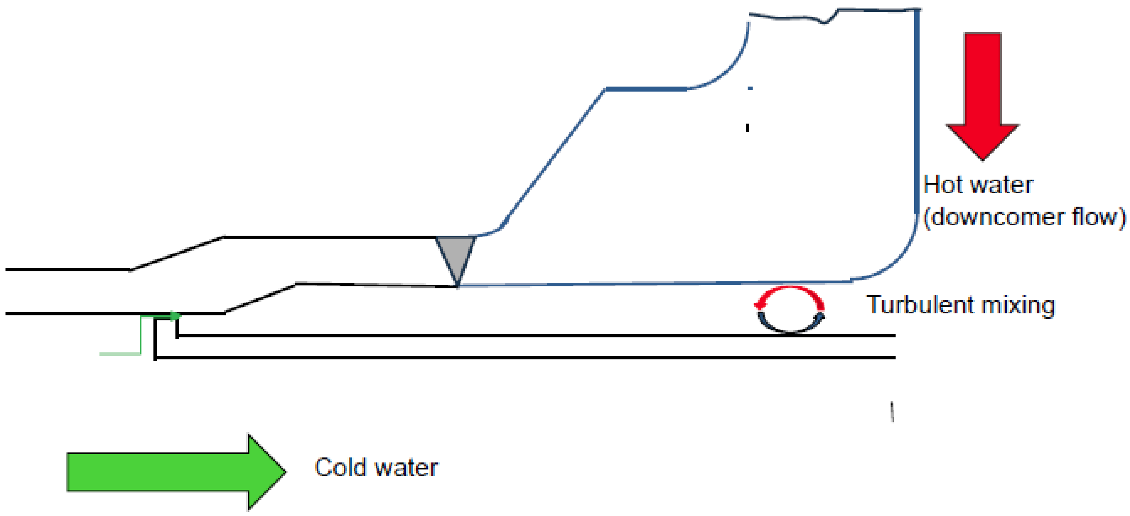


Figure 4-1
Slip Fit FW Sparger Design

Several steps were taken to address the feedwater nozzle fatigue problem:

- The thermal sleeve was redesigned to eliminate the leakage associated with the slip/interference fit designs. Two types of designs were implemented. The first design referred to as the 'triple sleeve design (Figure 4-2) had two spring loaded seals to prevent leakage of cold water. Any leakage from the first seal would be blocked by the second seal. The original intent of the triple sleeve design was to make sure that it was removable, but in reality it has been difficult since springs were so tight that easy removal was not possible. The tight fit from the seal of course was a major factor in the success of the design. Leakage has been virtually eliminated and there have been no instances FW nozzle cracking. An alternate design, the welded sparger design (Figure 4-3) which was developed to prevent flow induced vibration (similar to that observed at Millstone) also has the benefit of no leakage.
- The clad has been removed in all plants where the FW nozzle had a clad (newer plants did not have cladding). The clad was eliminated so that UT of the nozzle could be performed from the OD. Since cyclic stresses on the clad are higher (because of the higher thermal expansion coefficient), elimination of the clad reduces the potential for fatigue crack initiation.
- System modifications were implemented to assure that the FW sparger and piping run full during low FW flow operation (e.g. hot standby).

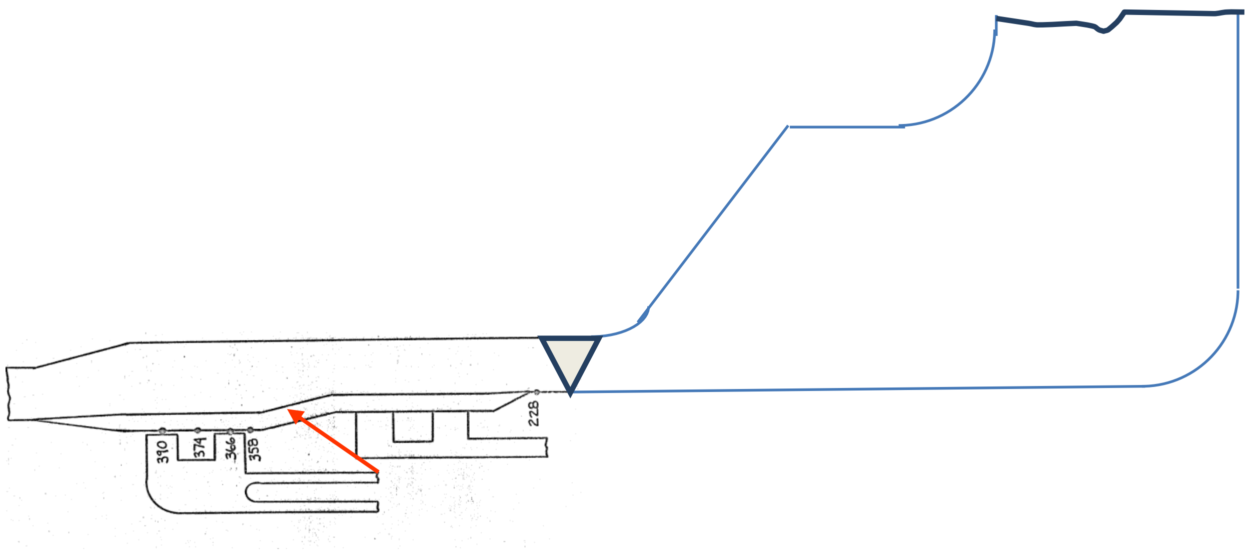


Figure 4-2
Triple Thermal Sleeve Design

4.1.2 Different Feedwater Nozzle Designs

As stated earlier, there are three different FW nozzle/safe end designs:

- The triple sleeve design shown in Figure 4-2 is used in most BWR designs.
- The welded FW sparger design shown in Figure 4-3 uses a tuning fork safe end design and the thermal sleeve is welded to the safe end. This design has been used widely in Japan, but has seen limited use in US BWRs (River Bend is an example).

The designs described above mostly use stainless steel safe ends. There are some plants that have Alloy 600 safe ends, mostly with triple sleeve spargers.

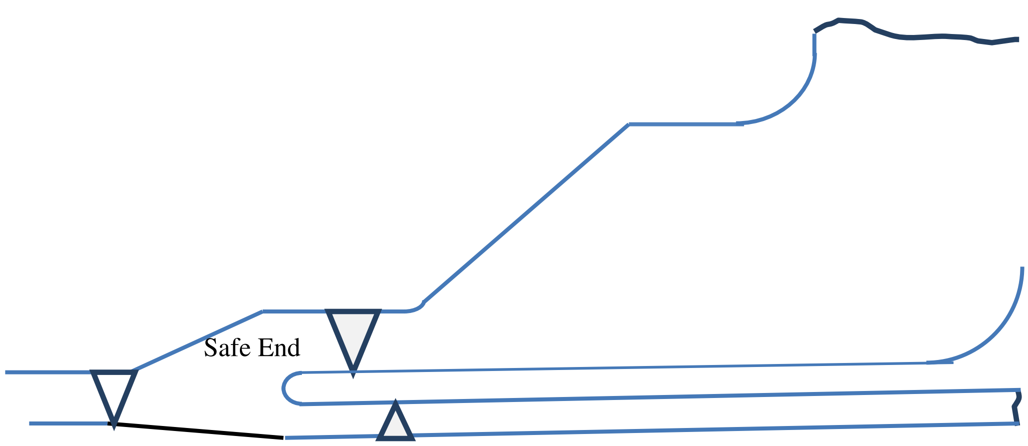
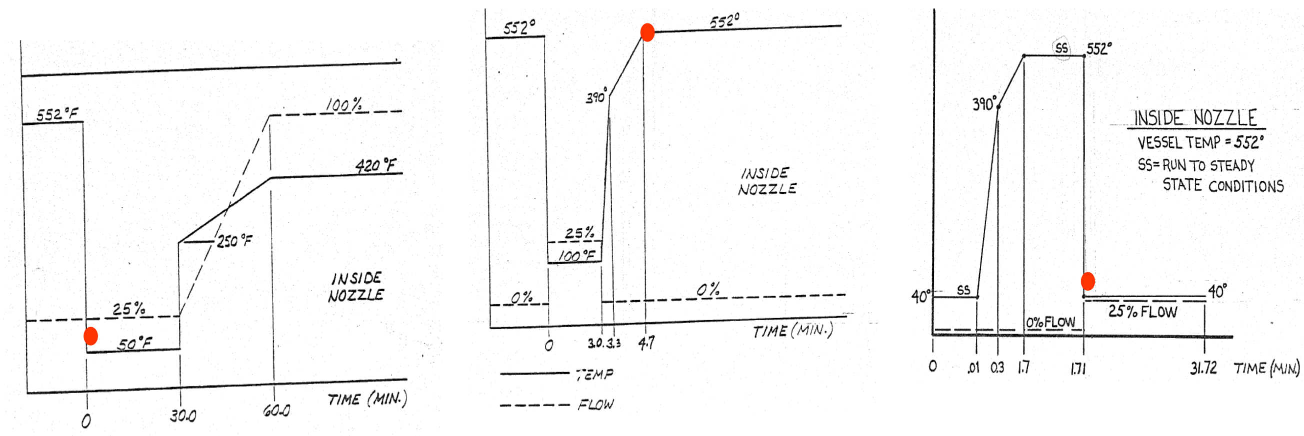


Figure 4-3
Welded in Thermal Sleeve Design

4.1.3 Loading on Feedwater Sparger

The pressure and temperature cycling on the different BWR FW sparger designs are very similar (Figure 4-4). Over 90% of the fatigue usage comes from three transients: i) turbine roll where the cold feedwater is turned on in a hot nozzle; ii) hot standby where the plant is on hot standby (and the plant is not producing power) at temperature and feedwater is turned on occasionally and iii) loss of feedwater pumps when the feedwater pumps are turned on after being unavailable for some time. All the transients assume limiting temperatures (highest possible deltaT) and most BWR plants include these bounding transients. Other than some differences in the number of such cycles, these transients are essentially the same for all BWRs. Because of this, the BWR FW nozzle is amenable for the generic analysis.



Turbine Roll | Hot Standby | Loss of FW Pumps

Figure 4-4
Governing Generic FW Nozzle Transients

4.2 Recirculation Nozzle/Safe End Evaluation

The recirculation outlet nozzle is not a high fatigue usage component and may even be acceptable with a straight forward multiplication of the ASME Code CUF with the F_{en} factor. The major transient of interest is the startup shutdown cycle. Temperatures in the recirculation outlet nozzle follow that in Region B of the vessel. Reflecting the absence of any significant thermal cycling, the outlet nozzle has no thermal sleeve. In summary, the generic option is feasible and can be implemented without any reanalysis of existing stress reports.

Like the recirculation outlet nozzle, the inlet nozzle is not a high fatigue usage component. However, it has a thermal sleeve because the nozzle connects to the recirculation riser pipe which ultimately connects to the jet pump assembly. The main differences between different BWRs lie in the thermal sleeve configuration. There are three basic configurations of the thermal sleeve. Type A thermal sleeves, known as "tuning fork type" for their shape, Type B thermal sleeves and Type C thermal sleeves, known as "trombone type". All three types are shown in Figure 4-5. The number of welds in the thermal sleeve depends on its configuration. Type A thermal sleeves typically have one weld (which connects the thermal sleeve to the nozzle safe end), Type B thermal sleeves typically have two welds, and Type C thermal sleeves typically have three.

Table 4-1 from Reference 12 shows the different material used in the construction of the thermal sleeves for BWR/3 through BWR/6 plants. The piping is typically either Type 304, Type 316L, or Alloy 600. It should be noted that this table presents the original material of construction, but in many cases, the piping as well as the safe end might have been replaced with L-grade materials.

Fatigue usage in the recirculation nozzle/safe end and piping is generally low. All three components may be acceptable with a straight forward multiplication of the ASME Code CUF with the Fen factor. As noted above, the main differences between the different designs lie in the thermal sleeve attachment, but this is unlikely to affect the ASME Code fatigue analysis for the safe end or the nozzle. The main transient of interest is the startup-shutdown cycle; there are no other cycles that contribute significantly to the ASME CUF. Because of the differences in material for the safe end and the attached piping, it may be necessary to consider 2-3 design configurations for the generic EAF evaluation.

Table 4-1
Materials used in the Recirculation Inlet Safe End/Piping in BWRs

Plant	Thermal Sleeve Material
Millstone, Pilgrim	Type 304
Monticello	Type 304
Quad Cities 1,2, Dresden 2	Type 304
Dresden 3	Type 316NG
Garoña	Type 304
Vermont Yankee	Type 304
Fermi	Type 304
Hope Creek 1, Limerick 1,2, Susquehanna 1,2	Type 316L
Browns Ferry 1	Type 304
Peach Bottom 2,3	Type 304
Browns Ferry 2,3	Type 316NG
Brunswick 1,2	Alloy 600
Hatch 1, Fitzpatrick	Type 304
Cooper	Type 316L
Hatch 2	Alloy 600
Duane Arnold	Alloy 600
BWR/5	Type 316L
BWR/6 (not incl. Cofrentes)	Type 316L
Cofrentes	Alloy 600

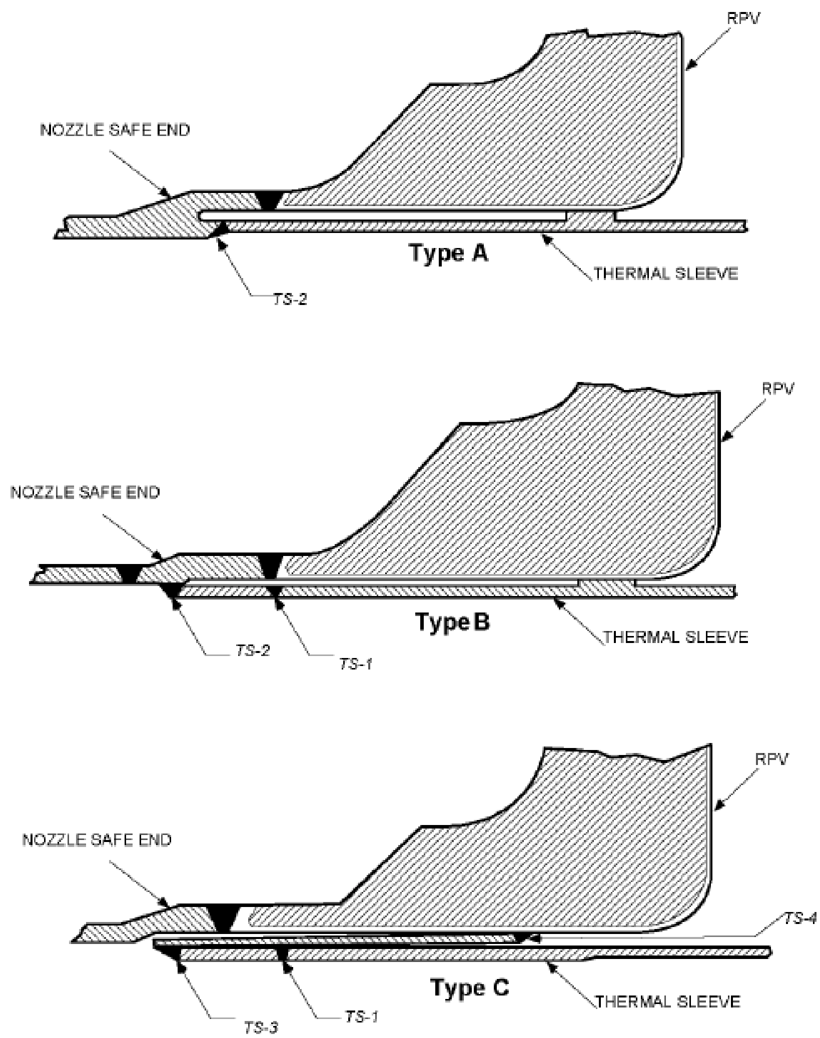


Figure 4-5
Recirculation Inlet Nozzle Configurations

4.3 Core Spray System

As in the case of the recirculation inlet nozzle, the main design differences are in the attachment of the thermal sleeve to the safe end. The configurations described here are based on the description in Reference 13. The original designs of the thermal sleeves in the vessel nozzles varied considerably from plant to plant. Many plants have modified their core spray safe ends and thermal sleeves as part of a core spray external piping replacement. Therefore, it is important to review plant data and select the as found design configurations.

There are different designs of thermal sleeves, but they can be grouped into three categories: i) welded-in thermal sleeves, ii) mechanically connected thermal sleeves, iii) slip-fit thermal sleeves. In the first category, the thermal sleeve is welded to the safe end and to the junction box (Figure 4-6). The second category covers thermal sleeves that have a threaded connection to the safe end (Figure 4-7), that have a bayonet connection or that are press-rolled against the safe end. In the third category shown in Figure 4-8, the thermal sleeve has a slip-fit attachment inside the safe end.

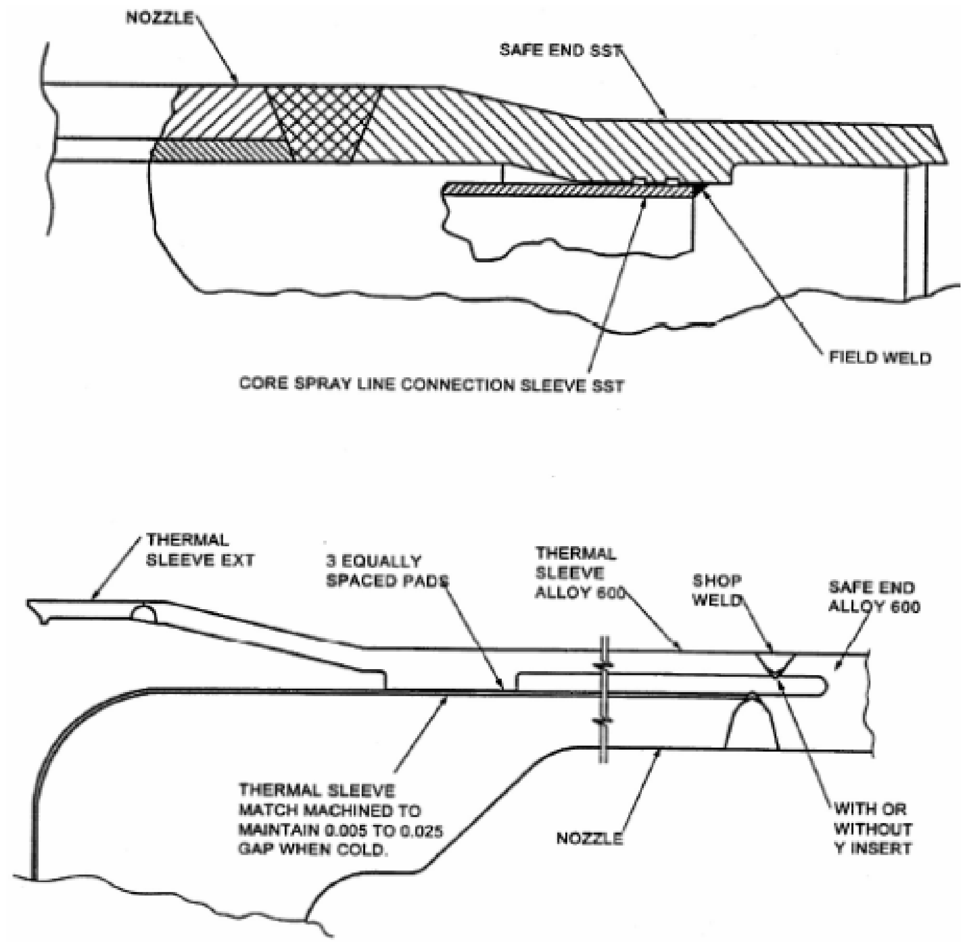


Figure 4-6
Core Spray Safe End Design: Welded Thermal Sleeve

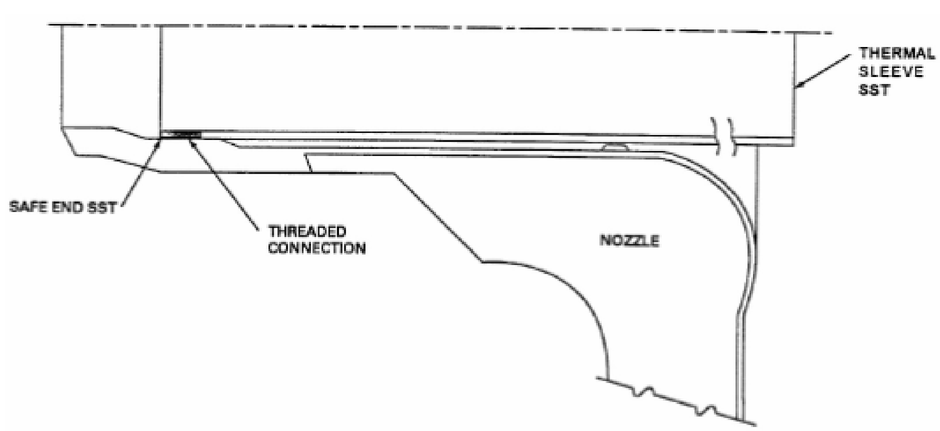


Figure 4-7
Core Spray Safe End Design: Threaded Thermal Sleeve

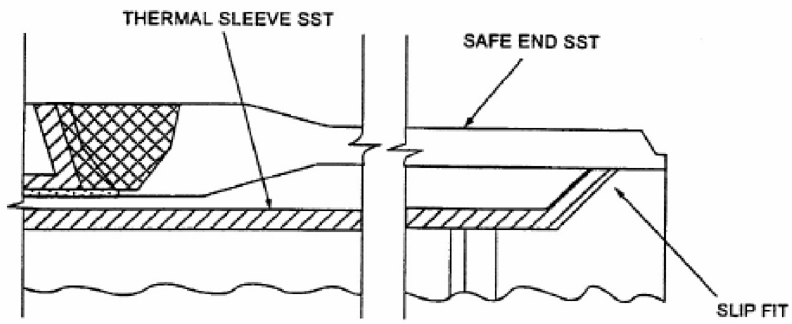


Figure 4-8
Core Spray Safe End Design: Slip Fit Thermal Sleeve

The core spray safe end and piping has experienced extensive cracking due to IGSCC in some plants and have been changed out. In most cases, the stainless steel piping and safe end have been changed to carbon steel. NUREG/CR-6260 classified the core spray safe end as ‘acceptable’ from the EAF viewpoint in newer vintage BWRs, but shows relatively high usage in older BWRs. This may be due to the use of carbon steel safe ends in the new plants vs. stainless steel in older plants. The fact that several plants replaced the stainless steel safe ends with carbon steel adds further confusion to the EAF assessment. Sample comparisons of final safe end configurations may be needed before any conclusions can be made on the applicability of generic EAF assessment.

The core spray safe end and piping are not subjected to any severe thermal or stress cycling other than startup-shutdown and inadvertent actuation of core spray. The later transient may contribute to higher usage, but the CUF can be reduced by eliminating conservatisms. Fundamentally, the core spray safe end and piping do not experience high fatigue usage and should be acceptable for EAF and 60-year life with some modest analytical effort to reduce conservatisms.

As in the case of the core spray nozzle and safe end, the core spray piping is not a high fatigue component. The cycles of interest are the startup shutdown cycles and the inadvertent actuation of core spray. A review of typical core spray piping analysis may show the conservatisms in current analysis. In any case, EAF analysis to show acceptability should not be a major challenge.

4.4 Feedwater Piping

Like the feedwater nozzle, the feedwater piping is subject to the same severe transients as the FW nozzle. Thus the calculated ASME 40-year fatigue usage is expected be relatively high. With the inclusion of 60-year life and the EAF Fen factors, the resulting fatigue usage, CUFen, is expected to be well in excess of 1.0. Furthermore, unlike the FW nozzle, the piping layout can be very different from plant to plant. This raises the question of whether the generic ‘safe harbor’ approach is feasible for the EAF analysis of FW piping.

Although the use of gradual temperature changes (ramp change) instead of step changes will reduce the fatigue usage drastically, the fact that piping configurations are different makes it difficult to use generic EAF analysis for FW piping. Piping layout differences should not a big difference in CUF and in theory, a demonstration for one piping configuration should, in principle, apply to other configuration. However, it may be difficult to develop a case for generic applicability.

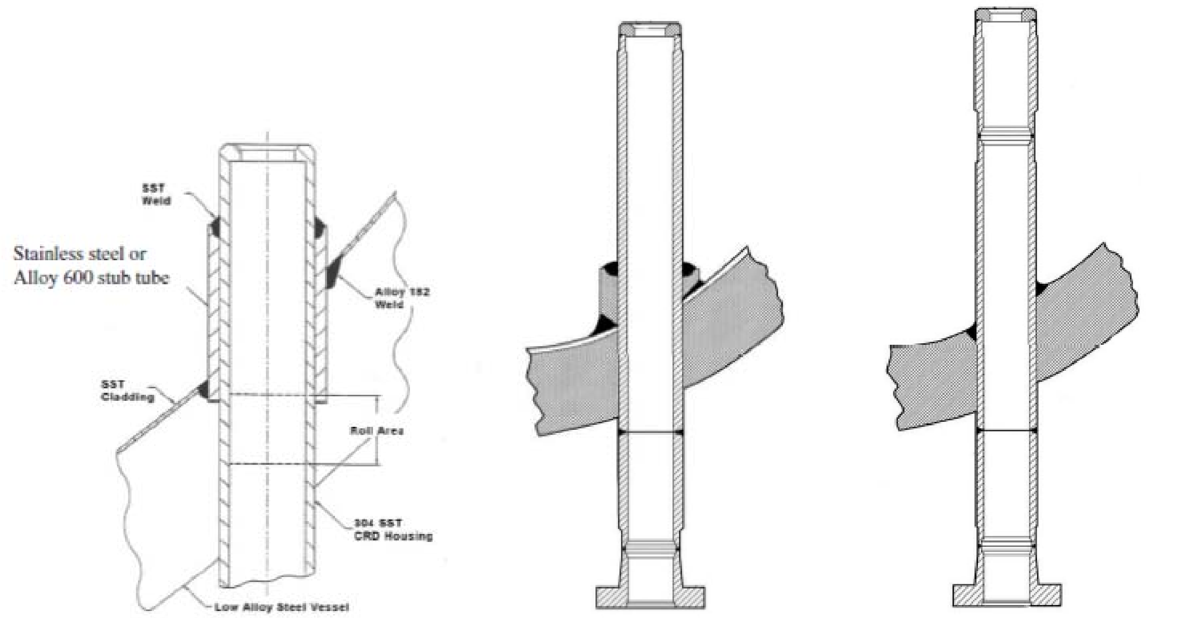
The alternate approach described in the FW nozzle analysis may be a more realistic option. This approach retains the step change assumption, but address only the part related to simplified elastic plastic analysis which is almost always a factor in FW piping fatigue analysis. As stated earlier, it is well known that the K_e factor in the ASME Code is extremely conservative. If elastic plastic finite element analysis can be used to show that the actual K_e factor is much less than the values specified in NB-3228 of Section III, ASME Code, a generic alternate K_e approach could be used. This would be somewhat similar to Code Case N-779 (currently not yet approved by the NRC) but would be different in that it would be developed specifically for the FW piping and for the specific transients. The likelihood of the new K_e approach being approved by the NRC is higher since it is narrower in scope (applicable for the specific FW transients) and is intended for limited use in EAF analysis. The advantage of using the revised K_e is that any BWR plant performing EAF analysis can implement this with minimal additional work. All that is needed is to replace the Code K_e values in the existing ASME fatigue analysis with the revised K_e. This will reduce the ASME fatigue usage drastically, and in turn reduce the CUF_{en}. As with the FW nozzle analysis, the K_e based approach has the advantage that actual plant transients are not needed to justify the higher ramp time. Thus, it has higher generic acceptability.

4.5 RHR Nozzle and Piping

The Residual Heat Removal (RHR) nozzle and piping are identified in NUREG/CR-6260 as one of the higher fatigue locations. The high fatigue usage in the RHR system is in the piping rather than in the nozzle. There are differences between the RHR piping in early vintage BWRs and newer BWRs. In the older BWR/2-4 plants, the RHR line feeds into the recirculation system and is made of stainless steel. In the older plants, it is likely that the piping analysis was performed using B31.1 code which does not explicitly require CUF calculation. In such cases, new analysis to determine the CUF using ASME Code Class 1 criteria would be needed for the EAF usage assessment. The highest CUF in the older vintage plant is in the tapered transition in the stainless steel pipe. In the newer BWR/6 plants, the coolant return enters the feedwater line and is composed of both stainless steel and carbon steel segments. The CUF (original value from the Code analysis without considering EAF) in the newer vintage plants is highest in the carbon steel segment. This is further exacerbated in the EAF analysis with the higher Fen factor for carbon steel. The CUF including EAF is therefore the highest in the carbon steel segments. Generic evaluation of RHR is possible but will require two cases, one for the old design and one for the new.

4.6 Vessel Lower Head (CRDM Penetrations)

The location with the highest CUF in the BWR vessel head region is the bottom head CRD penetration. There are two fundamental designs for the CRDs: i) the stub tube design CRD in BWR/2-5 plants where the stainless steel CRD housing is welded to a stub tube which in turn, is welded to the bottom head and ii) the directly welded design in BWR/6 plants where the Alloy 600 housing is directly welded to the bottom head. Figure 4-9 shows the two designs. In the stub tube design, there are two variations, the set-in design where the stub tube is recessed in a socket in the bottom head prior to welding and the set-on design where the stub tube is set on the weld prep and welded to the bottom head. The highest stress and fatigue usage location is the weld attachment to the vessel in both the stub tube and the directly welded designs. The set-in and set-on stub tube designs are somewhat similar from the stress viewpoint. However, the stress in the stub tube design is different than the directly welded BWR/6 design not only because of the difference in the housing material (stainless steel vs. Alloy 600) but also because the stub tube offers higher flexibility and slightly lower stresses. Because of the significant differences in the materials and design configuration, separate analysis would be needed to assess EAF usage for the two configurations.



Set-in Stub tube Design (BWR/2-5 Plants)	Set-on Stub tube Design (BWR/2-5 Plants)	Directly welded Design (BWR/6 Plants)

Figure 4-9
BWR Bottom Head CRD Penetrations

4.7 Applicability to the Advanced BWRs

The previous discussion focused entirely on operating BWRs. The new generation reactors such as the Advanced Boiling Water Reactor (ABWR) and the Economical Simplified Boiling Water Reactor (ESBWR) share some common features as well as some significant differences. Table 4-2 shows a comparison of the latest version of the current BWR/6 plant with the ABWR and the ESBWR. Table 4-2 is not intended as a comparison of all features of the three designs, but only as it applies to the EAF issue and the applicability of the generic approach.

There are components such as the recirculation inlet and outlet nozzles that do not exist in the ABWR and ESBWRs. Also, because of the passive, gravity driven ECCS system in the ESBWR, the core spray system may not be applicable for the ESBWR evaluation. However, the limiting component, the feedwater nozzle is common to all three systems. The severity of the thermal cycling for the FW nozzle is about the same for all three designs, but the ABWR and ESBWR are designed for a 60-year life rather than the original 40-year design life for BWR/6 with the accompanying lower number of cycles. So the scope of the EAF problem is slightly reduced for the advanced plants. As discussed before, two approaches were proposed for the generic analysis: one based on actual transients being more gradual (ramp) rather than a step change and the second based on refining the K_e factor. The first option requires that we use the BWR transients for the ESBWR (for which plant data is not available). Since the designs are not that different, one can make the case that use of BWR data for the ESBWR is not an unreasonable approach. The second option of redefining the K_e factor is definitely applicable for both the current and the advanced BWR designs. Thus, the generic approach appears to be applicable to both existing BWRs and the advanced reactors. For components such as the depressurization valve and isolation condenser (DPV/IC) that are not explicitly covered in current BWR analysis, additional ESBWR specific evaluation may be required. However the concepts used in the generic approach used in this report are applicable across the board for the current BWRs and the ABWR and ESBWR designs.

Table 4-2
Comparison of Current BWRs with Advanced ABWR and ESBWR Designs

Feature	BWR/6	ABWR	ESBWR
Feedwater nozzle	Triple sleeve spring loaded sparger or single welded thermal sleeve	Single welded thermal sleeve	Double thermal sleeve welded to the safe end
Recirculation system	Two loop recirc system with jet pumps;	Vessel mounted reactor internal pumps	Natural circulation
ECCS	High pressure core injection (HPCI) and core spray (HPCS)	High pressure core flooder	Passive gravity driven system
Steam nozzle	Conventional design	Conventional design	Nozzle with flow restrictor venturi
Bottom head penetration (CRD and in-core nozzles)	Similar nozzle designs but users locking piston CRDs	Similar nozzle designs but users fine motion CRs	Similar nozzle designs but users fine motion CRDs
Core spray nozzle			
Original design basis	40-year design life	60-year design	60-year design
Limiting EAF component	FW nozzle/safe end/piping	FW nozzle/safe end/piping	FW nozzle/safe end/piping
Thermal cycles	Thermal cycles are similar for all 3 designs, but BWR/6 is analyzed for 40-year life	Thermal cycles are similar for all 3 designs, but ABWR is analyzed for 60-year life	Thermal cycles are similar for all 3 designs, but ESBWR is analyzed for 60-year life

4.8 BWR Summary

In summary, it appears that most of the NUREG/CR-6260 locations for BWRs are amenable to a generic analysis. The core spray system may represent the most significant challenge from the standpoint of component variability, however usage factors are low for this system. The feedwater system may be challenging from the standpoint of high usage, but techniques appear to be available to reduce the usage considerably.

Table 4-3 lists a set of tasks that addresses the majority of the locations. They are listed in order of priority based on providing the highest benefit to the BWR industry. The sequence of analysis is to select the most challenging EAF components (FW nozzle and FW piping) and show EAF adequacy. If the FW nozzle and piping can be shown to be acceptable, it is highly likely that other components will show acceptable results.

Table 4-3
Implementation Plan for BWR Components

Priority	Component	Different Designs	Methodology	Discussion
1	FW nozzle/safe end	Triple sleeve sparger	Ramp rate change	Assess effect of ramp rate based on plant transient data to show adequate EAF margin
		Welded sparger	Ramp rate change	
		Alloy 600 safe end	Ramp rate change	
		Triple sleeve sparger	K_e model change	Use step change transient, but revise K_e factor based on elastic-plastic analysis
2	FW piping	Single piping run	K_e model change	Revised K_e based on elastic plastic analysis
3	Recirc outlet nozzle	Single design	Use existing analysis but reduce conservatism	Reducing conservatism to show adequate EAF margin
4	Recirculation inlet nozzle	Tuning fork design	Use existing analysis but reduce conservatism	EAF margin can be demonstrated with minimal effort, using existing ASME Code report
		Welded sparger design	Use existing analysis but reduce conservatism	
5	Core spray nozzle/safe end	Original material welded sleeve	Use existing analysis but reduce conservatism	There have been several core spray safe end replacements and materials have changed.
		Select different repair material		
6	Core spray piping	Single piping run	K_e model change if necessary	EAF margin can be demonstrated with minimal effort, using existing ASME Code report
7	RHR piping	Old design	Use existing analysis but reduce conservatism	Reducing conservatism to show adequate EAF margin
		New design		
8	Bottom head CRD penetration	Set-in stub tube	Use existing analysis but reduce conservatism	Reducing conservatism to show adequate EAF margin
		Set-on stub tube		

5
FEASIBILITY STUDY RESULTS - PWRS

For PWR plants, NUREG/CR-6260 identified six locations that were expected to be representative of components that had higher CUFs and/or were important from a risk perspective. Those locations are:

1. Reactor vessel head
2. Vessel inlet and outlet nozzles
3. Surge line
4. Charging system nozzle
5. Safety injection nozzle
6. RHR system piping

A discussion of the feasibility of performing generic calculations for each of these locations is discussed below. A Westinghouse design was used for the purposes of this feasibility study

5.1 Reactor Head and Inlet/Outlet Nozzles

For Westinghouse designed plants, the reactor vessel shell and lower head, and the reactor vessel inlet and outlet nozzles were selected as locations for evaluation in NUREG/CR-6260. It is believed that all plants who have submitted license renewal applications to-date have successfully evaluated these locations for environmental fatigue without the need for explicit reanalysis. Typically, the maximum design basis CUF is multiplied by the maximum F_{en} for the ferritic material, resulting in U_{en} values under 1.0. If 60 year projected cycles remain under 40 year design values, which is typically the case, no additional monitoring or analysis is required. Occasionally, a cycle-based fatigue approach is required to meet acceptance criteria at 60 years, either from the standpoint of projected cycles exceeding 40 year design assumptions, or because the plant-specific analysis had a CUF value somewhat higher than average. Nevertheless, the ferritic NUREG/CR-6260 locations associated with the reactor vessel have not required detailed stress modeling or new fatigue analysis to meet license renewal commitments. Therefore, no generic approach to addressing these locations is warranted.

5.2 Surge Line

Westinghouse surge lines vary in size from 10” to 16”, include both schedule 140 and 160 wall thicknesses, and have generically unique geometric and support characteristics. Pressurizer surge nozzles on the lower head of pressurizers include both 14” and 16” designs and are fabricated with either a cast or spun head design. In cases where the surge line piping is a different size than the pressurizer nozzle, a reducer connects the nozzle safe end to the surge piping. In addition, many of the pressurizer nozzles, including the spray nozzle, have dissimilar metal welds that have been mitigated with various weld overlay designs, requiring modern fatigue analyses. Designs therefore vary considerably among the plants.

With respect to loading, plant heatup and cooldown operating procedures vary among plants, requiring unique (plant-specific) loading definitions for the pressurizer components. For these reasons, components related to the surge line and pressurizer are generally not well-suited to generic analyses.

5.3 Charging System Nozzle

Westinghouse charging nozzles have multiple different designs. General variations can be summarized as follows.

- Butt welded or socket welded (butt welded designs are 3” or 4”)
- With or without thermal sleeve
- 30° or 45° nozzle reinforcement transition

Each branch nozzle is an austenitic stainless steel assembly. The older vintage socket-welded nozzles were designed to ANSI B31.1. Due to the age of these plants, it is believed that most if not all with this design have already performed the EAF evaluation.

For the butt-welded design, the limiting configuration is one without a thermal sleeve, which was later deleted due to concerns about flow-induced vibration. Four-inch nozzles are rare, and are therefore not included in any generic analysis.

Because of the severity of the Loss of Letdown events, and relatively large number of cycles relative to design, the charging nozzle is typically considered to be one of the highest fatigue locations in the plant and often requires a significant removal of conservatism from the original analyses. Successful strategies to disposition these nozzles in the past include a combination of nonlinear plastic analysis, to reduce the simplified elastic-plastic penalty factor, K_e, and the use of stress-based fatigue monitoring to account for variations in severity and number of occurrences of the plant transients.

Generic analyses for this location can be performed. It is anticipated that a 3-inch nozzle without a thermal sleeve will be the bounding configuration and should be selected for the generic analysis.

5.4 Safety Injection Nozzle

Westinghouse high head safety injection branch nozzles are designed similarly to the charging nozzles, but have some additional variations. The 3” butt welded nozzles are attached to 1-1/2” safety injection line piping with a reducer that must be considered as part of any EAF analysis. As with charging nozzles, the limiting configuration is the one without a thermal sleeve.

Separately, some plants include 6” high head safety injection nozzles. Similarly, the absence of a thermal sleeve represents the limiting condition.

Safety injection transients involve injection of ambient fluid through an initially hot nozzle and therefore in practice are not extremely less severe than design transients. However, actual numbers of cycles are typically much fewer than postulated by design. Therefore, use of a modern ASME NB-3200 analysis combined with a cycle-based fatigue monitoring approach can typically be used to successfully manage EAF of the component.

A generic analysis is expected to be successful and should include both 3-inch and 6-inch nozzles, both without thermal sleeves.

5.5 RHR System Piping

For the residual heat removal (RHR) piping, a generic approach to evaluating these locations does not appear to be advantageous. The maximum fatigue usage factor in the RHR piping for the newer vintage Westinghouse plant in NUREG/CR-6260 was located at the RHR inlet branch nozzle on the reactor coolant system (RCS) hot leg piping. At this particular sample plant, the branch nozzle was subjected to thermal stratification, caused by in-leakage from a nearby check valve. Fatigue evaluations of this type tend to be highly plant-specific in nature. Moreover, this type of loading is now addressed primarily through inspection programs, per guidance provided in MRP-146.

When stratification is not present, the fatigue usage at this hot leg branch nozzle is negligible, because it experiences only suction flow from the RCS. The high fatigue location in the RHR piping is then typically located at the point where the RHR piping tees into the medium head safety injection piping, as was the case for the older vintage Westinghouse plant in NUREG/CR-6260. Because piping geometry, mechanical boundary conditions and system designs are very plant-specific in nature, a generic approach to addressing these locations does not appear to be warranted.

5.6 PWR Summary

In summary, the prospects for performing generic analyses that consider all of the PWR 6260 locations are not as favorable as for BWRs. While some locations (e.g., the vessel head and vessel inlet and outlet nozzles) do not warrant a generic analysis because of low fatigue usage, other locations such as the surge line and the RHR nozzle are not amenable to generic analysis due to the multiple design configurations that exist. Two locations, the charging nozzle and the safety injection nozzle appear to be appropriate candidates for generic analysis as shown in Table 5-1.

Table 5-1
Implementation Plan for PWR Components

Component	Configurations	Generic Analyses
Charging Nozzle	4" BW[2]; no TS 3" BW; no TS 3" BW; with TS 2" SW[3]; with TS	3" BW; no TS
High Head Safety Injection Nozzle	3" BW; no TS 3" BW; with TS 6" BW; no TS 6" BW; with TS	3" BW; no TS 6" BW; no TS

Table notes:

1. BW = butt welded; SW = socket welded; TS = thermal sleeve.
2. Rare configuration.
3. Already addressed for plants with this design.

6
SIMPLIFIED STATISTICAL EVALUATION OF ENVIRONMENTAL FATIGUE

During development of the work discussed in the previous chapters an effort was directed at developing a statistical approach that would better quantify the impact of uncertainty in the CUF calculation. The approach does not involve a Monte Carlo sampling of the variables affecting factor. The proposed statistical approach analytically combines variances of the variable uncertainties leading to an acceptable estimation of the total variance of the CUF. This approach is documented in a paper (14) and is provided in Appendix A.

Additionally, it was recognized that industry may need a simple, clear way of presenting a fatigue statistical approach that the ASME Section III Subgroup on Fatigue could use to set deterministic safety margins on fatigue. Such an approach may be palatable also to the NRC since it could be validated by the Monte Carlo analysis effort NRC RES is applying to primary system piping fatigue phenomena in their xLPR initiative. The approach uses the concept of "reliability" in evaluating the failure susceptibility of a component. This approach and the statistical evaluation of uncertainties (similar to that presented in Appendix A) that provides the input to the reliability concept is presented in Appendix B (author: David A. Steininger).

7 CONCLUSIONS

The results of the evaluations described in Sections 4 and 5 demonstrate that many of the components that require evaluation per NUREG/CR-6260 could be evaluated in a generic manner. It is likely that for many of these, it would be possible to show that environmental effects are bounded by conservatisms inherent in the original fatigue design analysis. For BWRs, all of the 6260 locations appear to be amenable to generic analyses, although the core spray system involves sufficient design variability to make a generic evaluation of that component challenging. For PWRs, the variation in design and operating transients are more complex than in BWRs and consequently only two of the 6260 locations are judged to be amenable to generic analysis.

While these results indicated that a significant number of locations could be addressed by generic analysis, other issues were considered in deciding whether to proceed. First, the effort to perform the required analyses and obtain approval from the NRC was estimated to cost between $700k and $900k and would take a minimum of two years. Given anticipated annual funding available, that schedule would likely need to be extended by one to two years.

The second consideration, earlier discussed in Section 2, relates to the number of utilities that would be able to take advantage of the project results. It was observed that most existing plants have already performed the EAF calculations necessary to satisfy license renewal requirements. By the time a generic methodology could be developed and approved, only a handful of plants would be in need of its use. For new plants the situation was similar in that an overwhelming need was not demonstrated. While it is likely that most existing plants that will submit a request for a subsequent license renewal (80 years) will need to perform additional EAF calculations, the ability of the generic analysis to demonstrate acceptable performance to 80 years is uncertain. It is likely that plant-specific analyses will be required for subsequent license renewal.

Overall, the limited need for the generic results did not appear to warrant the estimated cost to complete the project. As such, the proposed project has not been pursued.

8
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A

AN APPROACH FOR A STATISTICAL EVALUATION OF UNCERTAINTY IN ASSESSING FATIGUE USAGE INCLUDING ENVIRONMENTAL EFFECTS

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Y. S. Garud
SIMRAND, LLC
San Jose, California, USA

David A. Steininger
Electric Power Research Institute
Palo Alto, California, USA

Ken Wolfe
Electric Power Research Institute
Palo Alto, California, USA

Abstract

The assessment for adequacy in managing the effects of fatigue in the ASME Code Class-1 (pressure boundary) components is based on a calculated measure of the projected fatigue damage. This measure is the highest cumulative usage factor (CUF) in a given component under a specified set of cyclic loadings and their expected number of repetitions. The Code-based calculation of CUF and its adjustments for potential environmentally-assisted fatigue (EAF) damage accumulation utilize a multitude of inputs, and conservative assumptions and applied margins. To support the extended service life beyond the original design, or longer life of new designs, changes in inputs and/or conservative assumptions used in these deterministically calculated CUFs are often made to meet a deterministic performance criterion. This makes the impact of uncertainty in the inputs and/or changes in the conservative adjustments difficult to assess.

This paper presents a generic, engineering approach for estimation of the uncertainty distribution of CUF based on the expected statistical characteristics of input variables used in the calculation of EAF-based CUF. The approach does not involve Monte Carlo sampling. The proposed statistical approach analytically combines variances of the inputs leading to an acceptable estimation of the total variance of the CUF. The approach does not require specification of full probability distribution(s) for the input variables, nor is the dependence between variables a critical issue from the analytical point of view. Feasibility and limitations of the approach are discussed in relation to the NB-3200 and NB-3600 procedures of the ASME Code and the current F_{en}-based augmentation for environmental effects. This approach is further examined in the framework of stress–strength interference methodology to account for the uncertainty in the fatigue performance criterion, that can lead to a rational deterministic safety factor interpretation and its relation to a quantifiable measure of the probability of exceeding the fatigue performance criterion.

Introduction and Objective

Metal fatigue has been recognized as an important aging effect and a significant design consideration for ASME Code, Class 1 pressure vessels and piping components exposed to the light water reactor coolant environments. The assessment for adequacy in managing the effects of fatigue for specified service is done in the framework of sections NB-3200 and NB-3600 of the ASME Boiler and Pressure Vessel Code, Section III, Division 1 – Subsection NB (referenced below as *the Code*) [1]. This assessment is based on a calculated measure of the projected fatigue damage which is the highest cumulative usage factor (CUF) in a given component under the specified set of cyclic loads and their expected number of repetitions. The Code did (does) not explicitly account for or exclude the possibility of the accelerating effect of coolant environments on the damage due to fatigue, as compared with fatigue in air. Such an accelerating effect has been demonstrated in laboratory testing of small samples under certain combinations of the loading and environment conditions as tested [2]. As a result, the regulators have provided guidance, NRC Reg. Guide 1.207 [3] and basis, NUREG/CR-6909 [2] that augments the calculations of fatigue usage from NB-3200 and NB-3600 assessments.

The above calculation of Code based CUF and its augmentation for the potential accelerating effect of environment are performed deterministically, although it is generally recognized that the fatigue damage and various inputs used in its quantification have, and are influenced by, uncertainty associated with them. In both of the above assessments, that is, the Code procedure and its augmentation for environmentally-assisted fatigue (EAF), the uncertainty aspect is addressed implicitly by applying several fixed multiplicative adjustment factors to the expected fatigue life response. Additionally, since these assessments are done deterministically, the other component specific inputs, such as the loading severity, operating conditions, and expected number of loading cycles, are typically used with their own conservative factors or assumptions. The net result of such an assessment is expected to be overly conservative, with undefined actual margin and unknown (low) probability of fatigue initiation, both of which being non-uniform from component to component and from plant to plant.

The main objective of this paper is to present a generic engineering approach for estimating the expected total uncertainty in the calculated CUF which accounts for varied sources of uncertainty in the inputs that influence the CUF, including environmental factors. The approach is simplified in that it utilizes only the mean and standard deviation expected to characterize the input variables typically used in the calculation. The estimation of total uncertainty is characterized around the expected value of CUF in terms of its variance. The implementation utilizes exact algebraic expressions where possible or the Taylor series approximation in combining the various sources of uncertainty and addresses any significant correlation between inputs where necessary. The other objective is to discuss the application and results of the presented approach to assessing effects of uncertainty in the case of a typical component fatigue evaluation including EAF influence. The feasibility of the approach and its utility are demonstrated. It is argued that by utilizing the expected uncertainty in inputs, while maintaining the essential elements of conservatism embedded in the Code procedures and its augmentation for EAF, the effects of using more realistic and unbiased inputs can be assessed and factors with significant influence on probability of exceeding a specific value of CUF can be identified and ranked.

CUF Uncertainty Analysis Method

The uncertainty analysis proposed below utilizes the currently accepted method of fatigue assessment [1, 2] as a base model that computes a reference CUF value of interest in a deterministic manner. This base model, including the calculation of EAF, can be summarized with the following equation:

$$CUF_0 = \sum_i F_{en,i} \cdot n_i / N_i \qquad \textbf{Eq. A-1}$$

where,

i (subscript) refers to the i^th stress cycle or load-set pair defined in the Code calculation,

n_i/N_i is the incremental (partial) fatigue usage in air, in which:

n_i is the number of occurrences to be evaluated for the i^th stress cycle or load-set pair,

N_i is the allowable number of occurrences, given by the stress–life *("S–N")* design curve/relation in air, corresponding to the i^th fatigue stress amplitude (or intensity), and

$F_{en,i}$ is the environmental correction factor applicable for the conditions of ith usage.

The analytical expressions of F_{en} for material–environments of interest were described in [2], and revised [4] based on additional data and review. For example, in the case of austenitic stainless steels, using the revised expression:

$$F_{en} = \exp[-T' \cdot O' \cdot R'] \qquad \textbf{Eq. A-2}$$

where,

T' is a factor with linear dependence on water temperature,

O' is a factor dependent on oxygen content of water (constant for austenitic SS), and

R' is a factor with logarithmic dependence on the effective tensile strain rate.

Additionally, the adequacy for the specified/analyzed cyclic service and suitability from the standpoint of possible fatigue failure is assessed by comparing the computed total CUF to 1, where a value less than 1 is the accepted fatigue performance criterion.

Within the framework of above deterministic base model it is clear that the estimation of total CUF, as the main output of calculation, involves many sub-models and inputs all of which are subject to uncertainty, large or small. Given that an acceptable deterministic formulation has been selected in relating the output of calculation (CUF), or the performance criterion (CUF < 1), to significant variables (inputs), it is common to model the uncertainty about the expected values of the inputs by treating these as random variables or as deviations from the expected values. Since the expected or mean values used in the deterministic analysis are known inputs, as a minimum one additional parameter is needed to characterize the uncertainty in each of these inputs. Deviation from the expected value is considered or addressed here as the variability that accounts for the uncertainty, excluding any bias or errors (mistakes) in the method or in the process of estimating the expected values; e.g., the applicability of Miner's Rule and its extension to EAF as assumed in Equation A-1. Depending on the analytical convenience, the measure of this variability used below refers to relative uncertainty or the so-called coefficient of variation, CoV (the ratio of standard deviation to the mean), or standard deviation (or variance equal to the square of standard deviation).

An essential goal of uncertainty analysis is to allow for a realistic assessment of CUF while accounting for the resultant uncertainty in this determination. Therefore, while maintaining the links to the design based calculation of CUF, the Code procedure, and the regulatory guidance for EAF based on the F_{en} adjustment, the following alternative form of Equation A-1 is used as the basis for this analysis:

$$CUF = (a_1\, a_2)(k_m\, k_s\, k_r\, k_h) \sum_i F_{en,i}(n_i / N_{des,i}) \qquad \textbf{Eq. A-3}$$

where: k_m, k_s, k_r, and k_h are factors for material variability and data scatter, size effect, surface roughness effect, and loading history effect, respectively; $a_1=N_{des}/N_{dat}$ and $a_2=N_{dat}/N_{upd}$, where N_{des}, N_{dat}, and N_{upd} refer to the allowable cycles from the Code design curve, the corresponding air data curve, and updated air data curve as applicable, respectively. More generally the factors a_1 and a_2 may depend on the allowable number of cycles which can be accounted for by taking these two inside the summation; in any case these are fixed deterministic factors with no associated variability and are treated as constants as discussed in the next section.

For illustration, the S–N relation between the stress amplitude and number of cycles is described by a power-law dependence of the general form:

$$N = N_0 \cdot (S_a - S_0)^{-b} \qquad \textbf{Eq. A-4}$$

where N_0, S_0, and b are material dependent constants. Thus, in addition to the uncertainty in k_m, k_s, k_r, and k_h factors of Equation A-3, the uncertainty in applied stress amplitude S_a, in the number of applied or projected cycles of this stress amplitude, and in the associated environmental correction factor F_{en} (Equation A-2), for each load-pair or stress level i, affect the uncertainty in the estimated value of CUF. The total CUF uncertainty is then determined by combining the sub-factor uncertainties as described below.

By considering the summation term in Equation A-3 as an auxiliary variable U, it is noted that the CUF is equal to the product of five independent variables: k_m, k_s, k_r, k_h, and U. For the case of simple product of two independent random variables, $z=xy$, it can be shown that the CoV of product is related to the individual CoVs of x and y as: $C_z=C_x+C_y+C_xC_y$, where, and throughout this paper, *C(.)* is used to denote the squared coefficient of variation of the respective variable *(.)*. Generalizing this to the above case of five independent variables it follows that:

$$C_{CUF} = (1+C_{km})(1+C_{ks})(1+C_{kr})(1+C_{kh})(1+C_U) - 1 \quad \textbf{Eq. A-5}$$

In order to get the estimate of C_U for the auxiliary variable U (the summation term of Equation A-3 it is noted that U itself is a sum of the product of $F_{en,i}$, n_i, and $(1/N_i)$ for all the incremental usage estimates. In principle, since F_{en} is independent of N_i, it may appear that F_{en} and incremental usage can be treated as independent variables. However, in the case of several thermal transients it has been known and correctly noted that the peak stress intensity is likely to be affected by the ramp rate that is related also to the strain rate which influences the F_{en}, so that the dependence between S_a and F_{en} may need to be taken into account. Using the power-law dependence for the S-N relation (Equation A-4) it follows that the ith term inside the summation of Equation A-3 can be written as:

$$u_i = a \cdot F_{en.i} \cdot n_i \cdot S_i^b \quad \textbf{Eq. A-6}$$

where a is a constant and S_i denotes the difference $S_{a,i} - S_0$. Extending the use of Equation A-5 for the product form to combine the uncertainties in this case, including the possible dependence between $F_{en.i}$ and S_i with the correlation factor ρ_i, the following gives an estimation for $C_{u.i}$:

$$C_{u.i} \approx (1+C_{Fen.i})(1+C_{n.i})(1+b^2 C_{S.i}) + 2b\rho_i\sqrt{C_{Fen.i}C_{S.i}} - 1 \quad \textbf{Eq. A-7}$$

With regard to the CoV of F_{en} it is noted that the environmental factor is derived from the data and is assumed independent of the number of cycles (hence independent of the applied stress intensity) [2]; as such, the factor remains the same irrespective of the fatigue design curve and it has no associated uncertainty of its own other than that derived from its relation with the parameters of temperature, dissolved oxygen, strain-rate (and sulphur content depending on the class of material). These parameters also appear in the product form (Equation A-2) so that the 3-term form of Equation A-5 can be used to determine its coefficient of variation on log-basis, using the individual CoV values for oxygen, temperature, and strain rate.

From the deterministically calculated mean value of the ith incremental usage and the above estimate of its squared CoV (Equation A-7) the corresponding variance is given by $C_{u.i}(u_i)^2$. Based on the well known Taylor series approach [5, 6] the total variance in U due to all incremental usages is simply the sum of these individual variances. The squared coefficient of variation, C_U, to be used in Equation A-5 is thus obtained from the total variance and the deterministic mean value of U.

In this work the CoV values for the k-factors of Equation A-3 are derived (Table A-1 in the next section) to be consistent with their ranges suggested in [2]. With the value of C_U determined as above, Equation A-5 gives the estimate for combined uncertainty to be expected in the total CUF in the form of its coefficient of variation. Based on the form of Equation A-3 for CUF it is expected that a log-normal distribution for the calculated CUF would be a reasonable assumption to make to further utilize or interpret the results of above uncertainty analysis as discussed subsequently.

Table A-1
Statistical Characteristics of Fatigue Life Adjustment Factors

Adjustment Factors on Life	at 5%	at 95%	mean	standard deviation
Material & Data Scatter, k_m	1.744	2.261	1.992	0.158
Size Effect, k_s	1.200	1.400	1.298	0.061
Surface Finish, etc., k_r	2.000	3.500	2.684	0.460
Loading History, k_h	1.200	2.000	1.568	0.245

Summary of Inputs

The inputs characterizing various uncertainty parameters of the above CUF analysis are summarized in this section. In [2] the form of strain-life model for fatigue data, analogous to the S-N representation used in the Section III criteria document [7], has been expressed as follows:

$$ln(\mathrm{N}) = \mathrm{A} - \mathrm{B}\ ln(\varepsilon_a - \mathrm{C})$$

Here, ε_a is the applied strain amplitude (%), N is the number of cycles to failure, and A, B, and C are material–environment dependent parameters. The effects of material variability and data scatter were determined in terms of the model parameter A. The median value of 6.891 was estimated from the fatigue life data of several heats of austenitic stainless steels (in air for temperature range of about 21°C to 400°C), and it is considered representative of the median performing heat [2]. Relative to this value the lower 95th percentile (worst) heat was represented by the following values of A for its 5th percentile: 6.205 at 50% confidence level and 6.075 at 95% confidence level. These values for the worst heat correspond to the margin on life (i.e., the material variability factor, k_m) of about 2.0 to 2.3, respectively, as reported in [2]. With these, assuming log-normal distribution for the factor k_m, the mean and standard deviation for the 5th percentile of the worst heat are estimated here to be about 1.992 and 0.158, respectively. The more exact values of the material variability factor are shown in Table 1 where the symmetric bounds are given to match the other factors with the corresponding values of their upper and lower 95th percentile bounds as reported in [2].

Assuming log-normal distribution for all these k-factors the resulting mean and standard deviation values estimated here are also given in Table A-1. These are used in calculating the respective CoV values. Note that the Table A-1 estimates of mean values for the austenitic stainless steels yield their product as 10.88 that is about ten percent smaller than the overall data adjustment factor rounded-up to 12 on life in [2]. Therefore, using the Table A-1 values without rounding, the fatigue usages estimated in the following are about ten percent lower than would result if one uses the rounded-up values of k-factors.

The uncertainty values for all transients are assumed to be the same and expressed as a percent of the mean (i.e., as the CoV) of each parameter as follows: 5% for the applied stress intensity $S_{alt,i}$, 10% for the applied number of stress cycles n_i, 2% for the peak temperature T_i in ºC, and 300% for the strain rate R_i. No (or zero) value is assigned to the uncertainty in the dissolved oxygen content since its impact on F_{en} is fixed below 0.1 ppm for wrought austenitic steels used in the following application. Other application-specific inputs are included in the next section.

Application Case – Safety Injection Nozzle Safe-End

The application of the uncertainty analysis method described above is illustrated here with the safe-end of a safety injection nozzle in the case of a newer vintage Combustion Engineering design PWR type plant, with reference to the deterministic analysis reported in [8]. This is chosen for illustration since the major contribution to CUF is mostly from only the top two load-transient pairs – namely, shutdown cooling and safety injection test cycles – thereby reducing the computational and checking effort. It is also one of the locations of relatively high design CUF as well as a high estimated value of the environmental factor, F_{en}. An additional reason is that it is representative of a case of high thermal stress contribution due to radial stress gradient (i.e., through-wall gradient), typically resulting from sudden change of inside surface temperature.

The particular safe-end analyzed in [8] was made of Type 316 austenitic stainless steel and its inside surface was in direct contact with the primary reactor coolant of the PWR with operating temperature of 290ºC. Since this environment has less than 0.1 ppm dissolved oxygen, any variability in the oxygen level during normal operation does not affect the F_{en}. In calculating F_{en} for the applicable load-sets the peak operating temperature of 290ºC is used in all cases examined below. For estimating fatigue usage in air, the updated design values for the austenitic stainless steels given in [2], same as in the 2010 Code Edition [1], are used here with interpolation; the constant a_1 is 1/12 and a_2 is 1 in Equation A-3. The stress exponent b in Equation A-6 for this is 2.17 with the endurance limit of 93.6 MPa; parameter a in Equation A-6 is not needed since the ith term inside the summation of Equation A-3, denoted as u_i, is calculated using the interpolated allowable cycles.

Based on the reported 40-year design basis cycles of fatigue significance the following inputs are used for the applied stress intensity (S_{alt}) and the corresponding number of occurrences: the two transient pairs had 260 cycles of 839 MPa and 500 cycles of 497 MPa. All these were conservative estimates used in the design fatigue analysis [8]. The applicable positive strain rates are taken here to be 0.0017 %/s and 0.001 %/s, respectively, in estimating the F_{en}. This set of inputs is referenced below as Case ID #1.

Since the actual safety injection test cycles were performed at much lower temperature, the design assumption of 839 MPa stress intensity was corrected to reflect this, with the resulting value of 454 MPa [8]. The applicable positive strain rate for this is also taken here to be 0.001 %/s. With only these differences in the inputs from Case ID #1 the calculations of usage and uncertainty were repeated here and referenced below as Case ID #2.

The most relevant case of inputs for the application of current uncertainty analysis method is where more realistic values for these operating conditions are used. For example, these were reported to be [8]: 100 cycles of 454 MPa, as-calculated stress intensity, and 90 cycles of 497 MPa. This set is labeled here as Case ID #3.

Three additional cases were also analyzed to examine the influence of uncertainty in relation to fatigue life margins which are discussed in the next sections.

Results of Application

The CUF uncertainty analysis method was applied to the safety injection safe-end conditions with inputs as summarized in the preceding sections. The key results are given in Table A-2.

The effect of varied sources of uncertainty is reflected in the combined total uncertainty in terms of the resultant standard deviation of the calculational end-result, namely, the CUF including the F_{en} based adjustment for EAF. Note that the estimated mean and standard deviation of the environmentally adjusted CUF in all of the cases still include the essential elements of conservatism embedded in the Code procedure and the selection of factors and F_{en} expressions as presented in [2]. These estimates provide to a first approximation the quantification of uncertainty in CUF calculation; for example, with the assumption of log-normality, the probability of exceeding the CUF value of 1.0 can be estimated as a measure of performance criterion given the various input uncertainties. These probability estimates (for CUF > 1) for the six cases are also compared in Table A-2.

As noted earlier, the Case ID #1 represents the design calculation where the thermal stress contribution from testing cycle transients was biased upward and the number of design transient occurrences was also very high. When these biases are reduced as in Case ID #3 the probability distributions of CUF are significantly altered as shown in Figure A-1. A point of interest to note is that since the input uncertainties, individually and collectively, can be taken into account the permissibility of more realistic and unbiased inputs can be assessed and factors with significant influence on probability of exceeding a specific value of CUF can be identified and ranked.

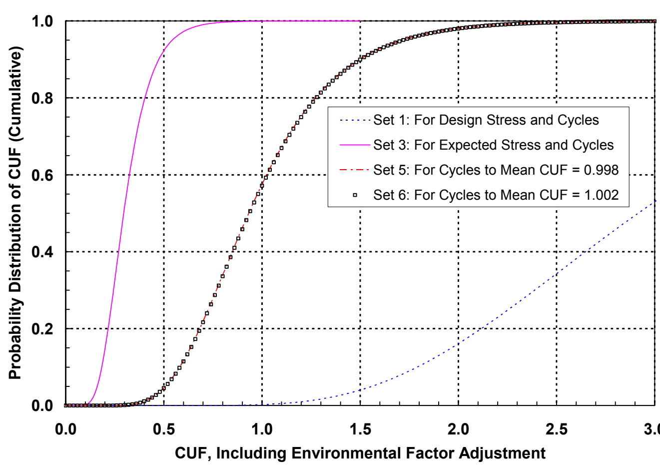

Figure A-1
Comparison of the Impact of Service Loading Assumptions on the Probability Distribution of CUF taking into Account Various Sources of Uncertainty Affecting the Calculated CUF

Case ID #4 in Table A-2 differs from the more realistic Case ID #3 only in the projected or assumed number of transients, namely, there are almost 85% more transients in #4. This increase represents a margin of error (or allowed extra transients) over the number of transients used in the analysis case #3, which would still keep the probability of exceeding the performance criterion of CUF = 1 to just below the 5% level, again, allowing for (or taking into account) all other uncertainties as before.

Also, Case ID #5 and #6 differ from the realistic analysis case #3 in the number of transients only. These are selected such that the usual deterministically calculated CUF, with high F_{en} as before, would reach just below 1 (0.998 in case #5) and just above 1 (1.002 in case #6). As can be seen from Table A-2, such a scenario requires a difference of only one transient count in each load-set. In this case the probabilistic interpretation in Figure A-1 shows virtually no impact of the difference between the two sets of inputs (#5 and #6).

Table A-2
Results of EAF Usage and Associated Uncertainty Analyses for Safety Injection Nozzle Safe-End Illustration Cases

Case ID	Load-pair i	S_{alt} for i $S_{a,i}$, MPa	Load Cycles, n_i	Allowable Cycles, N_i	In-air Usage, u_i	In-air CUF	EAF factor $F_{en,i}$	Adjusted Usage, u_{en_i}	Adjusted CUF	CUF Std. Deviation	Probability CUF > 1
#1	1	838.9	260	736	0.353	0.493	6.77	2.169	3.135	1.237	0.998
	2	497.0	500	3574	0.140		7.61	0.966			
#2	1	454.1	260	4850	0.054	0.194	7.61	0.370	1.336	0.537	0.710
	2	497.0	500	3574	0.140		7.61	0.966			
#3	1	454.1	100	4850	0.021	0.046	7.61	0.142	0.316	0.121	0.000
	2	497.0	90	3574	0.025		7.61	0.174			
#4	1	454.1	184	4850	0.038	0.084	7.61	0.262	0.582	0.223	**0.050**
	2	497.0	166	3574	0.046		7.61	0.321			
#5	1	454.1	316	4850	0.065	0.145	7.61	0.450	**0.998**	0.382	0.425
	2	497.0	284	3574	0.079		7.61	0.548			
#6	1	454.1	317	4850	0.065	0.145	7.61	0.451	**1.002**	0.383	0.428
	2	497.0	285	3574	0.080		7.61	0.550			

Discussion

The preceding sections essentially provide a complete description of the uncertainty analysis, requisite inputs, and key results illustrating the application of the proposed method, in a simplified engineering framework. Discussed in this section are some additional considerations and possible useful extensions of the uncertainty analysis/results, albeit in exploratory manner.

The surface finish effect as assumed in Table A-1, as large as it is, may need to be further examined or supported, especially when the environmental effect or the stress severity is relatively high which may mask or dominate the otherwise likely influence of a practically smooth but not highly polished surface, in contrast to the assumed synergy or multiplicative factor for it that is also assumed to be independent of the cyclic life. It would seem reasonable that the surface effect would be relatively more pronounced near the endurance limit range and when the environment is more benign.

As noted earlier, the factor for material variability and data scatter, k_m, has been effectively assessed at the 95/95 level (i.e., 95th percentile at 95% confidence level), unless heat-specific data are available. While such a conservative assessment may be judged necessary for thin components, such as the steam generator tubing or the fuel cladding, the basis or need for this in the case of thick components is not as clear since in these cases considerable margin exists between the so-called initiation and the through-wall penetration, especially with significant through-wall stress gradients common for the thicker components.

Since the hypothesis for cumulative fatigue damage, the so-called Miner's Rule, has been used as the basis to determine the design adequacy according to the ASME B&PV Code [1, 7] it is important and of interest to re-state the basic premise, as expounded by Miner, underlying the rule generally associated with his name and his paper [9]: (i) *As a simple concept of fatigue damage,* the increment of damage is expressed as a ratio of the number of cycles applied to the number of cycles required to produce failure at a given stress level (or a specified load cycle). (ii) When the summation of these damage increments under several different stress levels reaches unity then it corresponds to the failure under such a combination of stress levels, irrespective of the order of application of these stress levels (i.e., load history effects on CUF at failure considered negligible in relation to, or as part of, the material variability). (iii) *The appearance of a crack* is considered as a failure – this was regarded to be so since the crack appearance required repair or replacement. (i.e., the term failure did not actually refer to the final or imminent fracture, rather to the appearance of a crack of engineering significance requiring a due repair or replacement.)

While observations on the scatter in usage factor at failure, representative of the material strength, exist in a few cumulative fatigue damage investigations, there is no consolidated or definitive compilation to narrow down the essential statistical characterization (unlike that of the raw, constant/single-stress amplitude materials testing). Furthermore, very little systematic effort or quality data, if any, seems to exist or be referenced for the case of cumulative fatigue under the typical reactor water environments. Nevertheless, it is possible to incorporate the uncertainty in CUF that represents the material strength (or resistance to CUF) in relation to the CUF uncertainty that represents the component fatigue loading. The former may be characterized by a log-normal distribution with its median and 95th percentile values. By comparing the overlap of this material strength CUF (i.e., the performance criterion) distribution with the distribution obtained from the above analysis for the CUF due to the applied loading, as illustrated in Figure A-2, the resulting probability of failure under cumulative fatigue condition can be quantified using the principles of stress–strength (or load–resistance) interference method [10, 11]. This is a more realistic approach to assess the impact of various inputs and associated uncertainties on the expected distribution of failure due to cumulative fatigue usage. Note that the material strength distribution of CUF effectively includes or reflects the material variability and data scatter, if not the loading history; as such, use of k_m and k_h factors would need re-interpretation or adjustment in the above uncertainty analysis. Nonetheless, by associating the material variability and data scatter with the distribution of material strength for CUF, the remainder of the uncertainty analysis as above would result in the appropriately labeled CUF of the applied loading (i.e., the generalized stress, in the terminology of the stress–strength interference method). The resulting probability of failure (i.e., the applied CUF exceeding the material strength CUF, taking into account their uncertainty) would be a quantifiable measure of the realistic margin on a consistent basis.

The above approach purposely retains the deterministic base model familiar to the design analysts in the context of current Code procedure and its augmentation for EAF which generally utilize linear elastic analysis results, with a bounding plasticity correction factor as needed, and provides a common basis for comparison. If one were to choose a detailed non-linear finite element analysis in determining the essential stress intensities then one would expect the related inputs to be more realistic, although the same approach can still be applied provided the uncertainty in such a detailed analysis is also reasonably quantified. The differences between various methods of deterministic analysis would likely remove (or introduce) certain biasing elements, but their impact would be ascertained in terms of the net probability of applied CUF exceeding the material strength CUF.

To the extent that the approach is intended to allow for a realistic assessment of cumulative fatigue damage the use of plant specific data, especially the observed and expected key transients, with a reasonable estimate of associated uncertainty would provide an acceptable alternative to a generic implementation. In this context it may be noted that monitoring of plant data at critical locations has often been considered in assessing the state of fatigue damage [e.g., 12, 13]. Such monitoring, with appropriately verified interpretation of the monitored data, may be useful in optimizing future plant operation (from the point of view of fatigue damage) in addition to confirming and/or defining the assumed loading severity (frequency and amplitude) of transients. It is important, however, to note that the future loading severity is not predictable without an associated uncertainty that would need to be accounted for in the fatigue assessment even with monitoring.

The logarithmic assumption used in developing some of the inputs was for consistency with the results as noted in References 2 and 7. The interpretation or extension of results of uncertainty analysis in terms of the load–resistance type overlap for the probability of failure (Figure 2) is subject to the assumptions regarding these overlapping distributions the choice of which, at present, is empirical. This can be addressed in several ways, for example, by incorporating alternative distributions or by using extreme value statistics or, as a limiting case, by using non-distributional assumptions (which would be conservative). However, it is expected that the impact of these distributional assumptions would be noticeable mainly in the very low failure probability (tail) region, well below a few percent. As noted earlier, since the primary event of interest in the CUF based analysis is not one of imminent final failure, but rather a condition of engineering significance, it is expected that the sensitivity to distributional assumptions should not be an issue. Of course, the observations and points made in this discussion section are meant to complement the main premise of the proposed uncertainty analysis and be used for further consideration in its implementation.

The use of statistical data and analysis to account for various sources of uncertainty in the broader context of aging related degradation in general, including fatigue, has been proposed in several recent works [e.g., 14, 15]. These works also suggest that a systematic assessment of uncertainty would be useful not only in the proactive management of the degradation but in a better understanding of actual design margins and in a better focused inspection guidance that aims at reducing the significant source(s) of uncertainty.

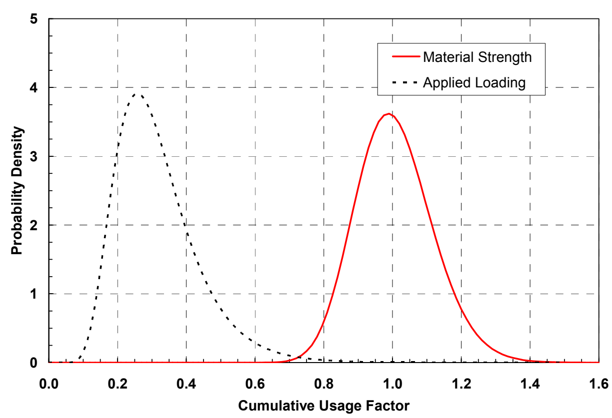

Figure A-2
Overlap of the EAF-Adjusted Applied CUF Distribution and the CUF Distribution Representative of the Material Strength (Or Failure Criterion)

Summary and Conclusion

A generic, engineering approach for estimation of the uncertainty distribution of CUF has been described utilizing the expected mean and standard deviation of input variables used in the calculation of EAF-based CUF. The approach does not involve Monte Carlo sampling; instead it analytically combines variances of the inputs leading to an acceptable estimation of the total variance of the CUF. The approach does not require specification of full probability distribution(s) for the input variables.

The uncertainty analysis utilizes exact algebraic expressions where possible or the Taylor series approximation to combine the various sources of uncertainty and addresses any significant correlation between inputs where necessary. Results of its application are presented, as an illustration (and not as a design study), for the case of a safety injection nozzle safe-end of Type 316 austenitic stainless steel where a significant environmental effect has been suggested by the EAF methodology. These results and application demonstrate the feasibility and utility of proposed approach. By utilizing the expected uncertainty in inputs, while maintaining the essential elements of conservatism embedded in the Code procedures and EAF methodology, a more realistic and unbiased assessment is possible. The utility of the approach is further examined in the framework of the stress–strength interference technique of analysis to account for the uncertainty in the fatigue performance criterion which is expected to provide a simplified rationale or basis in interpreting safety factor/margin and its relation to a quantifiable measure of the probability of exceeding the fatigue performance criterion. Further validation of the simpler approach proposed in this work for uncertainty estimation and the probability of exceeding the fatigue usage may be possible with the fatigue module in the more detail framework of Extremely Low Probability of Rupture (xLPR) Monte Carlo program [16] being developed by the USNRC for failure probability of reactor coolant system components.

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B

AN ALTERNATE FORMULATION OF THE STATISTICAL EVALUATION OF EAF - BY D.A. STEININGER

This document presents an alternative statistical approach to environmental assisted fatigue. It also provides a way of packaging the statistical result in a deterministic manner that may be more palatable to both ASME and the NRC if industry decides to suggest an alternate way of evaluating and presenting fatigue damage, and its allowable value.

The approach evaluating the statistical nature of fatigue is used in European structural codes and in other industries in the US. It is based essentially on Reference 1. The statistical approach is similar to that presented in Appendix A.

The approach begins with Miner's Rule which is consistent with the ASME Code.

Miner's rule:

Damage = D = $\sum_{i=1}^{n_s} n(i) / N(i)$ **Eq. B-1**

Typically, failure is assumed when D = 1.0.

where

$N(i)$ = number of cycles to failure at a given stress according to S-N fatigue correlation

n_s = total number of stress increments

$n(i)$ = number of cycles experienced at a given stress increment

A distribution of stress (loading) frequency of occurrence as a function of the average stress for each stress bin/block, ΔS_i is required. Strain could also be the dependent variable.

Below in Figure B-1 is an example of a histogram presenting the fractional number of alternating stress/strain cycles per bin/block (allocation of a small range of stresses per bin). Each bin is represented by an average stress/strain value. Of course, the actual histogram for a plant probably doesn't look this monotonic. But it doesn't matter to the analysis.

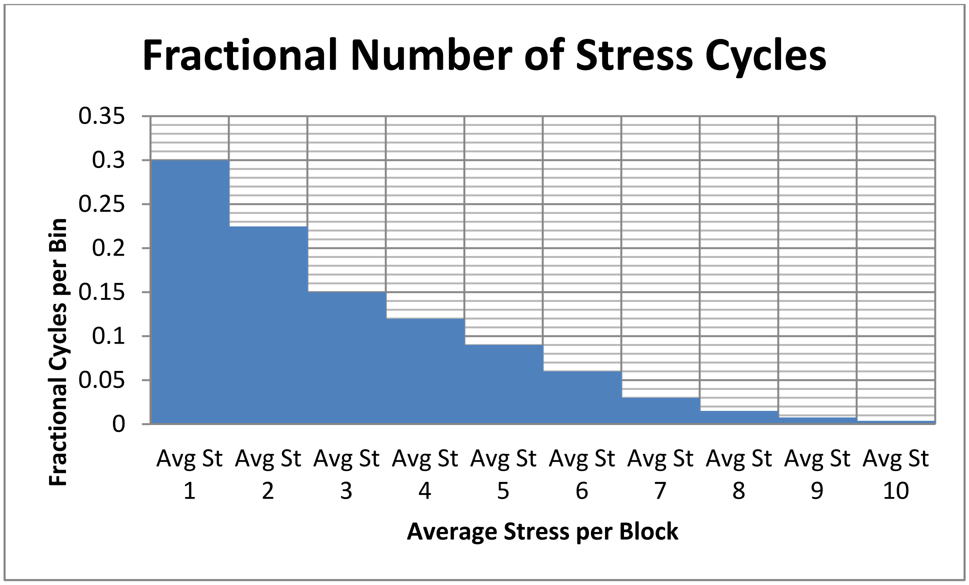

Figure B-1
Fractional Number of Fatigue Induced Stress Cycles Allocated To Bins of Width ΔS

Additionally Figure B-2 displays an air fatigue curve showing stress/strain vs cycles. The figure shows also a linear approximation to the fatigue curve. The linear function will be used in the analysis.

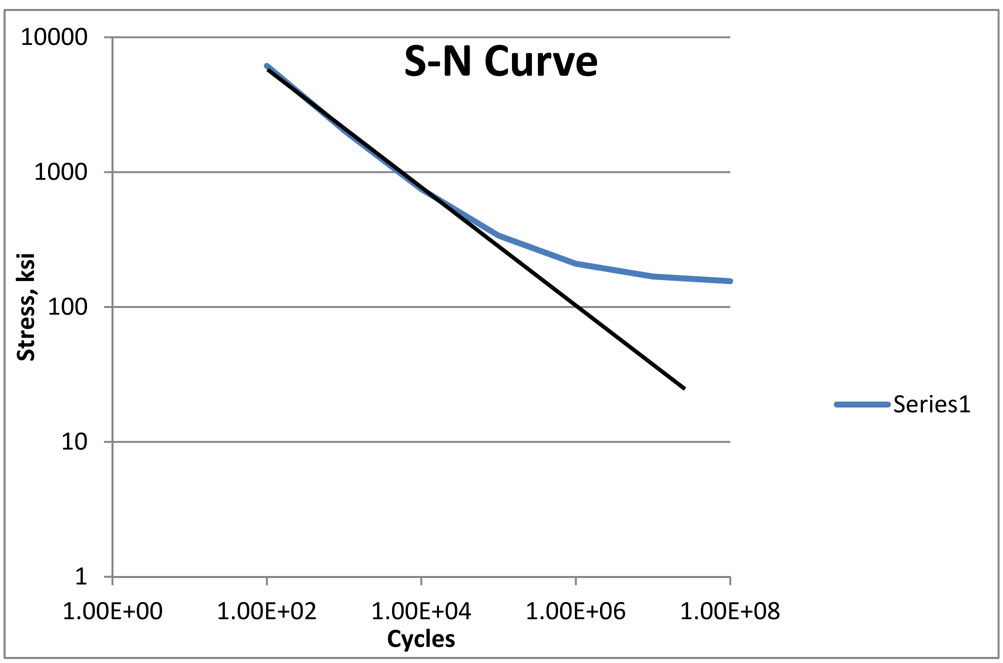

Figure B-2
Fatigue S-N curve with Linear Approximation

Equivalent Stress calculation:

The European Code addressing fatigue uses the concept of an equivalent stress (or one could use strain). This stress or strain is developed as follows

1. Model air fatigue curve as

Log N = log A – m(log S) or

$$\mathbf{N = A/S^m,} \qquad \textbf{Eq. B-2}$$

N is the total number of cycles causing fatigue failure,

A is the line intercept,

S is the stress variable (alternatively, it could be a strain), and

m is the fatigue line slope.

Note that the above fatigue equation does not account for the environmental effects of water.

Figure B-1 divides the random cyclic load distribution into a number of stress bins/blocks of width ΔS.

Using Equations B-1 and B-2, and noting that

$N_i = Nf(S_i)\ \Delta S_i$,

results in

$$D_L = \left(\sum_{i=1}^{n_s} f(S_i) * \Delta S_i * N\right) / (A/S_i^m). \qquad \textbf{Eq. B-3}$$

Rearranging terms,

$$D_L = N/A \sum_{i=1}^{n_s} S_i^m\ f(S_i)\ \Delta S_i. \qquad \textbf{Eq. B-4}$$

D_L quantifies the damage due to the applied loading.

Proceeding to the continuous case ($\Delta S_i \rightarrow 0$) for Equation B-4,

$$D_L = N/A \int_{i=1}^{n_s} S^m\ f(S_i) dS_i \text{ and} \qquad \textbf{Eq. B-5}$$

$\int_{i=1}^{n_s} S^m\ f(S_i) dS_i$ is simply the "expected value" of S^m.

where

n_s = total number of stress blocks.

Therefore,

$$D_L = (N/A)\ E(S^m) \qquad \textbf{Eq. B-6}$$

One can obtain the equivalent stress/strain from Equation B-6. Substituting the linear function for the fatigue curve into Equation B-6, one obtains the following

$$S_e = \sqrt[m]{\left(1/D_L\right) E(S^m)} \qquad \textbf{Eq. B-7}$$

If you assume for example $D_L = 1$ at fatigue failure, then

$$S_e = \sqrt[m]{E(S^m)} \qquad \textbf{Eq. B-8}$$

Therefore, if one developed an uncertainty value for D_L, *m*, and S^m, an uncertainty on the equivalent stress/strain, S_e would be obtained.

It is not clear as yet how one could use this result in regulatory space. But presented later in this document is an alternate approach that may be an acceptable to both ASME and NRC.

In any event, the uncertainties in the D_L calculation are required and this analysis is presented below.

To account for NRC/ANL air fatigue correction factors and so called F_{en} factors used to account for environmental (i.e., water) fatigue effects, and all variable uncertainties associated with the calculation of D_L, modification to the appropriate equation is necessary. Equation B-4 will be used in the remaining analysis:

$$D_L = (N/A) \sum_{i=1}^{n_s} S_i^m \, f(S_i) \, \Delta S_i. \qquad \textbf{Eq. B-9}$$

ANL NUREG/CR-6909 applies factors to the mean values of air curve to deterministically account for 1. material variability, 2. size and geometry effects, 3. data variability and 4. surface finish. These conservative factors will be represented as ε_1, ε_2, ε_4, and ε_4. These factors could easily be handled statistically if there is sufficient data to support the calculation of their variance. But for now, these factors will be included as conservative margin on the mean fatigue curve.

To account for the above deterministic factors, Equation B-9 is modified by translating the linear relationship in the conservative direction.

$$D_L = N/(10^{(\log(A) - \varepsilon_1 - \varepsilon_2 - \varepsilon_3 - \varepsilon_4)}) \sum_{i=1}^{n_s} S_i^m \, f(S_i) \, \Delta S_i \qquad \textbf{Eq. B-10}$$

Rewriting Equation B-10 and representing $(10^{(\log(A) - \varepsilon_1 - \varepsilon_2 - \varepsilon_3 - \varepsilon_4)})$ as A_ε ,

$$D_L = (N/A_\varepsilon) \sum_{i=1}^{n_s} S_i^m \, f(S_i) \, \Delta S_i \qquad \textbf{Eq. B-11}$$

Equation B-11, which applies to fatigue in air, requires modification to account for the procedure used by NRC/ANL to incorporate environmental effects as prescribed by NUREG/CR-6909.

The NRC/ANL approach for environmental fatigue evaluation requires a correction factor be applied for various environmental conditions to the fatigue usage factor calculated using the air fatigue curve and the calculated stress for a specific load pair (note: the load pair can experience many occurrences which are considered cycles in the formulation). This modification results in

$$D_{L,en} = (N/A_\varepsilon) \sum_{i=1}^{n_s} F_{en,i} S_i^m \, f(S_i) \, \Delta S_i \qquad \textbf{Eq. B-12}$$

Developing the uncertainty associated with $D_{l,en}$ requires uncertainties associated with the various terms of Equation B-12. Note, the discussion use stress, but strain could be used also.

There are uncertainties in A_ε, the linear equation intercept, and m, linear equation slope.

The stress frequency distribution, $f(S_i)$ has uncertainty because the anticipated fractional stress cycles per bin can obviously be in error for any number of reasons. Because of this uncertainty in the stress frequency, there will be a corresponding uncertainty in the total number of stress events, N. All of the uncertainty will be placed in the cycles per bin parameter and not in the variation of stress associated with these cycles. This last constraint will need to be evaluated once a better understanding of how the frequency distribution is developed and the bin size selected.

Expanding Equation B-12 into its first two terms from the summation results in

$$D_{L,en} = (N/A_\varepsilon)\, \boldsymbol{F_{en,1}}\, (\boldsymbol{S_1^m})\mathbf{f}(\boldsymbol{S1})\Delta\boldsymbol{S1} \;+\; (\mathbf{N/A}\varepsilon)\, \boldsymbol{F_{en,2}}\, (\boldsymbol{S_2^m})\mathrm{f}(S_2)\,\Delta S_2 + \qquad \textbf{Eq. B-13}$$

An uncertainty will be expressed as a standard deviation or the square of the standard deviation (i.e., the variance).

An uncertainty of zero will be assumed to be associated with ΔS since it involves the same variable S expressed as a difference of two constants with no uncertainty. It will be considered a constant in each term.

The uncertainty associated with for example the first term of Equation B-13 is obtained using the formula for the variance of a function of multiple variables.

The general formula for the variance, δ_z^2 of a function of multiple variables, that is for example $\delta_z(t,u,v,w,x,y\,)^2$, is:

$$\delta_z^2 \quad = +\delta_t^2\left(\frac{\partial z}{\partial t}\right)^2 + \delta_u^2\left(\frac{\partial z}{\partial u}\right)^2 + \delta_v^2\left(\frac{\partial z}{\partial v}\right)^2 + \delta_w^2\left(\frac{\partial z}{\partial w}\right)^2 + \delta_x^2\left(\frac{\partial z}{\partial x}\right)^2 + \delta_y^2\left(\frac{\partial z}{\partial y}\right)^2 + 2\delta_{tu}\left(\frac{\partial z}{\partial t}\right)\left(\frac{\partial z}{\partial u}\right)$$

$$+2\delta_{tv}\left(\frac{\partial z}{\partial t}\right)\left(\frac{\partial z}{\partial v}\right) + \text{additional covariance terms} \qquad \textbf{Eq. B-14}$$

The above equation will be applied to the first term of Equation B-13 as an example of the uncertainty analysis. Application to the remaining terms is identical.

First, calculating the partial derivatives for the first term of Equation B-13, repeated here:

$$\boldsymbol{D_{L,en}} = (\mathbf{N/A}\varepsilon)\, \boldsymbol{F_{en,1}}\, (\boldsymbol{S_1^m})\mathbf{f}(\mathbf{S_1})\Delta\mathbf{S_1} \qquad \textbf{Eq. B-15}$$

$S_1 = constant$ and $\Delta S_1 = C_1$ = constant,

$$\left(\frac{\partial D_{L,en}}{\partial A\varepsilon}\right) = \left(-\frac{1}{A\varepsilon^2}\right) N F_{en,1}\, S_1^m f(S_1) C_1$$

$$\left(\frac{\partial D_{L,en}}{\partial N}\right) = \left(\frac{1}{A\varepsilon}\right) F_{en,1}\, S_1^m f(S_1) C_1$$

$$\left(\frac{\partial D_{L,en}}{\partial F_{en,}}\right) = \left(\frac{N}{A\varepsilon}\right) S_1^m f(S_1) C_1$$

$$\left(\frac{\partial D_{L,en}}{\partial S_1}\right) = \left(\frac{1}{A\varepsilon}\right) N\, F_{en,1}\, m S_1^{m-1} f(S_1) C_1 = m\frac{D_{L,en}}{S_1}$$

Note that the S_1 in $f(S_1)$ for the above operation is considered without uncertainty that is, all the uncertainty is captured by the dependent variable f. Further consideration of this approach as valid will be required.

$$\left(\frac{\partial D_{L,en}}{\partial f}\right) = \left(\frac{1}{A\varepsilon}\right) N\, F_{en,1}\, S_1^m C_1$$

$$\left(\frac{\partial D_{L,en}}{\partial m}\right) = \left(\frac{1}{A\varepsilon}\right) N F_{en,1} \ln S_1\, e^{lnS_1 m} f(S_1) C_1 = D_{L,en} \ln S_1$$

$$\boldsymbol{\delta^2_{D_{L,en}}} =$$

$$\boldsymbol{\delta^2_{A\varepsilon}\left(\left(-\frac{1}{A\varepsilon^2}\right) N F_{en,1}\, S_1^m f(S_1) C_1\right)^2 + \delta_N^2\left(\left(\frac{1}{A\varepsilon}\right) F_{en,1}\, S_1^m f(S_1) C_1\right)^2 + \delta^2_{F_{en,1}}\left(\left(\frac{N}{A\varepsilon}\right) S_1^m f(S_1) C_1\right)^2 +}$$

$$\boldsymbol{\delta^2_{S_1}\left(m\frac{D_{L,en}}{S_1}\right)^2 + \delta_f^2\left(\left(\frac{1}{A\varepsilon}\right) N\, F_{en,1}\, S_1^m C_1\right)^2 + \delta_m^2\left(D_{L,en} ln S_1\right)^2} \qquad \textbf{Eq. B-16}$$

The cross correlation term has been neglected.

Note, all of the variables in the above equations are evaluated at their mean values.

The variance $\delta_{S_1}^2$ is calculated by performing an uncertainty analysis on the calculated stress. Since there are many variables and parameters involved in such an analysis, which may be complicated by non-linearity, and a finite element computer code is used to perform the stress calculation, a simplified approach will be used to calculate this variance.

Essentially, it will be assumed that the calculation of $\delta_{S_1}^2$ can be obtained by assuming that the calculation of S_1 can be approximated by a Taylor's Series expansion. By doing so,

$$\delta_{S_1}^2 = \delta_{x_1}^2 \left(\frac{\delta S_1}{\delta x_1}\right)^2 + \delta_{x_2}^2 \left(\frac{\delta S_1}{\delta x_2}\right)^2 + \delta_{x_3}^2 \left(\frac{\delta S_1}{\delta x_3}\right)^2 + \cdots\cdots + \delta_{x_n}^2 \left(\frac{\delta S_1}{\delta x_n}\right)^2$$

where

x represents the independent variables and parameters used in the stress calculation performed by the finite element code. The variance of each of these variables and parameters are $\delta_{x_1}^2\ \delta_{x_2}^2, \delta_{x_3}^2, \cdots\cdots\cdots\ \delta_{x_n}^2$. The covariance terms, such as $2\delta_{x_1 x_2}\left(\frac{\delta S_1}{\delta x_1}\right)\left(\frac{\delta S_1}{\delta x_2}\right)$, etc have been neglected. These terms can be calculated if necessary.

Derivatives $\left(\frac{\delta}{\delta x_n}\right)$ will be approximated by $\Delta/\Delta x$, where the Δ on x will be approximated by a 10% variation about the mean value of x with all other independent variables and parameters, x_{n-1} .... x_0 held at their mean values. This calculation gives ΔS_1. The calculation is performed using the finite element code for the stress calculation.

One now has all the uncertainties that make up the uncertainty on $D_{L,en}$.

$$\boldsymbol{\delta}_{\mathbf{D,en}}^2 = \sum_1^{n_s} \Delta \mathbf{S}_i^2 \boldsymbol{\delta}_{\mathbf{Z}_i}^2 + \left(\prod_{i=1}^{n_s} \Delta \mathbf{S}_i\right) \boldsymbol{\delta}_{\mathbf{S}_i \Delta \mathbf{S}_{i-1} \Delta \mathbf{S}_{i-2} \cdots\cdots\cdots\cdots \Delta \mathbf{S}_{i_{n_s}}} \qquad \textbf{Eq. B-17}$$

The covariance term (2nd term) of Equation B-17 is assumed to equal 0.

When changes are made in the calculation of $D_{L,en}$, the resulting change in uncertainty can be quatified.

Fatigue damage is modeled by a so called cumulative usage factor CUF. When this factor equals 1 it is assumed by the Code and NRC that fatigue failure occurs. Although, not generally recognized, this factor can vary over a considerable range according to the literature. This variation calls into question the applicability of Miner's Rule in evaluating fatigue damage. The NRC heretofore has not recognized this fact. In any event, there is uncertainty in this parameter's value. So the actual value can be greater or less than 1.0.

The nominal value of the CUF is calculated by using Equation B-1. Additionally, as noted above there is uncertainty in the value of the applied load leading to fatigue damage. Fatigue damage caused by the loading is expressed by a damage factor and the corresponding equation is given in Equation B-4. It is necessary to develop an approach that uses this information to quantify the probability that fatigue cracking will appear in a given component. This approach is presented below.

One may need a simple way of presenting a fatigue statistical approach that the ASME Section III code committee could use to set deterministic safety margins on fatigue. Such an approach may be palatable also to the NRC since it could be substantiated by the Monte Carlo analysis effort NRC RES is applying presently to primary piping fatigue phenomena in xLPR. Two different approaches are presented but are similar in their basic approach.

The approach uses the concept of "reliability" in evaluating the failure susceptibility of a component. Reliability is defined as the probability that the component's structural strength exceeds the applied loads. This definition is expressed mathematically as

$$R_1 = P(R>L)$$

R is the structural resistance or strength of the component and L is the load effect expressed in the same units as the resistance R. In general, R and L are functions of several random variables. Reliability analysis defines a performance function describing the performance of the component in handling the load without experiencing a predefined damage failure (e.g., fatigue crack initiation). The performance function Z is expressed as

$$Z = R-L$$

Z is therefore a function of random variables as

$$Z = g(X_1, X_2, X_3, \cdots\cdots X_n),$$

The probability of failure is expressed as

$$P_f = P(R<L)$$

$$= 1- R_1$$

In terms of the performance function, this probability is expressed as

$$P_f = P(Z<0)$$

$$= P[g(X_1, X_2, X_3, \cdots\cdots X_n) < 0]$$

In general, when the variables are correlated, the performance function is

$$P_f = \int \cdots \iint_{over\ Z \leq 0} f_x\,(x_1\,, x_2, \cdots\cdots\cdots\cdots x_n) dx_1, dx_2, \cdots\cdots dx_n$$

where, f_x is the joint PDF of the random variables, X_n and the integration is performed over the region where Z = g() < 0, the region of failure. For a nonlinear function, evaluating the integral can be a difficult undertaking.

The above approach will be applied to the fatigue issue. In this regard, the concept of fatigue damage and its maximum allowable value will be used to calculate the performance function.

The performance function will be

$$\Delta = CUF_{Allow} - D_{L,en}$$

ASME Code dictates that the allowable value of CUF_{Allow} is equal to 1. But as noted earlier, the literature suggests that the CUF value at which fatigue failure occurs (it's not clear what defines fatigue failure in the open literature) varies over a range of values.

A review of the literature is needed to identify the range and frequency associated with the allowable CUF_{Allow}. For discussion purposes, it will be assumed that the CUF_{Allow} uncertainty can be modeled by a normal (lognormal) distribution with a known mean and standard deviation.

It will also be assumed that $D_{L,en}$ can be modeled by a normal (lognormal) distribution with a mean and standard deviation developed from the earlier discussion presented in this write-up.

For simplicity of presentation both $D_{L,en}$ and CUF will be assumed to be modeled as normal distributions. Figure B-3 shows both parameters plotted along the same horizontal. The intersected area that is shaded is the area that allows one to calculate the probability of $D_{L,en}$ not exceeding CUF_{Allow}. One could end the procedure at this point and calculate the probability of the occurrence of fatigue damage. This is one approach.

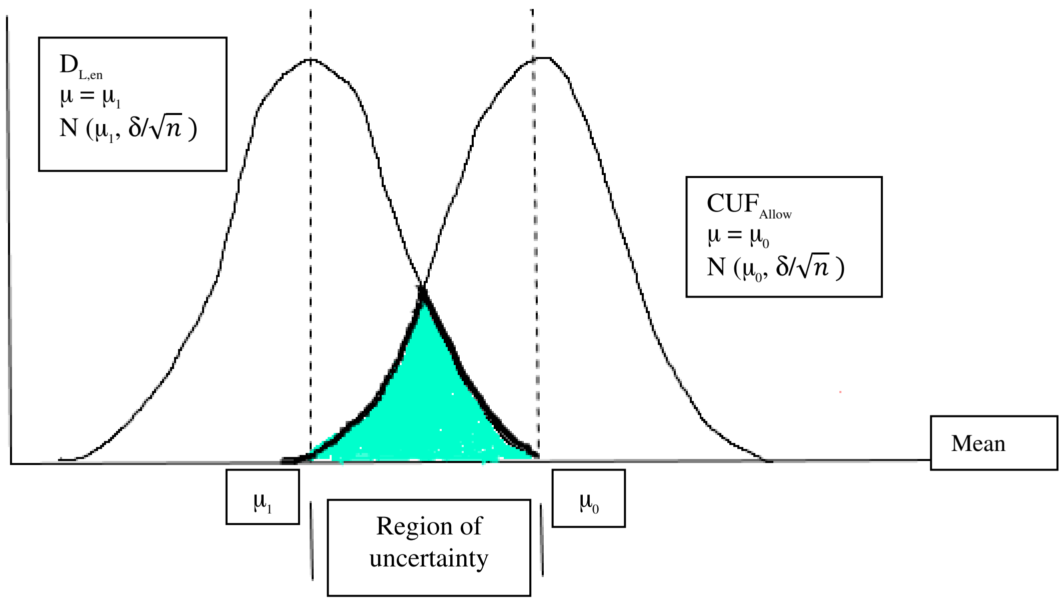

Figure B-3
Overlapping Areas of the Probabilistic Distributions for Fatigue damage, $D_{L,en}$ and D_{Allow}

Because the ASME Section III Code committee would probably rather deal in deterministic space in the Code than probability space (although, this is an assumption), the following mixed approach taken from Reference (2) is offered.

Essentially, application of a safety factor approach will be applied to the probabilistic situation presented in Figure B-3. In previous applications of safety factors in ASME Code space and in regulation promulgated by NRC, safety factors are selected rather arbitrarily or based on some type of experience of what appears to have worked in the past. The approach presented here will base the safety factors on the probabilistic information for environmentally assisted fatigue as presented in Figure B-3.

Figure B-3 has been modified by including additional parameters and is presented as Figure B-4.

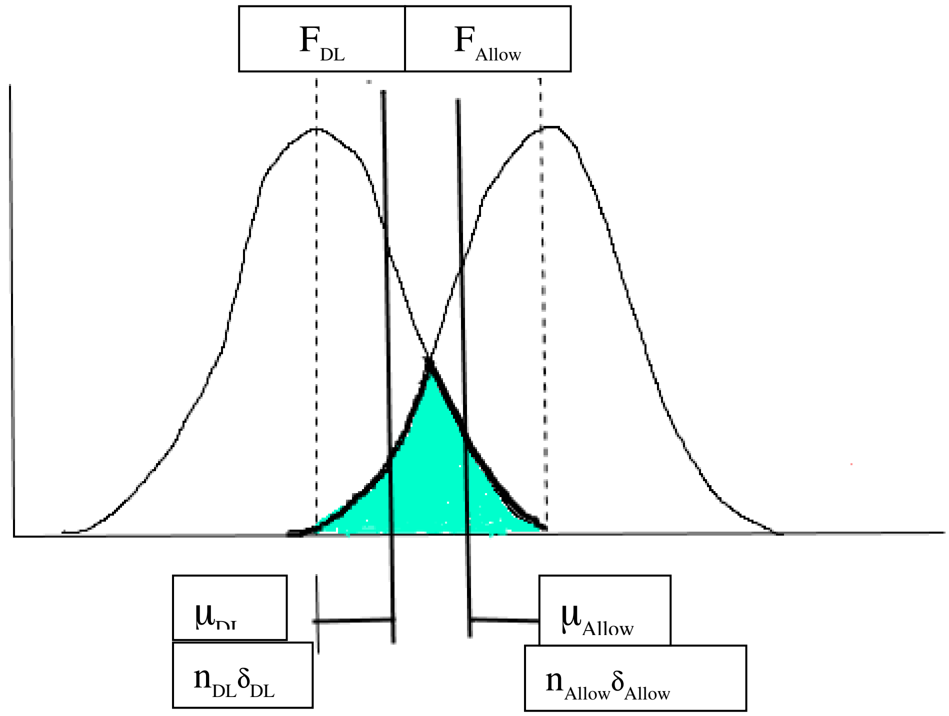

Figure B-4
Probability Density Curves for Accumulated Fatigue Damage and Allowed Fatigue Damage to the Component

where

μ_{DL} = mean value of accumulated fatigue damage by stress loading

δ_{DL} = standard deviation of accumulated fatigue damage by stress loading

n_{DL} = safety factor for accumulated fatigue damage

F_{DL} = design maximum expected accumulated fatigue damage

μ_{Allow} = mean value of allowable fatigue damage

δ_{Allow} = standard deviation of allowable fatigue damage

n_{Allow} = safety factor for the allowable fatigue damage

F_{Alllow} = design minimum allowable fatigue damage

S = safety factor = F_{Allow}/F_{DL}

The safety factors S, n_{DL}, and n_{Allow} are related to the statistical quantities by the following equation:

$$\mu_{Allow} - \mu_{DL} = \mu_{DL}(S - 1) + Sn_{DL}\delta_{DL} + n_{Allow}\delta_{Allow}$$

Defining a safety index Z as:

$$Z = \{(S-1) + S(n_{DL}C_{DL}) + (n_{Allow}C_{Allow})(\ \mu_{Allow}\ \mu_{DL})\}/\{C_{DL}^2 + (C_{Allow}\mu_{DL}/\mu_{DL})^2\}^{1/2}$$

where

$C_{DL} = \delta_{DL} / \mu_{DL}$, and

$C_{Allow} = \delta_{Allow} / \mu_{Allow}$.

Defining "Reliability", R as the probability that a component meets a specified performance under specified conditions, R equals the probability that the component's accumulative fatigue damage will not exceed the allowable fatigue damage over the design life of the component and is the cumulative probability density function of the safety index.

Figure B-5
Reliability of a Component (In Terms of the Probability that the Accumulated Fatigue Damage Will Not Exceed the Allowable Fatigue Damage) is A Cumulative Probability Density Function of The Safety Index

One can set the safety factors and obtain a desired reliability, R through the safety index, Z. The safety index depends on D_L,en, D_{Allow}, and the safety factor values. It would be expected that the ASME Section III Code committee for fatigue or NRC would set these safety factors.

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