NSD-JHL-6136, Rev. 1

SG-96-05-001

From: Steam Generator Design and Analysis
WIN: 284-5932
Date: July 25, 1996
Subject: L* Criterion for Farley Unit 2 - Non-Proprietary

To:	G. W. Whiteman	WECe 4-07
cc:	W. K. Cullen	WECe 4-07
	J. N. Esposito	Waltz Mill
	V. J. Esposito	Waltz Mill
	R. F. Keating	Waltz Mill
	D. D. Malinowski	Waltz Mill
	L. E. Markle	Waltz Mill
	G. Pierini	Waltz Mill

References: 1) "Tubesheet Region Plugging Criterion for the Alabama Power Company Farley Nuclear Station Unit 2 Steam Generators," WCAP-11306, Revision 2, April 1987.

2) Letter NSD-JLH-6138, "L* Calculations Performed for Farley Unit 2," J. M. Hall to E. Paxson, dated 5-1-96.

L* plugging criteria for the 7/8-inch diameter SG tubes of Farley Unit 2 have been developed using a combination of analyses and adjustment of test data from L* program for 3/4 inch diameter tubes. The L* evaluation is also based on the results of the F* analysis for Farley Unit 2, previously presented in Reference 1. Results are reported by Reference 2.

With sufficient information about degradation in the upper 2.60 inch length of degraded roll expansions, the recommended L* value of 0.50 inch (plus ECT uncertainty), is expected to provide for continued reliable operation of the Farley Unit 2 SGs.

Inspection of a 2.60 inch or longer distance should determine if there is at least 1.87 inch, plus ECT uncertainty, of sound tube with no more than two bands of axial degradation separating the sound portions. The largest ϕ permissible for the ECI's in a degradation band is 30 degrees. A reasonable number of tube ends in a SG which may be dispositioned is 600.

A complete summary of the L* evaluation is provided in the attachment to this letter.

J. M. Hall
J. M. Hall
Steam Generator Design and Analysis

R. A. Taylor
R. A. Taylor
Steam Generator Design and Analysis

L. A. Nelson
L. A. Nelson
Steam Generator Design and Analysis

J. L. Houtman
Approved: J. L. Houtman, Manager
Steam Generator Design and Analysis

9607300372 960725
PDR ADOCK 05000364
P PDR

ELM144:GWW072596

DEVELOPMENT OF L* CRITERIA FOR FARLEY UNIT 2

1.0 GENERAL

L* plugging criteria for the 7/8-inch diameter SG tubes of Farley Unit 2 have been developed using a combination of analyses and adjustment of test data from an L* program for 3/4 inch diameter tubes. Tests conducted for 3/4 inch tubing are summarized in Appendix A of this document. The L* evaluation is also based on the results of the F* analysis for Farley Unit 2, previously presented in Reference 1.

2.0 Leakage Considerations

The selection of an L* length has as a primary consideration the goal of minimizing significant leakage from tubes which have been accepted using the L* criteria. Significant leakage would be an aggregate leakage from the L* tubes which would cause a shutdown or operation at reduced power due to safety, regulatory, or operational reasons. Determination of the leak rate through the interface between the expanded tube and the tubesheet is not conducive to calculational methods. For some tube joint ECT data, more than one degradation array, or degraded roll expansion (DRE) per joint may be found. Refer to Figure 1. For various degradation arrays, the resistance-to-leakage (RTL) of the tube/tubesheet (T/TS) interface extending over a portion of the sound roll expansion portion (SRE) immediately below the bottom of the roll transition (BRT) has been determined by testing for 3/4 inch tubing. These test results are extrapolated for use in 7/8 inch tubing in order to develop the L* criteria in the following sections.

In the case of the Farley Unit 2 full depth roll expansions, degradation to which the L* criteria may be applied occurs below the top of the tubesheet or the BRT, whichever is lower in elevation. By design, the BRT is approximately flush with the top of the tubesheet; however, in the as-built condition, the BRT may be slightly above or below the top of the tubesheet. The uppermost DRE can be considered to be the most limiting location, i.e., with the highest leakage because the uppermost DRE forms a manifold which intercepts all leakage flow , if any exists, in the T/TS interface.

3.0 STRENGTH CONSIDERATIONS

Determination of the load bearing capability of DRE's is based on the limiting assumption that the L* region recommended length of 0.50 inch, plus ECT uncertainty, provides no axial fixity. Therefore, the most stringent axial pullout loads are assumed to be applied directly to the uppermost DRE. The criteria also considers the degradation to be axial or to have an inclination angle, ϕ, of up to 30°, and to be throughwall. In this case, the minimum axial length of sound, undegraded roll expansions between the BRT and the top of the highest-elevation degradation, designated as L*, for operation with acceptable leakage was determined. In this criteria, the tube axial loads are borne by the sound portion of the tube above L*, the uppermost degraded portion, the sound portions, other axial or near-axial degradation portions, if any, below the uppermost degraded portion and extending to the T/TS weld. Determination of the characteristics, e.g., ECI number, inclination angle, etc., of all of the degraded regions in a given tube joint would be necessary if the tube pullout load were to be applied to the T/TS weld. However, this load need not be assigned to be reacted by the SRE within the L* distance and the weld. Instead, it can be reacted by the appropriate aggregate length of SRE's of the tube below the uppermost degradation array if this aggregate length is sufficient. Below this pullout load reaction length (PLRL), the degradation need not be quantified to the same extent as within the length; it need be quantified only to the same extent that degradation is quantified below the F* distance as in Reference 1. In the most stringent case, these SRE's are interspersed with DRE's and several interspersed SRE's may be assigned to react the pullout load. Part of the pullout load is reacted by the SRE within the L* distance. Each DRE must transmit part of the pullout load to the SRE(s) and DRE(s) below it; the pullout load to be transmitted decreases with decreasing elevation. Therefore, the required DRE strength could also reduce with elevation. However, for the sake of simplicity, all DRE's were treated the same. Calculation of the holding force from each SRE was performed in the same way that the holding force was calculated for the F* distance in Reference 1. This involved calculation of the axial frictional force, preventing pullout, as the product of the T/TS radial contact pressure (s_r) of the area of the SRE and the T/TS static coefficient of friction. The reduced holding force of the two ends of each of the SRE's was also considered. Therefore, it was concluded that the configuration involving multiple bands of DRE's can be determined by calculation and does not need to be tested.

Thus far in this discussion, the strength aspects of the uppermost portion of the T/TS joint has involved strictly axial pullout loads on the tube. Other loads, such as axial compression, bending and torsion about the tube vertical axis were considered but were judged to be negligible. For example, the axial compression, downward, load occurring during large break LOCA, hereinafter referred to in this report as LOCA, was judged to affect the tube at the upper SRE to an insignificant extent. If the T/TS interface within the L* distance were not leak tight and permitted the LOCA pressure to penetrate to the DRE(s), radial compression would act on the DRE(s). The within-TS tube collapse strength characteristics in the presence of axial and circumferential indications are discussed in Reference 1. It was pointed out in Reference 1 that significant margin exists between tube collapse strength and the limiting secondary-to-primary pressure differential (LOCA).

The effects on the tube of other applied loads such as tube static and dynamic bending in the vicinity of the top of the roll expansion have also been considered. Static bending occurs as a result of TS bending during heatup to the N.O. condition. Dynamic bending results from the small amplitude vibration of the tube span between the top of the tubesheet and first support plate. In general, tube bending loads at the top of the tubesheet, i.e., above the BRT, and the resulting axial stresses are low. Therefore, it is concluded that stresses resulting from the applied loads in the intended L* region are also low, and that degradation due to these applied loads will be low. It is further concluded that degradation due to residual stress within the L* distance will be low, due to the lack of abnormal roll effects such as skips.

4.0 DETERMINATION OF RESISTANCE TO LEAKAGE OF DEGRADED ROLL EXPANSIONS -- FARLEY UNIT 2

4.1 Background

There is generally a correlation, for a given tube-to-tubesheet joint design, between the tube pullout strength, i.e., force, and tube roll expansion thinning; there is a desired thinning range for maximum strength. The desired thinning is achieved by the use of a certain roll expansion tool operated in a given stalling torque range. This facet of power plant heat exchanger design decreased in importance approximately two to three decades ago, with the widespread use of welded-and-rolled joints because of the addition of the weld to the structure. The resistance to leakage (RTL) of a given roll-only design was generally consistent with the maximum strength requirement for the design. However, the RTL of the expanded portion of a welded-and-rolled design typical of nuclear steam generators has not been extensively studied because of the very good sealing and strength properties of the weld. Therefore, the RTL of short lengths of the rolled Farley Unit 2 design is not available. In order to determine the RTL of the Farley SG (7/8 inch) tubing, results from tests conducted for an L* program for 3/4 inch tubing are utilized and extrapolated to the 7/8 inch tubing of Farley Unit 2.

4.2 Applicability of 3/4 Inch Tube Tests

Resistance to leakage through the interface between a rolled tube and the tubesheet is determined by the axial length (or simply length), width (tube circumference) and other geometrical characteristics of the interface. The length was a parameter in the 3/4 inch tube tests of Appendix A. The leakage flow is one dimensional, along the length of the interface, parallel to the tube axis. The total leakage will be proportional to the circumference of the tube. If the test conditions, length and other interface geometric characteristics are the same, leakage for a 7/8 inch tube test article (and in a steam generator using 7/8 inch tubing) would be greater than a 3/4 inch tube test article by the ratio of the tube expanded outside diameters, 1.17. This 1.17 ratio on 7/8 inch to 3/4 inch tube diameter leakage conservatively ignores that the crack leak rate for a given crack length would be lower for the 7/8 inch tubing, due to the thicker tube wall (0.050 vs. 0.043 inch).

The key parameter which determines interface flow resistance is the contact pressure between the rolled tube and the tubesheet. Higher contact pressure results in higher flow resistance. Other geometric factors which affect the interface flow characteristics, such as the tube and tubesheet surface finishes, are expected to be approximately the same for 3/4 inch and 7/8 inch tubes. The interface flow resistance is therefore a function only of the contact pressure, interface length and tube OD circumference.

The contact pressure is determined by the tube and tubesheet characteristics and the roll process. Test programs have been performed to simulate the interface of a tube-to-tubesheet roll expansion for both 3/4 inch tubes (shown in the appendix) and 7/8 inch tubes. Evaluations of the results of these two test programs have shown that the contact pressure is approximately 20 percent lower for the 7/8 inch tubes than for the 3/4 inch tubes. Therefore, it is judged that, everything else being equal, the leakage flow for

the 7/8 inch case will be approximately 20 percent higher for both N.O. and faulted conditions than for the 3/4" tube case. If the 3/4 inch test leakage rates are therefore multiplied by the factors 1.17 for the ratio of the expanded OD's of the tubes and 1.20 for contact pressure considerations, the resulting numbers represent conservatively high leakage rates for equivalent 7/8 inch tube leakage tests (and steam generators using 7/8 inch tubing). These data are contained in Table 1. The 3/4 inch tests and test results are described in
Appendix A.

4.3 Resistance to Leakage

The purpose of this section is to determine tube-to-tubesheet joint resistance to leakage for application of L* plugging criteria for Farley Unit 2. Testing conducted for 3/4 inch OD x 0.043 inch wall thickness tubes is extrapolated to determine RTL for the 7/8 inch OD x 0.050 inch wall thickness tubes of Farley Unit 2. The tests for 3/4 inch tubing, described in Appendix A, were performed with T/TS test specimens fabricated of short sections of prototypic tubes rolled into collars which provided the same structural compliance as a unit cell of the TS. The tests were performed at prototypical pressures, temperatures and tube axial loads for normal operation (N.O.), the most stringent faulted primary-to-secondary condition, Feed Line Break (FLB), and the most stringent faulted secondary-to-primary condition, Loss of Coolant Accident (LOCA).

The effect of prototypical loads on 3/4 inch tube joint leakage was addressed in the leak test. For example, a load such as primary-to-secondary pressure differential during N.O. causes tubesheet bending, with a resultant decrease the T/TS radial contact pressure, s_r, in the L* region near the top of the tubesheet. This bending effect, which is detrimental for the interior full depth expanded tubes, was accounted for in the laboratory tests conducted for 3/4 inch tubing. Peripheral tubes would experience much less of a related decrease.

The acceptable leakage for a reasonable number of 3/4 inch OD tube joints was established for normal operating and faulted conditions. It was obvious that for all acceptable-leakage cases, the 7/8 inch joint must also exhibit acceptable strength. It was concluded that the 7/8 inch tube (L*) strength determination could be decoupled from the leakage determination, as it was for 3/4 inch tubes. However, prototypical radial contraction or expansion of the tube, causing a reduction or increase in the T/TS radial contact pressure (s_r), was a part of the leakage determination. Leakage testing of the 3/4 inch tubes was performed at prototypical temperatures and pressure differentials. The primary-to-secondary pressure differential effect and a secondary-to-primary pressure differential effect were expected to have negligible impact on the tube joint strength and strength testing was performed at room temperature and without pressure differentials.

5.0 LEAKAGE CRITERION AND PROJECTED LEAKAGE FROM DEGRADED ROLL EXPANSIONS -- FARLEY UNIT 2

5.1 Leakage Criterion

The leakage acceptance criterion was developed for evaluation of the projected leak rates for Farley Unit 2. Individual acceptance values were developed for differential pressure conditions for normal operation, feedline break and LOCA.

Normal Operation

The administrative leak limit (ALL) for normal operation primary-to-secondary leakage in one steam generator is 150 gpd (0.105 gpm). However, it is reasonable to allocate only a fraction of this permissible leakage to tubes with degradation left in service using the L* criteria. It was determined based on commercial and operational considerations that the initial leak acceptance criterion for N.O. would be based on having the total leakage from L* tubes to be one-fourth of the 150 gpd limit ([ ]^{a,c,e} gpd). Using only a portion of the administrative leak limit to establish the leakage criterion for L* tube ends accommodates leakage from other locations. As calculated from the data in Table 1, the average leakage of the L* (L* = 0.5 inch) tubes under N.O. conditions is 0.763 dpm. Therefore, 2,560 L* tubes could be dispositioned with an L* of 0.5 inch. (There are approximately 75,000 drops in one gallon.) Therefore, 600 tubes per SG is a conservative number of tubes to be addressed.

The use of one-fourth of the ALL to determine the leakage rate criterion is an arbitrary decision for operational flexibility and does not require that the current ALL be altered. The initial leak rate acceptance criterion may have been reevaluated in the light of actual test results and the allowance for potential degradation in the SG. Use of a larger fraction of the ALL and/or use of a smaller leak test acceptance criterion closer to the actual results also could have been used to support application of the L* criteria to a number of tubes larger than the number on which the initial leak acceptance was based.

Faulted Condition: Feedline/Steamline Break

Postulated feedline break conditions provide the maximum primary-to-secondary differential pressure across the tube. The steamline break (SLB) conditions provide the most stringent radiological conditions for postulated accidents involving a loss of pressure or fluid in the secondary system. To establish the leak rate acceptance criteria for faulted conditions, the assumed SLB leakage rate is used with the feedline break pressure differential. This is the most stringent case because the associated primary-to-secondary side differential pressure causes the greatest amount of tubesheet upward bending. This, in turn, causes the greatest amount of tubesheet hole dilation for the interior locations, which causes the greatest reduction in s_r. The smallest s_r leads to the most likely conditions for the largest primary-to-secondary leakage. A site-specific determination of acceptable leakage during a SLB event using the criteria of 10CFR100 resulted in the primary-to-secondary leakage in the SG in the faulted loop being 11.2 gpm. Using the same allocation factor as for normal operation, the total leakage from 600 L* tubes at test conditions simulating SLB conditions must be less than [ ]^{a,c,e} gpm or approximately [ ]^{a,c,e} dpm per tube end. The total number of tube ends which could be dispositioned is 15,849. Therefore, the 600 tube ends, set by the N.O. condition is a small fraction of the 15,849 tube ends.

As with the N.O. test leak acceptance criteria, use of a larger fraction of the allowable leakage and/or an acceptance criteria closer to actual results will support a larger number of tubes to which L* is applied.

The primary-to-secondary pressure differential for this condition was 2650 psi. It should be noted that dynamic loads on the tube joint, viz., secondary side fluid drag on the tube during the postulated accident, need not be added to this value. This is because the safety valve relieving event and the maximum fluid drag event are not concurrent. Therefore, 2650 psi is the most stringent condition for the evaluation and an additional pressure differential corresponding to the drag load did not need to be considered. This sequence of events also eliminated the need for augmenting the axial load during the test to determine the axial load bearing strength of DRE's. The axial load during the leak test was prototypical because it was the end cap load caused by the prototypical differential pressure.

5.2 Leakage Projected for Farley Unit 2

Normal Operation

The N.O. leakage test data projected for Farley Unit 2 is shown in Table 1.
For L* = 0.5 inch, the average leakage is 0.763 dpm per tube.

Feedline Break

The FLB leakage projected for Farley Unit 2 is also shown in Table 1. The average leakage at L* = 0.5 inch is [ ]^{a,c,e} dpm per tube. Although the leakage is very small at X = 0.25 inch, and the number of tube ends which could be dispositioned thusly is large, it is advisable not to consider any X less than 0.5 inch without further study.

6.0 GENERAL APPLICATION OF THE 3/4 INCH TUBE AXIAL LOAD BEARING TEST RESULTS TO FARLEY UNIT 2

The strength tests of the 3/4 inch tube degraded roll expansions, described in Appendix A, Section A.3, were applied to Farley Unit 2 by means of a fracture mechanics evaluation. This evaluation is provided in Section 7. The 3/4 inch data applied to the 7/8 inch case because of the commonality of tube joint materials, respective material properties and fabrication processes between the two designs. These features are shown in Table 2.

7.0 APPLICATION OF 3/4 INCH TUBE TEST RESULTS FOR THE AXIAL LOAD BEARING CAPABILITY OF DEGRADED ROLL EXPANSIONS TO FARLEY UNIT 2

7.1 Maximum Axial Load Criterion

Single Band Degradation

The maximum axial tensile load which a degraded roll expansion (DRE) must bear will be the most stringent of the N.O. and FLB axial loads. The LOCA axial load is axially compressive and cannot cause tube pullout.

Multiple Band Degradation

If degradation occurs in discrete circumferential bands of one or more linear indications, i.e., as single band degradation (SBD) or arrays in a roll expansion above the F* elevation, thereby preventing use of F*, it may also occur in multiple discrete bands below the F* elevation. This is termed multiple band degradation (MBD). As discussed earlier, it's desired to avoid quantifying each of the possible several degradation bands to apply the pullout load to the T/TS weld. Instead, it's desired to apply the load only to the uppermost sound roll portions (expansions) which are interspersed with the uppermost degradation bands. For the purpose of simplicity, each degradation band requires the same strength. This strength is the same as that for the SBD. Therefore, for the MBD case, the number of individual bands which must be quantified is one less than the number of SRE's (N) needed to react the pullout load. If N is 3 N-1 is 2. Conservatism may be added to this calculation in the form of a larger N than is needed.

Normal Operation

The tube ultimate load required for normal operation at the Farley Unit 2 condition resulting in the largest N.O. ΔP is approximately [ ]^{a,c,e}. A safety factor of three is applied to this load. Therefore, the tube axial load bearing requirement is [ ]^{a,c,e} lb. This load is the highest for the three conditions considered.

Feedline Break

The Farley Unit 2 tube ultimate load required for FLB is approximately 1724 lb., i.e., (2650) x (π/4) (0.910 inch)^2. A safety factor of 1.43 (corresponding to an ASME Code factor of 1.0/0.7 for allowable stress for faulted conditions) is proposed for this load. Therefore, the tube axial load bearing requirement for FLB is [ ]^{a,c,e} lb. As discussed in Appendix A, this load need not be further augmented for dynamic loading effects during FLB because of the sequence of events during an FLB.

Loss of Coolant Accident

An axial load bearing requirement for a LOCA event acts to move a tube downward, the opposite of pullout. Therefore, this condition does not apply to pullout. (However, the DRE must not collapse under LOCA conditions. This condition was addressed in Reference 1 and collapse for the 7/8 inch tube case is not expected.)

7.2 Evaluations

Application of the L* approach requires that the degraded region has sufficient strength to resist axial pullout forces with the appropriate safety margins. The limiting case is the axial force generated by a pressure load equal to 3 times the operating pressure differential. For Farley Unit 2, this force is [ ]^{a,c,e} lbs., generated by a 3ΔP value of 4371 psi. If ligaments of sound material in the degraded region are not subject to significant local bending loads, only [ ]^{a,c,e} percent of the mean circumference of the expanded region need be present to meet the axial strength requirements. Typically, degradation found in roll expansions occurs as multiple parallel cracks. If these cracks are inclined at an angle to the tube axis, as shown in Figure 2, then an axial force leads to bending of the ligaments between cracks and rotation of the tube and crack array as deformation proceeds. The axial strength of a tube with an array of "slanted" cracks depends on the slant, a.k.a., inclination crack angle and, to a lesser extent, on the number of cracks present. The following paragraphs describe a pull strength model for slanted crack arrays. This model was benchmarked by pull strength tests on 3/4 inch diameter tubing. A comparison of test data and a pull strength design curve for 3/4 inch diameter tubing is shown and a pull strength design curve for 7/8 inch diameter tubing is presented. This latter curve is appropriate for Farley Unit 2.

As a tube with an array of parallel slanted cracks is loaded axially, deformation proceeds as follows. Initial elastic elongation is followed by plastic yielding. Depending on the number of cracks and the crack inclination angle, yielding may first occur in the virgin unrolled tube section or in the crack array. Large numbers of cracks and high crack angles favor yielding of the crack array. Yielding of the crack array proceeds by plastic bending of the ligaments between cracks and subsequent rotation of the cracks toward the longitudinal axis of the tube. This is easily visible in pull strength tests. As rotation occurs, the applied axial force must increase substantially since the effective moment arm for ligament bending is continually decreasing. Strain hardening of the ligaments adds to the geometric hardening produced by crack rotation. As the load is increased, general yielding of the virgin unrolled tube section may occur, followed by strain hardening and then yielding of the rolled but uncracked tube section. If deformation proceeds to this point, the rolled tube peels away from the tubesheet and the axial pull strength is limited by the tube-to-tubesheet weld. At some point, depending on the crack morphology, fracture will terminate the process of plastic deformation.

Using the notation of Figure 2, the axial load required to yield a tube with an array of slanted cracks is

[ ]^{a,c,e}

This expression only considers ligament bending and assumes deformation is confined to the initial minimum ligament cross section.

The axial plastic displacement resulting for yielding and then rotation of the crack array is given by

$$[\delta_p = h - h_0 = \ell \cdot (\sin\Theta - \sin\Theta_0)]^{a,c,e}$$

If the crack array plastic displacement is added to the baseline load displacement record of an uncracked tube, then the computed load displacement records closely approximate actual measured load displacement records. The load displacement record for the uncracked tube essentially provides elastic tube and test fixture displacement and an indication of yielding in the uncracked section at high loads. Figure 3 shows a test of a 3/4 inch diameter tube terminated by ligament fracture. Figure 4 shows the test record for another 3/4 inch diameter tube where the test load has become high enough to yield the uncracked as well as cracked sections. (Note: Figures 2 through 5 are repeated in Appendix A.)

The prediction of ultimate pull strength load as opposed to yield load is an elastic-plastic fracture mechanics problem rather than a plastic collapse problem because maximum loads are caused by crack tearing (beyond some minimum crack angle). A J-integral approach has been applied to the problem of estimating the onset of crack tearing with good success. The equation describing the plastic load-displacement behavior of the crack array was integrated to related plastic work, crack length and displacement. The compliance definition of J was then used to compute J. Since the value of J for the onset of crack tearing in Alloy 600 is very high, the neglect of elastic contributions to J is appropriate.

In pull tests of 3/4 inch diameter Alloy 600 tubes containing slanted EDM notches to simulate cracks, maximum loads were consistent with an applied J of about 3600 inches in.-lbs./in.^{2}. This is true when the crack array limits the pull strength. For low numbers of cracks and small crack angles, yielding of the uncracked sections developed and pull strengths were limited by the tube to tubesheet welds. The use of narrow EDM slits to simulate cracks in tests of the 3/4 inch diameter tubing is justified by the fact that the initial notch root radius was much smaller than the final blunted crack tip crack opening displacement (COD) which led to crack tearing.

For the 3/4 inch diameter tubing, calculated pull strengths as a function of crack angle were adjusted to high temperature, lower tolerance limit (LTL) material properties to construct a pull strength design curve. (Lower bound statistical tolerance limits, LTL, for pull strength values were computed in accordance with the accepted industry practice such that there is a 95 percent probability that 95 percent of the tubes will have strength greater than LTL values.) At small crack angles, actual test data were adjusted to LTL properties to arrive at design curve values. The crack length selected for the design curve was $[0.5]^{a,c,e}$ inch. This is a reasonable bounding value for throughwall crack lengths in standard roll transitions.

Figure 5 illustrates the pull strength design curve for 3/4 inch diameter tubing along with actual test data. The good consistency between test results and calculations for 3/4 inch diameter tubing provides the basis to extend such calculations to construct a pull strength design curve for 7/8 inch diameter tubing.

A pull strength design curve for 7/8 inch diameter was constructed by making appropriate changes in the input to the slant crack pull strength model. The tube diameter, wall thickness and LTL tensile properties were adjusted. The J level at the onset of tearing was kept at the same value. This was somewhat conservative since at the very low material thicknesses of interest, a 20 percent increase in the toughness might be expected for the larger wall thickness tubing. The crack length was maintained at a value of [ ]^{a,c,e} inch. The baseline pull strength at low crack angles was obtained by multiplying the experimentally based value for 3/4 inch diameter tubing by the ratio of the circumferences of the 7/8 and 3/4 inch diameter tubing and then adjusting for differences in LTL flow strengths. The 7/8 inch diameter pull strength design curve is shown in Figure 6. It is about [

]^{a,c,e}

7.3 Results

Using the normal operating plant conditions, the most stringent axial load developed during normal operating conditions for Farley Unit 2, [

]^{a,c,e} degree inclination angle.

The curve should be applied to tubes with relatively well characterized ECI's, e.g., tubes for which substantiated rotating pancake coil (RPC) ECT data are available. The strength criterion per this curve, must be met for tubes with eddy current indications.

7.4 Conclusion

With sufficiently quantified information about degradation in the anchor regions of the Farley Unit 2 S/G roll expansions, use of the Design Curve is expected to provide reliable operation. (The leakage criterion for this case is discussed elsewhere in this report and involves an upper limit on the number of tubes per steam generator which may be dispositioned.)

8.0 PULLOUT LOAD REACTION AND RPC INSPECTION LENGTHS - FARLEY UNIT 2

8.1 General

The methods used to inspect, evaluate, and define the tube degradation for the application of the L* alternate plugging criteria require an RPC eddy current probe or other advanced inspection method. If sufficient length of sound expanded tube exists in the upper portion of the roll expansion, the lower portions of the expansion contribute relatively little to the structural integrity and resistance to leakage of the tube. Criteria have been developed which limit evaluation to the more important upper portion of the tube expansion.

The analysis to support limited evaluation of the degradation in a tube is based on a combination of the methods used to qualify the F* and basic L* criteria. The analysis demonstrates that sound portions of expanded tube between degraded sections can provide for structural integrity of the tube. In the same manner that the friction force between the tube and the tubesheet in an F* tube will resist pullout, the sound portions of a tube with degradation meeting the L* criteria will provide a frictional force to resist pullout. The calculation of the length with degradation within the portion of the expanded tube required to resist pullout includes the possible reduction in friction force next to the degraded portions due to end effects. The length of tube required to resist pullout plus the length of the degraded tube portions establishes the length of tube which must be inspected and evaluated by rotating pancake coil or other advanced inspection methods. Therefore, rather than survey the entire length of the RE, to the T/TS weld, only that portion near the RT which will provide adequate anchoring for all axial loads need be surveyed. This length, in addition to the SBD or MBD within it, is the minimum length, beginning at the BRT and extending downward, and is referred to as the pullout load reaction length (PLRL).

8.2 Single Band Degradation

The PLRL for this case consists of four parts, the SRE within L* consisting of the central and end-effect portions (the recommended 0.' nch), plus the required SRE immediately below the SBD. Calculations for N. O. and FLB are shown in Table 3. The limiting case was FLB, for which a PLRL of 0.74 inch was determined. Addition of the SBD length, [ ]^{a,c,e} inch, and the ECT uncertainty of 0.10 inch to the PLRL of 1.74 inch resulted in a length of [ ]^{a,c,e} inch of RE to be RPC inspected. The RPC inspection length is the length below the bottom of the roll transition.

8.3 Multiple Band Degradation

The PLRL for this case consists of six parts, the SRE within L*, i.e., the recommended 0.5 inch consisting of the central and end-effect portions, the uppermost two degradation bands and the two SRE's below these two respective bands. Calculations for the N.O. and FLB are shown in Table 3. The PLRL for N.O. conditions was [ ]^{a,c,e} inch. An RPC inspection length of 2.50 inch for this N.O. condition was determined by addition of the two degradation band lengths and the ECT uncertainty of [ ]a,c,e inch to the PLRL. For the FLB conditions, the PLRL was determined to be 1.87 inch. Addition of the postulated two degradation bands and the ETC uncertainty of [ ]^{a,c,e} to the PLRL resulted in a length of 2.51 inch of RE to be RPC inspected.

8.4 Technical Specifications Requirements

In specifying the length of degraded tube required to be inspected by an advanced inspection technique, for the plant Technical Specifications, the information provided above can be combined into a single set of criteria. The calculated length of tube required to be inspected is 2.34 inches for the SBD case.

Inspection of the top 2.34 inches rounded off to 2.4 inches, of the tube should determine if there is at least 1.74 inch, plus ECT uncertainty, of sound tube with no more than one band of axial degradation separating the sound portions and with an L* of 0.50 inch plus ECT uncertainty. A similar calculation for the MBD case, for the postulated two degradation bands of 0.25 inch axial extent, axially separated by 0.50 inch, discussed above, results in a length of 2.51 inches rounded off to 2.6 inches to be inspected.

9.0 SUMMARY OF L* EVALUATION

9.1 General

The semi-empirical evaluation uncoupled the strength and projected leakage effects. The issues were determined separately.

9.2 Leakage

Normal Operation

The average, projected per-joint leak rates for the N.O. condition for all L* = 0.5 inch, were low relative to the permissible, per-joint leak rate of [ ]^{a,c,e} dpm for 600 joints. This was based on the arbitrary selection of [ ]^{a,c,e} gpd as a fraction of the 150 gpd administrative leak rate limit for one S/Gs during N.O. for L* tube ends. Using the [ ]^{a,c,e} gpd leakage limit, 600 of the tube ends in any S/G could be easily be dispositioned at L* = 0.5 inch.

Faulted Conditions

Based on the FLB/SLB permissible per-joint leakage criteria of 1050 dpm and the small average projected leak rates, as many as 600 joints could be dispositioned with L* = 0.5 inch.

9.3 Recommended L* Criteria

With sufficiently quantified information about degradation in the upper approximately 2.60 inch length of degraded roll expansions, use of the recommended L* value of 0.50 inch, plus ECT uncertainty ([ ]^{a,c,e} inch was used for an example), is expected to provide continued reliable operation of the Farley Unit 2 S/G's.

Inspection of a 2.60 inch or longer distance should determine if there is at least 1.87 inch, plus ECT uncertainty, of sound tube with no more than two bands of axial degradation separating the sound portions. The largest ϕ permissible for the ECI's in a degradation band is 30 degrees. (The ϕ = 45 degrees curve is presented only for trending purposes.) A reasonable number of tube ends in a S/G which may be dispositioned is 600.

10.0 REFERENCES

1. "Tubesheet Region Plugging Criterion for the Alabama Power Company Farley Nuclear Station Unit 2 Steam Generators", WCAP-11306, Revision 2, April 1987.

Table 1

3/4 Inch Tube Roll Expansion Leakage Test Results and Projected Leakage for Farley Unit 2 (7/8 inch tubes)

Sample Number	Roll Expansion Length, in.	Plant Condition	Differential Pressure, psi	Temp., °F Planned	Temp., °F Actual	Leak Rate, dpm 3/4" tube	Leak Rate, dpm 7/8" tube[1]

a,c,e

Table 1 (Continued)

3/4 Inch Tube Roll Expansion Leakage Test Results and Projected Leakage for Farley Unit 2 (7/8 inch tubes)

Sample Number	Roll Expansion Length, in.	Plant Condition	Differential Pressure, psi	Temp., °F Planned	Temp., °F Actual	Leak Rate, dpm 3/4" tube	Leak Rate, dpm 7/8" tube[1]

a,c,e

Table 2

Basis for Use of 3/4 Inch Tube Joint Loadbearing Test Data for Projected Loadbearing Capability for Farley Unit 2 Degraded Tube Joints

	Steam Generator	
Parameter	**3/4 Inch Tube**	**Farley Unit 2**
Tube Material	Alloy 600 MA	Alloy 600 MA
Tube Mechanical Properties	Prototypical used in test & analysis	Prototypical used in analysis
Tube-to-TS Joint Interference Fit (Contact pressure)	Determined by test	Determined by test & was conservative relative to 3/4 inch tube test results
Applied Loads	Prototypical or "overtest"	Prototypical used in analysis

Table 3A

Calculation of Pullout Load Reaction Length and Tube Length to be RPC Inspected

a.c.e

Table 3B

Calculation of Pullout Load Reaction Length and Tube Length to be RPC Inspected

a,c,e

Table 3C
Calculation of Pullout Load Reaction Length and Tube Length to be RPC Inspected

a,c,e

Table 3D (Continued)
Calculation of Pullout Load Reaction Length and
Tube Length to be RPC Inspected

a,c,e

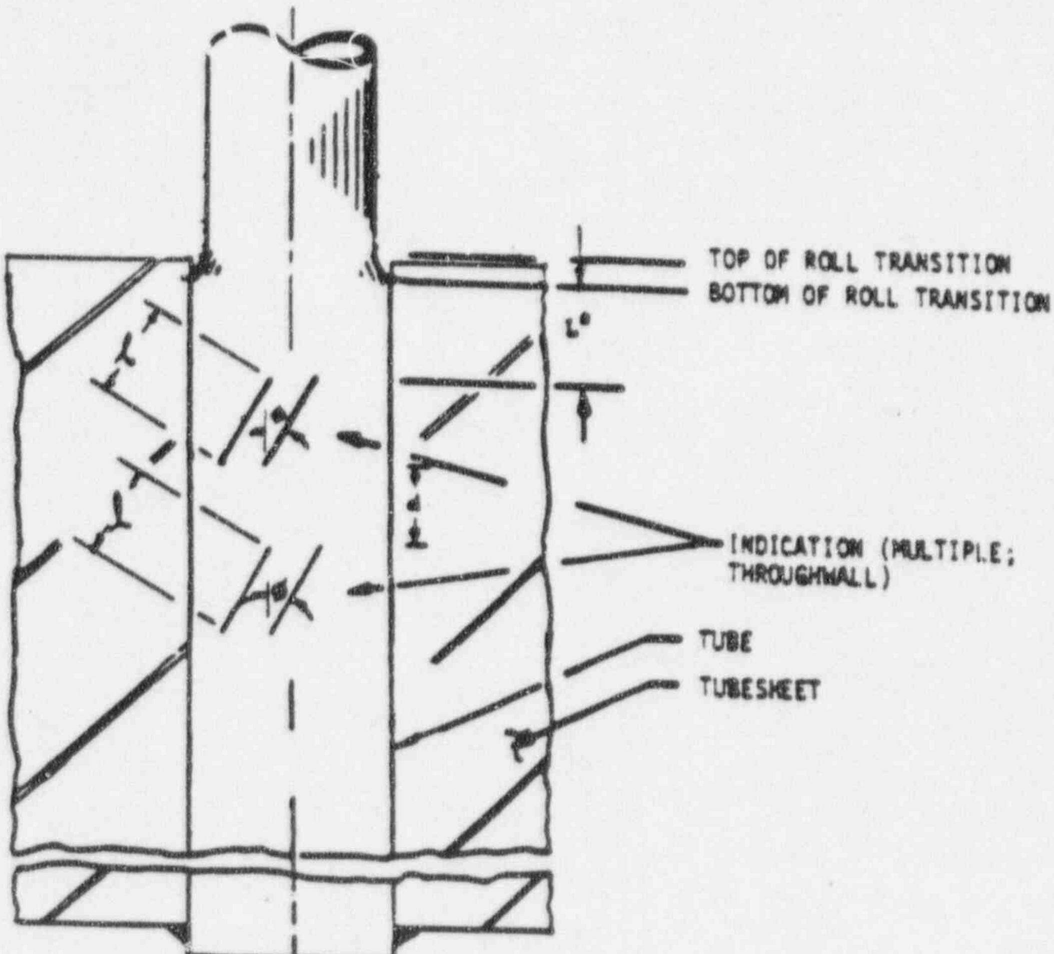
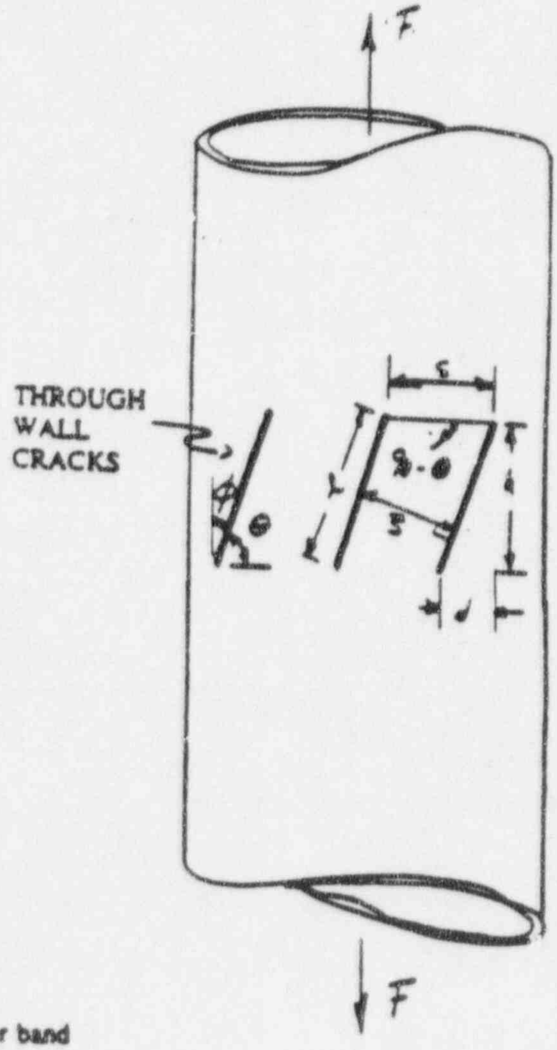


Figure 1

Configuration for Tubesheet Region L* Alternate Plugging Criteria for Full Depth Roll-Expanded Steam Generator Tubes, Multiple Band Degradation



R_m	=	Tube mean radius
t	=	Wall thickness
σ_{y}	=	0.2% yield strength
σ_f	=	Flow stress
n	=	Number of cracks per band
θ_o	=	Initial θ before yielding
$\hat{S}$	=	S cos (90-θ)
$\tilde{S}$	=	S sin θ_o
ℓ	=	Crack length

Figure 2
Model for Plastic Collapse
7/8 Inch Diameter, Mill Annealed, Alloy 600 Tubing

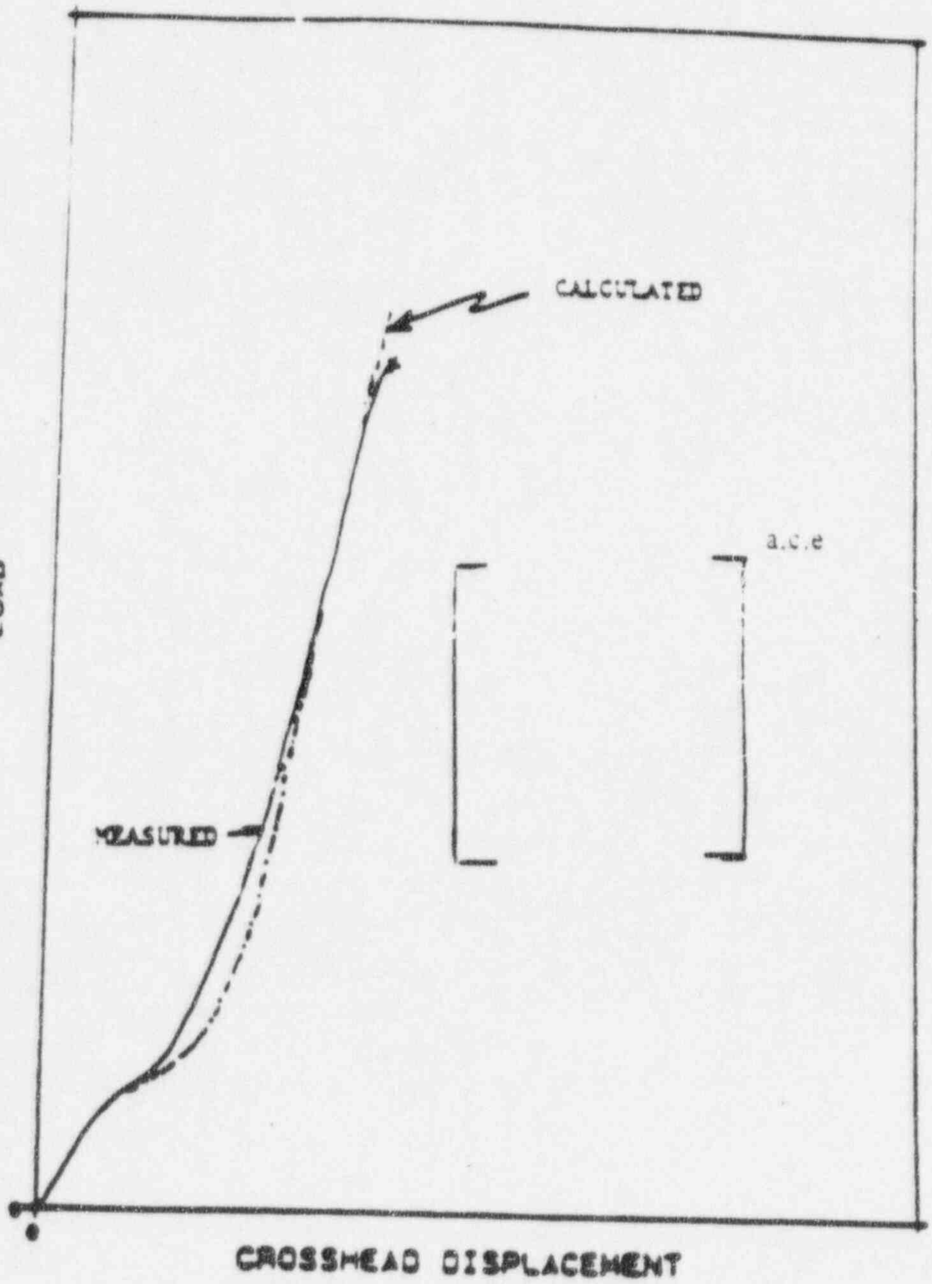


Figure 3
Comparison of Load-Displacement Records, Computed vs. Measured for 30 Slots at ϕ = 45°, 3/4 Inch Tubes

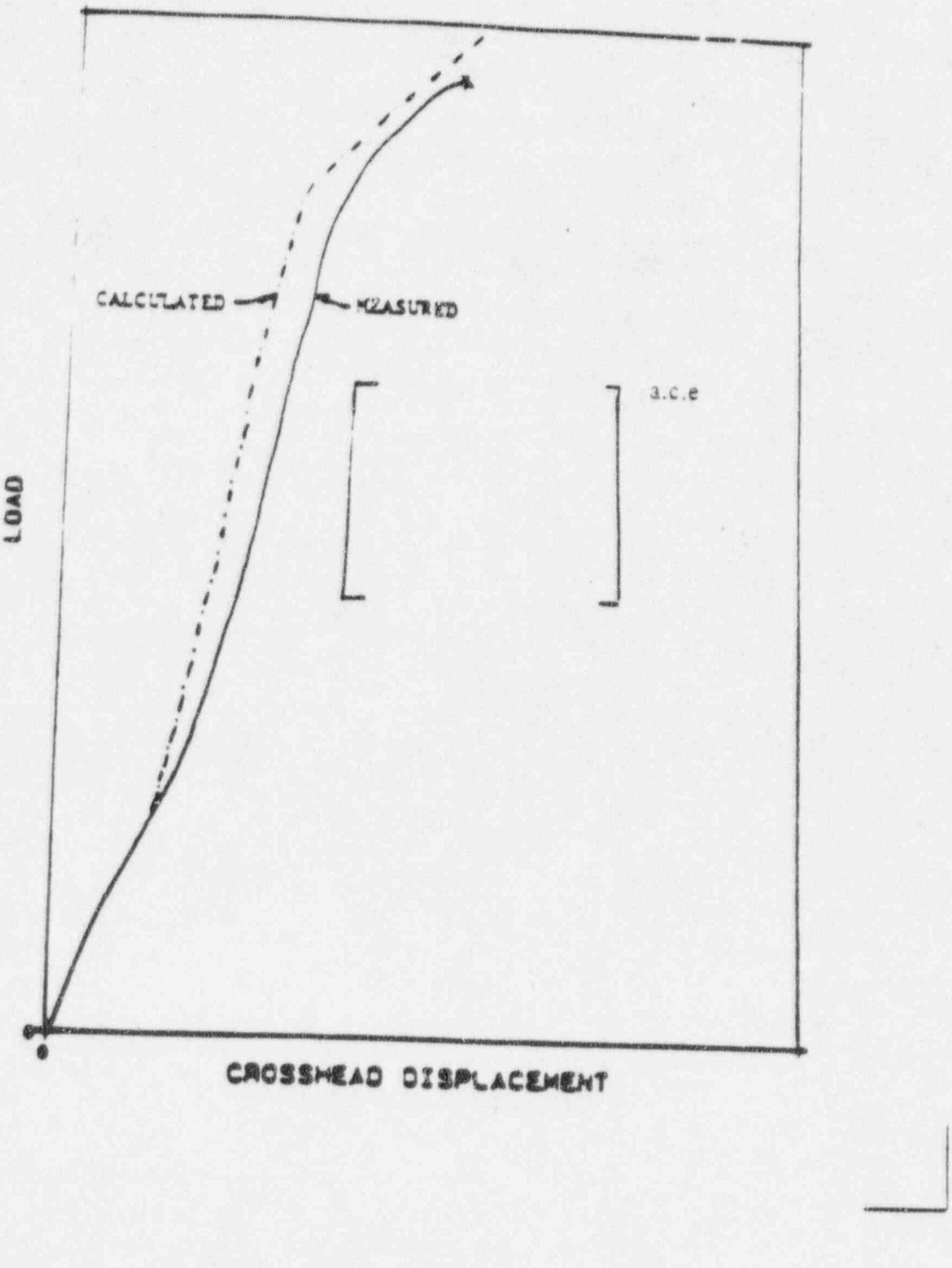


Figure 4
Comparison of Load-Displacement Records,
Computed vs. Measured for 30 Slots at $\phi = 30°$, 3/4 Inch Tubes

a,c,e

Figure 5
Ultimate Pull Force as a Function of Slot Angle
for Collared and Decollared Tubes, 3/4 Inch Tubes

a,c,e

Figure 6
Pull Force Design Curve For
7/8 Inch Tubes at Farley Unit 2

APPENDIX A

L* TESTS FOR 3/4-INCH DIAMETER TUBE STEAM GENERATORS

A.1 TEST FOR THE DETERMINATION OF RESISTANCE TO LEAKAGE OF DEGRADED ROLL EXPANSIONS -- 3/4 INCH TUBE S/G

A.1.1 Background

The resistance to leakage (RTL) of short lengths of this tube joint design was not available and a test was designed to determine it.

A.1.2 Objective

The purpose of this test was to determine tube-to-tubesheet joint resistance to leakage for short axial lengths of sound, i.e., undegraded, rolled joints. The tests were performed with T/TS test specimens fabricated of short sections of prototypical tubes rolled into collars which provided the same structural compliance as a unit cell of the TS. The tests were performed at prototypical pressures, temperatures and tube axial loads for normal operation (N.O.), the most stringent faulted primary-to-secondary condition, i.e., feedline break (FLB), and the most stringent faulted secondary-to-primary condition, Loss of Coolant Accident (LOCA).

The effect of prototypical loads on 3/4 inch tube joint leakage was addressed in the leak test. For example, a load such as primary-to-secondary pressure differential during N.O., causes direct pressure effects on the tube in the L* region to increase the T/TS radial contact pressure, s_r. It also causes tubesheet bending. The differential pressure increases the T/TS radial contact pressure for all tubes in the TS, peripheral as well as interior, i.e., away-from-periphery, tubes if the L* region is above the neutral bending axis of the TS. However, the upward bending also causes a decrease in radial contact pressure between tube and tubesheet for interior tubes and therefore a decreased RTL. This bending effect, which is detrimental for interior tubes, was accounted for in the laboratory.

The acceptable leakage for a reasonable number of 3/4 inch OD tube joints was established for normal operating and faulted conditions. It was obvious that for all acceptable-leakage cases, the joint was also required to exhibit acceptable strength. It was concluded that the (L*) strength testing could be decoupled from the leakage testing and that the strength limits could be determined by a separate test. The primary-to-secondary pressure differential effect and a secondary-to-primary pressure differential effect were expected to have negligible impact on the tube joint strength. However, prototypical radial contraction or expansion of the tube, causing a reduction or increase in the T/TS radial contact pressure (s_r), was achieved in the leakage test.

A.1.3 Test Equipment

1) Tubes, Alloy 600 (I600) mill annealed (MA) 0.75 inch OD x 0.043 inch wall (nominal).

2) Tubesheet Simulants (Collars): Cold rolled carbon steel, AISI 1018, 7.0 in. long with [ ]^{a,c,e}.

3) Airetool Roll Expander, No. [ ]^{a,c,e}; rolls per original (shop) fabrication specification.

4) Roll Expander Motor, of 30 to 95 in-lb torque capacity, 1250 RPM no-load speed.

5) Laboratory leak test equipment such as pressurizing systems, furnaces, etc.

A.1.4 Test Major Steps

1) Determine roll torque reduction to simulate the effect of tubesheet bending on reduction of tube-to-tubesheet s, for interior tubes. (Note: As discussed in Section 3.2, the resistance to leakage for the 3/4 inch tube configuration was conservatively used for the 7/8 inch tubes of D. C. Cook Unit 1, based on a higher interference fit contact pressure for the 7/8 inch tubes. Part of this conservatism was based on the increase in contact pressure at the tubesheet bottom, during the most important operating conditions, i.e., N.O. and FLB, involving tubesheet upward bowing, for the 7/8 inch tubes. Under the same conditions, the upward bowing of the tubesheet causes a reduction in contact pressure for the full-depth expanded, 3/4 inch tubes, at the top of the tubesheet.

2) Fabricate leak test samples with [ ]^{a,c,e} in. of sound roll between the BRT and the DRE. This distance is referred to as "X". Each sample has a separate "X". Refer to Figure A.1-1. For X = 0.5 in., the desired length of sound RE above the degradation, concentrate the most samples. An X of [ ]^{a,c,e} in. is important for trending purposes only; X values of [ ]^{a,c,e} in. were expected to be leaktight or to have negligible leakage.

2a) Estimate collar ID surface finish in area of leak path. During the roll expansion process, the tube becomes fully plastic and flows into very small depressions occurring on the S/G tubesheet hole surface, or on the tubesheet simulant (collar) hole surface in this case. Conceptually, the amount of tube flow, determined by hole surface roughness, may be related to joint RTL. Ascertain that the collar finish is on the order of the prototypical finish, i.e., [ ]^{a,c,e} RMS.

2b) Fabricate T/TS samples per Figures A.1-1 and A.1-2. Set roll expansion motor torque to the middle of the torque range specified during S/G manufacturing, as reduced to account for TS upward bending. The torque reduction shall correspond to the s, caused by TS bending.

2b-1) Partial roll expansion (non-interference fit). Remove tube from collar.

2b-2) Drill holes in tube to simulate DRE crack top tips. Reinsert tube in collar.

2b-3) Finish (hardroll) RE.

2c) Heat T/TS samples in furnace at [ ][a,c,e] hours to duplicate the most stringent conditions caused by post-weld heat treatment of the channelhead-to-TS weld during shop fabrication.

2d) Leak test the FLB primary-to-secondary configuration with [ ][a,c,e] in. of sound roll between the BRT and the degraded portion of the joint. Refer to Figure A.1-1. The limiting case for the degradation is a 360° circumferential, throughwall "crack", machined in the tube. However, because this machining may loosen the tube for small X values and therefore possibly bias the testing, a better method is to use the discrete-hole approach. The number of holes [ ][a,c,e] is chosen to be large, to approximate or exceed the number of linear ECI's observed in these S/Gs. After the FLB samples are tested, convert the samples to the N.O. configuration by rerolling to the higher, N.O., torque; [ ][a,c,e] hours and leak test at the N.O. conditions.

2e) Leak test the secondary-to-primary (LOCA) configuration using the same sequence as used in primary-to-secondary configuration above. The LOCA samples are completely separate from the FLB/N.O. samples. Refer to Figure A.1-2.

A.1.5 Test Facility

The tests were performed at the Westinghouse Research and Development Laboratory. A four-cell manifold was used to apply pressurized, deionized water simultaneously to the tube ID's of four specimens for primary-to-secondary tests; i.e., the normal operating and FLB conditions, and to the tube OD's for secondary-to-primary tests, i.e., the LOCA condition. System pressure was measured by a transducer system with a ±5 psi accuracy. The electrical resistance heaters and insulation were positioned on the T/TS specimen such that the temperature acting over the leak path and ending at the BRT, the top end of the leak path in the plant, was [ ][a,c,e] for the N.O. and FLB conditions. Above the BRT, the temperature dropped as designed by placement of the insulation so that any leakage was captured and condensed as droplets. The droplets were manually counted during the test period, usually 60 minutes. For the LOCA tests, the temperature acting over the leak path and beginning at the BRT was [ ][a,c,e]. In this case, all leakage was captured from the tube ID and condensed as droplets and counted manually during the 60 minute test period.

A.1.6 Test Procedure

The test was performed in accordance with a written procedure, a referenceable document, under Quality Assurance (QA) surveillance. The tube specimens were fabricated from QA-controlled stock; the collars were fabricated of known-strength AISI 1018 cold rolled carbon steel and were also fabricated under QA surveillance.

Figure A.1-1
Roll Expansion Leakage Test Sample for 3/4 Inch Tube Steam Generator
Normal Operation and FLB Conditions

a,c,e

X = 0.25, 0.50, 1.00, or 2.00 In. Tolerance to be ± 0.030 In.

NOTES:

1.Primary Side source, dionized water, subcooled
2.Step roll 4 passes, include overlaps, partial roll and hardroll
3.Roll 1st Pass (outside of test region) at high torque to ensure leaktightness.
4.Plug this end of tube.
5.Test FLB condition first, then roll top 1 or 2 passes only, at appropriate higher torque, to include X, to achieve N.O. T/TS radial pressure, then leak test N.O. condition.

Figure A.1-2

Roll Expansion Leakage Test Sample for 3/4 Inch Tube Steam Generator LOCA Conditions

a,c,e

X = 0.25, 0.50, 1.00, or 2.00 In. Tolerance to be ± 0.030 In.

NOTES:

1. Secondary side source, deionized water, subcooled
2. Step roll 4 passes, include overlaps, partial roll and hardroll
3. Plug this end of tube.

A.2 TEST RESULTS FOR LEAKAGE FROM DEGRADED ROLL EXPANSIONS -- 3/4 INCH SG TUBES

A.2.1 Leakage Criterion

The leakage acceptance criterion was developed for evaluation of the leak rate test results. Individual acceptance values were developed for test differential pressure conditions simulating normal operation, feedline break and LOCA.

Normal Operation

The total normal operation primary-to-secondary leakage allowed in any one steam generator is specified in the Standard Technical Specification as 500 gpd, or 0.35 gpm for steam generator tubes. For conservatism, it was decided that the leakage contribution from L* tubes must not exceed this approximatley one-third of this value, or 150 gpd (0.1 gpm). The number of tubes to which the L* criteria can be applied was arbitrarily set at [ ]^{a,c,e} tubes. For the specified number of applicable tube ends, an allowable normal operation L* leakage limit of [ ]^{a,c,e} per L* tube was established. (This was based on the equivalence of 75,000 drops per gallon.)

The use of [ ]^{a,c,e} of the limit in the Technical Specification, i.e., an allocation factor of [ ]^{a,c,e} to determine the leakage rate criterion was an arbitrary decision for operational flexibility. The initial leak rate acceptance criterion may have been reevaluated in the light of actual test results and the allowance for potential degradation in the steam generator. Use of a larger fraction of the Technical Specification leakage limit and/or use of a smaller leak test acceptance criterion closer to actual results also could have been used to support application of the L* criteria to a number of tubes larger than the number on which the initial leak acceptance was based.

Faulted Condition: Feedline Break

Postulated feedline break conditions provide the maximum primary-to-secondary differential pressure across the tube. The steamline break (SLB) provides the most stringent radiological conditions for postulated accidents involving a loss of pressure or fluid in the secondary system. For establishment of the leak rate acceptance criteria for faulted conditions, the assumed SLB leakage rate is used with the feedline break pressure differential and was referred to as FLB in the test. This was the most stringent case.

The primary-to-secondary pressure differential for this condition was [ ]^{a,c,e} psi. It should be noted that dynamic loads on the tube joint, viz., secondary side fluid drag on the tube during the postulated accident, need not be added to this value. This is because the safety valve relieving event and the maximum fluid drag event are not concurrent. Therefore, the [ ]^{a,c,e} psi was the most stringent condition for test and an additional pressure differential, corresponding to the drag load need not be considered. This sequence of events also eliminates the need for augmenting the axial load during the test to determine the axial loadbearing strength of DRE's. The axial load during the leak test is prototypical because it is the end cap load caused by the prototypical differential pressure.

Faulted Condition: LOCA

Typically the small leakage from steam generator tubes into the primary system is not a significant consideration in the analysis of a postulated LOCA.

The test conditions used to simulate the LOCA conditions of [ ]^{a,c,e} psi secondary-to-primary differential pressure and a temperature equal to normal operating temperature were conservative for analyzed LOCA conditions.

A.2.2 Roll Torque Reduction Test Results

This test related s_r to rolling torque (T). Simulation of the TS-bending effect for the subsequent leak tests in the laboratory was accomplished by reducing the T corresponding to the s_r reduction in the plant. This was necessary because the L* length was near the top of the tubesheet and therefore detrimentally affected by tubesheet bending upward during primary-to-secondary side pressure differentials.

Leakage Test Results

The N.O., FLB and LOCA leakage test results are shown in Table A.2-1.

TABLE A.2-1

ROLL EXPANSION LEAKAGE TEST RESULTS - 3/4 INCH TUBE S/G

Sample No.	Roll Expansion Length, in.	Plant Condition	Differential Pressure, psi	Temp., °F Planned	Temp., °F Actual	Leak Rate, dpm

a,c,e

TABLE A.2-1 (Continued)

ROLL EXPANSION LEAKAGE TEST RESULTS - 3/4 INCH TUBE S/G

Sample Number	Roll Expansion Length, in.	Plant Condition	Differential Pressure, psi	Temp., °F Planned	Temp., °F Actual	Leak Rate, dpm

a,c,e

A.3 TEST FOR THE DETERMINATION OF AXIAL LOADBEARING CAPABILITY OF DEGRADED ROLL EXPANSIONS - 3/4 INCH TUBE S/G

A.3.1 Introduction

Tests were conducted to determine the axial loadbearing capability of the 3/4 inch diameter tube steam generator degraded roll expanded T/TS joints. The simulated degradation consisted of a single band of axial or near-axial slots. The results of the single band degradation (SBD) tests were also applicable to the most stringent, i.e., uppermost band of multiple band degradation (MBD) in roll expanded tube joints. The MBD configuration did not require testing. A series of tests was performed to determine the ultimate pull force (i.e., tensile strength) for non-degraded and artificially degraded, prototypical, [ ]^{a,c,e} in. (nominal) wall Alloy 600MA tubes rolled into cold rolled carbon steel AISI 1018 collars. The limiting test condition involved application of the pullout load directly to the top of the SBD. The roll expansion was performed utilizing a prototypical 3/4 inch tube steam generator T/TS roller with regulated rolling torque to provide proper levels of tube thinning and joint preloads. Slots were electric discharge machined (EDM) into the wall of tube specimens to simulate throughwall indications. All aspects of the test were designed to bound typical plant respective conditions. Refer to Table A.3-1. For example, tubes were machined to have [ ]^{a,c,e} slots inclined at angles of [ ]^{a,c,e} degrees from the axial centerline of the tubes. The length of the slots was [ ]^{a,c,e} in. which was considered to bound typical plant lengths by a factor of approximately 2, based on eddy current test assessments. Based on tube pull destructive examination results that indicate a throughwall distance less than the measured eddy current distance, using a 2 times multiplier to the eddy current crack length data as a representative throughwall crack length is quite conservative. The test axial distance "X" between the tops of the slots and the BRT was [ ]^{a,c,e} in.; plant "X" values for the intended tubes was approximately 0.5 in. The smaller (test) X was judged to bound plant potential X values; lower strength generally results from a smaller X. The collars were geometrically sized to simulate the equivalent structural reaction of a unit cell of the steam generator tubesheet. Figures A.3-1 and A.3-2 illustrate the basic shape and geometries of the collared and decollared specimens, respectively. The never-collared tube specimens are shown in Figure A.3-3.

All tests were performed at ambient atmospheric conditions employing a tension-compression machine shown in Figure A.3-4. Because the plant tubes are stressed at elevated temperature, the room temperature test results obtained with tubes of [ ]^{a,c,e} ksi UTS were analytically adjusted (decreased) to account for the small effects of temperature and UTS, for the Pull Strength Model discussed later in this report.

A.3.2 Objective

The objectives of this test were: (1) Determine the ultimate pull force necessary to structurally fail non-degraded and artificially degraded [ ]^{a,c,e} inch wall Alloy 600 tubes rolled into collars that simulated a unit cell of the steam generator tubesheet. (2) Measure the ultimate pull force necessary to structurally fail degraded non-rolled alloy tubes free of collars. (3) Evaluate the type of failure mechanism that induced structural failure in the tubes.

A.3.3 Test Equipment

1) Tubes, prototypical Alloy 600MA 0.75 in. OD, 0.043 in. wall (nominal thickness). Refer to Table A.3-1 for specimen features, and to Table A.3-2 for specific specimen configurations tested.

2) Tubesheet simulants (collars), cold rolled carbon steel, AISI 1018, 4.5 In. long. This material is in general use for simulants because the yield strength and modulus of it approximate the respective properties of the tubesheet material. Refer to Figure A.3-1.

3) Airetool Roll Expander, No. [ ]^{a,c,e} rolls per original (shop) fabrication specification.

4) Roll Expander Motor, of 30 to 95 in.-lbs. torque capacity, 1250 RPM no-load speed.

5) Zetac Tension-Compression Testing Machine. Refer to Figure A-3-4.

6) Other laboratory strength test equipment such as furnaces, measuring instruments, etc.

A.3.4 Test Major Steps

Collared Tubes

The collared tubes were prepared by rolling approximately two per cent thinning, then removed from the collar for EDM of the slots, if slots were required; the tubes were then replaced in the respective collars, hardrolled to the prototypical midrange torque and welded. The specimens were then "pulled" in a configuration similar to that shown in Figure A.3-4.

Decollared Tubes

The decollared samples were prepared by rolling the tubes to a small amount of wall thinning, a non-interference fit, then removed from the collar for EDM of the slots; the tubes were then replaced in the respective collars, and (hard)rolled to the specified midrange torque.

After hardrolling, the specimens were decollared, i.e., the collars were carefully removed by machining. This step prevented the collar from providing the known beneficial effects, primarily friction, during the strength test. The slot geometry and axial location are shown in Figure A.3-2. Table A.3-2 identifies the specimen configuration in terms of the number of slots, angle, and slot top location with reference to the BRT.

Never-Collared Tubes

These tubes were prepared for pulling by EDM of the slots. The reason for including this configuration was to show that it involves higher, i.e., non-limiting ultimate strengths. The specimens were pulled in a configuration similar to that shown in Figure A.3-4.

Test Facility

The tests were performed at the Westinghouse Research and Deveopment Laboratory. A Zetac tension - compression test machine was used to apply a continuously increasing axial pull force to the tube specimens. Figure A.3-4 is a sketch of the test machine. The tube top ends were permitted to rotate as dictated by the slot angle, ϕ. A certain amount of rotation about the tube vertical axis could occur in the plant in keeping with the conservative assumption that the SRE within L* provides no fixity to the TS, owing to the tube straight leg acting as a torsional spring and being anchored at the U-bend. It was judged that this provided lower ultimate values than if axial rotation were prevented.

A.3.5 Data Acquisition System

Axial pull forces were recorded on Cartesian coordinates as a function of tube top end displacement by employing an X-Y analog plotter.

A.3.6 Procedure

The specimens were vertically mounted in the Zetac testing machine as shown in Figure A.3-4. A steadily increasing axial pull was applied to the specimens until ultimate load failed the tube. A curve was simultaneously drawn by an X-Y analog plotter of the pull force versus tube top deflection. Curves were produced for non-degraded and degraded tubes; non-degraded tubes were tested to establish reference data in order to assess the strength of the degraded tubes.

The pull specimens were tested per Table A.3-2.

Table A.3-1

Selection of Bounding Features for
Axial Loadbearing Test of Degraded Roll Expansions

a,c,e

Table A.3-2

Axial Loadbearing Strength Test
of Degraded Roll Expansions

Specimen Number	Specimen Configur.	Array Top Distance Below Bottom of Transition, in.	No. of Slots	Slot Angle, Degrees	Ultimate Load, lb.

a,c,e

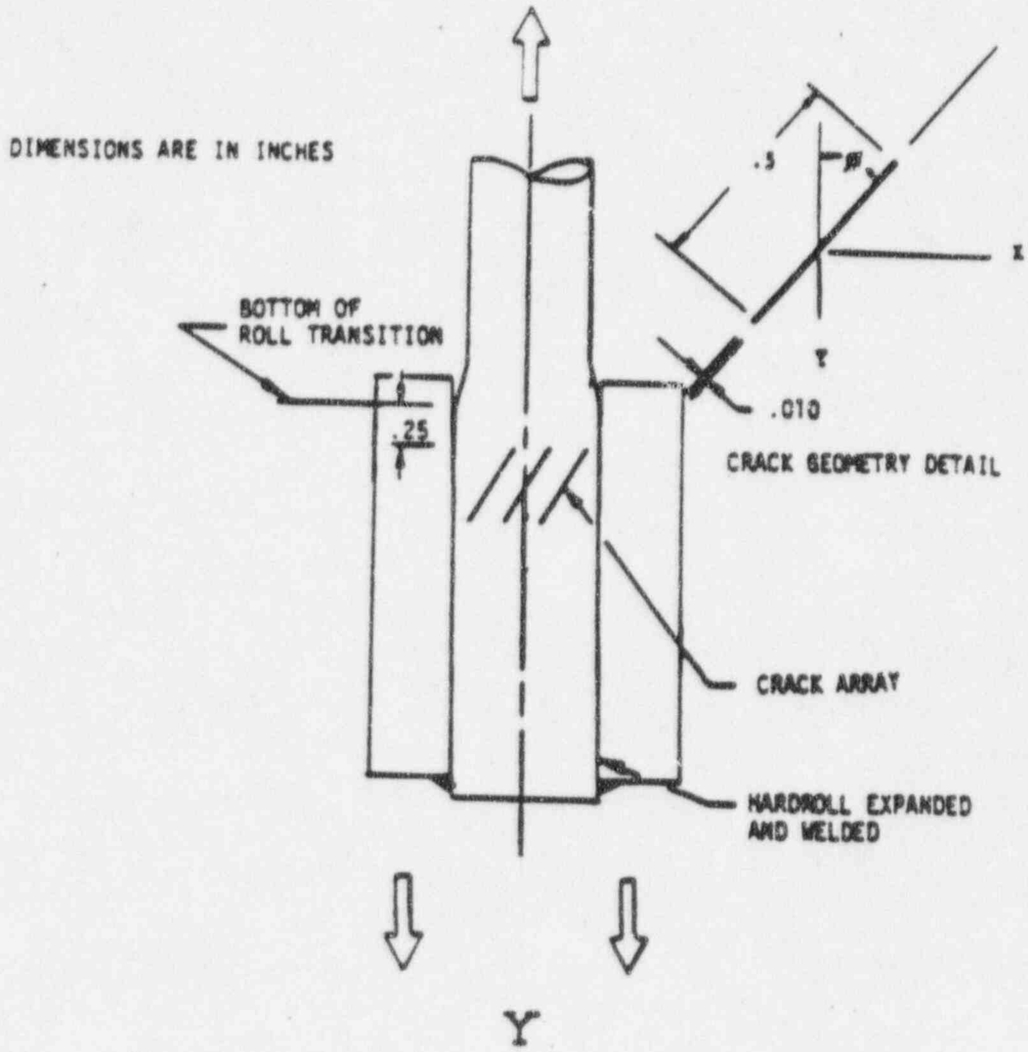


Figure A.3-1

Geometry of Slots in Collared Tube Specimen - Ultimate Strength Test

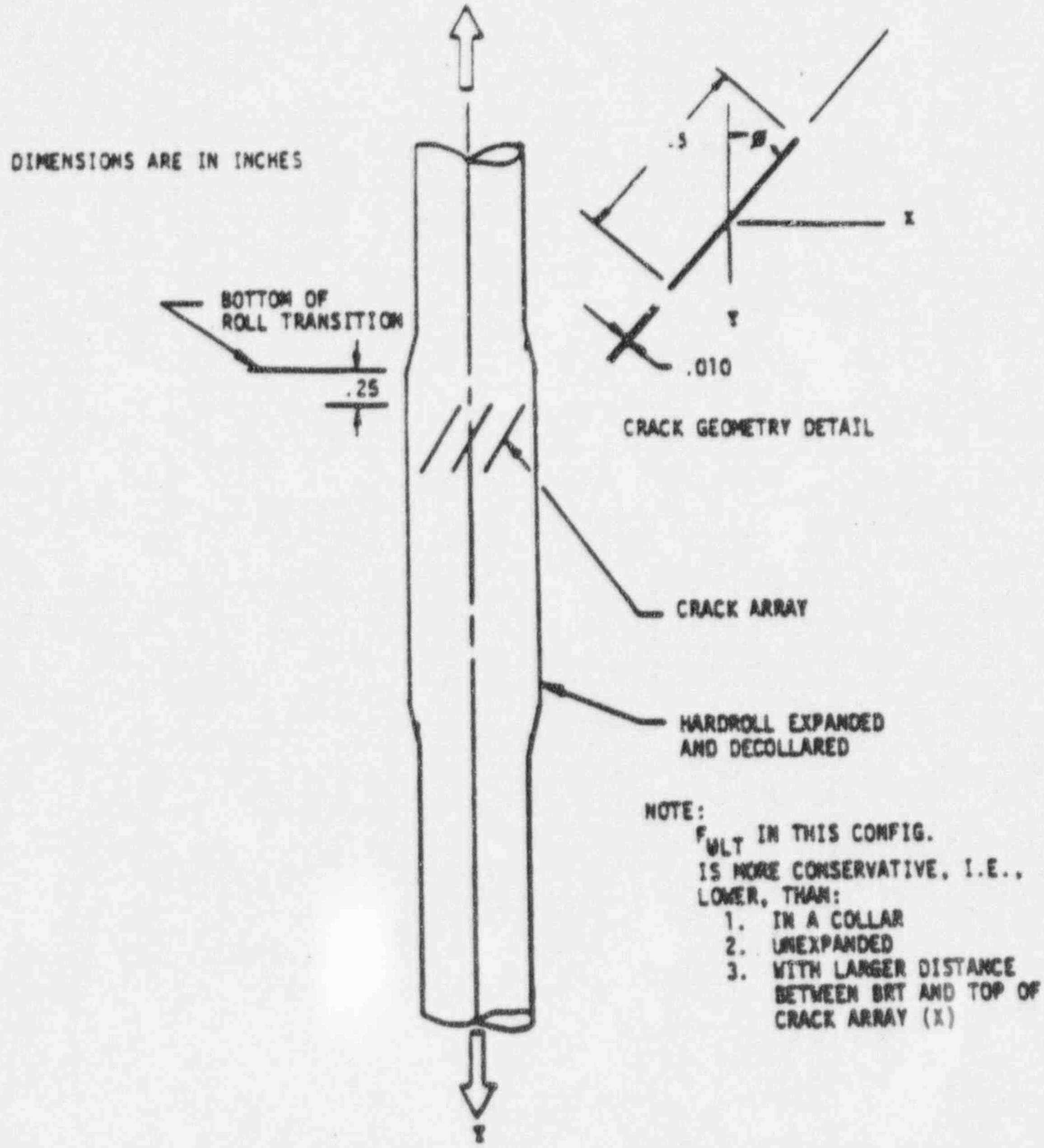


Figure A.3-2

Geometry of Slots in Decollared Tube Specimen - Ultimate Strength Test

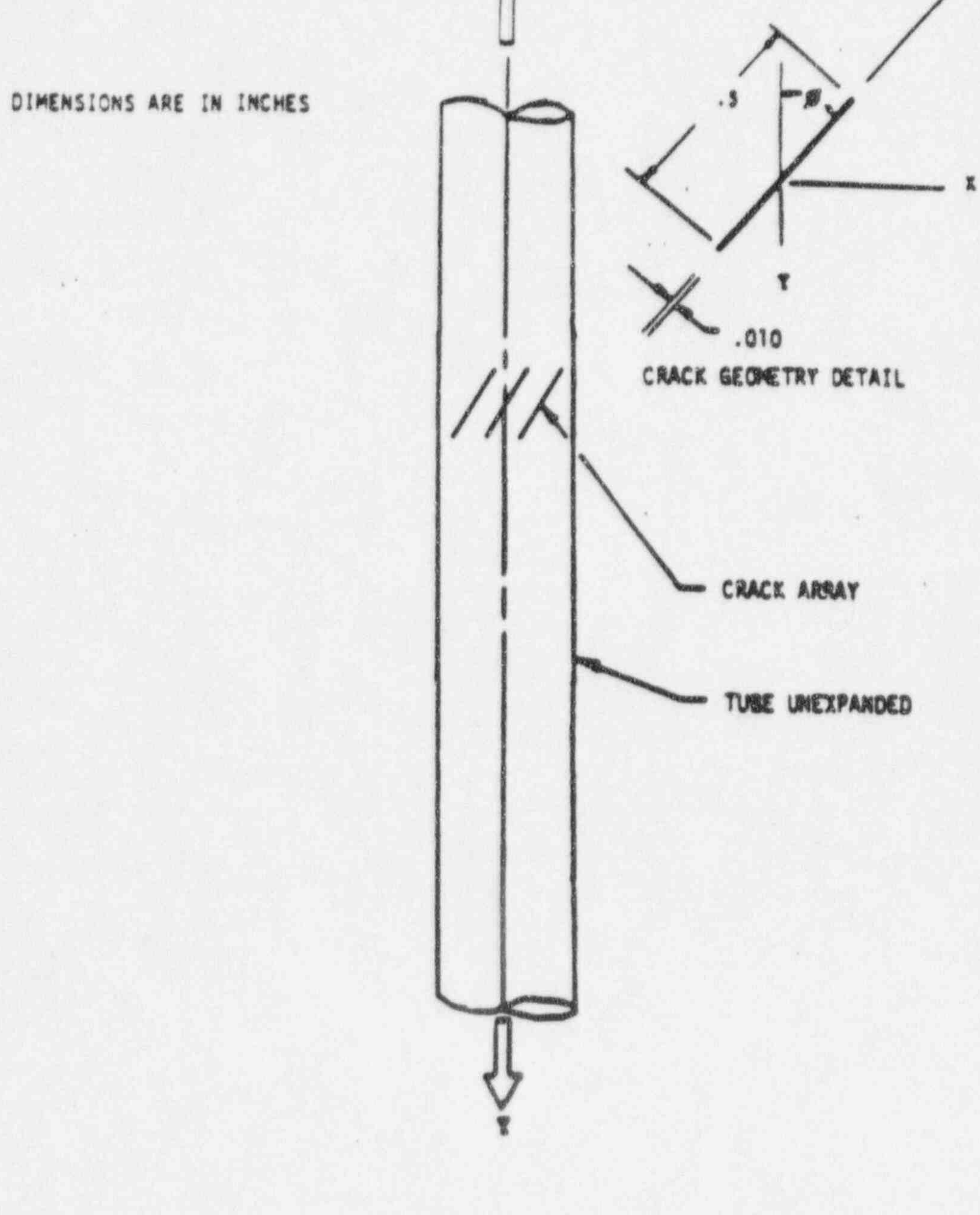


Figure A.3-3

Geometry of Slots in Unexpanded, Never-Collared Tube Specimen - Ultimate Strength Test

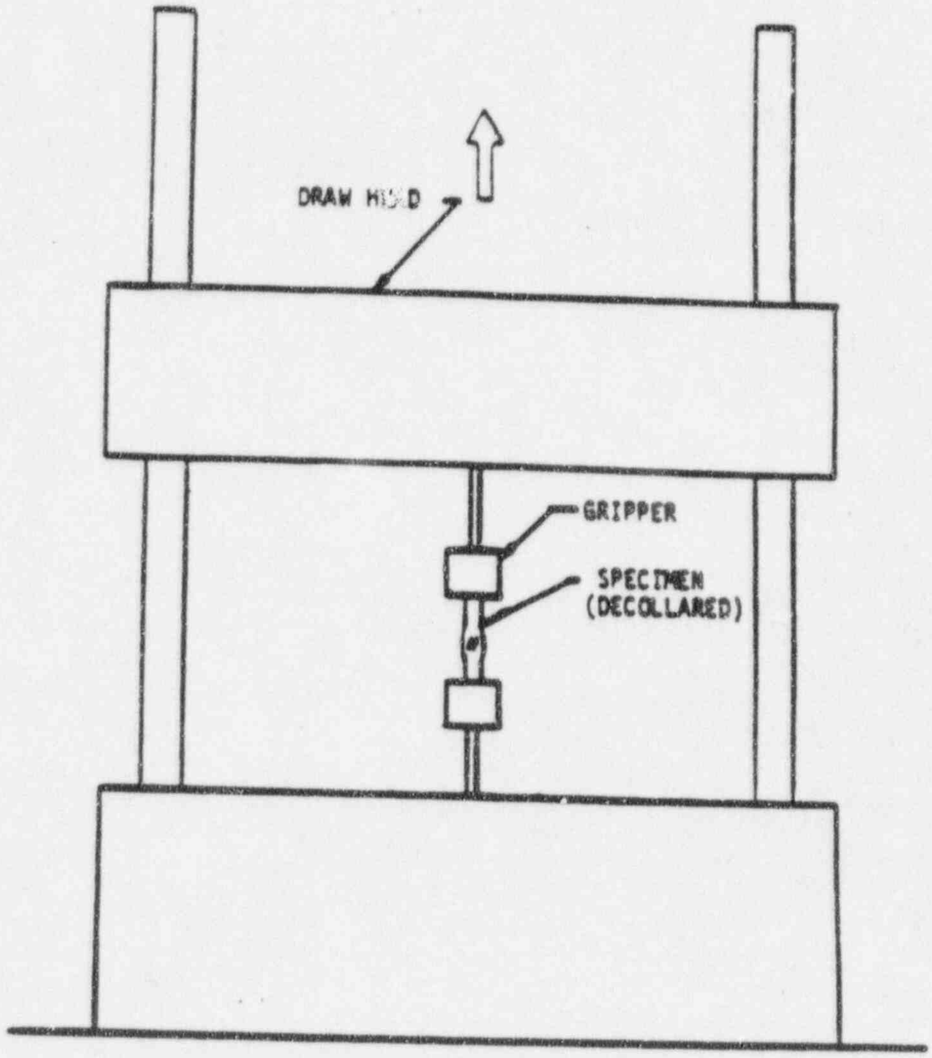


Figure A.3-4

Sketch of Zetac Tension and Compression Testing Machine

A.4.0 CRITERION AND TEST RESULTS FOR THE AXIAL LOADBEARING CAPABILITY OF DEGRADED ROLL EXPANSIONS - 3/4 INCH TUBE S/G

A.4.1 Maximum Axial Load Criterion

Single Band Degradation

The maximum axial tensile load which a DRE must bear will be the most stringent of the N.O. and FLB axial loads. The LOCA axial load is axially compressive and cannot cause tube pullout.

Multiple Band Degradation

If degradation occurs in discrete circumferential bands i.e., as single band degradation (SBD), or arrays, in an RE above the F* elevation, thereby preventing use of F*, it may also occur in multiple discrete bands below the F* elevation. This is termed multiple band degradation (MBD). As discussed earlier, it's desired to avoid quantifying each of the possible several degradation bands to apply the pullout load to the T/TS weld. Instead, it's desired to apply the load only to the uppermost sound roll portions (expansions) which are interspersed with the uppermost degradation bands. For the purpose of simplicity, each degradation band requires the same strength. This strength is the same as that for the SBD. Therefore, for the MBD case, the number of individual bands which must be quantified is one less than the number of SRE's (N) needed to react the pullout load. If N is 3, N-1, is 2. Conservatism may be added to this calculation in the form of a larger N than is needed.

Normal Operation

The tube ultimate load required for normal operation is the "end cap" load resulting from the N.O. primary-to-secondary side pressure differential multiplied by the cross-sectional area of the roll expanded tube OD. A safety factor of three is applied to this load. This load is the highest for the three conditions considered and the data for the 15 slot case is plotted in Figure A.4-3.

Feedline Break

The tube ultimate load required for FLB for these 3/4 inch tube SGs is the endcap load resulting from the FLB primary-to-secondary pressure differential multiplied by the cross-sectional area of the roll expanded tube OD. A safety factor of [

]^{a,c,e} for allowable stress for faulted conditions) was used for this load. This load did not need to be further augmented for dynamic loading effects during FLB because of the sequence of events during an FLB. This load was less than the N. Op. load and therefore was not plotted on Figure A.4-3.

Loss of Coolant Accident

An axial loadbearing requirement for a LOCA event acts to move a tube downward, the opposite of pullout. Therefore, this condition does not apply to pullout. (However, the DRE must not collapse under LOCA conditions. This condition was tested with prototypical, i.e., compressive, end cap loads, in the leak test section.)

A.4.2 Strength Test Results

A sample of the strength test results for non-slotted and slotted tubes is presented in Figures A.4-1 and A.4-2. The results for the entire test series were previously summarized in Table A.3-2. [

]a,c,e Structural failure of the tube was attributed to the axial and shear forces acting through the tube wall as a function of the vectorial relationship between the axial pull force and the angle of the respective slot to the tube axial centerline.

The strength test results discussed above dealt with pullout loads. No deleterious structural effects are expected from LOCA i.e., compressive loads on DRE's. No collapse of the tubes in the LOCA leakage resistance test occurred.

The basic objective of the L* program was to demonstrate the acceptability, for leakage and strength, of degradation arrays of known or bounding dimensions and configurations. The leakage issue was addressed elsewhere in this report. The strength design curve will be developed here and is based on the strength test results.

A.4.3 Evaluations and Conclusions

Pull Strength Model

As a tube with an array of parallel slanted cracks is loaded axially, deformation proceeds as follows. Initial elastic elongation is followed by plastic yielding. Depending on the number of cracks and the crack angle, yielding may first occur in the virgin unrolled tube section or in the crack array. Large numbers of cracks and high crack angles (ϕ) favor yielding of the crack array. Yielding of the crack array proceeds by plastic bending of the ligaments between cracks and subsequent rotation of the cracks toward the longitudinal axis of the tube. This is easily visible in pull strength tests. As rotation occurs, the applied axial force must increase substantially since the effective moment arm for ligament bending is continually decreasing.

Strain hardening of the ligaments adds to the geometric hardening produced by crack rotation. As the load is increased, general yielding of the of the virgin unrolled tube section may occur, followed by strain hardening and then yielding of the rolled but uncracked tube section. If deformation proceeds to this point, the rolled tube peels away from the tubesheet and the axial pull strength is limited by the tube-to-tubesheet weld. At some point, depending on the crack morphology, fracture will terminate the process of plastic deformation.

Using the notation of Figure A.4-4, the axial load required to yield a tube with an array of slanted cracks is

a,c,e,

[ℓ]

This expression only considers ligament bending and assumes deformation is confined the initial minimum ligament crossection. The axial plastic displacement resulting for yielding and then rotation of the crack array is given by

a,c,e

[ℓ]

If the crack array plastic displacement is added to the baseline load displacement record of an uncracked tube, then the computed load-displacement records closely approximate actual measured load displacement records. The load displacement record for the uncracked tube essentially provides elastic tube displacement and an indication of yielding in the uncracked section at high loads. Figure A.4-5 shows a test of a 3/4 inch diameter tube terminated by ligament fracture. Figure A.4-6 shows the test record for another 3/4 inch diameter tube where the test load has become high enough to yield the uncracked as well as cracked sections. These figures show that there is good agreement between the calculated and test measured load displacement curves and that the mathematical model can be used to accurately predict the integrity limits of a tube with inclined cracking within the L* region.

The 3/4 inch tube design curve (Figure A.4-7) was developed to be used for comparison with tube axial loads, multiplied by suitable factors of safety caused by the most stringent normal and faulted operation conditions.

a,c,e

Figure A.4-1

Pull Force as a Fuction of Pull Displacement
for a Collared, Nondegraded Tube (Specimen No. 11)

a,c,e

Figure A.4-2

Pull Force as a Function of Pull Displacement
for a Collared Tube (Specimen No. 3), with (15)-30 Degree Slots,
Tops of Slots 0.25 Inch Below the Bottom of Roll Transition

a,c,e

Figure A.4-3

Ultimate Pull Force as a Function of Slot Angle for Expanded, Collared and Decollared Tubes, 15 Slots, Tops of Slots 0.25 Inch Below the Bottom of Roll Transition - 3/4 Inch Tubes

a,c,e

Figure A.4-4

Model for Plastic Collapse

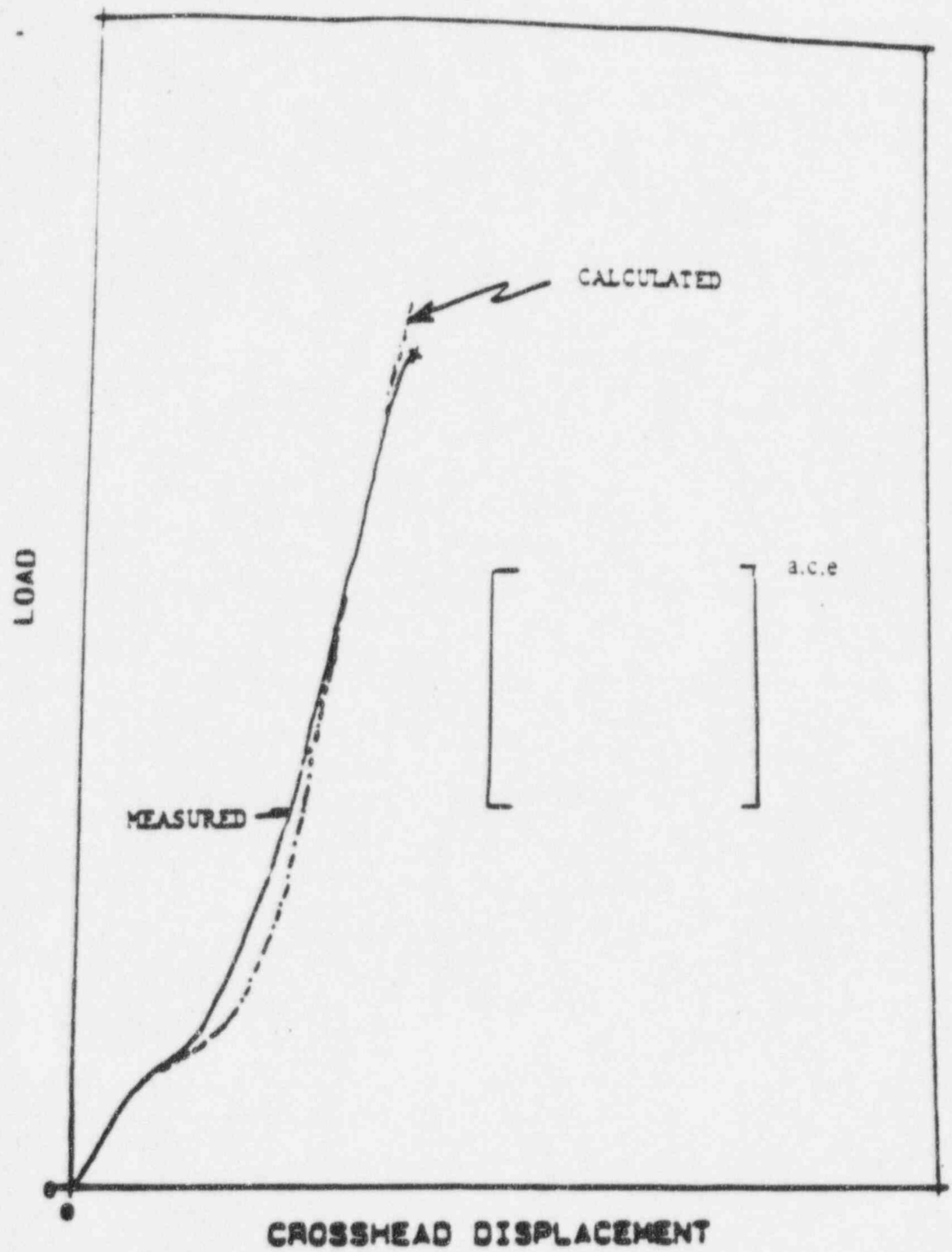


Figure A 4-5

Comparison of Load-Displacement Records, Computed Vs Measured for 30 Slots at $\phi = 45°$ - 3/4 Inch Tubes

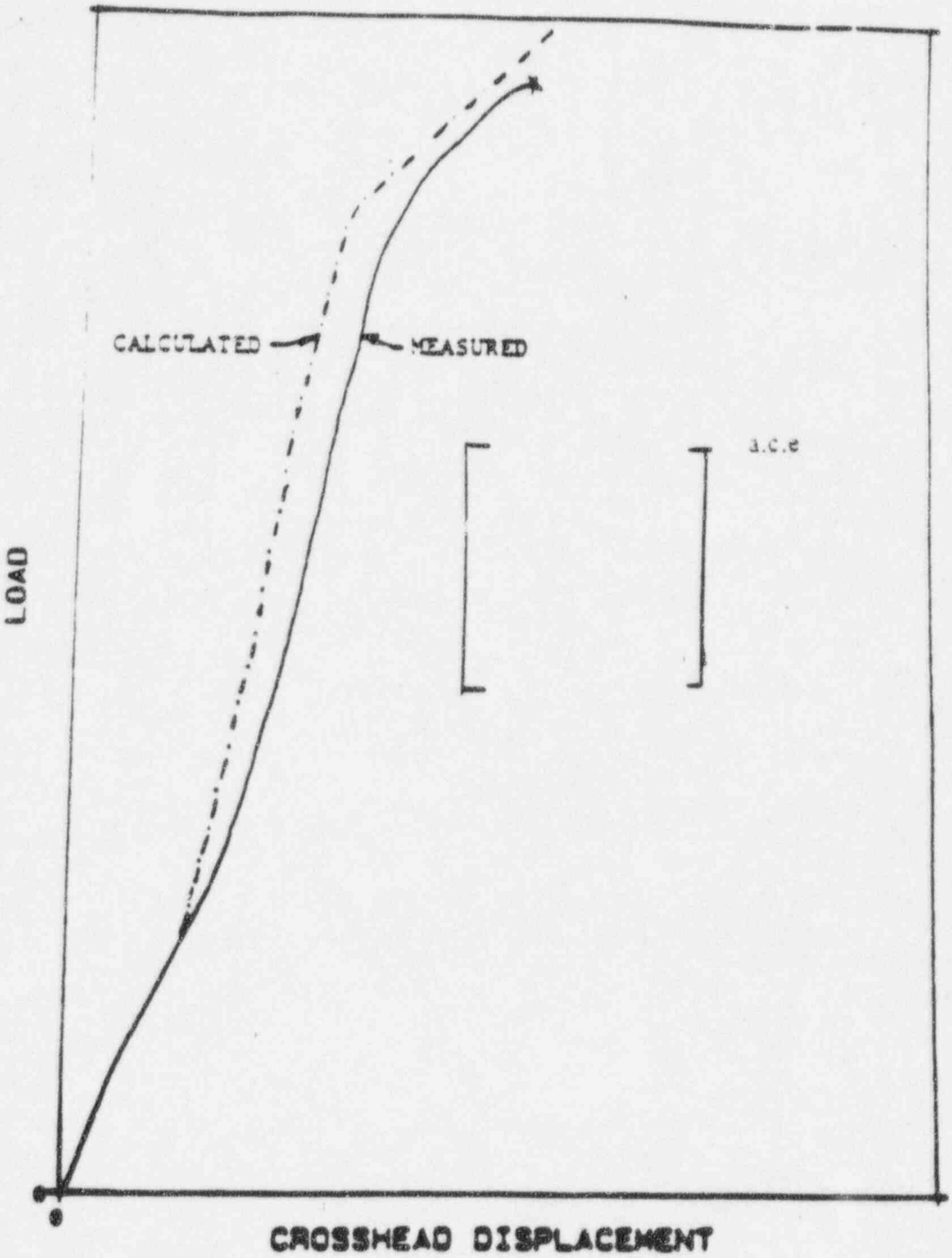


Figure A 4-6

Comparison of Load-Displacement Records, Computer Vs Measured
for 30 Slots at ϕ = 30° - 3/4 Inch Tubes

a,c,e

Figure A.4-7

3/4 Inch Tube Steam Generator Degraded Tube Pull Strength Design Curve for L*

A.5 CONCLUSION

With sufficiently quantified information about degradation in the anchor regions of the applicable 3/4 inch tube S/G roll expansions, use of the Design Curve is expected to provide reliable operation. (The leakage criterion for this case is discussed elsewhere in this appendix and involves an upper limit on the number of tubes per steam generator which may be dispositioned thusly.)