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HEJ Sleeved Tube Length Based Degradation Acceptance Criterion

Westinghouse Energy Systems

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Table of Contents

1. Introduction 1
2. Joint Configuration, Degradation Location, & Mechanism 4
3. Failure Pressure and Leak Rate Test Programs 5
   3.1 Historical Database 5
   3.2 Concept Testing 6
   3.3 Qualification Tests 8
   3.4 Other Testing Differences 9
4. Database for Evaluation 10
5. Evaluation of the Failure Pressure Data 11
   5.1 Adjustment of the Pressure Data 12
   5.2 Structural Analysis of the Sleeved Tube Joints 16
      5.2.1 Unadjusted Failure Data 16
      5.2.2 Adjusted Failure Pressures 17
      5.2.3 Conclusion from the Failure Pressure Analysis 18
6. Evaluation of Leak Rate Data 19
   6.1 Summary of Former Leak Rate Tests 19
   6.2 Concept Test Program Leak Rate Results 19
   6.3 Qualification Program Leak Rate Tests 19
   6.4 Field Leak Rate Experience 20
7. Nondestructive Examination Considerations 21
   7.1 Effect of Rolldown on Hardroll Flat Length 21
   7.2 Field Examination Data Evaluation Results 21
   7.3 Laboratory Specimens Examination Data Evaluation Results 22
8. NRC Staff Comments 22
9. Conclusions 25
10. References 27

List of Figures

Figure 1: Installed HEJ Sleeve Configuration 33
Figure 2: Hybrid Expansion Joint Nomenclature 34
Figure 3: Comparison of Sleeve & Tube Hardroll Profiles 35
Figure 4: Key to the HEJ Qualification Tests' Specimens 36
Figure 5: Failure Pressure vs. Displacement at Failure 37
Figure 6: Failure Pressure vs. Leak Rate at 2560 psi 37
Figure 7: Illustration of the Sleeve/Tube Interference Fit 38
Figure 8: Failure Pressure vs. Interference Pressure 39
Figure 9: Temperature at Failure vs. Temperature at 4800 psi 39
Figure 10: Failure ΔT vs. 4800 psi ΔT 41
Figure 11: Distribution of Measured Failure Pressures 41
Figure 12: Ratio of SLB to NOp Leak Rates 42
Figure 13: Failure Pressure vs. PTI Location 42
Figure 14: Distribution of PTI Lengths at Kewaunee 43
Figure 15: PTI Acceptance Criterion for HEJ Sleeved Tubes 44

HEJ Sleeved Tube Length Based Degradation Acceptance Criterion

1. Introduction

The purpose of this report is to provide the technical justification for changing the location of the pressure boundary for Hybrid Expansion Joint (HEJ) sleeved tubes. The current location of the tube pressure boundary is defined in Section 3.5.4.3 and Figure 3.5.4-1 of Reference 1, as including "the portion of the tube in the upper joint." The information presented herein supports changing the tube plugging limit to apply above the top of the hardroll lower transition (HRLT) on the outside of the tube. The net effect of this change is to support the application of a length based parent tube repair criterion for HEJ sleeved tubes with parent tube indications (PTIs) located at elevations up to and slightly above the top of the HRLT as measured on the inside diameter of the sleeve. The configuration of the installed sleeves is illustrated on Figure 1. To support the relocation of the pressure boundary, failure and leak rate testing of laboratory specimens fabricated to be conservatively representative of the joints in the Kewaunee steam generators (SGs) was performed. The results from the tests lead to the conclusion that joints with 360° by 100% deep PTIs ≥ 0.92" from the bottom of the hardroll upper transition, as reckoned on the inside of the sleeve and without inclusion of inspection uncertainty, see Figure 2, would be expected to meet the applicable regulatory guidelines with regard to structural integrity and leak resistance, e.g., Regulatory Guide (RG) 1.121 and Part 100 of Title 10 of the Code of Federal Regulations, a.k.a. 10CFR100.

Between 1994 and 1996, criteria were developed to disposition PTIs in steam generator (SG) tubes that had been repaired by installing HEJ sleeves, see References 1, 3, 4 and 5. Such sleeves had been installed at Kewaunee in 1988, 1989, and 1991. In the spring of 1994, circumferential PTIs were detected with the majority of the PTIs located at the bottom of the hardroll portion of the joint, see Figures 1 and 2. Acceptance criteria based on RG 1.121, by which the tubes would be allowed to remain in service, were developed based on the size of the indications and their propensity for future growth. Because of potential uncertainties associated with future growth and time constraints associated with reviewing the criterion, NRC staff concurrence to apply the criteria was not sought. In 1995, a criterion was developed based on meeting a minimum specified distance, or length, from the bottom of the upper transition of the hardroll to the elevation of the indication, as measured using eddy current testing (ECT) technology. A series of tensile tests of surrogate specimens was performed to demonstrate that a tube with a 360° crack located just below the top of the hardroll lower transition would meet RG 1.121 margins against failure, i.e., separation of the joint, during normal and accident conditions, i.e., steam line break (SLB). It is noted that Reference 4 provided information to confirm that the postulated SLB event presents the most severe loading on an HEJ sleeved tube with PTIs.

In other words, the proposed criterion was intended to meet the structural requirements of RG 1.121 during normal and accident operating conditions and restrict leakage such that the dose requirements of 10CFR100 and General Design Criteria (GDC) 19 would be met, without relying on limitations on crack size or growth rates. This was based on assuming an initial crack size of 360° by 100% throughwall, the worst possible configuration, near the top of the hardroll lower transition, i.e., close to the worst known location.

The basic assumption inherent in the length criterion proposed in 1995 was that an interfering lip, or overlap, of a minimum specified size, e.g., on the order of 1 or 2 mils on the diameter, would exist between the tube and the sleeve at the elevation of the circumferential cracking. Because of unresolved staff concerns about the length of the hardroll when a condition known as *rolldown*[1] existed, a petition to the NRC staff to use the criterion was amended by Wisconsin Public Service Corporation (WPSC). The potential for using a new multicoil eddy current probe to measure the overlap directly led to the development of a limiting acceptable value, including consideration of the associated measurement error, for that overlap, Reference 5 (again assuming the tube to be severed at the elevation of the degradation). The criterion was referred to as the ΔD criterion, and relied on a direct measurement of the radial overlap rather than being inferred as with the length criterion. Based on the results of leak and strength tests documented in the earlier references, the criterion was approved by the NRC staff to be used to disposition PTIs in the Kewaunee SG tubes, References 6 and 7. In summary, it was accepted by the staff that if a physical diametral overlap of 3 or more mils was present, the leak tightness and the strength of the joint would be sufficient to result in compliance with applicable leak rate and structural integrity requirements. However, when measurement uncertainties were included in the criterion applied in the field, many fewer tubes were retained in service than anticipated.

As part of the testing programs performed to demonstrate the integrity of the sleeved tube joints, two surrogate and two Kewaunee pulled tube specimens with no overlap were tensile tested in 1994 and 1995 respectively, see References 1 and 3. Both of the surrogate specimens exhibited strengths less than required to provide the required margin of safety, while the pulled tube specimens exhibited strengths well in excess of that required to meet regulatory requirements. In order to meet the most stringent requirement of the RG, the joint must have a margin to failure of a factor of three relative to normal operation (NOp), which was taken as the design differential pressure for analysis and/or test purposes. A simple calculation indicates that a joint with a circumference of 2.5 inches and a length of one inch would be expected to have sufficient strength if the interface pressure between the sleeve and the tube was on the order of 2500 psi and the coefficient of friction between the sleeve and the tube

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[1] Rolldown is a term used to indicate that the hardroll lower transition became elongated during the removal of the rolling tool.

was on the order of 0.4. Hence, the test specimens were not exhibiting expected strength while the pulled tube sections were. A later examination of the above two test specimens revealed that the locations where the tube had been severed were significantly above the top of the lower transition. It was also known that pulled tube specimens with no or negligible overlap exhibited separation forces of 4200 to 5400 lbf when tested in the laboratory, see the ΔD criterion development figure in Reference 8. The end cap load associated with three times the design differential pressure, ΔP, of the steam generators at Kewaunee is on the order of 2450 lbf. Failure load tests were performed on four additional pulled tube specimens in 1996. The loads to separate the tubes from the sleeves after a 360° by 100% throughwall indication had formed in the tube ranged from 4250 to 5400 lbf. The latter value was for a sleeved tube that had severed during SG operation. In addition, the leak rate at a ΔP of 2560 psi and a temperature of 600°F through the joint was $3 \cdot 10^{-5}$ gpm.

These considerations led to the performance of tests in the Fall of 1997, designated as *concept* tests, on laboratory sleeved tube specimens aimed directly at estimating the leak resistance and strength of joints with the tube severed at the top of the hardroll lower transition, reckoned on the outside of the tube, i.e., no overlap between the tube and the sleeve. The results of the concept tests were presented to the NRC staff in December, 1997, Reference 8. These results demonstrated that such sleeve/-tube joints (no overlap) could be expected to exhibit adequate leak resistance and strength during normal and accident operating conditions.

Following the Reference 8 meeting, additional tests, designated as *qualification* tests, were performed on representative sleeved tube specimens to again qualify a length based criterion for accepting PTIs in HEJ sleeved tubes. The results of those tests were presented to the NRC staff in April of 1998, Reference 9. In summary, it was concluded that sleeved tubes with 360° by 100% deep PTIs at the top of the hardroll lower transition on the outside of the tube (physically, about 0.92" from the bottom of the upper transition on the inside of the sleeve) would also be expected to meet applicable regulatory requirements for leak rate and structural integrity. The analysis of the test data leading to these conclusions is presented in the following sections of this report.

The report is divided into the following discussion sections:

- the location of the degradation (Section 2),
- the failure pressure and leak rate test programs (Section 3),
- the database for the evaluation of the results (Section 4),
- the analysis and results from the failure pressure data (Section 5),
- the analysis and results from leak rate data (Section 6),
- the non-destructive examination considerations regarding the measurement of the location of the PTIs in the Kewaunee SGs (Section 7),
- responses to NRC staff comments from the April, 1998 meeting (Section 8), and,

- the overall conclusions regarding leaving HEJ sleeved tubes with PTIs in service (Section 9).

2. Joint Configuration, Degradation Location, & Mechanism

Figure 1 illustrates the installed configuration of an HEJ sleeve and Figure 2 illustrates the nomenclature used for describing the joint and identifies the general location of the degradation, i.e., within the hardroll lower transition. Figure 3 illustrates a typical comparison of the joint profile dimensions on the inside of the sleeve and the outside of the tube. Historically it was considered that the length of the hardroll on the inside of the sleeve would match the flat length on the rollers of the rolling tool. There is, however, elastic spring-back that occurs when the rolling process is complete. This gives rise to three main effects which are illustrated on Figure 3. The so-called flat length is really hourglass shaped with peak values at the top and bottom of the rolled region and a waist that is about 1 mil smaller on the diameter at the center. This results in an increase in the strength of the joint that would not be present if the joint were truly cylindrical. The second effect is that the length of the joint as measured by ECT on the inside of the sleeve is shorter than the 1.0" length of the rollers in the rolling tool. Dimensional analyses of the test specimens, see Figure 3 for example, shows that the length of the hardroll *flat*, that portion of the joint which is typically considered to be cylindrical, is shorter on the outside diameter of the tube than it is on the inside diameter of the sleeve. The third effect is that the tangent point at the top of the hardroll lower transition is higher than the corresponding tangent point on the inside of the sleeve. The actual location of the PTI should be no higher than the tangent point on the ID of the tube, or about midway between the tangent point on the OD of tube and the tangent point on the ID of the sleeve. An analysis of the ECT dimensions of field and laboratory tubes is discussed in Section 7.

The location of the degradation is predominantly within the hardroll lower transition (the lower transition from the hardroll expansion to the hydraulic expansion region of the joint), as opposed to the top of the transition. Some of the SG tubes have been degraded in the hydraulic expansion lower transition. It is noted that of the pulled tube specimens, only one had the cracking located at the top of the hardroll lower transition. All of the others had the cracking located within the transition. It has been previously established that the cause of the degradation is primary water stress corrosion cracking (PWSCC) originating on the inside diameter of the tube. Extensive information regarding the root cause of the cracking is contained in Reference 4, and a summary discussion can be found in Section 3.1 of Reference 7. In essence, the installation of the sleeve results in a residual tensile stress in the tube that is 10 to 15 ksi greater below the joint than above the joint. This, coupled with the residual stress associated with making the transitions, indicates that the lower transitions would be the preferred crack sites. It also indicates why no tubes at Kewaunee have been found with simultaneous cracking at both the hardroll and hydraulic expansions' lower transitions, i.e., the axial stress is relieved when the tube cracks at one of the transitions. Moreover, the evaporation of primary water as it weeps into the cavity between the sleeve and the tube may leave some deposit or concentration of boron and lithium at the evaporation location, Reference 4. This is likely to be the hardroll lower transition, but, could be the hydraulic expansion lower transition. Thus, the preferential cracking site could also be affected by the presence of the corrosive deposits, although not present in what would usually be termed large concentrations.

3. Failure Pressure and Leak Rate Test Programs

3.1 Historical Database

Leak rate testing of HEJs with PTIs dates from shortly after their discovery in 1994, References 1 and 3. Sleeved tube test specimens fabricated with the lower tube portion machined away at the top, the middle, and the bottom of the hardroll lower transition (HRLT) were tested at simulated SLB conditions (600°F). Because the differential pressure for these tests was 3000 psi instead of 2560 psi, the reported leak rate results are conservative relative to postulated SLB conditions. In addition, the specimens were heated from the outside, i.e., enclosed in a split shell furnace during pressurization, therefore, the temperature gradient during the testing was conservative relative to the gradient that exists during normal operation. This would tend to increase the leak rate and decrease the failure load of the test specimens. The effect of artificially lowering the failure pressure was compounded by the injection of cold makeup water needed to maintain the pressure when the joint is leaking. In the SG, the temperature gradient is in the opposite direction, i.e., the sleeve is hotter than the tube, and the temperature of the makeup water is constant. It is noted that no additional leak rate testing was performed for the preparation of References 4 and 5.

It is noted that Reference 4 also reported the results from the tensile testing of two tubes, R2C32 and R2C54, and leak rate testing one tube, also R2C54, removed from the hot leg side of SG "B" at Kewaunee 1995. The destructive examination indicated one PTI at or near the top of the transition in R2C32 and one near the middle of the transition in R2C54. Both specimens had failure loads at 600°F well in excess of the guidelines of RG 1.121 (R2C32 on the order of 3.5, and R2C54 on the order of 5, times the NOp end cap load). No leakage was observed from the R2C54 specimen during its leak test.

The results from the examinations of seven (not all were destructively examined) additional pulled sleeved tube specimens were reported in Reference 10. One of the specimen tubes, R2C21 from SG "A" was physically severed at 0.06" from the top of the hardroll lower transition. The axial separation load of the tube from the sleeve was 5400 lbf (about 6.6 times the NOp end cap load) at 600°F. This tube section also exhibited a leak rate of only 2 to 3 drops per minute at 600°F. Two other pulled tube specimens, R2C21 from SG "B" and R2C25 from SG "A", were leak tested at room temperature with siguificantly less leakage than the first tube.

Three other tubes from SG "B" were tensile tested, R2C21, R2C58, and R2C69. They were all cracked at 0.06" below the top of the HRLT, and exhibited strengths of 5250, 5050, and 4250 lbf.

In summary, one of the destructively examined tubes exhibited cracking at the top of the transition, while the remaining five exhibited cracking within, but, near the top of, the transition. All of the specimens exhibited strength in excess of the regulatory guidelines And a high resistance to leak under NOp and SLB conditions.

3.2 Concept Testing

In November of 1997, a *concept* test program to verify the expected strength of the sleeve/tube joint under operating conditions was conducted. Previous elevated temperature testing had simulated the increase in interface pressure resulting from the difference in the coefficients of thermal expansion between the two materials, but not the increase resulting from the temperature gradient across the sleeve/tube interface. The test configuration actually simulated a decrease in the interface pressure due to the thermal gradient. Moreover, the increase in interface force due to the pressure on the inside of the sleeve was not simulated. The information from this test program is included herein in the interest of complete reporting. The data were not necessarily taken for the purpose of formally quantifying the strength or leak tightness of the sleeve/tube joints during operation or accident conditions, but the results were intended to confirm or deny expectations of joint strength from the scoping strength calculation discussed in the introduction.

A group of ten (10) HEJ sleeved tube test specimens were made from representative tube sections and sleeves using procedures similar to those employed at Kewaunee during sleeve installation. The tubes were oxidized in air at 1150°F for four hours and honed using the field procedure. The sleeves were then inserted, expanded and hardrolled also using the Kewaunee field procedure. The tubes were partially slit above the upper hydraulic expansion transition and at the elevation of the top of the hardroll lower transition to provide detection marks for the eddy current analysis of the joint. The specimens were then inspected at Zetec to characterize the joint and the location of the slit by nondestructive examination (NDE). After the NDE, the tubes were circumferentially slit throughwall over 360° at the top of the hardroll lower transition. The top of the lower transition was visually located on a comparator image by magnifying the profile of the outside of the tube. Oxidation in a high-pressure and high temperature primary water environment to simulate operation of the plant was not performed on the concept specimens.

Following fabrication, the assemblies were pressurized to failure using the Westinghouse In Situ testing equipment as controlled by a manual control box. The leak rates at differential pressures of 1600 (design ΔP), 3000, and 4800 (three times the design ΔP) psi were measured for each specimen. The specified pressures were held for a period of two minutes to obtain a steady-state leak rate and temperature measurement. Five (5) of the specimens were tested at room temperature (RT) and four (4) were tested at approximately 600°F. Due to a malfunction of the test equipment no useful data was acquired for the tenth specimen. For the elevated temperature concept tests, a rod heater was installed in the specimen and the OD of the specimen was insulated. However, the makeup water was not heated before entering the specimen, leading to temperature gradients the inverse of those desired.

The results from this *concept* testing program were presented to the NRC staff in December of 1997, Reference 8. All of the specimens retained structural integrity at pressures in excess of 1.4 times the SLB differential pressure and the leak rates at the SLB differential pressure were small. Two of the elevated temperature specimens were censored at a ΔP of 6000 psi, i.e., the tests were terminated at the test system rated capacity, and the other two failed at differential pressures of 4500 and 5500 psi. The leak rates at a ΔP of 3000 psi at 600°F ranged from 0.004 to 0.032 gpm with a mean value of 0.022 gpm and a standard deviation of 0.013 gpm. These values are consistent with results from testing programs reported in References 1 and 3.

While the tests were being conducted it was found that the high temperature of the specimen could not be maintained. A total temperature gradient, from the inside of the sleeve to the outside of the tube, of about 100°F was not held, nor was the desired test temperature of about 600°F. The effect of the temperature gradient in operation is to increase the interference pressure between the sleeve and the tube, thereby increasing the strength of the joint and reducing the leak rate. The inverse gradient in the test specimens has the opposite effect, reducing the interface pressure and the strength of the joint. Because the coefficient of expansion of the Alloy 690 is higher than that of Alloy 600, a further reduction of the interference pressure between the sleeve and the tube occurred in the test specimens.

The leak rate would be expected to be dependent on the interference pressure, hence, the reduced temperature and negative temperature gradients would also be expected to increase the test leak rate results relative to the leak rates of the joint at normal or accident operating conditions. Since the post-installation oxidation step was not performed for these specimens, the data are considered to provide an understanding of the response to pressure increases which could be expected during operation and accident performance of the SGs. However, because of the unknown effect of the oxide on the leak rate and failure pressure, the data were not included in the failure pressure and leak rate data evaluations reported herein.

At the end of the structural and leak rate evaluation portion of the presentations at the Reference 8 meeting between WPSC and the NRC staff, the performance of additional, i.e., qualification, tests was discussed. The information presented indicated that fifty (50) sleeved tube specimens would be fabricated and tests would be performed to investigate the effects of oxidation techniques and the influence of a small, on the order of 1 mil, overlap. As presented at the Reference 9 meeting, the effect of air instead of high temperature water oxidation was to be investigated by failure testing five (5) specimens oxidized in high temperature air. Additional plans called for making twenty (20) specimens with no overlap to be leak and failure tested to increase the leak rate and joint strength database. Ten (10) specimens were to be made with a small overlap, about 1 mil on the diameter, to characterize the effect on leak rate at elevated temperature, and five (5) specimens would be made with a small overlap to extend the failure test database at ambient conditions. The distribution of tests was modified for a variety of reasons while conducting the actual test program. For example, the value of the results from room temperature testing of five small overlap specimens was judged to be minimal when compared to extending the number of elevated temperature tests. The same rationale would essentially apply to any planned room temperature tests. The actual tests performed and the results from those tests are discussed in the following section.

3.3 Qualification Tests

A plan was presented to the NRC staff at the Reference 8 meeting for the performance of additional tests, to be considered as *qualification* specimens, to verify the leak resistance and joint strength results from the *concept* testing. Forty additional specimens, designated K-98-001 through K-98-040, were prepared as before using field materials and fabrication procedures. In summary, the tubes were furnace oxidized for 24 hours at 825°F, honed using the field procedure, and the sleeves were inserted, hydraulically expanded and hardrolled. Reference location slits were then machined into the specimens and they were shipped to Zetec for NDE characterization. Following the examinations, the sleeve/tube assemblies were soaked in borated (boric acid) and lithiated (lithium hydroxide) water at a temperature of 680°F and a pressure of 3000 psi for one week, and the final 360° by 100% throughwall slits were machined in the tubes. This last oxidation step is different from the preparation of the specimens for the concept tests, but, is considered to be more representative of the process that takes place in the SG (based on a comparison to photographs of oxide films on the inside of pulled tubes). Following the oxidation process, the assemblies were pressurized to failure or 6000 psi using the Westinghouse In Situ testing equipment with a laptop computer control system. A detailed description of the test equipment and methods were presented to the NRC staff at the Reference 9 meeting.

A flow diagram of the test conditions of the qualification specimens is presented on Figure 4. Of the initial forty (40) specimens, two (2) were terminated without producing any data. The ten (10) specimens with a positive overlap were fabricated, however, only one was tested (at ambient conditions), one was terminated due to a malfunction in the heated water supply system (seal leak), and the remaining eight were retained as archive specimens. Thirty (30) specimens were fabricated with no overlap, of which four (4) were tested at ambient temperature. Three of these failed at high pressures and one failed at relatively low differential pressure. The leak rates at a differential pressure of 1600 psi were zero, and one specimen had a high leak rate at a differential pressure of 2560 psi. Recognizing that the results from tests conducted at ambient temperature would not be relevant to the operation of the plant, the remaining twenty-six (26) specimens were tested at elevated temperature. One of these was damaged by a malfunctioning rod heater and did not result in any useable data.

3.4 Other Testing Differences

There were other potential differences between the qualification testing program and the concept testing program that were evaluated as part of the qualification program. These are discussed in the following paragraphs.

1) Equipment Control & Pressure Attenuation — Several of the qualification tests employed a computer control box instead of a manual control box. This was evaluated and demonstrated to have no effect on the failure pressure or the standard deviation of the failure pressures. Pseudo steady-state pressure variations were found to be contained within a 100 psi range regardless of the type of control used. In addition, the differential pressure ramp rates were not significantly different, although in manual mode the ramp rate is more difficult to control. Two accumulators were used downstream of the pump, compared to one for the concept tests. The purpose of adding the second accumulator was to further assure that the pressure changes from the pump would be attenuated as efficiently as the test equipment would allow.

2) Differential Pressure Hold Points — A second difference in the testing was in the procedure, whereby the second ΔP value hold point for monitoring leakage was changed from 3000 to 2560 psi corresponding to the predicted ΔP during a postulated SLB event. This would not be expected to have an effect and was not singly investigated. The leak rate could be expected to slightly increase if the pressure was increased above the SLB ΔP.

3) Cycle Pressure Conditioning — During the test program it was hypothesized that the strength of the joints in the SG could be affected by the thermal cycling that occurs with the operation of the plant. To test this conjecture, special pressure conditions were, ultimately, applied to fifteen (15) of these test specimens. Two specimens were pressurized to 1600 psi and relieved a total of ten (10) times before proceeding with the qualification tests. Noting no change in the characteristics after the first pressure cycle, the remaining thirteen (13) were pressurized to 1600 psi and relieved once before proceeding with the rest of the test. Following the excursion(s) to 1600 psi and subsequent depressurization(s), the standard pressure/leak tests were continued, e.g., the pressure was increased to 1600 psi, then to the next hold point at 2560 psi, etc. The results from these

tests showed that there was a profound effect from the pressure cycling in that there was a significant increase in the average failure pressure and the standard deviation of the failure pressures was reduced. The results of the analysis are discussed in detail in Section 5.

4) Furnace Versus Autoclave Oxidation — The tubes for the concept testing were oxidized in air prior to the insertion of the sleeves. The qualification specimens were oxidized in air prior to being sleeved, and were oxidized in borated and lithiated water at 680°F after being sleeved. The interface surface gouges in the air oxidized (tube only) specimens were judged to be deeper and wider than in the water oxidized specimens. Since both sets of specimens exhibited similar strengths at elevated temperature, the gouges and smearing magnitudes, width and number, do not necessarily appear to be correlated with the strength of the joint, although such differences could have been masked by other factors, e.g., temperature differences. The trend was not evident from the results of the tests performed at ambient temperature. For tests performed at room temperature the average failure pressure was 5000 and 4200 psi for the concept test specimens and qualification test specimens (water oxidized) respectively. Thus, it would appear that the method of oxidation may be significant for specimens tested at ambient temperature, but, not at elevated temperature.

In addition to the above comparison, the results from the examinations of the qualification test specimens were compared to the results previously obtained from pulled tube specimens. It was found that the pulled tube specimens exhibited more axial scratches in the hydraulically expanded region of the sleeve above the top transition of the hardroll. This result could indicate that the field tubes would be expected to be stronger than the qualification samples.

4. Database for Evaluation

Not all of the specimens were successfully failure and/or leak rate tested. During the testing the types and number of tests were changed to suit real time developed information. An examination of Figure 4 indicates that the data from twenty-two (22) specimens, (nine w/o and 13 w/pre-cycling) were considered to be valid for the evaluation of the failure pressures, including five censored data points. It also indicates that three additional specimens provided valid leak rate data.

There were twenty-five (25) results from the failure pressure testing, of these, specimens K-98-010 and 021 were excepted from the analysis because temperature data were not available for standardizing the failure pressures to normal operation or SLB conditions. The failure pressure adjustment as described in Section 5 of this report cannot be performed without the temperature data. These specimens exhibited failure pressures of 4800 and 4050 psi respectively. In addition, data from specimen K-98-031 was omitted from the analysis on the basis that the surface temperature difference, 811°F to 174°F from the inside to the outside, was impossible to truly attain with the test setup, recall that the outside of the tube was insulated. It was confirmed that the thermocouple on the outside of the tube became separated during the testing. The failure pressure for specimen 031 was 5800 psi. Using the temperatures recorded at a ΔP of 2560 psi, and the process described in Section 5.1, failure pressures were calculated to be 5571 and 6075 psi corresponding to the sleeve and tube temperatures that would exist in the SGs during NOp and a SLB event. These results indicate significant margin relative to the gnidelines of RG 1.121. These values were not included in the evaluation of the test data for estimating the parameters of the distribution of the failure pressures. Therefore, the final database for the evaluation of the failure pressure characteristics of the joint consisted of the twenty-two (22) data points listed in Table 2. The results from the analysis of the data are provided in Section 5.

There were also twenty-five (25) results for the evaluation of the leak rate. Of those, only twenty (20) leak rate values were recorded at a differential pressure, ΔP, of 1600 psi. Specimens K-98-001, 004, 005, 007, and 025 were not held at the 1600 psi monitoring point. This occurred on the first and second day of testing when the leak rate at postulated SLB conditions was of most interest. Data from all twenty-five (25) specimens were available at a ΔP of 2560 psi. There was no reason to reject the leak rate data from the specimens for which it was decided that the failure pressure data was not useful. The leak rate database is listed in Table 3. The results from the analysis of the data are provided in Section 6.3.

5. Evaluation of the Failure Pressure Data

The purpose of the data analysis was to evaluate the results of the qualification tests to quantify potential acceptance criteria for HEJ sleeved tubes with PTIs in the hardroll lower transition. The results of the tests demonstrate that an HEJ sleeved tube with a 360° circumferential by 100% deep PTI located as small as 0.92" below the bottom of the HRUT on the inside of the sleeve would be expected to meet the structural guidelines of RG 1.121, and have a low probability of failure during a postulated SLB event.

Before proceeding with the discussion of the distribution of the failure pressures, it is noted that a comparison of the measured failure pressures to the recorded displacements, Figure 5, revealed essentially no dependence on the tube displacements that were observed at 1600 and 2560 psi, and a weak dependence on the observed displacement at 4800 psi. A similar comparison of the measured failure pressure to the recorded leak rates revealed essentially no dependence at pressures up to and including 4800 psi, see Figure 6 for data at a ΔP of 2560 psi.

Three approaches to dealing with the test data appear to be viable; analyze the data as-is, analyze a filtered data set, and mathematically adjust the data to a common thermal condition. The first approach makes use of all of the available data, but, includes some data which could be expected to be non-conservative or questionable.

The second, and what is considered to be the appropriate approach, is based on analyzing only that data which would be known to be conservative relative to the normal and faulted operating conditions in the SGs. The third approach involves an attempt to adjust all the failure pressure results to correspond to the normal and faulted thermal conditions in the SGs.

The second and third approaches assume that some correlation exists between the strength of the joint and the temperature of the sleeve/tube joint and the thermal gradient across the joint. It may be stated that the performance of the joint should improve if the radial interference between the sleeve and the tube is increased. Such an increase may be brought about by increasing the temperature of the specimen and/or increasing the radial thermal gradient across the joint. The second approach involves no assumptions regarding the nature of the correlations of the leak rate and failure pressure to the interference pressure, except that it be monotonic. In addition, it uses only those data that correspond to conditions of less interference than during normal or faulted operation. The third approach requires an assumption about the nature of the correlation, i.e., linear in interference pressure, and results in adjusting the failure pressures to correspond to temperature conditions other than those of the test. Depending on the accuracy of the model, the variance of the adjusted failure pressures could be greater or less than the variance of the measured data, hence the model of the third approach is considered appropriate for approximately verifying the results of either of the first two approaches.

Section 5.1 describes the method of adjustment corresponding to the third approach to interpreting the data and Section 5.2 provides a discussion of the results from the three approaches to analyzing of the data. Although the second approach is preferred, the results from all three of the evaluations support the continued operation of sleeve tubes with PTIs located ≥0.92" from the bottom of the HRUT on the inside of the sleeve.

5.1 Adjustment of the Pressure Data

Since the tests were performed at a variety of temperature conditions, the failure pressure results can be adjusted to correspond to values expected at normal operating and SLB thermal conditions. For example, the sleeve and tube temperatures at the time of failure of a test specimen could have been 525°F and 475°F respectively. During normal operation the sleeve temperature would be expected to be about 595°F and the OD of the tube would be expected to be at 495°F. The purpose of the adjustment is to estimate the failure pressure that would have resulted from a test exactly at the operating temperatures.

The fabricated joint is simply an interference fit between the sleeve and the tube, wherein the free-body outside diameter of the sleeve is greater than the free-body inside diameter of the tube at the hardroll elevation. The resulting interface between the sleeve and the tube will be between the two extremes, with the sleeve being radially compressed and the tube being radially expanded, see Figure 7. The actual radial displacements of the sleeve, δ_s, and the tube, δ_t, will be a function of their individual compliances and the equilibrium pressure at the sleeve/tube interface,

$$\delta_s = P_I \frac{\partial r_o}{\partial P_o}, \text{and } \delta_t = P_I \frac{\partial r_i}{\partial P_i}.$$

Here, the partial derivative of the outside radius of the sleeve, r_o, with respect to the outside pressure on the sleeve, P_o, is the compliance of the sleeve, which is independent of the actual radial pressure for an elastic preload. The second expression is for the radial displacement of the inside of the tube as a function of the pressure on the inside of the tube. The expression for the compliance is based on the standard stress and displacement equations for thick-wall cylinders, e.g., see Reference 11, Table 32.

The total interference in the joint, Δ, is the sum of the initial interference due to the rolling operation, Δ_i, the pressure caused displacements of the sleeve and tube, and the thermally induced displacements of the sleeve and the tube,

$$\Delta = \Delta_i + P_{si} \frac{\partial r_{so}}{\partial P_{si}} - P_{to} \frac{\partial r_{ti}}{\partial P_{to}} + \alpha_s r_{so} \delta T_s - \alpha_t r_{ti} \delta T_t .$$

Where α is the coefficient of thermal expansion and δT is change in temperature from ambient conditions. Also note that the compliance derivative in the third term on the right is negative, i.e., pressure on the OD of the tube increases the net interference. A subscript has been added to the *r*, *P*, α and *T* terms to indicate whether the sleeve, *s*, or the tube, *t*, is under consideration. For example, the second term above gives the displacement of the sleeve outside radius, subscript *so*, as a function of the sleeve inside pressure, subscript *si*. The third term gives the radial displacement of the inside surface of the tube as a function of the outside pressure. It is noted that a radially outward deflections of the sleeve and tube are taken as positive (the compliance in the third term of the equation is negative). The fourth and fifth terms account for the thermal expansion of the sleeve, positive, and the tube, negative, respectively. The thermal expansion of the tube is taken as negative because it reduces the magnitude of the radial interference between the sleeve and the tube. Historical testing of rolled joints in tubesheets indicated that a residual radial displacement of 1.2 to 1.5 mils remained at the completion of the installation process, Reference 12. For the analysis of the sleeve/tube combination, the initial radial interference was assumed to be 0.5 mil.

Note that the compliances are a function of the elastic modulus of the material under consideration. Hence, the contributing terms to the total interference are functions of the temperatures of the sleeve and tube. For the analysis of the interference effects, values of the elastic moduli and the coefficients of thermal expansion from the ASME Code were fitted by least squares analysis to first or second order polynomials in temperature for the sleeve and the tube.

To adjust the failure pressures, ΔP_f, it is assumed that the change in failure pressure is proportional to the interference pressure, P_I, at the time of failure. It may be argued that the strength of the joint is a combination of the resistance to motion from the friction force between the sleeve and the tube, and resistance to motion because of galling, i.e., self-welding, of the sleeve and tube at the interface. Both of these mechanisms could be expected to be proportional to the interface pressure. The friction force would be the product of the coefficient of friction and the normal force which is the interface pressure times the area of contact. Likewise, the propensity for galling at the interface would also be expected to be proportional to the interface pressure, i.e., the greater the pressure, the more galling that would take place. However, beyond a certain level of interference pressure, additional strength of the joint might not be realized, e.g., the relation between the strength and the interference pressure could become non-linear. Alternatively, non-linear structural behavior of the joint would ensue from the pressure of the water in the joint (leakage), reducing the interface pressure. This could take place over a very localized circumferential region or some varying path, thus, there is no simple method for accounting for the reduction of interface load effects. For example, using the average value of the pressure based on an assumption of a linear gradient over the length of the hardroll would likely offer no benefit over omitting consideration of the pressure associated with the leaking water/steam.

Estimates based on a post-test examination of specimen K-98-017 indicates that about 20% of the circumference is significantly galled. For this specimen the active length of the typical gall site appears to be about 50 mils. For a circumference of 2.5", the galled area is 0.025 in^2. Considering the true ultimate shear strength of the material to be on the order of 90 to 100 ksi, the failure load would be about 2375 lbf. The area of the inside of the tube at the elevation of the hardroll is about 0.51", thus, the failure pressure would be estimated at about 4700 psi. The specimen actually exhibited a failure pressure of 5300 psi.

Based on the above, the failure pressure at normal operating (NOp) condition temperatures may be estimated from the failure pressure at test conditions by the following relationship,

$$\Delta P_{f,NOp} = \Delta P_{f,test} \frac{P_{I,NOp}}{P_{I,test}}$$

where,

$$P_I = \frac{\Delta}{\dfrac{\partial r_{ti}}{\partial P_{ti}} + \dfrac{\partial r_{so}}{\partial P_{so}}}$$

The terms in the denominator are the respective compliances of the tube and sleeve relative to the radial pressure or stress at the interface. Figure 8 illustrates the failure pressure versus the calculated interference pressure for the qualification test specimens. If we refer to the ratio of the failure pressure to the interference pressure during the test as β and the sum of the compliances in the last equation as C, using NOp temperature conditions, the failure differential pressure can be written as,

$$P_{f,NOp} = \frac{1}{\left(\dfrac{C}{\beta} - \dfrac{\partial r_{so}}{\partial P_{si}}\right)}\left(\Delta_i + \Delta_T - P_{to}\frac{\partial r_{ti}}{\partial P_{to}}\right),$$

where Δ_T is the sum of the thermally induced deflections. The third term in the numerator is the increased radial interference due to the secondary side pressure under NOp. Similar adjustments can be made to normalize the data to postulated SLB thermal conditions. Since it is assumed that the PTI is 360° by 100% deep, there is no unbalanced pressure correction to be made to account for the axial end-cap pressure load on the secondary side of the SG acting over the outside diameter area of the tube.

When the temperatures were not available at the exact time of failure, the temperatures recorded at 4800 psi, or 2560 psi if failure occurred before 4800 psi, were used in the analysis. The adjusted values of the failure pressure are conservative relative to the values that would be expected if the true temperature gradient at failure were known. The reason for this is that the increased leakage through the specimen results in the makeup water being cooler and the temperatures at failure being lower than the temperatures at 4800 psi, see Figure 9. The same phenomenon results in the sleeve-to-tube thermal gradient at failure being less than the gradient at 4800 psi, Figure 10. Both of these factors reduce the interference pressure and thereby, conservatively, reduce the predicted failure pressures at NOp and SLB temperatures.

It is to be noted that there are limitations of the model in that the assumption of linearity might not apply over the total range of temperatures encountered during the testing. The ultimate shear strength of the galled region of the interface, which would be expected to be a major contributor to the strength of the joint, could be affected by large radial compressive loads. On the other hand, increasing the interference pressure could increase the galling area. However, yielding of the sleeve or the tube under the estimated loading conditions would not be expected. Based on the dimensions of the sleeve and the tube, the hoop stress at the joint would be expected to be about 4.0 times the differential pressure. The sleeve/tube interface pressure corresponding to the mean adjusted (described in the next section) failure pressure was 8203 psi. Using the Tresca criterion for yield, the maximum stress intensity would be about 47 ksi. The yield strength of Alloy 690 or 600 material that has been cold-worked by the rolling process would be expected to be greater than this value. For example, the sleeve material is cold-worked almost 4% and the yield strength would be expected to be increased by 70 to 80% of the initial value, to the range of say 60 to 80 ksi, which is significantly greater than 47 ksi. Even for an estimated interface pressure of 12 ksi, the stress intensity would be 60 ksi, borderline as to whether or not yielding would take place. These examples demonstrate, however, that the magnitude of the interface pressure would be approximately bounded, albeit at a high value.

5.2 Structural Analysis of the Sleeved Tube Joints

A summary of the results of the evaluations are provided in Tables 4 and 5. Columns one and two of Table 4 list the statistics of the measured failure pressures. Columns three and four present the adjusted failure pressures corresponding to NOp and SLB thermal conditions respectively. It is apparent that there is considerably more spread, i.e., variance, in the adjusted test data relative to the unadjusted data. This is to be expected because the uncertainties associated with the model contribute directly to the variance of the calculated results.

5.2.1 Actual Failure Data

The as-measured, elevated temperature failure pressures from the twenty-two valid specimens averaged 5571 psi with a standard deviation of 486 psi. The 90%/90% lower tolerance limit (LTL) for the failure pressures was 4737 psi based on using a normal distribution. The normal probability plot, Figure 11, indicates that the data may be evaluated as either normally or lognormally distributed (the skew is small). One of the data points on Figure 11, K-98-033, appears to be a significant outlier, i.e., it lies well to the left of the lower end of the probability line, however, there was no known physical basis, e.g., equipment malfunction, for rejecting the data point and it was retained in the analysis. The intercepts of the straight lines drawn on Figure 11 are on the order of –0.04 and the slopes are 1.00 and 1.03 for the measured and logarithm of the measured failure pressures respectively. If the data are drawn from a normal population, the expected value of the intercept is 0.00, and 1.00 for the slope. A normal probability plot of the data without the apparent outlying point results in a more linear alignment of the data. Used directly, the probability of a joint having a failure pressure of less than 4800 psi is 0.064, while the probability of failure during a postulated SLB event is $1.9 \cdot 10^{-6}$.

The second column of Table 4 lists the statistics of the test failure pressures when specimens with a sleeve temperature less than or equal to 600°F and a sleeve/tube temperature gradient less than or equal to 100°F were selected. Because all of these specimens had a sleeve temperature at failure lower than the NOp and SLB temperature say 600°F, and a smaller temperature gradient at failure than the NOp, about 100°F, and SLB temperature gradients, likely to be 200°F or more, the interface and failure pressures must be conservative with regard to the NOp and SLB failure pressures. Increasing the primary side temperature and/or the temperature gradient can only increase the interface pressure. The distribution of the standardized selected data was confirmed to be approximately normal with a mean of zero and a slope of 0.93 regardless of whether or not the data were considered to be normally or lognormally distributed. The mean failure pressure of the fifteen specimens is 5611 psi with a standard deviation of 461 psi, a 90%/90% LTL of 4766 psi, and a probability of failure during a SLB of $5.7 \cdot 10^{-6}$. These results are quite similar to those from using all of the data. This leads to a probability of failure of one joint of 1000 of 0.006 during a SLB event.

Recall that the location of the PTIs from the pulled tube data is predominately below the top of the HRLT, i.e., in most cases a lip is present. The strength of such tubes has been amply demonstrated in prior documents and by the results from testing of pulled tube specimens. A 50% confidence bound on the number of indications located at the top of the HRLT is 26%. Thus, for 2000 PTIs in service, 500 could be expected to located at the top of the HRLT, and the probability of failure of one or more tubes during a postulated SLB event would be on the order of 0.003. Moreover, a 90% upper confidence bound on the number of indications located at the top of the HRLT is 49%, leading to an expectation of failure of one, or more, tubes of about 0.006 at a 90% confidence level.

5.2.2 Adjusted Failure Pressures

The rationale for analyzing adjusting failure pressures was to estimate the potential increase in the strength of the joints had testing at NOp thermal conditions been achieved. In addition, preliminary results from the data adjustments were presented to the NRC staff at the Reference 9 meeting. Since almost all of the tests involved temperatures inside of the sleeve and tube less than 600°F, and the coefficient of thermal expansion of the sleeve is greater than the tube, the average interference pressure increases in adjusting from test temperature to NOp temperature.

Prior to proceeding with the adjustments, the failure pressures were plotted against the interference pressures, see Figure 8, to verify that the assumption that a linear adjustment expression was reasonable. Except for one outlier, retained in the subsequent analyses, the failure pressure appears to be proportional to the interference pressure. However, the variance of the adjusted failure pressures must increase because of the deviations from linearity of the test data. The results from adjusting all of the applicable failure pressures to correspond to NOp and SLB thermal conditions are presented in the third and fourth columns of Table 4. Additional results from segregating the Table 4 data based on whether or not the specimen was *pre-cycled* prior to performing the actual failure test are presented in Table 5. It is noted from the information presented in Table 5 that pre-cycling has a significant effect on the failure pressure. Referring to Table 5, the expected failure pressure increases with pre-cycling from 6668 to 8670 psi while the standard deviation decreases slightly from 2177 to 1892 psi under NOp thermal conditions. The corresponding failure pressures at SLB temperatures are 8214 and 10684 psi with standard deviations of 2682 and 2336 psi. Since the sleeved tubes in the Kewaunee SGs have been pre-cycled by virtue of operation of the plant, it is apparent that the pre-cycled test results should be preferred for the failure pressure evaluation using the adjusted data. The 90%/90% LTL failure pressure at NOp temperatures is 5092 psi and the probability of a failure pressure being less than 4800 or 1600 psi is 0.032 and 0.0014 respectively. For SLB temperatures the LTL failure pressure is 6267 psi and the probability of failure during a postulated SLB event is 0.0023.

Using the adjustment method described in the previous section, the mean failure pressure at NOp sleeve and tube temperatures would be expected to be 7851 psi with a standard deviation of 2206 psi, see Table 4. For a sample of 22 values drawn from a normal population the 90%/90% LTL failure pressure would be 4062 psi at NOp temperatures. The probability of a failure pressure being less than 4800 psi is estimated to be 0.091, and the probability of the failure pressure being less than 1600 psi is 0.005. Considering postulated SLB conditions, the expected failure pressure would be on the order of 9673 psi with a standard deviation of 2721 psi, a 90%/90% LTL of 4999 psi and a probability of failure of 0.008 during a postulated SLB event. The results obtained from the adjusted data are considered to confirm the results from using the test data directly. Although the adjustment model predicts a significant increase in the average failure pressure, as expected, it also introduces significant additional variance in the predictions which results in a net increase in the failure probabilities predicted from the adjusted data. This is contrary to the actual data results for which the failure pressures should be conservative owing to the test temperature conditions.

5.2.3 Conclusion from the Failure Pressure Analysis

Based on the analysis results presented in Section 5.2.1, joints with indications at elevations above the estimated upper end of the hardroll lower transition exhibited strengths commensurate with PTIs at other relatively close elevations. A range of lengths from the bottom of the HRUT to the PTIs were tested as part of the qualification tests. These lengths ranged from 0.90 to 1.01 inches below the bottom of the HRUT on the inside of the sleeve. All of the qualification tests yielded acceptable results with regard to the guidelines of RG 1.121. Joints with indications at elevations above the estimated upper end of the HRLT, ≥ 0.90" below the bottom of the HRUT, exhibited strengths commensurate with PTIs at other relatively close elevations. Although the test data suggest that PTIs as close as 0.90" from the bottom of the HRUT provide acceptable results relative to the guidelines of RG 1.121, a value of 0.92" was conservatively selected for determining the minimum acceptable physical length, i.e., without consideration of measurement error.

The overall conclusion from the results of the measured test data is that any sleeved tube with a 360° by 100% deep PTI located ≥ 0.92", exclusive of measurement uncertainty, from the bottom of the HRUT will meet the requirements of RG 1.121.

6. Evaluation of Leak Rate Data

6.1 Summary of Former Leak Rate Tests

The results from previous test programs were reported in References 1 and 3. Excepting data from specimens which were slit in the hydraulic expansion, data from eighteen (18) test specimens had previously been presented. At 1600 psi nine (9) of the specimens leaked and nine (9) did not leak. At 2560 psi only one (1) of the specimens did not leak. The average leak rate from the leaking specimens at 1600 psi was 0.0008 gpm with a standard deviation of 0.0011 gpm. For the specimens that leaked at 2560 psi the corresponding numbers were 0.0060 and 0.0088 gpm.

Reference 4 reported that there was no leakage during an elevated temperature leak test of a sleeved tube sections removed from Kewaunee. Likewise, the leak rate from a severed sleeved tube specimen removed from SG "A" in 1996 exhibited a leak rate of $3.0 \cdot 10^{-5}$ gpm at a differential pressure of 2560 psi at 600°F. The leak rates from the tests of the field sleeved tube sections were very small, and did not exhibit a strong dependence of the differential pressure.

6.2 Concept Test Program Leak Rate Results

Primary-to-secondary leak rates were recorded for the specimens involved in the concept testing. For tests performed at a temperature of 600°F and a differential pressure of 3000 psi the average leak rate was 0.022 gpm with a standard deviation of 0.013 gpm. This is similar to the results from the qualification tests, following, although the average leak rate is higher. It is noted that the leak rate should be reduced to be representative of a differential pressure of 2560 psi. For flow through narrow crevices the rate is approximately proportional to the differential pressure if flashing does not occur. Because flashing does occur somewhere along the leak path, the adjustment would only be approximate. However, owing to the small size of the sample, less than ten specimens total, and the difference in oxidation history from the qualification specimens, the data may be discounted from further analysis.

6.3 Qualification Program Leak Rate Tests

The primary-to-secondary leak rates from each of the specimens pressure tested was recorded at differential pressures of 1600, 2560, and 4800 psi. Because of the difficulties associated with maintaining a constant temperature during the leak testing, the average sleeve temperature was 522°F and the average tube temperature was

433°F. The average leak rate at a differential pressure of 1600 psi from the 20 specimens for which data were available was 0.012 gpm with a standard deviation of 0.013 gpm. The average leak rate at a differential pressure of 2560 psi from the 25 specimens for which data were available was 0.013 gpm with a standard deviation of 0.012 gpm. The ratios of the leak rates at 2560 psi to those at 1600 psi are presented on Figure 12. The minimum value is slightly less than one-half and the maximum value is 3.9. In essence, the leak rate distributions were the same at both pressure differentials.

The leak rates were also selectively evaluated using the second approach considered for analyzing the failure pressures. The sample data for estimating the leak rate at a differential pressure of 1600 psi were selected to have a sleeve temperature of ≤600°F and a sleeve to tube temperature gradient of ≤100°F. The average leak rate from the 13 specimens meeting the criteria was 0.014 gpm with a standard deviation of 0.015 gpm. For the SLB leak rate, the sample data were selected that had a sleeve temperature of ≤600°F regardless of the thermal gradient. The mean leak rate from 16 specimens was 0.013 gpm with a standard deviation of 0.009 gpm.

The ratios of the leak rate at SLB to that at NOp for the 13 specimens ranged from 0.233 to 3.90. Using Tchebychev's inequality, which is independent of the underlying distribution, the maximum ratio that would be expected at a 99% confidence level is 9.3, and at a 99.9% confidence level the ratio is 36.1. In effect, the leak rate during a 4LB event would almost always be expected to be less than one order of magnitude greater than the leak rate during normal operation. For an allowable total NOp leak rate of 150 gpd, the maximum total leak rate expected during a SLB event would be about 1 gpm, significantly below the allowable level at Kewaunee.

6.4 Field Leak Rate Experience

A graphical primary-to-secondary leak rate history was presented at the Reference 9 meeting. It was noted that at the end of September of 1996, when the plant was about to shut down for the service outage, the cumulative primary-to-secondary leak rate during normal plant operation was on the order of 2 (Argon 41 measurement) to 5 (tritium measurement) gallons per day. During the service outage it was discovered that approximately 1500 sleeved tubes with PTIs had been in service, i.e., indications had developed to the extent that they could be detected using non-destructive examination technology. Converting the actual leak rates to 0.0014 and 0.0035 gpm respectively, the actual leak rate from the 1500 sleeved tubes was less than might be expected from the lower regime of the test data for a single, 360°, 100% throughwall, sleeved tube PTI. This is consistent with the observed leak rate from one of the pulled sleeved tubes from Kewaunee.

7. Nondestructive Examination Considerations

As previously noted, the initial length based criterion, Reference 4, relied on the critical length value being such as to include an interfering overlap, or lip, of the ID of the tube on the OD of the sleeve. The application for the use of the criterion was amended by WPSC because information was not available at that time to demonstrate that the overlap would be present if there had been significant rolldown of the rolling tool at the completion of the rolling operation. Additional information was obtained during the implementation of the ΔD criterion which demonstrated that hardroll flat lengths are independent of the presence of rolldown as discussed in the following sections.

7.1 Effect of Rolldown on Hardroll Flat Length

The information presented in this section was discussed with the NRC staff during the Reference 8 meeting. A large number of tubes with PTIs were selected at random from SGs A and B at Kewaunee. The nondestructive examination data from the 1996 refueling outage inspection of each of the tubes was recorded with regard to the length of the hardroll on the ID of the sleeve, i.e., the distance from the tangent point or maximum diameter at the bottom of the upper transition to the tangent point or maximum diameter at the top of the lower transition, and whether or not rolldown was present. The presence of rolldown is readily apparent in the depiction of the bobbin coil ECT data.

Of the joints inspected in SG A, 100 of 338 exhibited rolldown. The corresponding numbers from SG B were 65 of 200. All of the data were examined by an analyst considered to be an expert in the industry. For SG A, the average length of the hardroll was found to be 0.976" with rolldown and 0.978" without rolldown. The standard error of the data was 0.0284" and 0.0307" respectively. For SG B the corresponding mean values were also 0.976" and 0.978", and the standard deviations of the length measurements were 0.0337" and 0.0311". These differences do not appear to be significant by inspection. Joining the data from the two SGs leads to a comparison of 165 measurements with rolldown and 373 without. The average length with and without rolldown was calculated to be 0.976" and 0.978" respectively with associated standard deviations of 0.0306" and 0.0308". In conclusion, the length of the hardroll as measured by the eddy current examination is independent on the presence of rolldown in the joint. In addition, the data exhibit constant variance between the specimens with and without rolldown. Hence, the length of the hardroll is unaffected by rolldown at the conclusion of the rolling process.

7.2 Field Examination Data Evaluation Results

The elevation of the PTIs was also compared to the length of the hardroll for all the Kewaunee tubes examined. It was found is that the elevation of the flaws in the tubes is somewhat dependent on the presence of rolldown. The average distance from the bottom of the HRUT to the elevation of the PTIs for the tubes with rolldown was 1.14" and 1.10" for SGs A and B, respectively. Without rolldown, the values were 1.03" and 1.02" respectively. Thus, the presence of rolldown tends to move the average location of the cracks down by about 0.1". This was also observed in the pulled tube specimens from Kewaunee. This also means that PTIs in joints with rolldown would be very unlikely to be located at (or above) the top of the hardroll lower transition. Based on the sample results from the previous section, about 31% of the joints in the SGs could be expected to exhibit rolldown, and hence, greater strengths than predicted by the qualification test program.

A possible explanation for this observation is that although the length of the hardroll is not increased, the length over which the interference between the tube and the sleeve exists does increase. If that is the case, then the location where the maximum concentration of boron and lithium deposits would form would also be lowered. Based on the results from the strength testing of the joints, the exact elevation of the cracking would not be deemed to be important. This is confirmed by the fact that an evaluation of the eddy current data revealed only a weak correlation (indices of determination of ≤ 8%) of the elevation of the PTI to the length of the hardroll, although, only about 18% of the indications were judged to be above the top of the HRLT.

Finally, the cumulative distributions of the length data from SGs A and B, see Figure 14, imply that 91% of the indications in SGs A and B are ≥ 0.95" from the bottom of the HRUT as measured on the inside of the sleeve.

7.3 Laboratory Specimens Examination Data Evaluation Results

NDE examination of the forty fabricated specimens, using the same expert analyst as for the field data, yielded an average length from the bottom of the HRUT to the center of the PTI of 0.96" with a standard deviation of 0.035". The range of lengths was from 0.90" to 1.01", with a median value of 0.96". The locations of the PTIs in the fabricated specimens were smaller, i.e., more conservative, in general than the elevations observed in the field. The failure pressure of the specimens as a function of ECT response distance is provided on Figure 13. Although the data imply that the shorter lengths exhibited higher strengths, contrary to any expectation, it is more likely that no relation exists over the range of lengths tested. The same expert analyst, the trainer for the other analysts, made the measurements of the field and test joints. However, for the range of hardroll lengths measured, it has been demonstrated that the actual measurement is not of major import.

8. NRC Staff Comments

At the close of the Reference 9 meeting the NRC staff requested that the following, not inclusive, issues be addressed in the formal submittal of this report for regulatory review.

1) Delineate the data exclusion actions taken regarding each data point used in the evaluation.

<u>Response</u>

The exclusion of the data is discussed in Section 3 and illustrated on Figure 4 of this report. In summary, only data from specimens for which a malfunction of the testing equipment was apparent were omitted from the analyses.

2) Discuss the fitting of the skew distributions to the data, if any. For example, the failure pressure data were analyzed as following a normal distribution while the leak rate data were analyzed as following a lognormal distribution.

<u>Response</u>

The failure pressure data were analyzed as following a normal distribution about a mean failure pressure while the leak rate data were analyzed as following a lognormal distribution about a median leak rate (mean logarithm of the leak rate). The use of the normal distribution mathematically admits the potential for a failure pressure of less than zero, which is physically impossible. However, the failure pressure data were also compared to a lognormal distribution with results similar to those from the normal distribution. Both normal and loguormal plots reasonably agreed with the failure pressure data. The reason that the normal distribution can be used when the actual data cannot be less than zero is that the mean of the data is large when compared to the standard deviation, hence, obtaining a failure pressure value near or below zero is a very low probability event.

Because the standard deviation of the leak rates is on the order of the mean, the use of the normal distribution would indicate a relatively high probability of occurrence of leak rate being less than zero. Again, a physical impossibility. Hence, the lognormal distribution is much more suited to the leak rates. A lognormal plot of the expected values from a lognormal distribution adequately matched the observed leak rates.

3) The failure pressure and leak rate data samples are rather small, i.e., ranging from 15 to 25 specimens. Report the 90%/90% tolerance level value for each of those variables.

<u>Response</u>

The values of the 90% confidence limit for the 90th percentile of failure pressures for the conditions analyzed are provided in Tables 4 and 5. For normal operating conditions, using all of the as-measured data, this value is expected to be ≥4737 psi. No value was calculated for the leak rate because the amount expected during a postulated SLB event is expected to be no more that 9 times that at normal operation at a 99% confidence level.

The results from using data from a small sample size are large confidence, prediction, and tolerance bounds for describing the potential parameters of the population from which the sample was drawn. For example, the mean and standard deviation of the filtered failure pressure data were 5611 and 461 psi, while the corresponding values for the non-filtered data were 5571 and 486 psi. Thus, the filtered data sample has a larger mean and a smaller standard deviation than the non-filtered sample. However, the calculated probability of burst at a ΔP of 2560 psi for the filtered data is three times greater than the value estimated from the non-filtered data. The reason for this is that the non-filtered data consisted of 22 data points while the filtered data consisted of 15 data points.

4) Provide an explanation of why the field tubes are stiffer than those used in the qualification test program.

<u>Response</u>

The total strain imparted during the rolling process is on the order of 3.5 to 4%. This means that both the sleeve and the tube are plastically deformed during the rolling process. The spring-back of the tube and the sleeve to an equilibrium position following the completion of the rolling process is an elastic process. As such, the residual interface pressure is somewhat independent of the material strength properties of the sleeve and the tube, but is very dependent on the elastic modulus of the material, which is not a function of the yield or ultimate strength. Thus, the residual preload would be expected to be similar for the test specimens as for the sleeves installed at Kewaunee.

5) Provide a discussion regarding the most severe or limiting accident. The specific question to be answered is the limiting accident the postulated SLB event.

<u>Response</u>

As previously noted, a discussion of the most severe accident condition was provided in Reference 4. Transients characterized by an initial increase in the primary water pressure, e.g., large step decrease in load or loss of load from full power, are accompanied by an increase in the steam temperature and steam pressure, resulting in no net increase in driving potential from the primary to the secondary side of the SG. For a loss of power event, the primary pressure decreases initially and then increases to about 250 psi. However, the steam temperature and pressure increase such that the net change in ΔP is negative, in addition to thermal tightening of the joint. The reactor trip from full power and reactor coolant pipe break both result in a significant decrease in ΔP. There are no differential pressures during other transient events that are as severe as that from the postulated SLB event.

9. Conclusions

One of the first facts to recall is that the joint has an hourglass shape, so, there is a material interference that must be overcome for a severed tube to separate from the sleeve, i.e., it is not only a compression joint. It is noted that five of six, or 83%, removed SG tube specimens had the cracking located below the top of the HRLT. This is a very limited database, and the median rank (50% confidence) for this percentage of the tubes in the Kewaunee SGs is 74% (instead of 83%). In addition, the fraction could only represent 49% of the actual SG tubes at a 90% confidence level (probability of 0.1 that the actual fraction is less than 49%). However, the expectation is that most of the PTIs would meet the approved ΔD criterion if more accurately measured. For the minority of tubes where the PTI is located at the top of the HRLT, the testing programs have demonstrated adequate strength and leak resistance. Some specific conclusions from the test programs are provide in what follows.

The length of the hardroll flat is not affected by tool roll-down during the installation of the sleeves. Therefore, although not needed based on the results of the current round of testing, there could be a significant likelihood that an interferring overlap exists between the ID of the tube and the OD of the sleeve at the elevation of the PTI for PTI location measurements on the order of 0.95".

An HEJ sleeved tube with a 360° by 100% throughwall circumferential PTI located ≥ 0.92" below the bottom of the HRUT has sufficient leak resistance such that the total leak rate from many such tubes would be expected to meet the exposure requirements of 10CFR100 and GDC 19. The main rationale for this is that the data demonstrate that the leak rate during a postulated SLB event would not be expected to significantly exceed the leak rate during normal operation. A 99% confidence bound on the ratio of the leak rate at SLB to NOp is 9.3. The application of high confidence limits to the leak rate still results in a value that is significantly below the allowable during a postulated SLB event.

The burst/failure strength of the joint is such that the structural requirements of RG 1.121 would be expected to be met. This is based on the evaluation of data which can be stated to be conservative relative to the SG thermal conditions during NOp and SLB. The probability of joint failure at a differential pressure of less than 3 times the design ΔP (4800 psi) is less than about 5%, and the probability of failure during a SLB event is on the order of $5.7 \cdot 10^{-6}$.

In summary, sleeved tubes meeting a location criterion of ≥ 0.92" below the bottom of the HRUT as illustrated on Figure 15, measured on the inside of the sleeve and without measurement uncertainty, meets the RG 1.121 guidelines with regard to primary-to-secondary leak resistance and structural integrity during normal operation and postulated accident conditions. Therefore, the sleeved tube pressure boundary may be relocated as discussed.

10. References

1. WCAP-11643, Revision 1, "Kewaunee Steam Generator Sleeving Report (Mechanical Sleeves)," Westinghouse Electric Corporation (November 1988).

2. WCAP-14157, "Technical Evaluation of Hybrid Expansion Joint (HEJ) Sleeved Tubes With Indications Within the Upper Joint Zone," Westinghouse Electric Corporation (August 1994).

3. WCAP-14157, Addendum 1, "Supplemental Leak and Tensile Test Results for Degraded HEJ Sleeved Tubes in Model 44/51 S/G's," Westinghouse Electric Corporation (September 1994).

4. WCAP-14446, " Repair Boundary for Parent Tube Indications Within the Upper Joint Zone of Hybrid Expansion Joint (HEJ) Sleeved Tubes," Westinghouse Electric Corporation (August 1995).

5. WCAP-14641, "HEJ Sleeved Tube Structural Integrity Criteria: 'ΔD' Diametral Interference at PTIs," Westinghouse Electric Corporation (April 1996).

6. USNRC Letter, "Amendment No. 128 to Facility Operating License No. DPR-43 – Kewaunee Nuclear Power Plant (TAC No. M95302)," R. Laufer (NRC) to M. Marchi (September 25, 1996).

7. "Safety Evaluation by the Office of Nuclear Reactor Regulation Relating to Amendment No. 128 to Facility Operating License No. DPR-43," G. Hornseth and K. Karwoski, USNRC, Attachment to Reference 6 (September 25, 1996).

8. Presentation Material, *WPSC/NRC Meeting on HEJ Inspection and Acceptance Criterion*, Wisconsin Public Service Company personnel with the NRC staff (December 9, 1997).

9. Presentation Material, *WPS/W/Zetec/NRC Meeting on HEJ Inspection and Acceptance Criterion*, Wisconsin Public Service Company personnel with the NRC staff (April 9, 1998).

10. Internal letter, "Kewaunee – EPRI Tube Exams," Kuchirka, P., to distribution (January, 1997).

11. Roark, R., and Young, W., Formulas for Stress and Strain, Fifth Edition, McGraw-Hill (1975).

12. WCAP-14677 (Proprietary), "F* and Elevated F* Tube Alternate Repair Criteria for Tubes with Degradation Within the Tubesheet Region of the Kewaunee Steam Generators," Westinghouse Electric Corporation (June 1996).

Table 1: Summary of Test Conditions & Analysis Usage for Kewaunee HEJ Sleeve Qualifications Tests' Data

Specimen No.	Lip	Test Type	No. Pre-cycles	Failure Anal.	Leak Anal.	PTI Location	Comments
K – 98 – 001	No	Hot	0	1	1	1.01	ΔT at ≥ 2560 psi only.
K – 98 – 002	No	Hot	0	1	1	0.93	
K – 98 – 004	No	Hot	0	1	1	0.97	ΔT at ≥ 2560 psi only.
K – 98 – 005	No	Hot	0	1	1	0.94	ΔT at ≥ 2560 psi only.
K – 98 – 006	No	Hot	0	1	1	0.94	
K – 98 – 007	No	Hot	0	1	1	0.90	ΔT at ≥ 2560 psi only.
K – 98 – 009	No	Hot	1	1	1	0.99	
K – 98 – 010	No	Hot	1	0	1	0.97	ΔT near failure not available.
K – 98 – 011	No	Hot	0	1	1	0.93	Hard to adjust furnace.
K – 98 – 015	No	Hot	1	1	1	0.92	
K – 98 – 017	No	Hot	10	1	1	1.00	Valid, but, large ΔT.
K – 98 – 019	No	Hot	1	1	1	0.94	
K – 98 – 021	No	Hot	0	0	1	0.97	ΔT near failure not available.
K – 98 – 022	No	Hot	1	1	1	0.93	
K – 98 – 024	No	Hot	1	1	1	0.97	
K – 98 – 025	No	Hot	0	1	1	0.98	ΔT at ≥ 2560 psi only.
K – 98 – 026	No	Hot	1	1	1	0.93	
K – 98 – 027	No	Hot	1	1	1	0.92	
K – 98 – 028	No	Hot	1	1	1	0.94	
K – 98 – 030	No	Hot	1	1	1	0.94	
K – 98 – 031	No	Hot	10	0	1	0.95	Not valid, ΔT not reliable.
K – 98 – 033	No	Hot	0	1	1	0.99	
K – 98 – 036	No	Hot	1	1	1	1.01	
K – 98 – 037	No	Hot	1	1	1	0.92	
K – 98 – 038	No	Hot	1	1	1	0.97	
K – 98 – 008	No	Cold	0	0	0	0.99	Used concept test pressures.
K – 98 – 012	Yes	Cold	0	0	0	0.93	Not held at any pressure.
K – 98 – 013	No	Cold	0	0	0	0.94	
K – 98 – 016	No	Cold	0	0	0	0.99	
K – 98 – 018	No	Hot	0	0	0	0.94	Destroyed by heater.
K – 98 – 023	No	Cold	0	0	0	0.95	
K – 98 – 034	Yes	Hot	0	0	1	1.01	Seal leak, not completed.
K – 98 – 003	Yes	No Test				0.97	Archive
K – 98 – 014	Yes	No Test				0.92	Archive
K – 98 – 020	Yes	No Test				0.92	Archive
K – 98 – 029	Yes	No Test				1.00	Archive
K – 98 – 032	Yes	No Test				1.00	Archive
K – 98 – 035	Yes	No Test				0.95	Archive
K – 98 – 039	Yes	No Test				1.02	Archive
K – 98 – 040	Yes	No Test				1.01	Archive

Table 2: Summary of Failure Pressure Measurements and Adjusted Failure Pressures.

Specimen No.	Test Failure Pressure (psi)	Temperatures [1.] Internal 4800 psi (°F)	Temperatures [1.] External 4800 psi (°F)	Failure Temps Int. at Failure (°F)	Failure Temps Ext. at Failure (°F)	Failure Estimates NOp Failure (psi)	Failure Estimates SLB Failure (psi)
K-98-001	5000	145.0	118.0	145.0	118.0	8106	9986
K-98-002	5800	480.0	340.0	480.0	340.0	6305	7763
K-98-004	5700	760.0	608.0	760.0	608.0	5981	7363
K-98-005	5600	500.0	385.0	500.0	385.0	6501	8004
K-98-006	>6000	750.0	557.0	530.0	385.0	>6439	>7929
K-98-007	>6000	240.0	215.0	210.0	185.0	>10436	>12866
K-98-009	5000	320.0	290.0	320.0	290.0	7564	9317
K-98-010	4800	Temperature Data Not Available					
K-98-011	5700	760.0	675.0	760.0	675.0	7567	9321
K-98-015	>6000	504.0	378.0	240.0	207.0	>10018	>12349
K-98-017	5300	680.0	400.0	680.0	400.0	3603	4432
K-98-019	5780	344.0	274.0	344.0	274.0	8065	9935
K-98-021	4050	Temperature Data Not Available					
K-98-022	5800	576.0	503.0	576.0	503.0	8054	9922
K-98-024	4850	140.0	139.0	140.0	139.0	8619	10620
K-98-025	5000	518.0	450.0	518.0	450.0	6494	7996
K-98-026	5300	187.0	165.0	187.0	165.0	8842	10895
K-98-027	6000	490.0	430.0	211.0	192.0	10682	13170
K-98-028	>6000	567.0	540.0	170.0	165.0	>11487	>14167
K-98-030	>6000	560.0	415.0	299.0	235.0	>8788	>10829
K-98-031	5800	811.0	174.0	Temperature Gradient Unreliable.			
K-98-033	4300	620.0	450.0	620.0	450.0	2180	2695
K-98-036	6000	465.0	390.0	465.0	390.0	8352	10290
K-98-037	6020	400.0	366.0	400.0	366.0	9838	12126
K-98-038	5420	275.0	250.0	275.0	250.0	8796	10838

Notes:
1. If the temperature values were not available because the specimen failed at a lower pressure, the last known temperatures were used.
2. If the failure temperatures were unknown, the temperature values at 4800 psi were used.

Table 3: Summary of Leak Rate Database						
	Leak Rate, gpm		Measured Temperatures, °F			
Sample No.	ΔP = 1600 psi	ΔP = 2560 psi	Internal 1600 psi	External 1600 psi	Internal 2560 psi	External 2560 psi
K - 98 - 001		0.059			675.0	628.0
K - 98 - 002	0.006	0.010	650.0	470.0	526.0	405.0
K - 98 - 004		0.003			605.0	544.0
K - 98 - 005		0.005	650.0	570.0	624.0	505.0
K - 98 - 006	0.007	0.006	675.0	500.0	435.0	315.0
K - 98 - 007		0.006			537.0	496.0
K - 98 - 009	0.007	0.009	600.0	532.0	529.0	459.0
K - 98 - 010	0.021	0.011	496.0	409.0	470.0	390.0
K - 98 - 011	0.005	0.008	614.0	551.0	685.0	613.0
K - 98 - 015	0.006	0.007	515.0	443.0	508.0	415.0
K - 98 - 017	0.011	0.004	480.0	298.0	630.0	381.0
K - 98 - 019	0.060	0.014	363.0	306.0	416.0	346.0
K - 98 - 021	0.012	0.003	485.0	367.0	589.0	466.0
K - 98 - 022	0.006	0.004	498.0	418.0	523.0	440.0
K - 98 - 024	0.006	0.012	507.0	413.0	560.0	440.0
K - 98 - 025		0.012			675.0	608.0
K - 98 - 026	0.010	0.039	483.0	400.0	495.0	460.0
K - 98 - 027	0.008	0.012	526.0	472.0	489.0	430.0
K - 98 - 028	0.009	0.012	400.0	362.0	556.0	500.0
K - 98 - 030	0.003	0.007	501.0	429.0	563.0	445.0
K - 98 - 031	0.006	0.011	609.0	371.0	660.0	422.0
K - 98 - 033	0.015	0.027	506.0	375.0	460.0	332.0
K - 98 - 036	0.005	0.017	512.0	436.0	500.0	438.0
K - 98 - 037	0.012	0.019	399.0	350.0	512.0	454.0
K - 98 - 038	0.026	0.016	510.0	466.0	482.0	434.0

Note: Specimen 018 was destroyed by a heater malfunction, and specimen 034 testing was terminated due to a seal leak.

Table 4: Summary of Failure Pressure Test Results for All, Filtered, & Adjusted K-98-0xx Data				
Parameter	As Measured (psi)	Filtered [1] (psi)	NOp ΔT (psi)	SLB ΔT (psi)
Count	22	15	22	22
Mean	5571	5611	7851	9673
Standard Deviation	486	461	2206	2721
95^{th} Percentile	4735	4800	4054	4990
90% / 90% Tolerance	4737	4766	4062	4999
Probabilities of Failure (Normal Distribution)				
Pr(P_f < 4800 psi)	$6.4 \cdot 10^{-2}$	$5.0 \cdot 10^{-2}$	$9.1 \cdot 10^{-2}$	
Pr(P_f < 3657 psi)	$4.0 \cdot 10^{-4}$	$4.1 \cdot 10^{-4}$		$1.9 \cdot 10^{-2}$
Pr(P_f < 2560 psi)	$1.9 \cdot 10^{-6}$	$5.7 \cdot 10^{-6}$		$8.1 \cdot 10^{-3}$
Pr(P_f < 1600 psi)	$2.9 \cdot 10^{-8}$	$2.5 \cdot 10^{-7}$	$5.0 \cdot 10^{-3}$	

Notes: 1. The selected values were based on a measured sleeve temperature of ≤600°F and a differential temperature of ≤100°F. Limiting the selected data to pre-cycled specimens does not significantly change the results.

Table 5: Comparison of Pre-Cycled Failure Pressure Test Results for K-98-0xx Data				
Parameter	No Pre-Cycle NOp ΔT (psi)	Pre-Cycled NOp ΔT (psi)	No Pre-Cycle SLB ΔT (psi)	Pre-Cycled SLB ΔT (psi)
Count	9	13	9	13
Mean	6668	8670	8214	10684
Standard Deviation	2177	1892	2682	2336
95^{th} Percentile	2619	5297	3225	6520
90% / 90% Tolerance	2159	5092	2659	6267
Probabilities of Failure (Normal Distribution)				
Pr(P_f < 4800 psi)	$2.1 \cdot 10^{-1}$	$3.2 \cdot 10^{-2}$		
Pr(P_f < 3657 psi)			$6.4 \cdot 10^{-2}$	$5.5 \cdot 10^{-3}$
Pr(P_f < 2560 psi)			$3.4 \cdot 10^{-2}$	$2.3 \cdot 10^{-3}$
Pr(P_f < 1600 psi)	$2.4 \cdot 10^{-2}$	$1.4 \cdot 10^{-3}$		

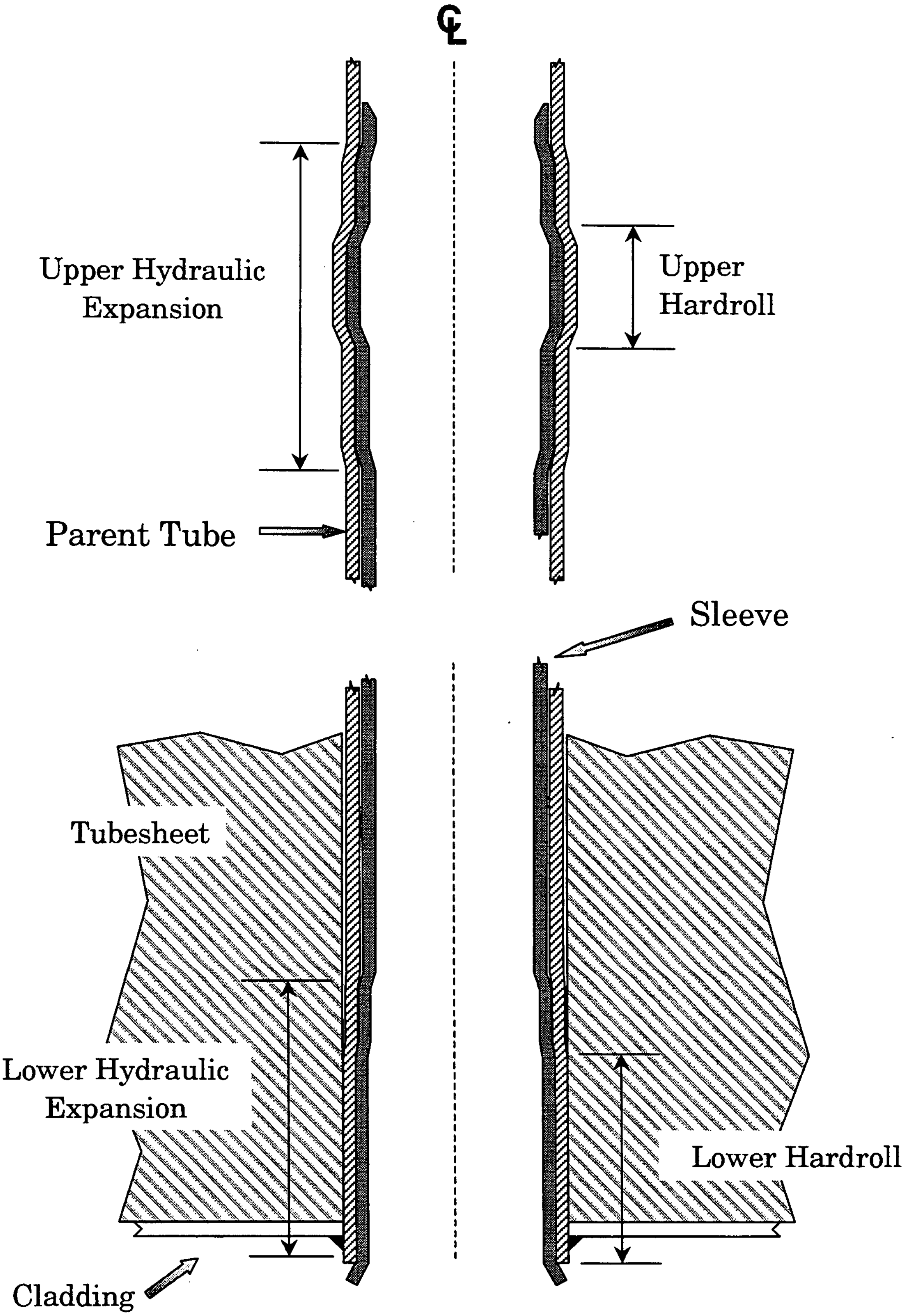


Figure 1: Installed HEJ Sleeve Configuration

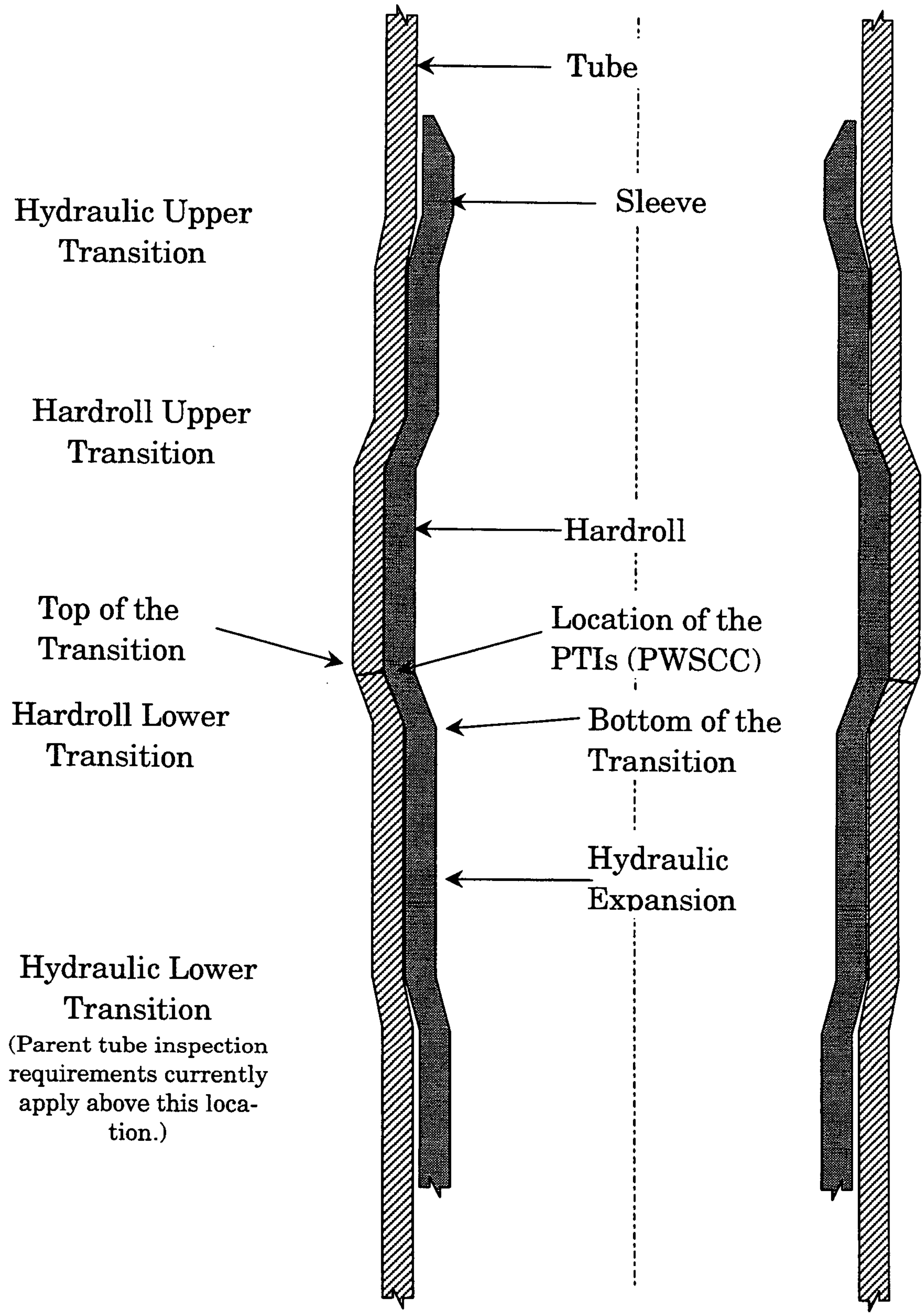


Figure 2: Hybrid Expansion Joint Nomenclature

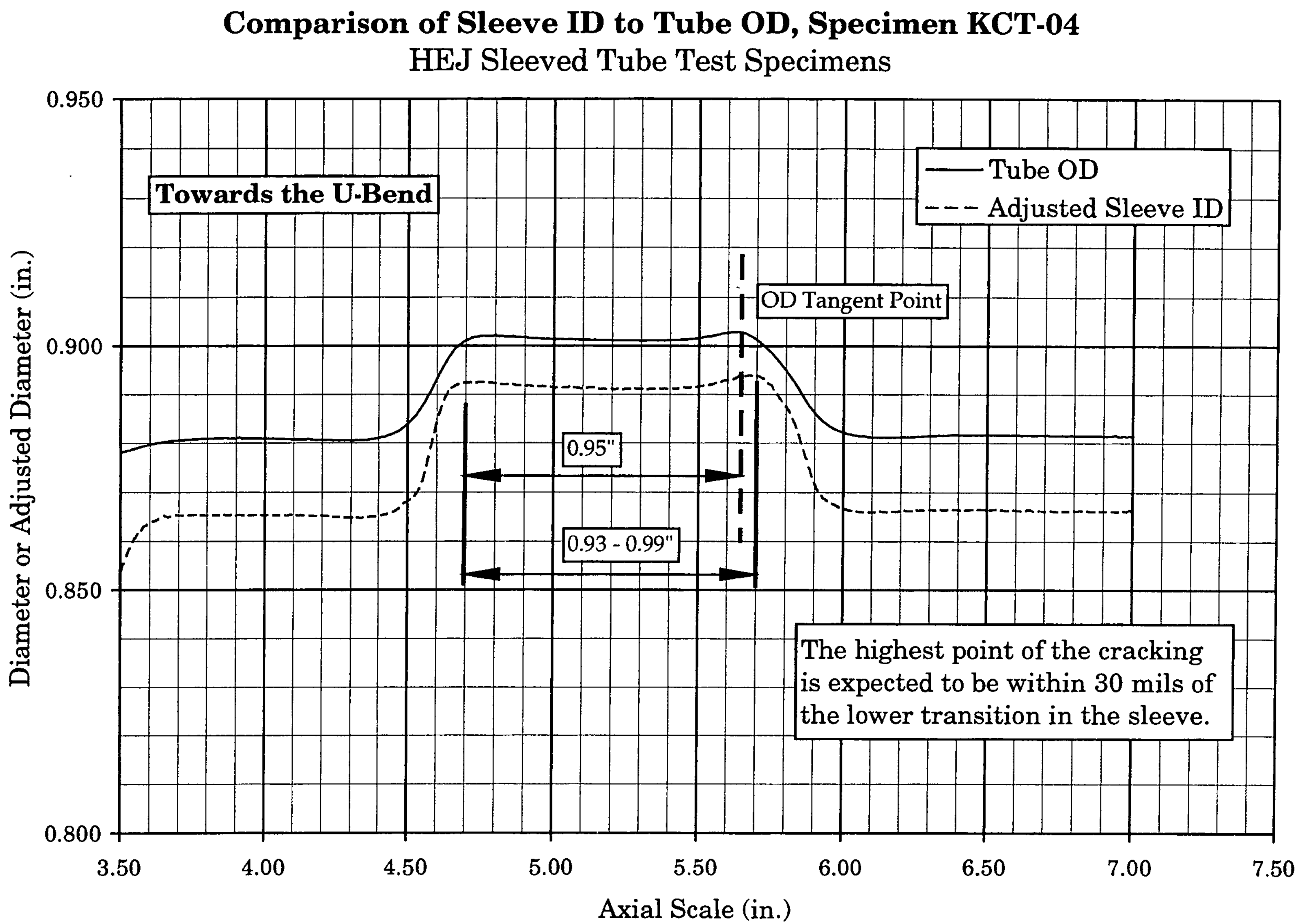


Figure 3: Comparison of Sleeve & Tube Hardroll Profiles

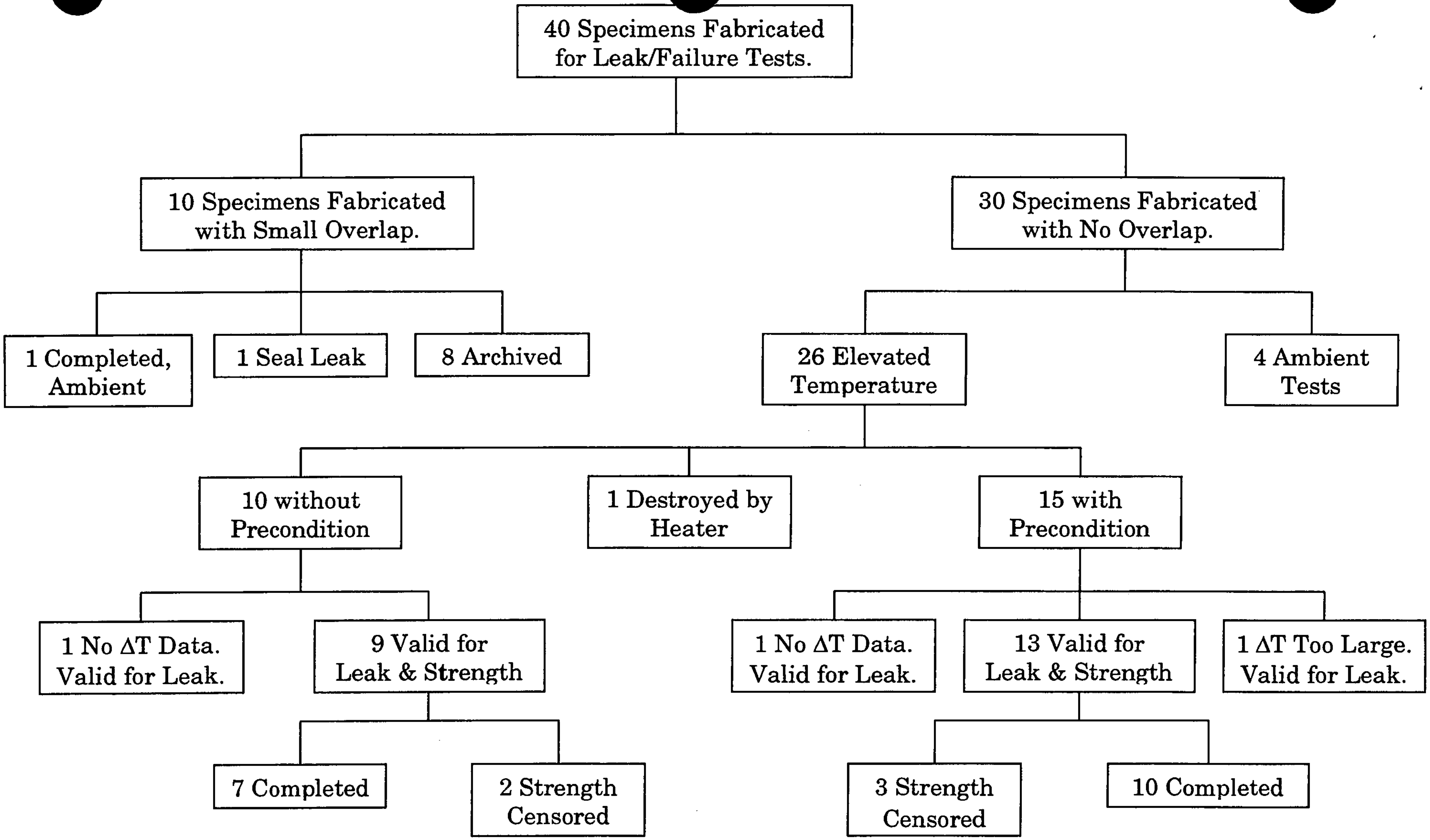


Figure 4: Key to the HEJ Qualification Tests' Specimens

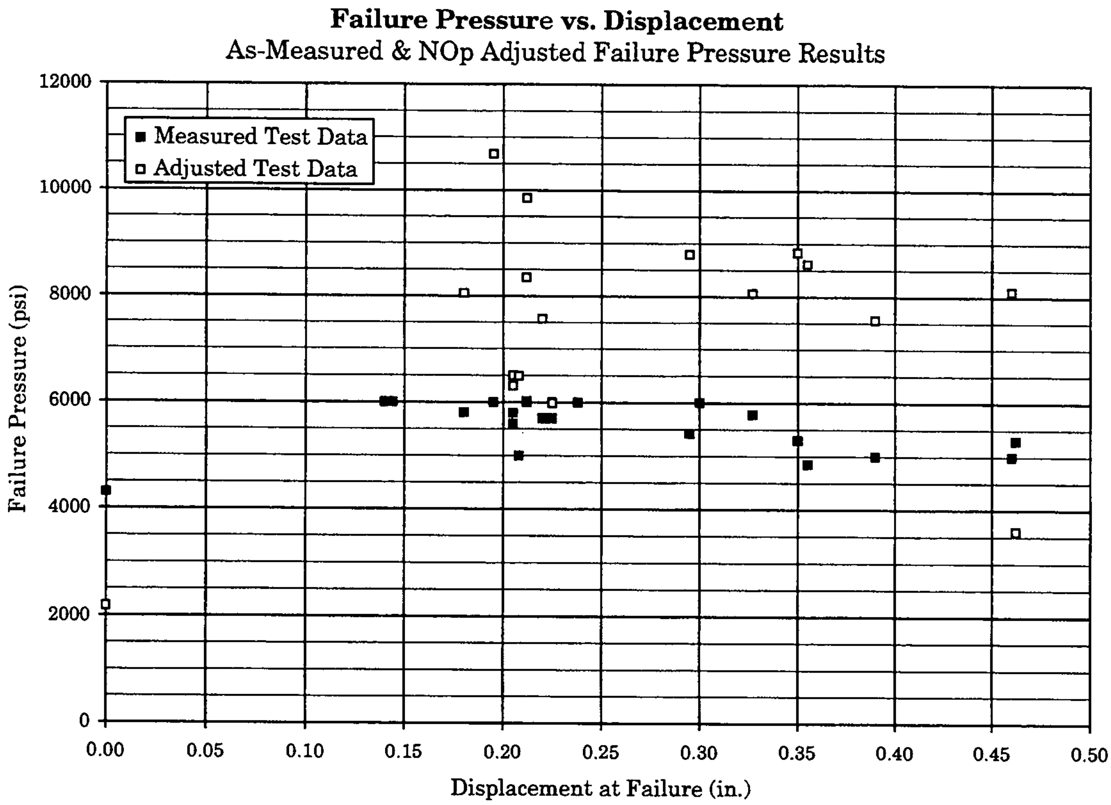


Figure 5: Failure Pressure vs. Displacement at Failure

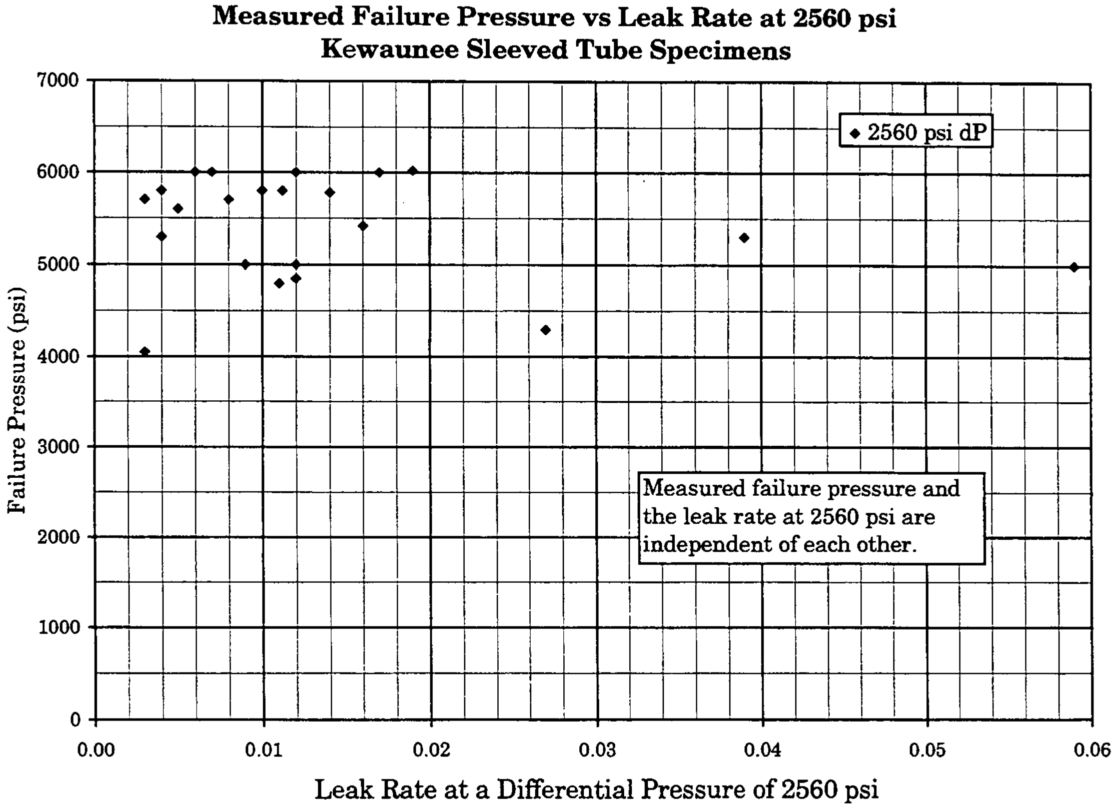
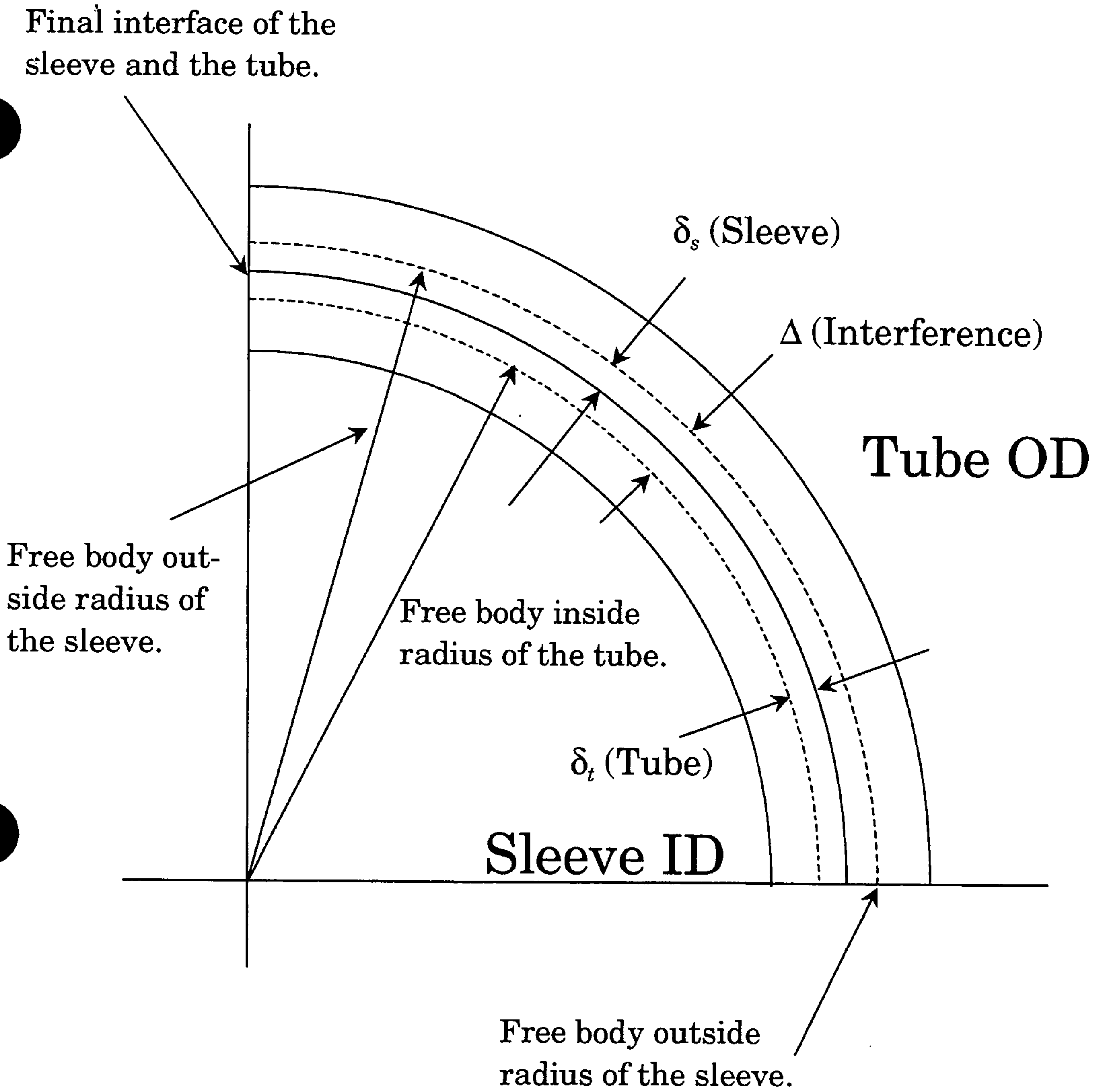


Figure 6: Failure Pressure vs. Leak Rate at 2560 psi



The solution is based on compatibility and equilibrium at the interface. The radial stress in the sleeve and tube are equal at the interface, and

$$\delta_t + \delta_s = \Delta$$

Figure 7: Illustration of the Sleeve/Tube Interference Fit

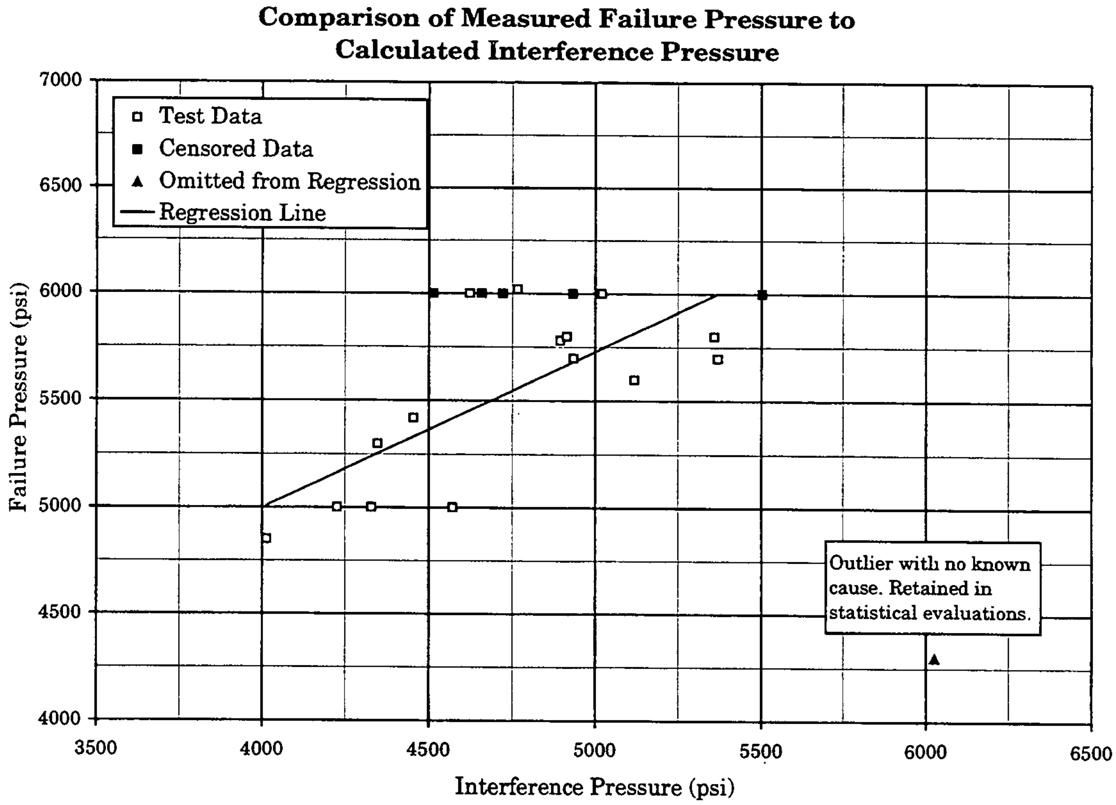


Figure 8: Failure Pressure vs. Interference Pressure

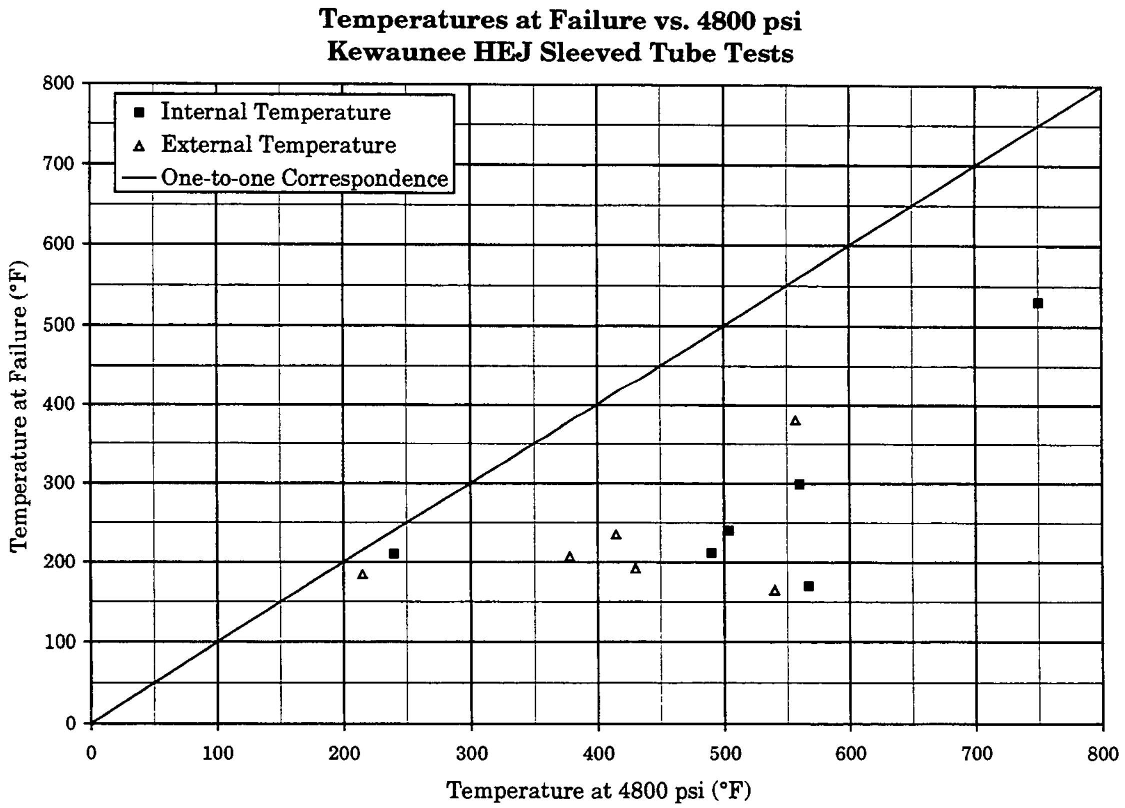


Figure 9: Temperature at Failure vs. Temperature at 4800 psi

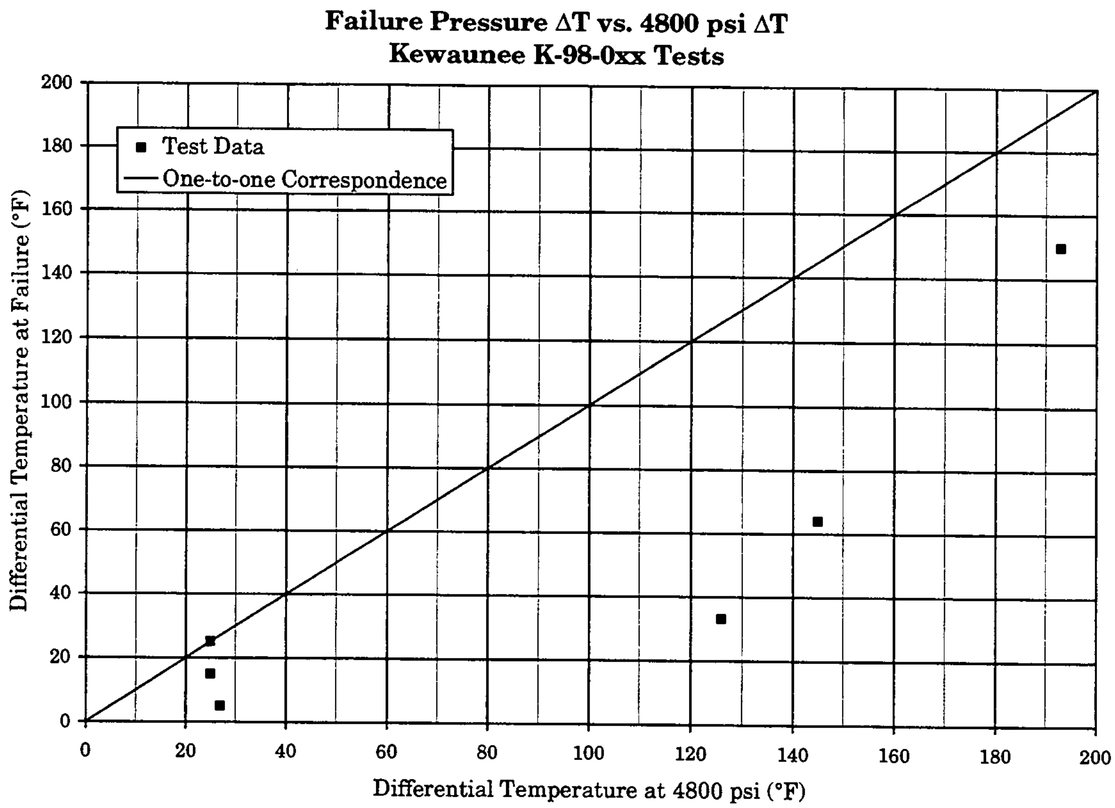


Figure 10: Failure ΔT vs. 4800 psi ΔT

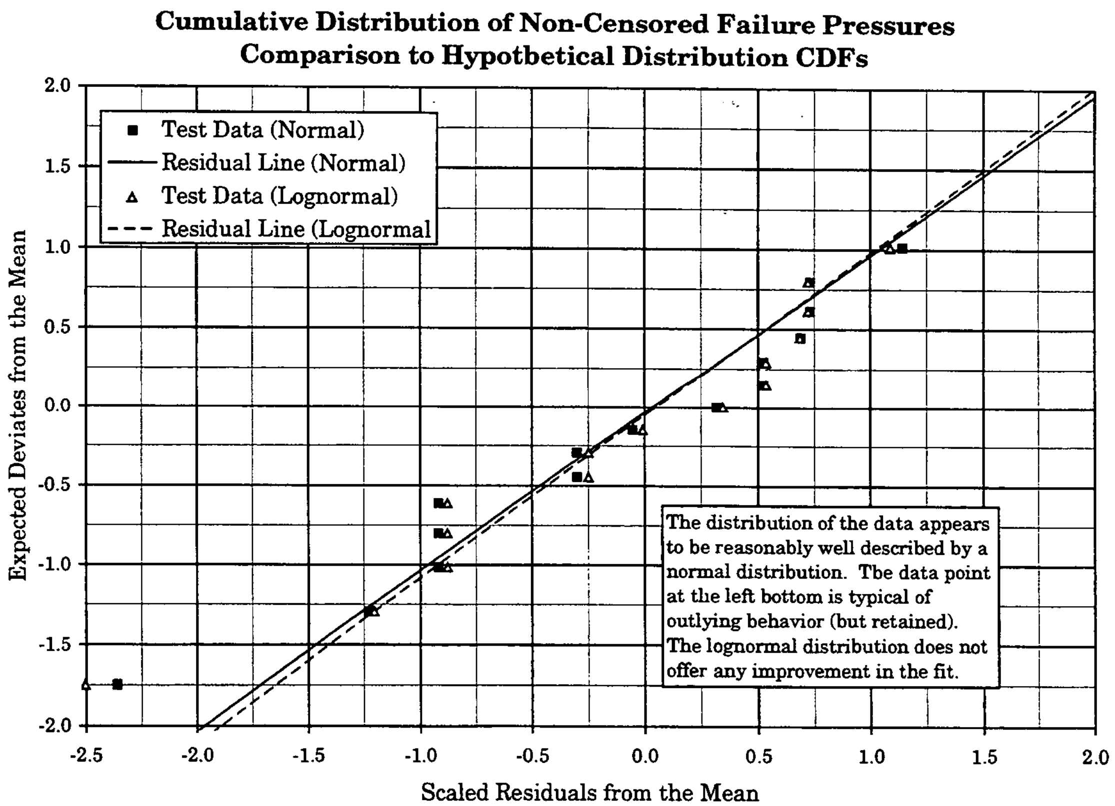


Figure 11: Distribution of Measured Failure Pressures

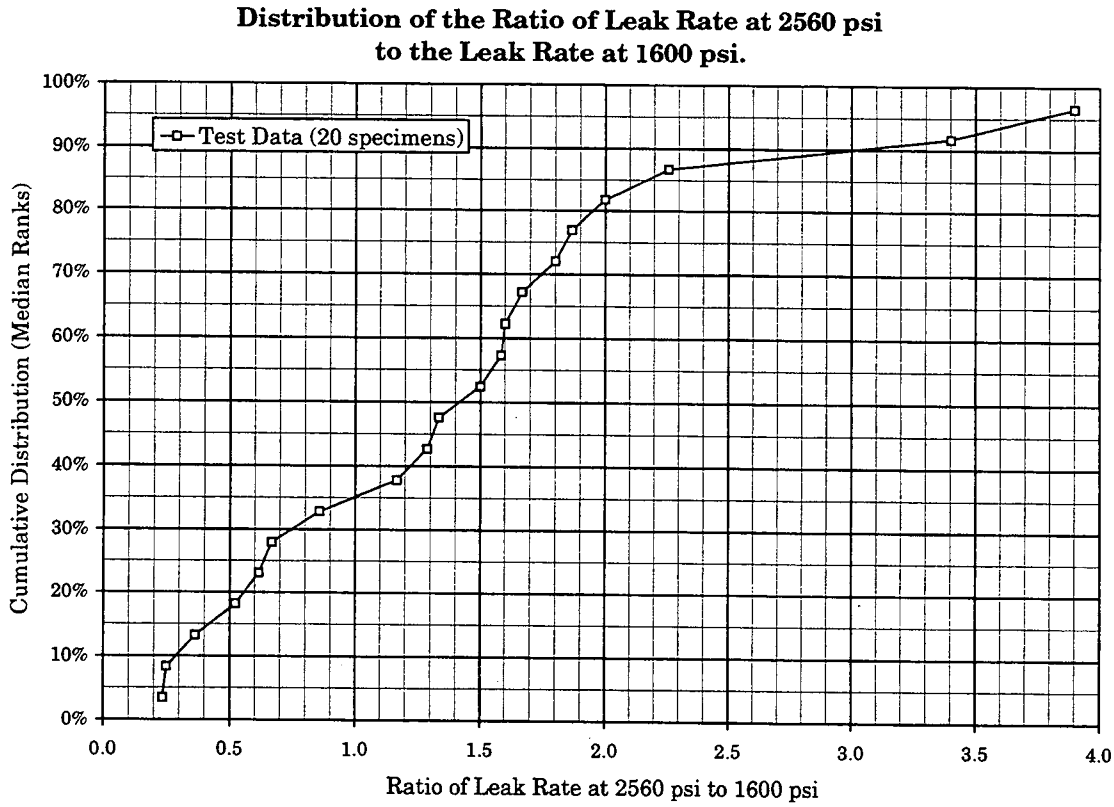


Figure 12: Ratio of SLB to NOp Leak Rates

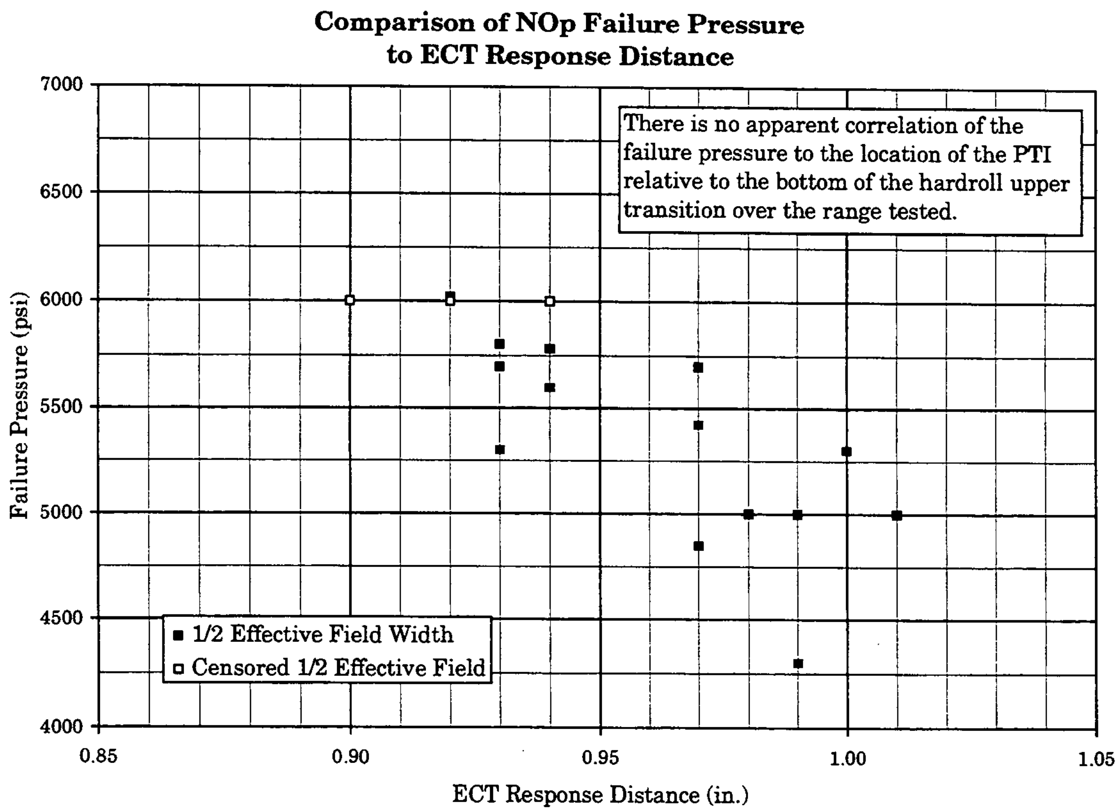


Figure 13: Failure Pressure vs. PTI Location

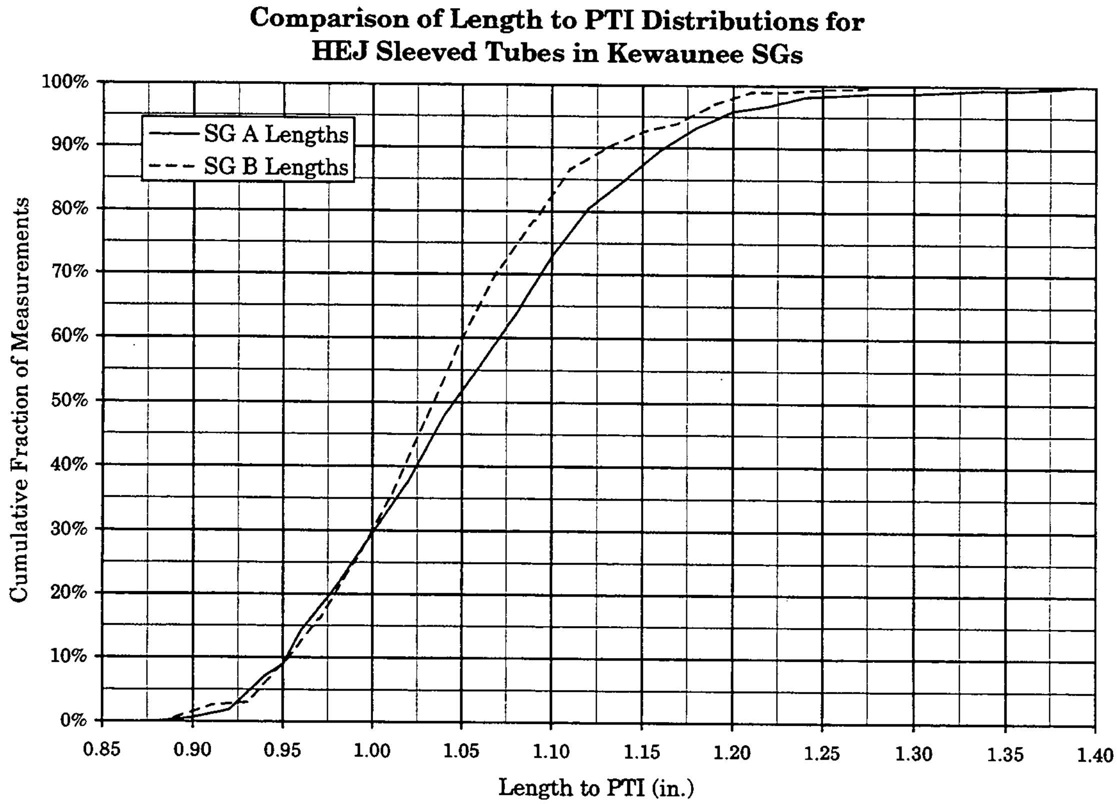


Figure 14: Distribution of PTI Lengths at Kewaunee

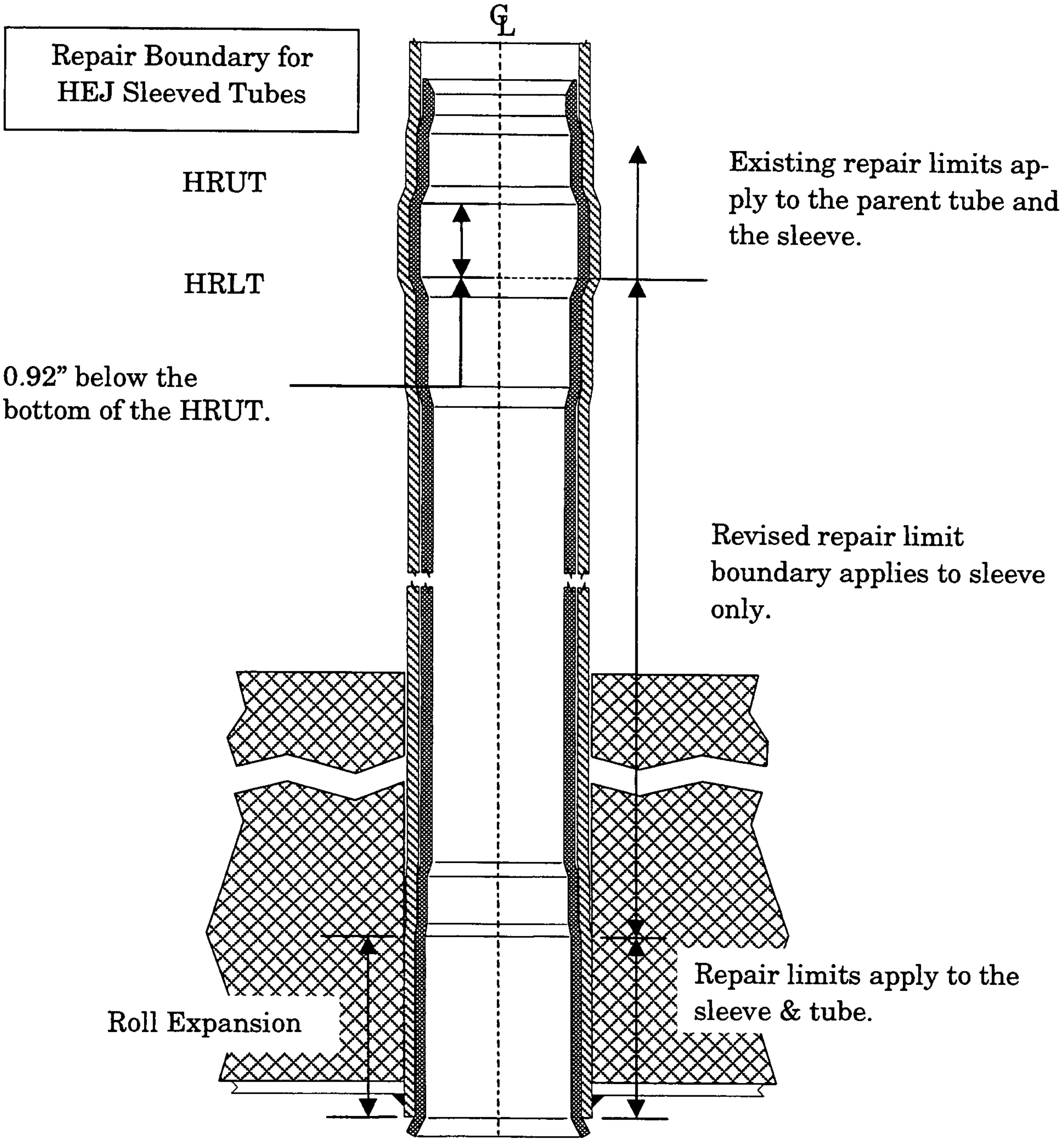


Figure 15: PTI Acceptance Criterion for HEJ Sleeved Tubes