BAW-2085
MAY 1989

THE B&W OWNERS GROUP

MATERIALS COMMITTEE

SUBMITTAL IN RESPONSE TO NUCLEAR REGULATORY COMMISSION BULLETIN 88-11 "PRESSURIZER SURGE LINE THERMAL STRATIFICATION"

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IN RESPONSE TO

NUCLEAR REGULATORY COMMISSION

BULLETIN 88-11

"PRESSURIZER SURGE LINE THERMAL STRATIFICATION"

Prepared for

Arkansas Power & Light Company
Duke Power Company
Florida Power Corporation
General Public Utilities Nuclear
Sacramento Municipal Utility District
Toledo Edison Company

(See section 10 for document signatures)

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1. INTRODUCTION

The purpose of this report is twofold: first, to describe the B&W Owners Group program and plans for addressing the surgeline thermal stratification and thermal striping issue, and second, to present the results of the preliminary work done to justify continued operation until the final program results are available. A portion of the Owners Group plan was presented to the Nuclear Regulatory Commission staff during a Regulatory Response Group meeting on September 29, 1988. A more detailed description of the Owners Group plan, including preliminary observations from the Oconee test program, was presented to the staff on April 7, 1989.

The report first provides the background of the thermal stratification and striping issue and describes the current regulatory requirements. The B&W Owners Group program is then described and the results obtained to-date are presented. These include the results of the bounding fatigue analyses, preliminary results from the measurement program at Oconee Unit 1, and a comparison of the configuration and dimensional characteristics of the various B&W plants. The latter is used to justify the use of Oconee Unit 1 as the plant on which measurements are taken. Together these elements of the program are used to justify continued near term operation.

The thermal striping program currently underway is then described along with preliminary results. Upon completion of the striping program, a final report will be prepared which will provide justification for operation for the remaining life of the plants or an action plan that will lead to this ultimate goal.

2. BACKGROUND

During heatup of a pressurized water reactor, the pressurizer is heated until a steam bubble is formed. The resulting pressurizer fluid temperature is thus significantly higher than the average fluid temperature in the reactor coolant system. Temperature differences along the pipe axis and vertical cross section of horizontal runs can also be established due to natural or forced convection between the two fluid volumes. At low velocities, the hotter fluid can flow along the upper portion of the pipe leading to stratification along all or a portion of the surgeline length. In addition, because the pipe connecting the two parts of the system is also at an elevated temperature it can develop radial temperature gradients simply due to heat losses.

An associated phenomenon which may occur during thermal stratification is called thermal striping. Since the warmer water is flowing across the cooler water, possibly creating interfacial waves and turbulent effects, a moving temperature interface may exist at the boundary between the two layers. These phenomena can alternately warm and cool the metal where they contact the inner surface of the pipe. The amount of alternate warming and cooling of the metal is dependent on the amplitude and frequency of the fluctuations as well as on the temperature difference between the hot and cold layers and the effective heat transfer coefficient to the pipe wall. In the extreme case, metal fatigue may result from this alternate warming and cooling of the pipe.

The most obvious effect of thermal stratification can be substantial bowing, either up or down, of the surgeline due to the vertical thermal gradient in the pipe. This resultant bowing and possible contact with adjacent structures was not considered in the original stress analysis since stratification was not an identified design basis condition at the time of the original stress analysis.

The effects of this bowing have been observed in the surgeline of the Portland General Electric Company Trojan plant during each refueling outage since 1982. The piping was observed to have lessened gaps on pipe whip restraints and, in some cases, actually contacted the restraints. Similar effects were also noted at Beaver Valley Unit 2. Both plants are Westinghouse PWRs.

During hot functional testing, the surgeline of the B&W plant at Muelheim-Kaerlich in West Germany was instrumented and temperature readings were taken during startup testing. The Muelheim-Kaerlich plant is different from the domestic B&W plants in terms of power level, surgeline layout, diameter and thickness. The measurements taken indicated that stratification, which was not part of the plant design basis or analysis, did occur in the surgeline. It was determined that this stratification was greatest during startup from cold conditions. Subsequent fatigue evaluations of this condition have shown that the Muelheim-Kaerlich surgeline meets its design goal of a forty year life.

Based upon the Muelheim-Kaerlich observations, the B&WOG defined a program to instrument one of the domestic B&W plants to determine if stratification was present and, if so, to determine its magnitude. During the latter portion of 1988, based upon information related to surgeline motions, the NRC began preparing a bulletin requiring further investigation. Each of the PWR owners groups were requested to meet with the NRC to discuss their knowledge of the surgeline concerns and to provide feedback on the content and schedule of the proposed bulletin. As a result of this meeting the B&WOG expanded the scope of their program to include measurements of surgeline movement as well as thermal striping.

3. REGULATORY REQUIREMENTS

On December 20, 1988 the Nuclear Regulatory Commission issued NRC Bulletin Number 88-11, Pressurizer Surgeline Thermal Stratification. This bulletin requires certain actions of licensees of all operating PWRs. The applicable actions are paraphrased below.

1. At the first available cold shutdown after receipt of the bulletin, and which exceeds seven days, conduct a visual inspection of the pressurizer surgeline.

2. Within four months of receipt of the bulletin, licensees of plants in operation over ten years are requested to demonstrate that the pressurizer surgeline meets the applicable design codes[1] and other FSAR and regulatory commitments for the licensed life of the plant, considering thermal stratification and thermal striping in the fatigue and stress evaluations. (For licensees of plants which have been in operation less than ten years, this action must be completed within one year of receipt of the bulletin.)

3. Update the fatigue and stress analyses to ensure compliance with the applicable Code requirements.

If the above schedule could not be met, licensees were required to submit an alternate schedule within 60 days with justification of the new dates. This was done by letter from the B&WOG Materials Committee Chairman on February 24, 1989. This letter stated that the thermal striping portion of the program would extend beyond the dates requested in the bulletin for item 1.b, and would be forwarded by October 31, 1989. The results of the other parts of the program and preliminary results of the thermal striping evaluation are contained herein.

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[1]For fatigue analysis the latest ASME Section III requirements incorporating high cycle fatigue.

4. B&W OWNERS GROUP THERMAL STRATIFICATION PROGRAM

The B&WOG program currently in place to resolve the pressurizer surgeline thermal stratification issue consists of the following four parts.

1. Bounding fatigue analyses of the two types of B&W plants operating domestically.
2. Measurement of surgeline temperatures and movements during plant heatup, power operation, and cooldown at Oconee Unit 1.
3. Comparison of plant configurations, operating procedures, and specific plant and operator practices.
4. Thermal striping evaluation.

The bounding fatigue analyses were performed on two operating plants: Oconee Unit 1 and Davis-Besse. The surgelines on all domestic B&W operating plants are similar in configuration, geometry, and materials of construction with the exception of Davis-Besse. All plants have the same surgeline diameter, thickness, and materials. The pipe routings are all similar with the exception of Davis-Besse. The Davis-Besse plant has a nozzle supported reactor vessel and raised reactor coolant loops while the other domestic plants have skirt supported reactor vessels and lowered reactor coolant loops. This results in a different surgeline routing at Davis-Besse, therefore two separate fatigue analyses were required.

A measurement program was initiated at Oconee Unit 1 to determine the surgeline temperatures and motions during plant heatup, and full power operation, plant upsets, and during cooldown. The results of this program for plant heatup and power operation are used to confirm that the fatigue analyses are indeed bounding and to permit definition of realistic surgeline transients for use in updated fatigue analyses. To date, no Oconee Unit 1 upsets or complete cooldowns have occurred. Therefore, no data for these types of events is available to integrate into this evaluation. The applicability of Oconee Unit 1 data to the other operating domestic plants from an operational standpoint is discussed below. The applicability from a configuration standpoint is discussed in Section 6.0. The applicability from a structural standpoint is addressed by the use of two fatigue analyses discussed above.

The plant operating procedures at Oconee Unit 1 have been compared to those of the other B&W domestic plants. Since the magnitude of thermal stratification in the surgeline is dependent on the plant heatup procedures, the similarity of procedures ensures that the measurements taken at Oconee Unit 1 are representative of all domestic B&W plants. The evaluation of operating procedures is discussed in Section 6.2.

The effects of thermal striping on the fatigue life of the surgeline are being evaluated in a longer term effort. Preliminary results of this work are reported in this submittal. The measurement program at Oconee Unit 1 was designed to determine the magnitude of thermal stratification in the surgeline and the plant parameters that affect the stratification. Since the temperature instrumentation was mounted on the outside surface of the surgeline (a 1" thick pipe), it has inherent limitations for the determination of inside wall temperature oscillations. Detailed heat transfer analyses of the surgeline wall have shown that at the relatively high frequencies associated with thermal striping (i.e. approximately 0.1 to 10 Hz) the outside mounted thermocouples will generally not detect the inside surface temperature changes and, in fact, striping phenomena have not been observed with the Oconee instrumentation.

A detailed description of each of the four parts of the program is contained in the following sections of, and appendices to, this report. Also included are the results of these efforts to date.

5. FATIGUE ANALYSES

The bounding fatigue analyses were performed on two operating plants, Oconee Unit 1 and Davis-Besse. B&W performed all aspects of the Oconee analysis on behalf of the B&W Owners Group for the purposes of providing a generic result representative of all the lowered loop plants; Toledo Edison supplied key assumptions and loading inputs to B&W for the fatigue analysis of Davis-Besse. Toledo Edison's surgeline program, described in additional detail in Section 5.2, preceded the B&W Owners Group program and was initiated independent of the owners group efforts. As a result, there are some differences between the Oconee and Davis-Besse analyses in regard to assumptions and the application of the Muelheim-Kaerlich data to the structural evaluations done on the surgelines. These differences are minor and do not affect the conclusions regarding unit operating lifetime. The Oconee analysis results are described in Subsection 5.1. Toledo Edison's program and results are discussed in Subsection 5.2. A comparison of the bounding assumptions for both the Oconee and Davis-Besse analyses to the Oconee test data is included in Subsection 5.3.

The two thermal stratification analyses have been performed using the following codes:

1. For Davis-Besse Unit 1: USA Standard B31.7, 1969 Edition, "Nuclear Power Piping."

2. For Oconee Unit 1: ASME Code Section III 1977 Edition, with Addenda Through Summer 1979.

These codes were chosen since they had been used for the previous surgeline analyses. NRC Bulletin 88-11 requests the use of the latest ASME Section III Code incorporating high cycle fatigue (10^6 to 10^{11} cycles). The latest ASME Section III requirements are less restrictive than the ones used herein except for the fatigue requirements. The latest ASME Section III, Figure I-9.2 fatigue requirements are extended up to 10^{11} cycles for low stress values. However, the new requirements do not affect surgeline cumulative usage factor with thermal stratification, since the number of stratification cycles is very low compared with the cut-off value of 10^6 cycles (low cycle fatigue). High cycle fatigue is being analyzed in the thermal striping evaluation.

5.1. Oconee Unit 1 Bounding Fatigue Analysis

The Oconee Unit 1 surgeline geometry is shown in Figure 6.1. This geometry is typical of the B&W lowered loop plants with the exception of Davis-Besse. The surgeline was modeled on the ANSYS finite element computer code using piping elements which allow a linear temperature gradient to be applied across the pipe diameter.

The loadings consisted of pressure, seismic, deadweight, and thermal expansion from the original stress report combined with new thermal stratification loadings. The thermal loading cases used are shown below. These temperatures were derived from those measured at Muelheim-Kaerlich. Surgeline data measured at Oconee indicates much smaller top to bottom temperature differences than were assumed in the bounding analyses. A comparison of the loading case assumptions to the Oconee data is provided in Section 5.3.

	LOAD Case 1 (Pre-Heatup)	LOAD Case 2 (Heatup)	LOAD Case 3 (Cooldown)
Pressurizer Temp. (^{o}F)	451	579	432
Hot Leg Temp. (^{o}F)	109	369	93
Horizontal Run Top Temp. (^{o}F)	439	531	399
Horizontal Run Bottom Temp. (^{o}F)	109	109	93
Delta T Between Top and Bottom (^{o}F)	330	422	306

Thermal stratification was assumed to occur over the entire lower horizontal pipe run. The pipe at the pressurizer end of the line was assumed to be at the pressurizer temperature while the pipe at the hot leg end of the line was assumed to be at the hot leg temperature. Load Case 1 was assumed to occur three times during each heatup-cooldown cycle while Load Cases 2 and 3 were assumed to occur once per heatup-cooldown cycle. The end motions of the surgeline at the hot leg and at the pressurizer were calculated by applying RCS loop temperatures to the model. The resultant cyclic loads at each joint were calculated and applied to a T3PIPE model to calculate stresses and determine fatigue usage factors.

The T3PIPE computer code calculates pipe stresses and fatigue usage factors using ASME Code methods for Class 1 piping. The appropriate stress indices are automatically included for the selected ASME Code dates. For the case in point, the 1977 Edition with Addenda through the Summer of 1979 were used. All Code criteria were met with the exception of the requirement that the expansion stresses not exceed three times the design stress intensity ($3S_m$) which, for austenitics, is equal to two times the material yield strength. This is intended to prevent the material from being cycled in the plastic range by thermal expansion. The $3S_m$ limit assumes elastic-perfectly plastic material behavior when, in fact, most steels used in nuclear power plants exhibit considerable strain hardening. Therefore the strain hardened yield strength was substituted for the virgin yield strength for purposes of this preliminary analysis. This meets the Code intent to prevent cycling in the plastic range. Refer to Appendix C for the technical justification for the use of twice the cyclically strain-hardened yield strength in place of the $3S_m$ limit specified in Section III of ASME Boiler and Pressure Vessel Code. It is expected that the final analysis will meet the more conservative $3S_m$ requirement of the ASME Code.

The fatigue usage factors were calculated for the surgeline, surgeline drain nozzle, hot leg nozzle, and pressurizer nozzle. The usage factors for the thermal stratification load cases were combined with those from the stress analysis of record to obtain the total usage factors. These include thermal stratification effects during all heatup-cooldown cycles, including those which occurred in the past. This was done using the specified number of heatup-cooldown cycles of 360 for the 40 year life of the plant or nine heatup-cooldown cycles per year. From the operating experience of these plants, the nine heatup-cooldown cycles per year is a conservative number. The fatigue life of each part affected by surgeline stratification was then calculated in terms of allowable number of heatup-cooldown cycles and is presented below:

Hot leg nozzle (carbon steel portion) .... 270 cycles
Hot leg nozzle (stainless steel portion) .... 162 cycles
Surgeline (straight or elbow) .... 153 cycles
Surgeline drain nozzle .... 135 cycles
Pressurizer nozzle (stainless steel portion) ...... 341 cycles
Pressurizer nozzle (carbon steel portion) .... 396 cycles

The B&W domestic unit which has the most heatup-cooldown cycles to-date is Oconee Unit 2 with approximately 96 (see Table 5-1). Thus, it can withstand another 39 cycles of heatup-cooldown without fatiguing to its limit the surgeline drain nozzle, the most limiting case, using the conservative analysis described above. This translates into five more years of operation using the specified heatup-cooldown cycle accumulation rate of 360 per 40 years which is, in itself, conservative.

5.2. Davis-Besse Bounding Fatigue Analysis

Toledo Edison initially became aware of the NRC's surgeline thermal stratification and striping concerns in September 1988 while Davis-Besse was in cold shutdown for a refueling outage. A program was immediately developed and implemented to assess the condition of the surgeline and to verify that the unit could be safely returned to power. The program included a broad spectrum of inspections, maintenance reviews, and analyses. The analyses were aimed at determining the remaining useful life of the surgeline. The results of this evaluation are reported in this section.

In order to define temperature transients upon which fatigue analyses could be based, Toledo Edison reviewed the Davis-Besse operating procedures and the temperature stratification data from Muelheim-Kaerlich. Surgeline conditions were estimated from the Muelheim-Kaerlich (M-K) data. Plant-specific adjustments to the M-K data were made to account for differences between Muelheim-Kaerlich and Davis-Besse. The following text addresses the specific analysis assumptions and results of this work.

The Davis-Besse surgeline geometry is shown in Figure 6.2. It can be seen that this geometry is different from other domestic B&W plants. The analysis was conducted in the same manner as the Oconee Unit 1 analysis. The thermal stratification stresses were combined with the stresses due to all other specified transients and determined the total usage factors for the surgeline and the nozzles at each end.

The stress analysis loading consisted of pressure, seismic, deadweight, and thermal expansion loadings from the original stress report combined with new thermal stratification loadings. Davis-Besse's unique configuration lends itself to different stratification conditions than Oconee 1. The 7.25 feet rise near the center of the surgeline will reduce transient thermal gradients that exist in either horizontal line depending upon the direction of flow. The thermal loading cases used are shown below. These temperatures used are derived from the temperatures measured on the surgeline at Muelheim-Kaerlich, modified to account for Davis-Besse operating limits.

	Steam Bubble Forms	Midway in Heatup	At End of Heatup	Cooldown
Hot Leg Temperature °F	100	375	500	100
Upr. Horiz. Run Temp. °F	100	375	500	100
Vertical Run Temperature °F	100	275	500	100
Lwr. Horiz. Run Temp. - Top °F	409	506	506	409
- Btm °F	100	120	205	100
Delta T Between Top & Bottom °F	309	386	301	309
Pressurizer Temperature °F	409	506	649	409

Thermal stratification was assumed to occur over the full length of the lower horizontal pipe run. Stratification transients were assumed to occur three times during the bubble formation of each heatup-cooldown cycle while the remaining cases occur once per heatup-cooldown cycle. The end motions of the surgeline at the hot leg and at the pressurizer were taken from the existing stress report and applied to the model. Impell Corporation performed the deflection/stress analysis of the thermal stratification events using an ANSYS model similar to that used by B&W for Oconee Unit 1. The Davis-Besse surge line met the stress criteria of 3Sm limits of USA Standard B31.7, Subsection 1-705 Equation 12.

B&W performed the fatigue evaluation utilizing the output of the Impell analysis. The total fatigue usage factors were calculated by B&W for the surgeline, hot leg nozzle, and pressurizer nozzle. The usage factors due to thermal stratification during all heatup-cooldown cycles, including those which occurred in the past, were combined with those from the stress analysis of record to obtain the total usage factors including thermal stratification effects. The resulting fatigue usage factors for the total of 40 heatup and cooldown cycles projected through the end of current Fuel Cycle Six are:

Hot leg nozzle (as a branch connection)	0.619
Hot leg nozzle (carbon steel portion)	0.704
Hot leg nozzle (stainless steel portion)	0.343
Surgeline (straight or elbow)	0.063
Pressurizer nozzle (stainless steel portion)	0.297
Pressurizer nozzle (carbon steel portion)	0.634

The above results show that the limiting component for fatigue is the carbon steel portion of the hot leg surgeline nozzle. The 0.704 usage factor for this nozzle is based on 40 heatup-cooldown cycles. Hence, the nozzle (and other parts of the surgeline) can withstand 57 (i.e. 40/0.704) heatup-cooldown cycles without exceeding ASME Code criteria. With only 37 cycles accumulated by Davis-Besse to date, 20 additional cycles remain. These 20 cycles will provide approximately seven additional years of operation at the rate of three cycles per year which Davis-Besse has been experiencing during the past 12 years. Even at the conservatively specified rate of six cycles per year, 3 1/2 years of additional operation are assured.

5.3. Comparison of Analysis Assumptions to Oconee Test Data

Comparative fatigue evaluations have been performed for both Oconee Unit 1 and Davis-Besse, taking into account the temperature measurements from the February 1989 heatup of Oconee Unit 1.

Table 5-2 gives an overview of the temperature differences assumed in the bounding fatigue analyses described in Subsections 5.1 and 5.2, compared to the ones measured during the February 1989 Oconee Unit 1 heatup.

The fatigue results from the bounding analyses have been found to envelope the fatigue using the Oconee Unit 1 temperature measurements for the most critical locations. A description of the fatigue comparison is included in Appendix B of this Document.

Table 5-1. B&WOG Plant Heatups and Cooldowns

Plant	Number of Heatups and Cooldowns	Limiting Number of Heatups[1]
Arkansas Nuclear One	86	135
Crystal River Three	29	135
Davis-Besse	37	57
Oconee One	84	135
Oconee Two	96	135
Oconee Three	66	135
Rancho Seco	35	135

[1]As determined by this evaluation.

Table 5-2. Top to Bottom Temperature Differences
(Temperatures in F)

Bounding Fatigue Analyses: Oconee Unit 1	Bounding Fatigue Analyses: Davis-Besse	Measurements at Oconee Unit 1 (February 1989)
HEATUP:		
422	386	280
330	309	250
330	309	250
330	309	240
-	301	220
		+ 23 additional cycles with temperature differences ranging from 206F to 65F.
COOLDOWN:		
306	309	No full cooldowns have occurred to date.

6. COMPARISON OF PLANT SURGELINES

The factors affecting surgeline performance have been evaluated to assess the potential for thermal stratification in the surgelines of B&W domestic plants as observed at Oconee Unit 1. The evaluation addressed two different types of factors: those that are inherent in the base design and the operating procedures that may influence the surgeline conditions. The following two subsections summarize these evaluations.

6.1. Dimensions, Configuration, and Thermal-Hydraulics

As shown in Figures 6.1 and 6.2 and tabulated in Table 6.1, the domestic B&W plants employ two different surgeline configurations. On the Davis-Besse plant the surgeline has a vertical drop of 7'3" instead of 13' from the hot leg connection elevation to the bottom of the surgeline. Davis-Besse's surgeline has a long horizontal run from the hot leg before turning downward to the low point of the surgeline. Hence, the overall run of pipe that constitutes the surgeline at Davis-Besse is essentially divided into two horizontal runs by the 7' vertical section. In the lowered loop plant configuration the single significant vertical run of pipe is very near the hot leg. The resulting short horizontal section entering the hot leg is only 21" long.

The surgeline for each configuration is 10" schedule 140 stainless steel pipe (inside diameter 8.75") with a wall thickness of 1". The surgeline is insulated, but not identically, at each plant. Table 6.2 summarizes some key insulation data for the plants.

The number and type of surgeline supports and restraints varies from plant to plant. Davis-Besse has several pipe whip restraints with surgeline mountings. These differences influence the heat losses from the surgeline (because of interruptions or discontinuities in the insulation), and the structural evaluations that must consider the effects of surgeline displacements.

The surgeline hydraulic conditions are similar from plant to plant, but vary significantly depending on the plant's operating mode and evolutions or upsets in progress. Each B&W plant is controlled to approximately 2155 psig which requires a saturation temperature in the pressurizer of about 647 F. Hot leg temperatures at full power vary a few degrees from plant to plant, but are all between 600 and 605 F. Therefore, the typical pressurizer to hot leg temperature differential is about 50 F. During normal power operation, the surgeline is exposed to very small flow rates from the pressurizer to the hot leg. This flow, provided by the pressurizer spray bypass line, is approximately 1.5 gpm and serves to minimize thermal cycling on the spray line and to promote chemical equilibrium in the pressurizer. Continuation of this flow from the pressurizer into the surgeline provides a steady heat input for warming the line. However, long transport times in the large line and heat losses through the insulation result in establishment of an equilibrium stratified condition in the absence of flow transients in the surgeline. The 1.5 gpm bypass spray flow has been utilized since plant startup and is a generic value. Small deviations can exist from plant to plant because of the accuracy involved in setting the needle valve that controls this flow. The bypass flow rate is also a function of the running reactor coolant pump combination. If both pumps are running in the loop connected to the pressurizer, the bypass flow is at or near the nominal value. With either of these pumps secured, the bypass flow is diminished. If neither pump in the pressurizer loop is running, the spray bypass flow may be near zero. The vast majority of plant operations involve running four pumps. Operation at power is not permitted with two pumps out of service in the same loop.

The frequency and magnitude of upsets is similar for the lowered and raised loop plants. Depending on the sequence of events, insurges or outsurges may take place that impose moderate to high flow rates through the surgelines. Table 6.3 summarizes the range of anticipated flow conditions that may occur for both raised and lowered loop plants. The values shown for purposes of illustration are arbitrarily based upon surgeline conditions at hot, full power.

In the 1.5 gpm bypass flow condition the velocity through the line is quite low, but the fluid displaced from the pressurizer by the bypass flow provides the heat source necessary to support long term stratification. Preliminary results from the Oconee test program confirm that stratification does occur in this mode of operation as well as in others where the surge flow rate is higher. The Oconee test data shows that during power operation the water leaving the pressurizer surge nozzle is approximately 590 to 600 F. This suggests that the water in the lower part of the pressurizer is below the saturation temperature even if some allowance is made to account for errors in the measurement. This is because the lower most pressurizer heaters are about 52 inches (Ref. 10 and 11) above the bottom of the pressurizer.[2] The upper part of the surgeline remains near this temperature in all horizontal sections of the line while the lower part of the surgeline may be significantly cooler depending on the plant conditions. Figure 6.3 displays a typical set of data at power for Oconee Unit 1. Top-to-bottom delta T is between 40 and 70 F in the lower horizontal piping sections. Figure 6.4 shows a typical top-to-bottom temperature profile at two horizontal sections of the surgeline. These data suggest the temperature gradient in the surgeline is relatively linear during normal power operation. A sharp temperature gradient is not discernible.

When an upset occurs that causes a large insurge or outsurge, the stratified conditions are swept out and the line becomes isothermal. This process imposes a thermal transient on the surgeline. The surgeline volume for the lowered loop plants is about 20 ft^3 (23 ft^3 at Davis-Besse). A pressurizer level change of about 6 to 8 inches is sufficient to displace the surgeline fluid. Once steady state conditions are reestablished in the reactor coolant system, even if at a new operating condition, the surgeline will restratify

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[2]Further evidence that the pressurizer liquid is stratified during equilibrium conditions with only the 1.5 gpm bypass spray flow is the Oconee data taken at hot zero power with full spray (278 gpm) for an extended period. In this higher flow condition, the top of the pressurizer surge line reached about 640^{O}F, very near saturation temperature.

and come to a new equilibrium condition assuming the bypass spray flow is operational.

The above information and mechanisms are applicable to each of the plants and the thermal conditions are expected to be quite similar. Davis-Besse's unique configuration lends itself to somewhat different overall stratification conditions. The vertical rise near the center of the surgeline will reduce somewhat downstream transient thermal gradients resulting from a transient flow condition involving a temperature change in the flow field. This is particularly true if the surgeline is near isothermal conditions when the surge transient occurs. This statement is based on tests done in a laboratory environment with an inverted loop and water as the test medium (reference 1). The Oconee data also support the effectiveness of the vertical run in reducing transmission of stratification gradients. During quiescent periods with significant stratification in the horizontal runs, the vertical rise at Oconee shows very small temperature differences between the three thermocouples located at the two measurement planes (refer to location #4 data on Figure 6.3). As a result, the short (21") horizontal run between the vertical section and the hot leg will experience a smaller degree of stratification than the lower horizontal section during transient outsurges. During steady state conditions only the lower horizontal run at Oconee will tend to stratify. Davis-Besse is expected to demonstrate similar behavior. A primary difference between the Oconee and Davis-Besse configurations is that at Oconee the upper horizontal piping is quite short and appears entirely mixed by the effects of the hot leg flow. This eliminates stratification at Oconee as evidenced by the data. However, Davis-Besse's relatively long upper horizontal run is not expected to be as strongly influenced by hot leg flow. Some stratification should occur in this part of the surgeline, although to a lesser degree than in the lower horizontal run.

The main conclusions at this point are that:

1. Davis-Besse's thermal stratification is expected to be of similar magnitude to that observed at Oconee.

2. Each of the lowered loop plant surgelines are nearly identical configurations and should have similar, if not identical, thermal-hydraulic conditions during normal power operation.

Related to the second conclusion, the similarity of conditions in the surgeline during shutdown operation is a function of the operational evolutions performed with the plant shutdown. The next subsection addresses this point.

6.2. Operating Procedures

Operational evolutions have a significant impact on the steady state and transient thermal conditions experienced in the surgeline. The Oconee data shows that evolutions which affect the inventory control in the RCS have the most influence. Pressurizer level changes are good indicators of transients in the surgeline. Since the operational evolutions are controlled by procedure, plant to plant procedural differences could have a strong bearing on the surgeline transients and conditions experienced during plant heatup and cooldown.

Peak thermal stratification is expected during the initial pressurization of the reactor coolant system (plant heatup). All of the plants first establish a steam bubble in the pressurizer and then increase system pressure by energizing the pressurizer heaters. The heaters have virtually no influence on the temperature of the rest of the system. Thus, the temperature difference between the pressurizer and reactor coolant system increases as the system is pressurized. During these early parts of the heatup procedure, the surgeline conditions are determined by the pressurizer temperature control (which is manual), reactor coolant system inventory control (primarily by the makeup and letdown systems), and auxiliary spray from the decay heat removal system if it has been in service. With a moderate outsurge from the pressurizer the surgeline stratification will be greater than it is during any other operating procedure.

Surgeline stratification is determined by the following three factors:

1. Surge flow rate
2. Cooling of the surgeline (ambient losses)
3. Surgeline boundary conditions (hot leg and pressurizer temperatures)

The degree of stratification is dependent on the direction of the surge and its magnitude. Outsurges of the hotter pressurizer fluid result in greater stratification than does an insurge. This was observed at Oconee. As discussed earlier, a large surge will flush the surgeline decreasing thermal stratification. Initial review of the Oconee data shows that the surgeline bottom temperatures are lower than the reactor coolant hot leg temperature by up to about 50 F when the plant is at power. This temperature difference depends on the quality of the installed insulation. As shown in Table 6.2, Oconee Unit 1's surgeline insulation type and thickness is representative of the insulation installed at other B&W plants.

There are operating restrictions that limit the maximum pressurizer to hot leg temperature differential. During heatup and cooldown, when the temperature differences are largest, the reactor vessel pressure/temperature curves limit the pressure for low temperature reactor coolant system operation. Since the pressure is controlled by the pressurizer temperature, the maximum pressurizer temperature is indirectly limited by this limit on RC pressure. Table 6.4 provides representative values for these limits from one B&W-designed plant. As the plants age, the pressure limits may be lowered. Industry activities are attempting to relax these limits in order to allow higher pressure limits which would simplify operation of the plants. There are other operational limits that bear on the typical differences between pressurizer temperature and RC loop temperature. However, the P/T limits provide a representative bound.

A preliminary comparison of operating plant procedures has been completed with the focal point being those evolutions encountered during the initial pressurization and heatup of the reactor coolant system. Each plant's controlling procedure for plant heatup from cold shutdown to hot shutdown was reviewed. For each evolution that has a potential impact on the surgeline, the approximate coolant system pressure and temperature were estimated and the loop to pressurizer temperature differential was calculated as a gauge of the temperature extremes that the surgeline could experience at its end points. Before reactor coolant pumps are started, there is no pressurizer spray line pressure differential to cause normal spray flow.

Tables 6.5 through 6.9 list the specific steps involved in the plant heatup for several stations. The tabulated values for coolant temperature and pressure are approximate. However, the Oconee data shows that the estimated temperature differentials correlate well with the observed peaks in stratification. The evaluation shows that there are similarities in the plant evolutions for startup although some differences exist. Based on this evaluation, the plants should experience similar surgeline transients in regard to frequency and the magnitude of the temperature differences that might exist in the surgeline.

This comparison shows that the units are operated similarly enough that gross differences should not exist from one plant to another. A detailed evaluation of the surgeline transients as they were noted during the Oconee measurement program will be included in the final evaluation of the structural effects of surgeline stratification as part of the effort for Bulletin item 1.d. This will include a more detailed comparison of plant specific procedures.

Table 6-1. Surgeline Dimensions

Plant	Reference	Section Identifier A	B	C	D	E
Oconee 1	2	21 13/32"	12'3"	25'7"	8'-3 11/32"	14 61/64"
Oconee 2	3	21 13/32"	12'3"	25'7"	8'-3 11/32"	14 61/64"
CR 3	4	21 13/32"	12'3"	26'1"	8' - 21/32"	14 61/64"
ANO-1	5	21 13/32"	12'3"	25'7"	8' - 21/32"	14 15/16"
Oconee 3	6	21 13/32"	12'3"	25'7"	8'-3 11/32"	14 61/64"
Rancho Seco	7	21 13/32"	12'3"	26'1"	8'-3 11/32"	14 61/64"

Plant	Refer.	A	B	C	D	E	F
Davis-Besse 1	8	3' 9/16"	21'	7'-2 13/16"	16'9'	4'-11 15/16"	1'3"

Note: 1. Refer to Figures 6.1 and 6.2 for the pipe section identifiers used in this table.

Table 6-2. Insulation Comparison

Plant	Installer	Type	Thickness
Oconee Units 1, 2, 3	Diamond Power	Reflective	3 inches
Arkansas Unit 1	Transco	Reflective	3 inches
Crystal River Unit 3	Transco	Reflective	3-1/2 inches
Davis-Besse Unit 1	Diamond Power	Reflective	3 inches
Rancho Seco	Diamond Power	Reflective	4 inches

Table 6-3. Surgeline Hydraulic Conditions

	177FA Plants 8.75" ID Plant Condition		
	Bypass Spray	Full Spray[1]	Mild Upset
Flow, lbm/s	0.12	15.66	500
Velocity, ft/s	<0.1	1.01	32.4
Reynolds No.	4E3	5.3E5	1.7E7

[1]Corresponds to 190 gpm. On the Oconee units this full spray flow rate is approximately 280 gpm.

Table 6-4. Limiting Loop to Pressurizer Temperatures for a Representative B&W-Designed Plant

Crystal River Unit 3			
RC Temp	RC Press	Pzr Temp	Pzr RC Temp
°F	psig	°F	°F
70	300	422	352
85			
157	300	422	265
175			
235			
236	528	476	240
377	2250	654	277
378			

Notes:

1. Data taken from reference 9 as tabulated on figures providing pressure/ temperature limits.

2. The above values of the temperature difference between the pressurizer and loop are upper bounds. Other plant limits and operating procedures provide even lower limits to this temperature difference. The above tabulation provides an easy way to demonstrate that there are practical limits on the magnitude of this temperature difference.

TABLE 6.5

HEATUP PROCEDURES AFFECTING THE SURGE LINE

PLANT: Davis-Besse Unit 1

PROCEDURE: DB-OP-06901 Rev 00 Plant Startup

LIMITS AND PRECAUTIONS		PROCEDURE REF
HEATUP RATE LIMIT :	0.83 °F/min (50 °F/hr)	
TEMP. DIF. BETWEEN PZR & RC ALLOW BY PT LIMITS	384 for 5 EFPY	(TS Fig 3.4.2)
MAX ALLOWABLE DEL T FOR PZR SPRAY USE:	250 °F unless reqd for plant safety	(4.2.7c)
	410 °F for all circumstances	(4.2.7b)

PROCEDURE	RC TEMP (°F)	RC PRESS (psig)	PZR TEMP (°F)	DEL T (°F)
4.3.6 Fill RCS and Vent w/ PZR level~80"	100	30	100	0
4.3.14 Check Chemistry	100	30	100	0
4.4.16 Establ vacuum in cond and MS lines	100	30	100	0
4.4.46 Establish Steam Bubble	100	75	321	221
4.4.47 Use PZR heaters to maintian~75 psig	125	75	321	196
4.4.57 Take PZR Chemistry Sample	125	75	321	196
4.4.60 Increase RC Pressure to 125 psig	150	125	353	203
4.4.65 Vent if Required	150	200	388	238
4.4.66 Cycle Pumps	150	210	392	242
5.4.17&5.4.18 SG Evacuation	180	210	392	212
5.4.17a. Draw OTSG Vacuum	180	210	392	212
5.4.17c. Maintain Condenser Pressure < 10 in	180	210	392	212
5.4.18 When TBVs open-throttle to 5% demand	180	210	392	212
5.4.22c. Start 2 RCPs in the same loop	180	210	392	212
5.4.22g. Secure the Decay Heat System	200	210	392	192
5.4.40d Verify AVV in AUTO,Close TBV & Place AUTO	250	225	397	147
5.4.47 Heatup to 280°F Remaining below 266 psig	280	266	411	131
5.4.51 Start the third RCP	280	370	440	160
5.4.55 RCS>352°F-PZR lvl limit of 85" lifted	352	600	489	137
5.4.67 Slowly Open TBVs to hold RC Temp	400	1725	616	216
5.4.72 Increase RC Pressure to 2000 psig	500	2000	637	137
5.4.75 Continue Heatup by closing TBVs	500	2000	637	137
5.4.77 Place PZR Heaters in AUTO	500	2000	637	137
5.4.84 Start 4th RCP	500	2000	637	137
5.4.98 Verify Hot Shutdown Conditions	530	2155	648	118

TABLE 6.6

HEATUP PROCEDURES AFFECTING THE SURGE LINE

PLANT:	Rancho Seco Unit 1		
PROCEDURE:	Rev.47,WP2153P,D-0052P,B.2	Plant Startup	
LIMITS AND PRECAUTIONS			PROCEDURE REF
HEATUP RATE LIMIT :	100°F/hr		(TS Fig.3.1.2-1)
TEMP. DIF. BETWEEN PZR & RC ALLOW BY PT LIMITS	365°F		(TS Fig.3.1.2-1)
MAX ALLOWABLE DEL T FOR PZR SPRAY USE:	410°F	250°F except for plant safety	(Rev.23,WP0722P,D-0013P)

	PROCEDURE	RC TEMP (°F)	RC PRESS (psig)	PZR TEMP (°F)	DEL T (°F)
4.2.16	Place RC Sampling System in Service	100	30	100	0
4.2.25	Complete RC filling and venting	100	30	277	177
4.2.26	Verify PZR Chemistry	100	30	277	177
4.2.26.1	Form PZR Steam Bubble	125	150	366	241
4.2.31	Increase to the needed NPSH for pump cycling	125	210	392	267
4.2.32	Adjust DH System to maintain RCS Temp<150°F	125	210	392	267
4.2.33	Cycle Pumps	125	210	392	267
4.2.34	Complete RCS Venting	150	210	392	242
4.3.6	Increase to the needed NPSH	150	210	392	242
4.3.7	Start first RC Pump	150	210	392	242
4.3.8	Start second RC Pump	160	210	392	232
4.3.14	Verify Chemistry	190	210	392	202
4.3.15	Secure the DHS	200	215	393	193
4.3.24	Stabilize RC Temp w/ TBVs	210	300	421	211
4.3.25	Increase RC Pressure to above 400 psig	210	450	459	249
4.3.27	Start the third RC Pump	220	475	465	245
4.3.28	Throttle the TBVs for desired heat-up rate	220	500	470	250
4.3.36	Stab RC Pres. w/PZR heaters and man.Trip RX	400	1700	614	214
4.3.36.9	Stabilize RC Temperature w/ TBVs	430	1925	632	202
4.3.37	Place PZR Spray Valve in AUTO	440	1950	633	193
4.3.38	Place PZR Heaters in AUTO	460	2155	648	188
4.3.43	Start the fourth RC Pump	500	2155	648	148
4.3.49.1	Place TBVs in AUTO w/ 885 psig setpoint	532	2155	648	116
4.3.50	Verify Hot Shutdown	532	2155	648	116

TABLE 6.7

HEATUP PROCEDURES AFFECTING THE SURGE LINE

PLANT:	Crystal River Unit 3		
PROCEDURE:	OP-202, Rev. 79	Plant Startup	
LIMITS AND PRECAUTIONS			PROCEDURE REF
HEATUP RATE LIMIT :	100°F/hour		(4.1)
TEMP. DIF. BETWEEN PZR & RC ALLOW BY PT LIMITS	352°F for 8 EFPY		(OP-103B, Curve 5)
MAX ALLOWABLE DEL T FOR PZR SPRAY USE:	250°F unless required for plant safety		(4.16)

	PROCEDURE	RC TEMP (°F)	RC PRESS (psig)	PZR TEMP (°F)	DEL T (°F)
5.2.27	Form PZR Steam Bubble	100	150	366	266
5.3.7	Check Chemistry	100	150	366	266
5.3.24	Increase RC Pres and Maintain at 215 psig	110	215	393	283
5.3.42	Start two RCPs in one loop (Referto OP-302)	125	220	395	270
5.3.42	Stop DH Pump and secure DHS	125	220	395	270
5.3.44	Verify RC Pressure is ~320 psig	150	320	427	277
5.3.44	Start third RCP	150	320	427	277
5.3.46	Vent RCS if Required	175	350	435	260
5.4.1	Increase RCS Temp to 240°F	240	400	448	208
5.4.3	Increase RCS Pressure to ~460 psig	240	460	461	221
5.4.4	Chemistry Sample of RCS	240	460	461	221
5.4.9	Increase RCS Temp to 270°F	270	550	479	209
5.5.4	Test PORV	300	650	497	197
5.5.10	Stop heatup w/ TBVs manually and Trip Reactor	400	1650	610	210
5.5.17	Place PZR Heaters in Automatic	450	1850	626	176
5.5.22	Start fourth RC Pump	500	1850	626	126
5.5.25	Cont Heatup to 532°F and verify hot shutdown	532	2155	648	116

TABLE 6.8

HEATUP PROCEDURES AFFECTING THE SURGE LINE

PLANT:	Oconee Nuclear Units 1, 2, and 3		
PROCEDURE:	OP/1/A/1102/01		
LIMITS AND PRECAUTIONS			PROCEDURE REF
HEATUP RATE LIMIT :	T<280 - 45 °F/hr	T>280 - 90°F/hr	(ENCL 4.12)
TEMP. DIF. BETWEEN PZR & RC ALLOW BY PT LIMITS	368 °F for 15 EFPY		(ENCL 4.12)
MAX ALLOWABLE DEL T FOR PZR SPRAY USE:	250 °F unless required for plant safety		(2.12)

PROCEDURE	RC TEMP (°F)	RC PRESS (psig	PZR TEMP (°F)	DEL T (°F)
OP/1/A/1102/01 Enclosure 4.1 From Cold Shutdown to 250°F and 350 psig				
2.1 Form Steam Bubble	100	45	295	195
2.1 Throttle DHS to obtain RC Temp=150	150	45	295	145
2.1 Isolate Aux. PZR Spray	150	45	295	145
2.2 PORV Test	150	45	295	145
2.3 Vent RCS and CRDMs	150	60	309	159
2.5 Increase to NPSH	150	350	435	285
2.5 Throttle DHS to obtain RC Temp=180	180	375	442	262
2.5 Establish OTSG Vacuum	180	375	442	262
2.6 Cycle RCPs and leave one running	180	375	442	262
2.6.3 Increase PZR to normal operating Level	180	375	442	262
2.7.2 Close DHS & Open TBVs to maintain 190F	190	403	449	259
2.8 Verify that 250 °F and 350 psig req-ments	250	350	435	185
2.9 Check Chemistry	250	350	435	185
OP/1/A/1102/01 Enclosure 4.2 From 250°F and 350 psig to Hot Shutdown				
2.4 Increase RC Temp from 250 to 325 °F	325	375	442	117
2.4 Start second and third RCPs	325	375	442	117
2.5 Check Chemistry	325	375	442	117
2.5 Perform Core Flood "Burp"	350	400	448	98
2.6 Start 4th RCP	375	800	520	145
2.6.3 If reqd. by chem. spray PZR for 4-6 hrs	400	900	534	134
2.6.9 Place TBVs in AUTO w/ 885 psig setpoint	525	2155	648	123
2.7.8 Place PZR spray and heater in AUTOMATIC	525	2155	648	123
2.8 Check Chemistry	525	2155	648	123
2.8 Verify Hot Shutdown Conditions	525	2155	648	123

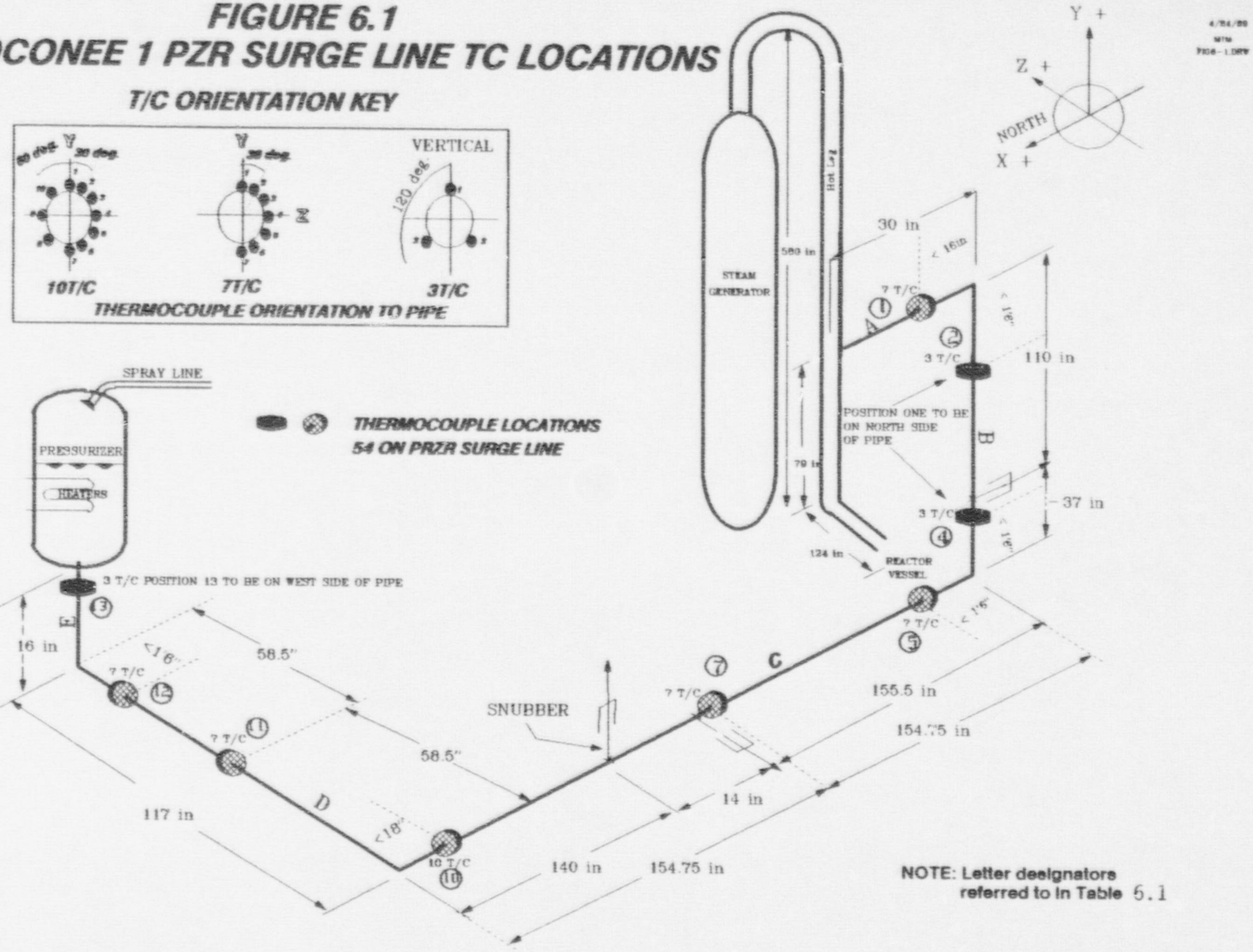
TABLE 6.9

HEATUP PROCEDURES AFFECTING THE SURGE LINE

PLANT:	Arkansas Nuclear One Unit 1		
PROCEDURE:	1102.02 Rev.42	Plant Startup	
LIMITS AND PRECAUTIONS			PROCEDURE REF
HEATUP RATE LIMIT :	1.67°F/min (100°F/hr)		(Attachment A)
TEMP. DIF. BETWEEN PZR & RC ALLOW BY PT LIMITS	326 for 15 EFPY		(Attachment A)
TEMP. DIF. BETWEEN PZR & RC ALLOWED	430°F		(Proc.1103.005 Rev17 5.2)
MAX ALLOWABLE DEL T FOR PZR SPRAY USE:	410°F		(Proc.1103.005 Rev17 5.2)

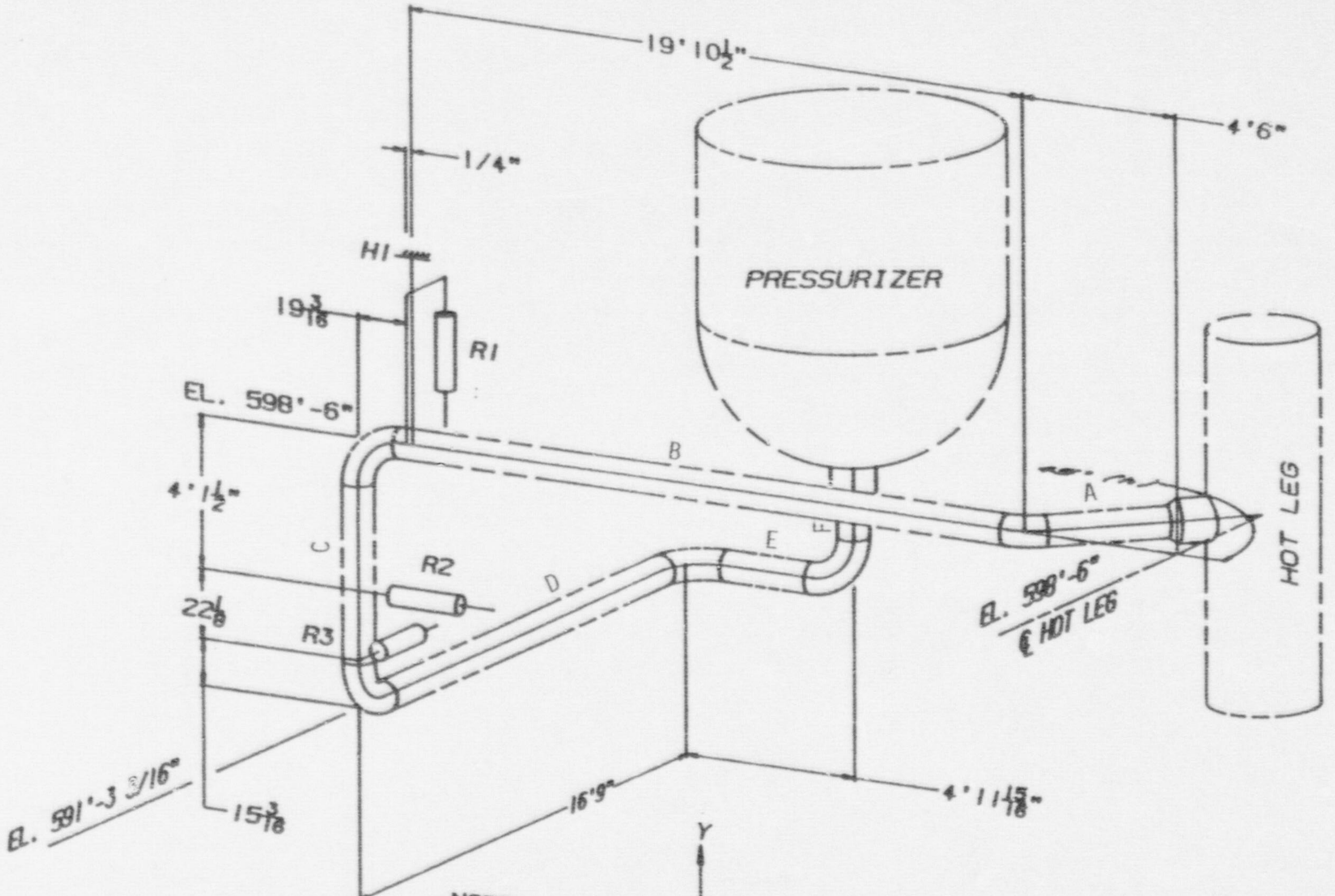
	PROCEDURE	RC TEMP (°F)	RC PRESS (psig)	PZR TEMP (°F)	DEL T (°F)
8.2.4	Fill and Vent RCS	100	30	100	0
9.6	Establish Steam Bubble	100	150	366	266
9.7	Test ERV	100	150	366	266
9.10	Increase to NPSH	150	225	397	247
9.15	Verify PZR level being controlled ~86"	150	225	397	247
10.2.5	Establ Vacuum w/MSIV foreseat drains	150	225	397	247
10.2.6	Verify TBVs are closed	150	275	414	264
10.3	When condenser vacuum~22",transfer pressure control from ADVs to TBVs	160	300	421	261
10.5.1	Start first RC Pump	170	325	429	259
10.5.2	Start 2nd RC Pump in the same loop as 1st	180	350	435	255
10.5.3	Stop DH Pump(s)	190	350	435	245
10.7	Sample and Establish RCS Chemistry	200	375	442	242
11.3	Adjust TBVs to maintain at 270°F	270	375	442	172
12.8	Operate PZR heaters & spray to control press	280	400	448	168
12.11	Start third RC Pump	330	440	457	127
12.12	Open TBVs to keep heatup rate <100°F	330	440	457	127
14.5	Adjust TBVs to maintain at 435°F	435	1400	588	153
14.6.5	Pressurize to 1750 psig while maintain. 435°F	435	1750	618	183
14.7	Place PZR Spray and Heaters in AUTO	465	2150	647	182
15.3	Start the 4th RC Pump	500	2150	647	147
15.3.2	Maintain PZR Level per procedure attach C	532	2150	647	115
15.14	Verify Hot Shutdown Conditions	532	2150	647	115

FIGURE 6.1
OCONEE 1 PZR SURGE LINE TC LOCATIONS



NOTE: Letter designators referred to in Table 6.1

FIGURE 6.2



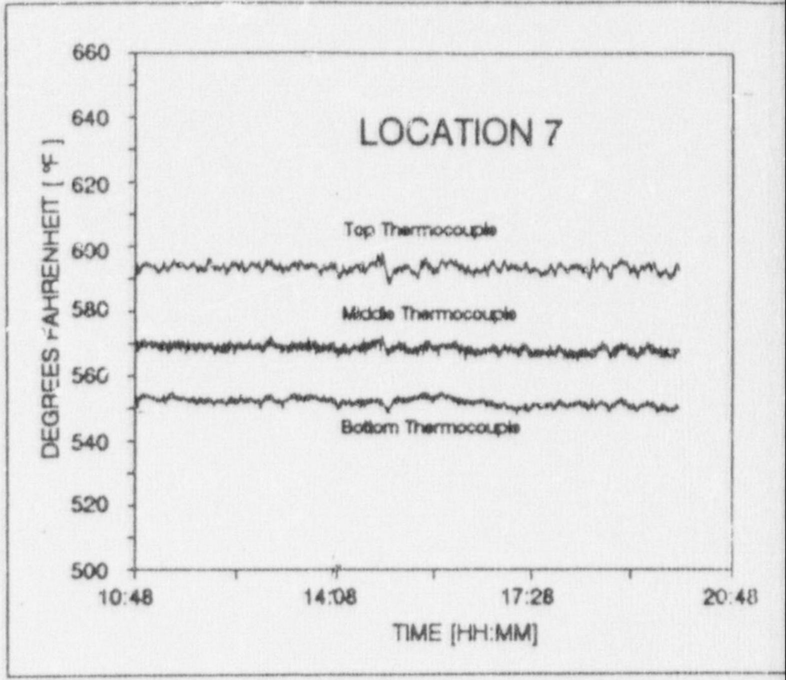
NOTE: Letter designators referred to in. Table 6.1

SURGE LINE

TOLEDO EDISON DAVIS BESSE UNIT I

OCONEE UNIT

FUL



PRESSURIZER

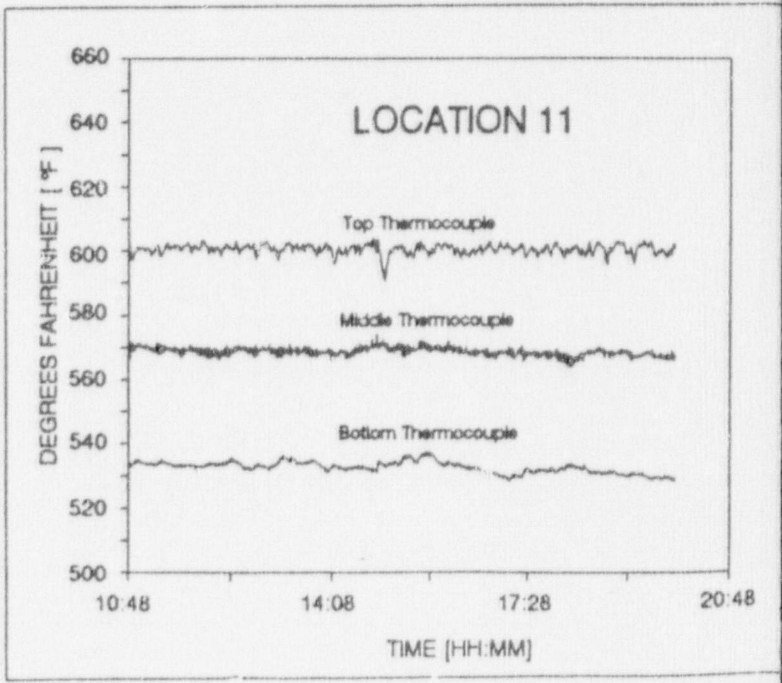
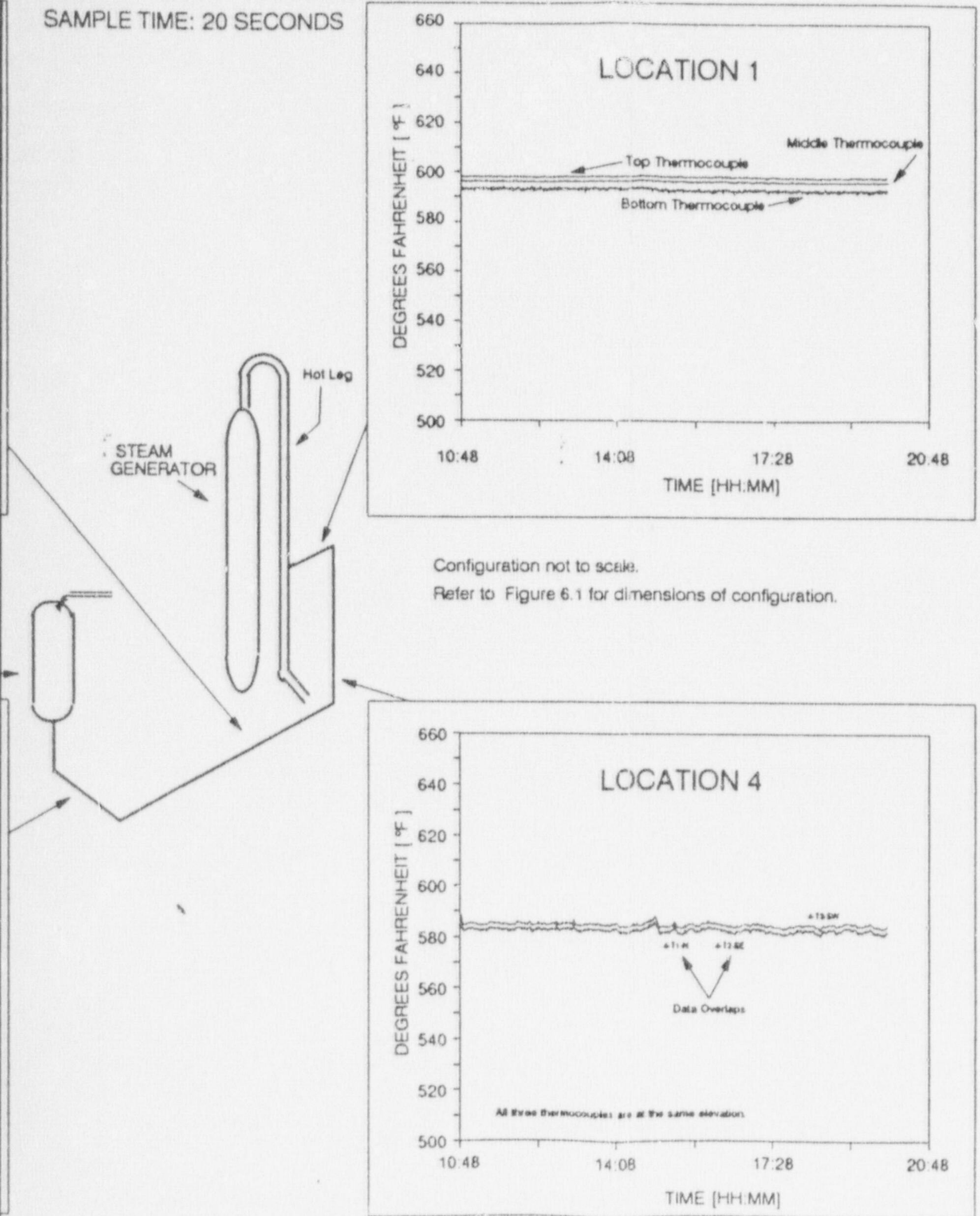


FIGURE 6.3

1 SURGE LINE STRATIFICATION DATA

POWER OPERATION TEST DATE: 2/21/89

SAMPLE TIME: 20 SECONDS



Configuration not to scale.

Refer to Figure 6.1 for dimensions of configuration.

4/12/89

SI APERTURE CARD

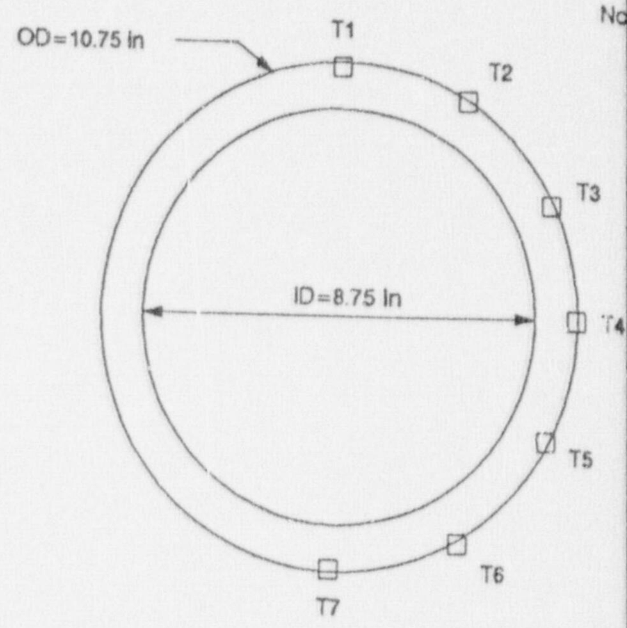
Also Available On Aperture Card

8905310161-01

OCONEE UNIT

FULL

No



SECTION A-A

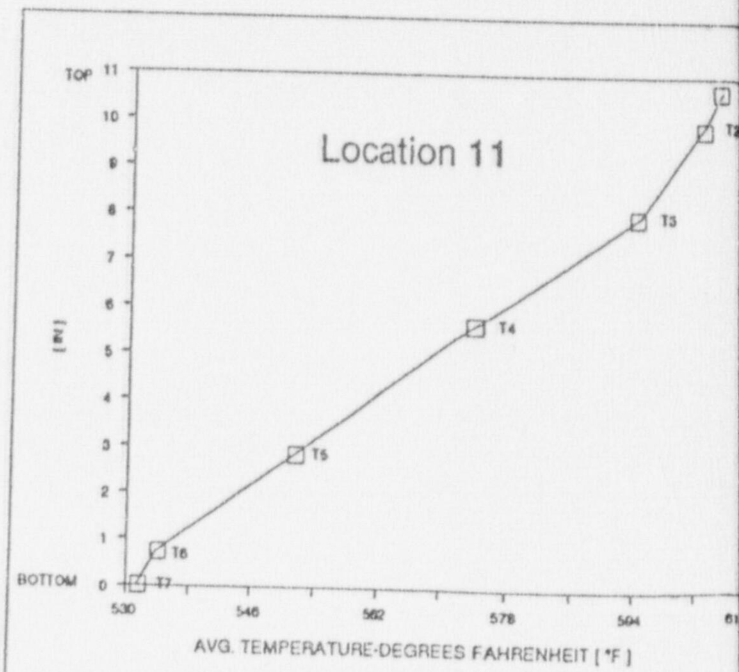
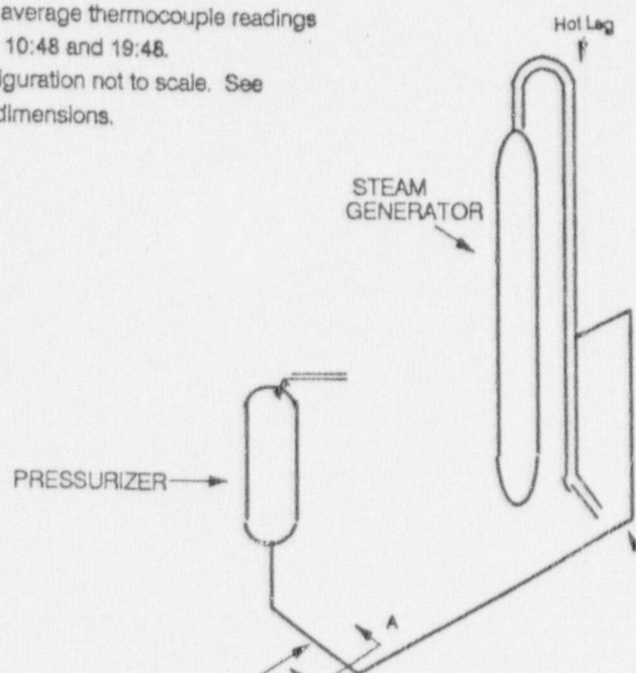


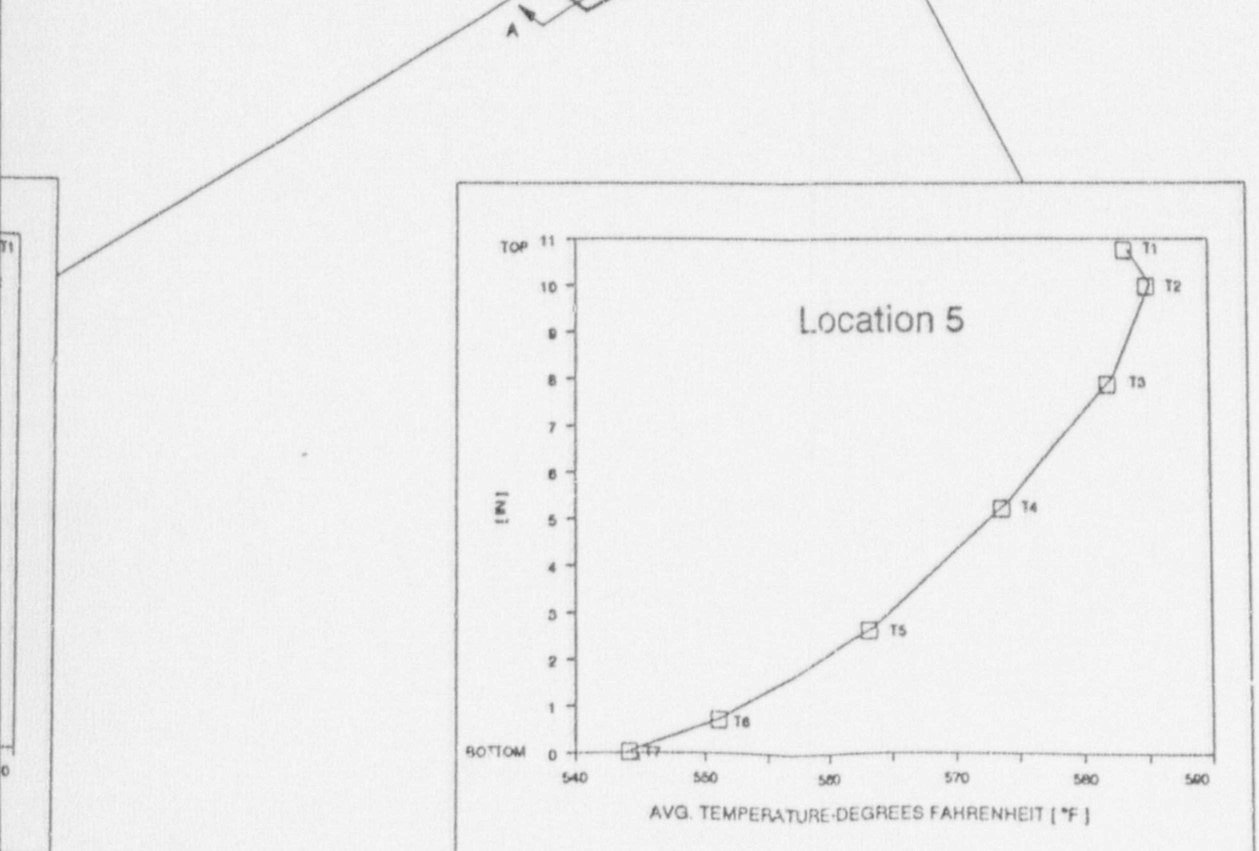
FIGURE 6.4

1 SURGE LINE STRATIFICATION DATA

POWER OPERATION TEST DATE: 2/21/89

tes: 1. Data points are average thermocouple readings taken between 10:48 and 19:48.
2. Surge line configuration not to scale. See Figure 6.1 for dimensions.





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7. THERMAL STRIPING

7.1. Definition

Thermal striping is the localized metal stress caused by repetitive fluctuations of the temperature at a fluid-metal interface. The fluid temperature fluctuations are due to the interactions between forced flow and buoyancy. The buoyant forces tend to stratify the fluid, obtaining vertical segregation (in a horizontal flow component) by temperature and density. The fluid shear forces associated with forced convection and fluid viscosity, on the other hand, tend to mix the fluid. The combination of these effects can generate undulations of the fluid-fluid interface, resulting in thermal striping. The existence and characteristics of these fluctuations depend primarily on the simultaneous occurrence of buoyant forces and fluid shear forces which are of comparable magnitude. The fluctuations are also responsive to the flow geometry; for example, they may be greatly amplified by the helical, secondary fluid motion induced by axial flow in a pipe bend. Finally, the fluctuations of the pipe surface temperature are directly responsive to the interactions between convective and conductive heat transfer at the fluid-pipe interface.

The basic concern associated with thermal striping is that it is a mechanism for crack initiation.

7.2. B&W Owners Group Program

The objective of the B&WOG program is to thoroughly evaluate and quantify the effects of thermal striping on the integrity of the surgeline. In order to resolve the surgeline thermal striping issue, a multi-element plan has been initiated. An important element in the plan is the measurement program at Oconee Unit 1. Thermocouples were installed around the surgeline outside circumference at several locations during the measurement program at Oconee Unit 1 (see Appendix A). These thermocouples were intended to measure the temperature distribution in the surgeline during stratification. The data from the measurement program are currently being evaluated. In order to detect thermal striping by temperature measurements on the outside of a pipe, the temperature oscillations must have both a large amplitude and a long period. Assessments performed to date on the Oconee test data have not detected the thermal striping phenomenon. This conclusion is supported by results from the thermal striping literature survey which show that the predominant striping frequencies are too high to be detected on the outside surface. In any case, thermal striping of some magnitude could have occurred without being detected by outside thermocouples.

The B&W Owners Group program has five basic elements that will contribute to resolution of the thermal striping issue. These are:

1. Evaluation of Surgeline thermal-hydraulic conditions

2. Surgeline Pipe Wall Heat Transfer Analysis (damping effects of surgeline wall on measured temperatures)

3. Evaluation of Oconee field data

4. Assessment of available industry stratification and striping data

5. Structural analysis of striping effects on the surgeline

The first four of the above elements serve to develop and justify the thermal striping input for the fifth element. The program is laid out to maximize what can be learned from all possible sources short of performing a laboratory test specifically aimed at striping phenomena. The following paragraphs briefly describe the work to be completed to support a submittal (technical report) to the Staff in October 1989.

7.2.1. Evaluation of Surgeline Hydraulic Mechanisms

The objective of this task is to identify and understand the relevant thermal-hydraulic mechanisms involved in the stratification and striping phenomena of the surgeline. This knowledge will help to ensure that important aspects of these phenomena are accounted for in the final fatigue evaluation of the surgelines.

This task will make use of the open literature and previous experimental work. Code analyses results, as they apply to the surgeline, will be reviewed. Hand or code calculations may be used to assess the impact of important variables that influence the degree of stratification. Variables of interest are likely to include the surgeline insulation characteristics, effects of pressurizer spray bypass flow, localized effects of pipe discontinuities (such as elbows), and surgeline endpoint conditions. This latter item includes the temperature differential between the ends of the surgeline and the various flow conditions that can arise over the range of temperature differences.

7.2.2. Surgeline Pipe Wall Heat Transfer Analysis

This analytical task will provide two basic types of information. The first is the steady state temperature distribution of the inside pipe wall given the outside pipe wall temperatures. The second, and perhaps more critical information it will yield, is an assessment of the transient heat transfer characteristics of the pipe wall. The relationship of inside fluid temperature amplitude and the associated period of oscillation will be investigated. This result will help to determine reasonable upper limits on the thermal oscillations that cause striping. This latter result may not be necessary depending on the results achieved from the survey of industry data (discussed in Subsection 7.2.4).

7.2.3. Evaluation of Oconee Field Data

The data evaluation will follow the data reduction and plotting process currently in progress. Some meaningful relationships and factors probably have yet to be considered, but the kinds of information of interest include maximum and minimum temperatures and temperature differentials for the various plant conditions involved in heating up, operating, and cooling down the RCS. Correlations between different locations in the surgeline will be considered. The correlation of plant operations to observations in the surgeline are important, including the assessment of differences in operating procedures and operator practices between B&W-designed plants that may have an impact on thermal stratification and striping. A by-product of this effort will be the identification of potential changes to operating procedures that can eliminate or reduce stratification or striping.

The Oconee data evaluation in conjunction with the thermal hydraulics evaluation of the surgeline should yield a physical model of the surgeline hydraulics and the processes which caused the Oconee surgeline transients. This is expected to provide insights that will lead to a better overall understanding of the striping and stratification phenomena and measures that can effectively reduce them.

7.2.4. Assessment of Available Industry Data

Literature searches on thermal striping have identified several papers that directly address the thermal-hydraulic conditions that lead to the oscillatory behavior of the fluid interface. These will be reviewed in detail for applicable data and correlations. If the review concludes that it is technically justified to do so, analytical/scaling work will be done to develop striping frequencies and amplitudes appropriate for surgeline conditions. The analysis may conclude that the potential for or existence of thermal striping in the surgeline is nil for certain plant conditions and not others. Such results will be extremely useful in determining the ultimate disposition of the striping issue for the B&W configuration and will be considered in the ultimate input provided to the structural evaluation.

7.2.5. Structural Evaluation of Surgeline for Thermal Striping

The successful disposition of the thermal striping issue is contingent on a demonstration that striping, if it does exist in the surgeline, has a negligible impact on the structural integrity of the pressure boundary. Until the earlier tasks are completed, the extent of this analysis can not be defined. Preliminary results, as discussed in the next section, show that the likelihood of initiating a crack in the surgeline from the thermal striping phenomenon is minimal. A complete accounting of surgeline conditions and transients is planned for the final submittal.

7.3. Preliminary Results

A conservative interim assessment of the cyclic thermal stress due to striping has been made based on information from the available literature and from measurements at Oconee. Results from this striping evaluation show that the fatigue impact on the surgeline is approximately 10 percent of the allowable usage factor. The evaluation shows that temperature differentials in the surgeline with the plant at power are not large enough to affect the usage factor; those existing during the early parts of plant heatup are most significant. The conclusion of this interim work is that potential fatigue effects of thermal striping are not sufficient to cause concern for continued plant operation.

The information in the literature suggests, however, that buoyant effects are important considering the temperature differences and flow ranges of concern to the pressurizer surge line, and therefore, the possibility of striping must be considered.

The following four areas of research provide valuable insight into the process of thermal striping:

- BWR Feedwater Nozzle Tests
- LMFBR Tests (Westinghouse)
- Argonne National Laboratory (ANL) Tests
- Project HDR (FRG) Tests

Based upon the reported ranges of frequencies observed in these experiments, and the proportion of the top-to-bottom temperature differences actually imposed on the wall, a conservative estimate of the pipe wall thermal exposure was made by assuming a single frequency and amplitude of the inside wall thermal fluctuations. The frequency of occurrence of 0.25 Hz and a value of 45% of the imposed fluid temperature differences were selected for the interim analysis. This interim set of characteristics of the striping phenomenon, was chosen to be representative of the available striping information, and to lie on the conservative side of the published ranges of striping amplitudes.

Oconee test data provided an important input to the interim assessment of striping fatigue effects. The data were processed to determine the duration during the plant heatup over which temperature differences of various magnitudes were experienced. Using the interim values extracted from the literature, the frequency and magnitude of the thermal exposure of the pipe wall were determined. From this information, thermal stress ranges were evaluated, and fatigue impact was assessed.

The contents of the published literature leading to this approach are described and the methodology leading to these interim conclusions is discussed in more detail in the following paragraphs.

7.3.1. Assessment of Available Industry Data

The estimated characteristics of surgeline striping are based on the available striping research. This research is described in detail in section 7.3.1.1. The four major areas of research are presented individually in this subsection and are then summarized. The surgeline striping characteristics are defined and discussed in section 7.3.1.2. The conservatisms associated with these assumed characteristics are also addressed.

7.3.1.1. Available Research

The thermal-hydraulic characteristics of thermal striping have been examined in the following four major areas of research:

1. BWR feedwater nozzle tests
2. LMFBR tests (Westinghouse)
3. Argonne National Laboratory (ANL) tests
4. Project HDR (FRG) tests

These programs are summarized below, and are described in more detail in the subsequent paragraphs.

The BWR feedwater nozzle studies were extensive, but the geometry of interest was quite unlike that of the pressurizer surgeline. The BWR studies did demonstrate the ability to combine low-temperature data with high-temperature data and with plant striping data, by suitably adjusting the low-temperature results. The application of the BWR feedwater nozzle results illustrated the use of probability density functions. Thermal fluctuations were analyzed to determine the frequency of occurrence of cycles having discrete ranges of amplitudes. These incremental-amplitude analyses were carried through the nozzle stress analysis by introducing plant time-at-conditions data. These BWR feedwater nozzle studies are described in detail later in this section.

Woodward examined the fluid temperature fluctuations in transparent horizontal pipes. The amplitudes of the near-wall fluid temperature fluctuations reached 60% of the imposed fluid temperature difference, with most of the cycles exhibiting amplitudes of 10 to 35%. The frequency of fluctuations ranged from 0.1 to 0.5 Hz. A film heat transfer coefficient was needed to determine the wall thermal fluctuations from those of the near-wall fluid. Woodward referred to the studies of Fujimoto et al. Fujimoto et al also studied striping in a transparent horizontal pipe. A fluid density difference was imposed by adding calcium chloride to the warmer fluid stream. Thin squares of copper were used to measure wall striping. The striping amplitude was less than 10% of the imposed temperature difference (the temperature difference between the interacting hot and cold fluid streams). The film heat transfer coefficient was 1.25 to 7 times the Dittus-Boelter coefficient. The studies of Woodward, and of Fujimoto et al, are described in detail after this section.

Kasza et al at ANL have tested extensively thermal stratification and striping in transparent horizontal piping with bends in both the vertical and horizontal planes. Based on a limited amount of published power-spectral-density information, the higher-amplitude fluctuations occurred at lower frequencies, 0.1 to 0.6 Hz, with amplitudes of 30 to 40% of the imposed temperature difference. These ANL studies are outlined later in this section.

Wolf et al, in the TEMR test series of Project HDR, measured striping in metal, horizontal pipes at plant-typical temperatures. The complete results of these tests have not been published, however. Typical striping frequencies were 0.1 to 10 Hz. The amplitudes of the wall temperature fluctuations were generally from 10 to 40% of the imposed fluid temperature difference, with peak amplitudes from 25 to 50%. Examining the single test presented in the published results (of the nine "PWR" tests), the maximum striping amplitude was approximately 30% and the frequency of occurrence of the larger fluctuations was approximately 0.2 Hz. Wolf et al noted the interactions between convective and conductive effects, and hence the difficulty of extrapolating to a plant the results of tests performed using a transparent model. They also noted the insensitivity of temperatures measured at the outside of a metal pipe to inside interactions. The TEMR tests are described later in this section.

BWR Feedwater Nozzles Tests

The BWR feedwater nozzle configuration was examined in relation to observed feedwater line cracks. The thermal striping of this configuration has been obtained from two test facilities, Two-Temperature and Moss Landing, as well as from plant measurements.[12] The Two-Temperature Test Facility was limited to atmospheric pressure; hot and cold fluid temperatures of 160 and 70F were used. The Moss Landing Test Facility, on the other hand, achieved plant-typical temperatures. The results of the two test facilities were combined with plant data by adjusting the Two-Temperature results to account for the changes of fluid density, viscosity, and thermal conductivity between the test and reactor conditions.

The composite data was processed to obtain the number of cycles having discrete ranges of stress amplitudes. These amplitude ranges, or windows, were prescribed to be relatively small at the higher amplitudes, and up to 20% wide at the smallest amplitudes. The results of this analysis were presented in tabular form.[12] This data has been restated in terms of windows of equal amplitudes, 10%, and plotted in Figure 7.1. The frequency of occurrence decreases rapidly and regularly up to a stress amplitude of 50% of the maximum stress, and then more slowly at the higher amplitudes. Although most of the fluctuations had low amplitudes, approximately 1% of the metal temperature fluctuations obtained stress amplitudes approaching the maximum stress.

It is estimated that the observed amplitudes of the near-wall fluid temperature fluctuations were reduced by one-half to obtain the amplitudes of the wall temperature fluctuations and hence the wall stress amplitudes. The maximum stress of Figure 7.1 thus corresponds to a wall temperature fluctuation of approximately 50% of the imposed temperature difference, the temperature difference between the two fluid streams of unequal temperatures and densities. Most of the fluctuations had stress amplitudes less than 50% of the maximum stress, which corresponds to wall temperature fluctuations less than 25% of the imposed fluid temperature difference.

LMFBR Tests

Woodward[13] investigated stratification and striping in a 1/5-scale model of an LMFBR at the Waltz Mills Test Facility. The model was plexiglass, therefore the hot and cold water temperatures were limited to 130 and 70F. Two lengths of horizontal piping, of 4" and 6.5" inside diameter, were examined. The tests were conducted in the turbulent transition range, with Reynolds Numbers (based on half-pipe flow areas) of 2×10^3 to 8×10^3. Dye and thermocouples were used, the thermocouples were typically inserted 1/32" into the fluid.

The thickness of the interface region, over which the fluid temperature changed from hot to cold, ranged from 0.6" to 2". The striping frequency was 0.1 to 0.5 Hz and the fluctuations were approximately sinusoidal. The amplitudes of the temperature fluctuations (of the near-wall fluid) approached 60% of the imposed temperature difference, and were most pronounced at low Richardson Numbers. Probability-of-occurrence information was presented for only three ranges of amplitudes (or windows). This information has been converted to the fractional occurrence for constant window widths of 10% amplitude, and plotted in Figure 7.2. Most of the fluctuations had mid-range amplitudes, 10 to 35%. The probability of occurrence dropped rapidly at the higher amplitudes, approaching zero at 60% amplitude.

A heat transfer coefficient was needed to obtain wall temperature information from the near-wall fluid temperature measurements. The heat transfer coefficients determined by Fujimoto et al were referenced by Woodward. Fujimoto et al[14] tested striping in a 14.2" horizontal pipe made of acrylite. Calcium chloride was added to the warmer fluid stream to obtain plant-typical density differences. (The fluid temperatures were used simply to track the streams of differing densities.) Wall striping was measured on thin squares of copper. The amplitude of the fluid temperature fluctuations was observed to decrease near the wall. The fluctuations at the interface between the fluids of differing densities evidenced frequencies of 0.3 to 3.0 Hz. The amplitude of the wall fluctuations was less than 10% of the imposed temperature difference. The convective heat transfer coefficient was calculated to be from 1.25 to 7 times that of the Dittus-Boelter correlation (for forced convection in tubes). The information obtained by Woodward and by Fujimoto et al was referenced in the evaluation of thermal stratification of the pressurizer surgelines of the South Texas Project power plants.

ANL Tests

Kasza et al have conducted extensive experimental studies of stratification and striping at ANL[15-25]. These studies have generally used water flowing turbulently in transparent pipes of 6-in inside diameter. Combinations of horizontal and vertical piping lengths have been tested, including bends in the horizontal plane. Vertical lengths of piping were observed to eliminate stratification. Stratification in horizontal lengths began at a Richardson Number of approximately 0.05; flow stagnation and reversal occurred near a Richardson Number of 0.7. Kasza et al applied the buoyancy index of Jackson and Fewster, namely

$$\gamma = Ri\ Re^{-0.625}/\sqrt{Pr}$$

Kasza et al observed a correlation between buoyant effects and this buoyancy index. The threshold of buoyant effects was found to correspond to a γ on the order of 10^{-4}; a γ on the order of 10^{-2} or larger obtained strong buoyant effects.

The more-recent investigations of Kasza et al[18-25] obtained some details of the thermal fluctuations. Whereas the bulk fluid temperature fluctuations were about 75% of the imposed temperature difference, the amplitude of the wall fluctuations was 30 to 40% of the imposed temperature difference. On the basis of limited power-spectral-density information, most of the signal energy was concentrated below 1 Hz, peaking between 0.1 and 0.6 Hz, and decaying approximately exponentially with increasing frequency. These maximum fluctuations were observed approximately one diameter downstream of a horizontal elbow.

HDR

Wolf et al have conducted extensive examinations of thermal mixing in the HDR project at Karlsruhe, FRG.[26-35] The TEMB test series examined pressurized thermal shock using a large-scale pressure vessel and various high-pressure injection configurations; the experimental results were compared to the predictions of many codes and correlations.[26-33] Fluid temperature fluctuations were observed and recorded, but received little emphasis.

The TEMR test series concentrated on thermal stratification in horizontal feedwater lines.[34-35] The test section was a 20-foot length of 15.6-in inside diameter metal pipe, extensively instrumented with 11-ms thermocouples. Cold water entered one end of the horizontal run through a bend from vertical upflow, the opposite end of the horizontal run was attached to a reservoir of hot fluid. The TEMR tests consisted of 3 subseries, 2 of which were labelled "BWR" and "PWR." In the BWR tests, a plate with slit orifices was installed at the junction of the horizontal run with the reservoir, to simulate the holes of a typical BWR feed sparger. The horizontal-to-reservoir junction was unobstructed in the PWR tests. The third subseries of TEMR tests considered the buildup and decay of hot water pockets. The horizontal-to-reservoir junction was blocked except for a orizontal slit at the bottom of the pipe cross-section.

The PWR tests of the TEMR series are most relevant to the surgeline configuration. The ranges of conditions of the 9 PWR tests are listed in Table 7.1. The average fluid temperature ranged from approximately 200 to 300F, and the imposed fluid temperature differences ranged from approximately 200 to 400F; the volumetric flow rates spanned 10 to 200 gpm. The Reynolds Numbers based on the flow area (rather than on a reduced flow area to account for stratification) were in the turbulent range. Kasza and Kuzay have employed a buoyancy index which is dependent on the Reynolds, Richardson, and Prandtl Numbers. In an order-of-magnitude sense, the threshold of buoyant effects occurs at an index of 10^{-4}, and strong buoyant effects occur for an index of 10^{-2} and larger. Applying this index to the PWR test conditions, all the PWR tests of Wolf et al were in the strong buoyant range.

The interface between the fluid of unequal densities was characterized as being wavy, with typical frequencies between 0.1 and 10 Hz.[22] Within the mixing layer, the fluid temperature fluctuations were not damped near the wall.[21] The fluid mixing did reduce the local maximum temperature difference from the imposed temperature difference, however. The amplitude of the wall temperature variations, expressed as a fraction of the amplitude of the fluid temperature fluctuations, was stated in two contexts.[22] For all the BWR and PWR tests, the fractional amplitude was 10 to 40%, but the peak fractional amplitude was 25 to 50%.

Measurements from one of the PWR tests, Test 33.19, were presented.[22] The conditions of Test 33.19 are listed in Table 7.1. Test 33.19 was characterized by a relatively high flow rate and ratio of inertial to viscous forces (Re), and by a mid-range temperature difference. The resulting ratio of buoyant to inertial forces (Ri) was low compared to that of the other PWR tests, as was the index of buoyant effects (γ). The temperatures measured in the fluid, on the inside pipe metal surface, and on the outside pipe surface were presented.[21,22] Examining these figures, the fluid temperature fluctuated with an amplitude which was almost equal to the imposed temperature difference, the difference between the temperatures of the hot and cold fluid streams. The temperature of the inside pipe wall fluctuated with an intermediate amplitude, but the temperature of the outside surface of the pipe evidenced no fluctuations.

The inside pipe surface temperature exhibited irregular fluctuations. The maximum amplitude of these fluctuations was approximately 30% of the imposed temperature difference; the larger-amplitude fluctuations occurred at intervals of approximately 5 seconds. This interval corresponds to a frequency of occurrence (of relatively large fluctuations) of 0.2 Hz. This frequency of occurrence must be distinguished from the characteristics of the individual fluctuations. Because the larger-amplitude variations generally occurred within groups of fluctuations of much smaller amplitude, the frequency of all fluctuations was approximately 1 Hz. That is, the larger-amplitude fluctuations, which occurred at intervals of approximately 5 seconds, each persisted for only approximately 1 second. These characterizations were obtained by examining the figures presented for PWR Test 33.19.

The HDR experimentalists drew the following conclusions from the TEMR results:[21,22]

- The extrapolation of model data to a plant, using a transparent model, is made difficult by the complex interactions between convective and conductive phenomena.

- There is no simple, unique correspondence between the thermal response of the exterior of the pipe and that of the interior.

Comparison of Thermal Striping Conditions

Figure 7.3 provides an overview of thermal striping. Both dimensional and dimensionless axes are presented. The dimensional axes, flow rate versus temperature difference, apply specifically to the surgeline geometry and conditions. The dimensionless axes, Reynolds Number (Re) versus Grashof Number (Gr), both correspond to the surgeline quantities and provide a more general basis with which to assess the thermal-hydraulic interactions. The Reynolds Number indicates the ratio of inertial to viscous forces whereas the Grashof Number provides a measure of the ratio of buoyant to viscous forces. The information presented in Figure 7.3 is to be regarded in an order-of-magnitude sense. For example, flow rates were converted to velocities using the whole-pipe flow area, rather than reducing the area to accommodate stratification; and the surgeline fluid properties were evaluated at 300F and slightly subcooled - they are relatively insensitive to pressure, but quite sensitive to temperature.

Regions of relatively weak and of relatively strong buoyant effects, compared to inertial effects, were estimated by evaluating the Richardson Number (Ri) and the buoyancy index (γ) which has previously been described. The conditions of interest to surgeline stratification and striping lie in the "strong buoyant effects" range shown in Figure 7.3. The range of interest is further refined by considering the surgeline temperature difference (DT): there is no fatigue concern for DTs less than 90F, and the maximum DT is approximately 300F. The conditions of interest are thus approximately $10^{11} < Gr < 10^{12}$ and $Re < 10^5$. (There is probably a lower-Re bound, below which the buoyant effects predominate to the extent that interface instabilities and thus striping are suppressed; this limit has not been quantified, except that Kasza et al have observed such a limit for the case of fluctuations downstream of a bend in the horizontal plane.)

The dimensionless axes of Figure 7.3 provide a convenient basis on which to compare the several investigations of striping. These are the singly cross-hatched regions in the figure. The conditions of Woodward, and of Kasza et al, lie far below the range of Grashof Numbers of interest. Both these striping investigations were conducted at atmospheric pressure, thus the reduced thermal expansion coefficient and DT, as well as the increased viscosity, resulted in relatively small Grashof Numbers. The conditions of Wolf et al, on the other hand, are just on the high-Gr side of the conditions of interest, due only to their larger pipe diameter compared to that of the surgeline.

Several data sets are not shown on the figure. The data of Fujimoto et al was obtained at atmospheric conditions, but with the inter-fluid density difference enhanced toward that encountered at surgeline conditions. The viscosity remained in the low-temperature range, however, and the data evaluation of Fujimoto et al depended on a correspondence between mixing and diffusion within a thermal gradient and a concentration gradient. The EDF data is also not shown. This data, although unpublished, was apparently obtained at atmospheric pressure and correspondingly low Grashof Numbers. Finally, the conditions of the BWR feedwater nozzle research are not shown because of the pronounced geometric dissimilarities between the nozzle and the surgeline.

It is uncertain whether the low-Gr data of Woodward and of Kasza et al apply at the conditions of interest for the surgeline. Certainly the visualizations available with the low-Gr tests provide valuable insight regarding striping mechanisms, characteristics, and limiting regions, but these insights may apply only at the tested conditions, even if the Richardson Number is preserved in the translation of conditions to those of interest.

The work of Wolf et al thus seems singularly pertinent to surgeline applications. It should be recognized, however, that the most-applicable subseries of tests by Wolf et al included only nine test conditions, utilized only a horizontal pipe, and each involved a transient obtained by injecting cold fluid into an initially hot and isothermal pipe. Also, the detailed results of Wolf's research are as yet unpublished.

Notwithstanding their dissimilar Grashof Number ranges, the published striping characteristics of the three investigations plotted in Figure 7.3 were quite similar. Woodward obtained frequencies from 0.1 to 0.5 Hz, and amplitudes of the near-wall fluid oscillations which generally ranged from 10 to 35% of the imposed fluid temperature difference, and which peaked near 60% of the imposed DT. The characteristics of wall temperature oscillations were not available in Woodward's research. Kasza et al obtained the following characteristics of wall temperature fluctuations in the horizontal piping downstream of a horizontal bend: amplitudes from 30 to 40% of the imposed fluid temperature difference, with the frequencies of the larger-amplitude fluctuations generally between 0.1 and 0.6 Hz. Wolf et al also obtained wall fluctuation characteristics. These fluctuations were reported to occur over the frequency range from 0.1 to 10 Hz; there amplitudes were generally between 10 and 40% of the imposed temperature difference, with peak amplitudes of 25 to 50%. Examining the single published wall temperature trace (of the PWR series), the peak amplitude was approximately 30% of the imposed temperature difference; the fluctuation frequency was roughly 1 Hz, the frequency of occurrence of larger-amplitude fluctuations was roughly 0.2 Hz. Finally, the characteristics of thermal striping were addressed in a 1980 report by the NRC which summarized pipe cracking in PWRs.[36] The range of frequencies was 0.1 to 10 Hz. The reduction of amplitude due to film heat transfer was described, resulting "... in a peak metal temperature variation at the surface of roughly one-fourth to one-half the water temperature variation."

7.3.1.2. Surgeline Striping Analysis Input Assumptions

Surgeline striping encompasses a range of frequencies and amplitudes, and would be well characterized by a probability density function which defined the frequency of occurrence versus incremental amplitude. Moreover, this probability density function would be responsive to the ongoing interactions between the fluid inertial, viscous, and buoyant forces as defined principally by the temperatures and flow rates of the interacting fluid streams. Because the available striping data is wholly insufficient to develop such a probability density function and its dependencies, an alternative characterization of surgeline striping has been adopted.

Surgeline striping has been characterized by a single frequency of occurrence and amplitude, namely 0.25 Hz and 45% (i.e., the amplitude of the wall temperature fluctuations is 45% of the temperature difference between the hot and cold streams). These characteristics were selected to be realistic and conservative. The selected amplitude of 45% lies on the high side of the observed ranges of amplitudes. The selected frequency-of-occurrence of 0.25 Hz corresponds roughly to the observed frequencies of occurrence of higher-amplitude fluctuations. It should be noted that the larger-amplitude fluctuations generally occur within groups of fluctuations of much lesser amplitude, such that the prevailing fluctuation frequency (of all fluctuations) is much higher than the frequency of occurrence of the larger-amplitude fluctuations. It should also be noted that amplitude-versus-frequency information, such as the power spectral densities obtained by Kasza et al, clearly indicate an inverse relation which appears entirely logical; namely, the amplitude of fluctuations drops off sharply with increasing frequency.

In summary, a single frequency and amplitude of fluctuation have been estimated based on the research available. Striping is better represented by a probability density function, giving the frequency of occurrence for discrete ranges of amplitude. This function is expected to vary somewhat with surgeline conditions, specifically flow rate and perhaps the imposed fluid temperature difference. The probability density functions would then be sampled using plant times at conditions. The resulting striping characteristics are expected to be more realistic, and less severe, than the single amplitude and frequency estimated herein. Another inherent conservatism involves the pipe location affected by striping. The interface between hot and cold fluids, and hence the striping-affected zone, slowly vary throughout a transient. This effect is ignored in this striping characterization and the subsequent structural evaluation.

7.3.2. Evaluation of Oconee Test Data

The Oconee test data provides important input to the fatigue evaluation, namely the magnitude of the top to bottom temperature differential, the duration of various top to bottom temperature differentials, and the number of transient temperature cycles that occurred during the heatup. These data, along with the assumptions resulting from Section 7.3.1, provide the basic input for the surgeline fatigue evaluation. The following paragraphs describe the Oconee test data reduction and the results of this process as they were input to the fatigue evaluation.

Surge line data was taken at Oconee Unit 1 during the 2/89 heatup, power escalation, and subsequent full power operation. Approximately 150 parameters were sampled every twenty seconds and saved in eleven data file sets. These parameters included reactor coolant system parameters, displacement measurements, and thermocouple readings. This process was totally automated for approximately eight hours before the data file had to be stopped and a new data file started.

Spreadsheet software macros aided in the conversion of the raw data into more easily evaluated information. Twenty-six plots were created for each data set for evaluation. These plots were evaluated for insight into the phenomenon of thermal stratification.

After a preliminary evaluation of the data, additional plots were created to aid in the structural evaluation of the surgeline. Plant operations between cold shutdown and hot shutdown resulted in the greatest stratification as expected. The greatest stratification was well represented by surgeline location 11 which is located near the middle of the horizontal run nearest the pressurizer (see Figure 6.1). Files were combined to plot the top and bottom thermocouple readings at location 11 for each day of the heatup (see Figures 7.4-7.11). These figures were used to estimate the number of thermal cycles throughout the heatup for the stress analysis.

An additional evaluation determined the length of time different magnitudes of stratification (delta Ts) existed during the heatup. A spreadsheet was used to determine the amount of stratification at location 11 each time step (every 20 seconds) throughout the heatup. The length of time stratification existed above certain delta Ts shown in Figure 7.11.

The location 11 plots and the length of time stratification existed were considered when making assumptions for the thermal stratification and thermal striping evaluations. The thermal striping fatigue evaluation is discussed in the following section.

7.3.3. Structural Evaluation of Thermal Striping Based on Oconee-1 Measured Data

To account for thermal fluctuations in the wall of the surgeline an ANSYS model was built to assess the temperature distribution through the thickness of the pipe. The temperature of the wall was assumed to be 45% of the peak amplitude of the stratified fluid temperature profile. Four stratified fluid temperature differentials were analyzed: 280F, 250F, 225F, and 200F. The stratified fluid temperature profile for this analysis is a sine wave for a period of 4 seconds. This sine wave was closely approximated as a "cut-sawtooth" wave. For each case the actual inside wall temperature range is 45% of the stratified fluid temperature profile. The average temperature of the inside wall was based on a minimum fluid temperature of 123F plus one half the stratified fluid temperature range. The 45% inside wall temperature then fluctuates about this average temperature.

From the ANSYS computer runs, the data was reduced to produce a temperature profile for the nodes versus the distance through the thickness for a given time where the inside wall temperature is at a maximum and a minimum. From this temperature distribution, the linear and nonlinear thermal gradient stresses are calculated. These calculations are based on the piping equations in paragraph NB-3653.2 of the ASME Code. These stresses result in a peak stress range and thus an alternating stress for each of the temperature cases run. An allowable number of cycles is calculated from the fatigue curves in the appendices of the ASME Code for stainless steel.

A typical number of striping cycles is based on the data taken at Oconee as described in Section 7.3.2. The time in which the fluid is stratified for a given temperature range is presented in this data. The time duration for which surgeline fluid was stratified between 250^{o} and 280^{o}F was utilized for calculation of number of striping cycles for 280^{o}F profile case. The total time duration between 225^{o}F to 250^{o}F was utilized for 250^{o}F profile case. This process was repeated for other temperature profiles. These temperatures and times are representative of a typical heatup and cooldown cycle at a B&W operating plant. From this information, a typical number of cycles can be calculated based on the multiplication of: 1) the time (minutes) in which the fluid is stratified for a given temperature range; 2) twice this value to account for both heatup and cooldown; 3) 240 heatup and cooldown cycles in the design life; 4) 60 seconds per minute; and 5) one over the period (seconds). The actual number of cycles is then divided by the allowable number of cycles resulting in a fatigue usage factor for each temperature profile. The usage factors are then added to give a total usage factor due to fatigue. This information is presented in Table 7.3. The cumulative usage factor due to thermal striping for this analysis is 0.10.

From Table 7.3, it can be seen that striping during fluid stratification temperature ranges of less than 200F result in no fatigue damage. Since fluid stratification at power was less than this 200F temperature range, it is concluded that fatigue will only be impacted during heatup and cooldown transients.

Table 7-1. HDR Test Series TEMR-PWR: Ranges of Conditions and Conditions of Test 33.19[37]

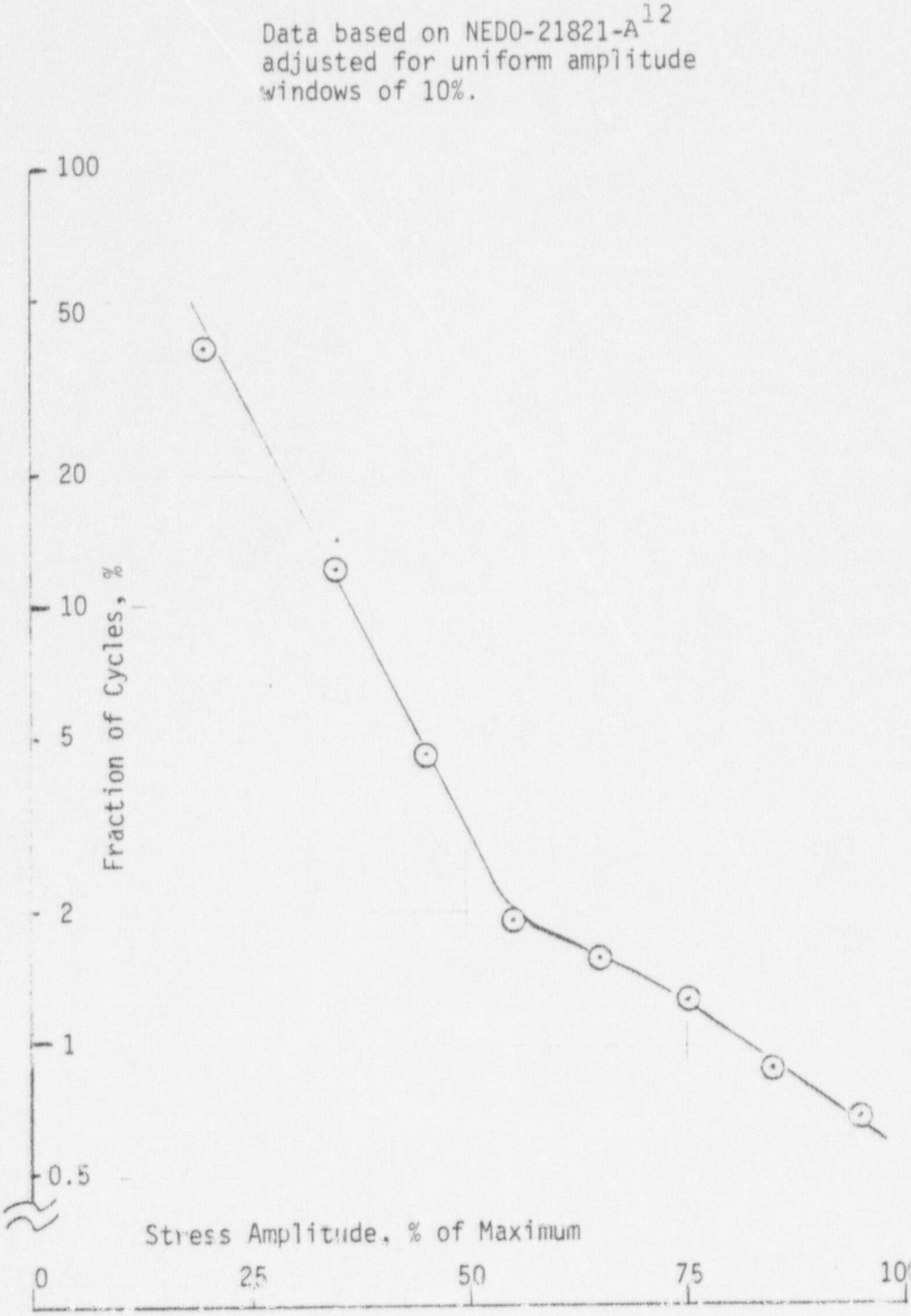
- The extreme conditions are listed for any of the 9 tests, rather than for the tests having extreme combinations of conditions.
- The flow rates and dimensionless numbers use the flow area of the whole pipe; properties are evaluated at the average fluid temperature.
- γ is the buoyancy index used by Kasza and Kuzay, where $\gamma > 10^{-2}$ obtains strong buoyancy effects.

Condition	Minimum	Maximum	Test 33.19
Fluid temperatures, F			
Hot	314	486	417
Cold	79	130	130
(Hot-Cold)	201	391	287
Average	198	290	274
Flow Rates			
Volumetric, gpm	10	200	200
Velocity, ft/s	0.016	0.34	0.34
$Re = vd/\nu$	10^4	2×10^5	1.8×10^5
$Ri = g\Delta T\beta d/v^2$ $(= 1/Fr^2)$	5×10^1	3×10^4	5.5×10^1
$\gamma = Ri\ Re^{-0.625}/\sqrt{Pr}$	0.025	84	0.026

Table 7-2. Striping Cases and Results

Case	Temperature Range (F) Fluid	Metal	Period (sec)	Sa (ksi)	Allowable Cycles	Typical # Cycles (Based on Oconee Data)	Usage Factor
A -	280	126	4.0	22.62	2.11E+06	93600	0.0444
B -	250	112	4.0	20.11	4.52E+06	120000	0.0265
C -	225	101	4.0	18.13	1.37E+07	444000	0.0324
D -	200	90	4.0	16.16	INFINITE	------	0.0000
E -	METAL TEMP RANGES BELOW 90F				INFINITE	------	0.0000
					Cumulative Usage Factor =		0.10

Figure 7.1 Frequency of Occurrence Versus Stress Amplitude



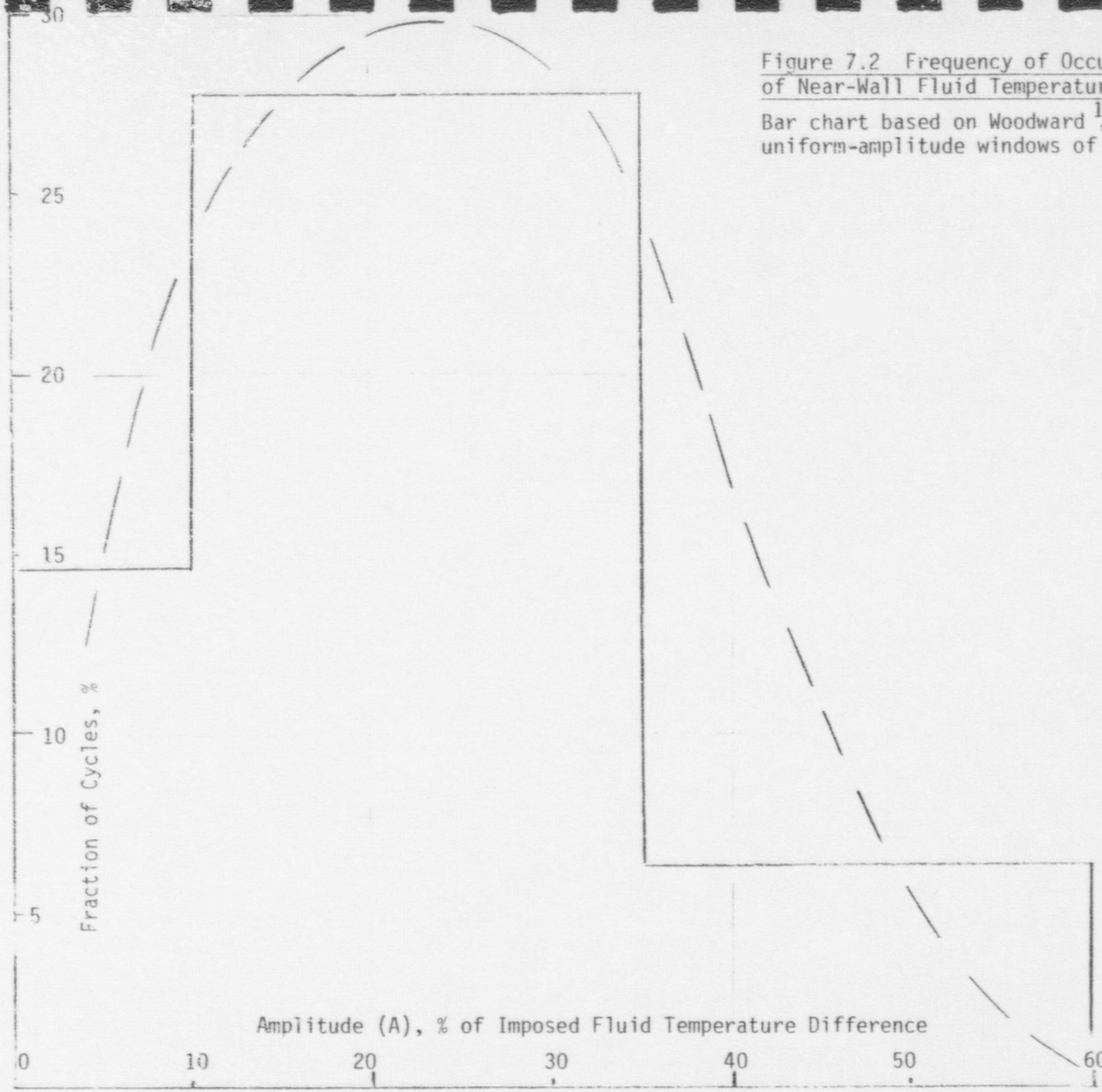


Figure 7.2 Frequency of Occurrence Versus Amplitude of Near-Wall Fluid Temperature Fluctuations

Bar chart based on Woodward[13], adjusted for uniform-amplitude windows of 10%.

Figure 7.3 Striping Conditions

Gr ~ (Buoyant/Viscous) Forces
Re ~ (Inertial/Viscous) Forces

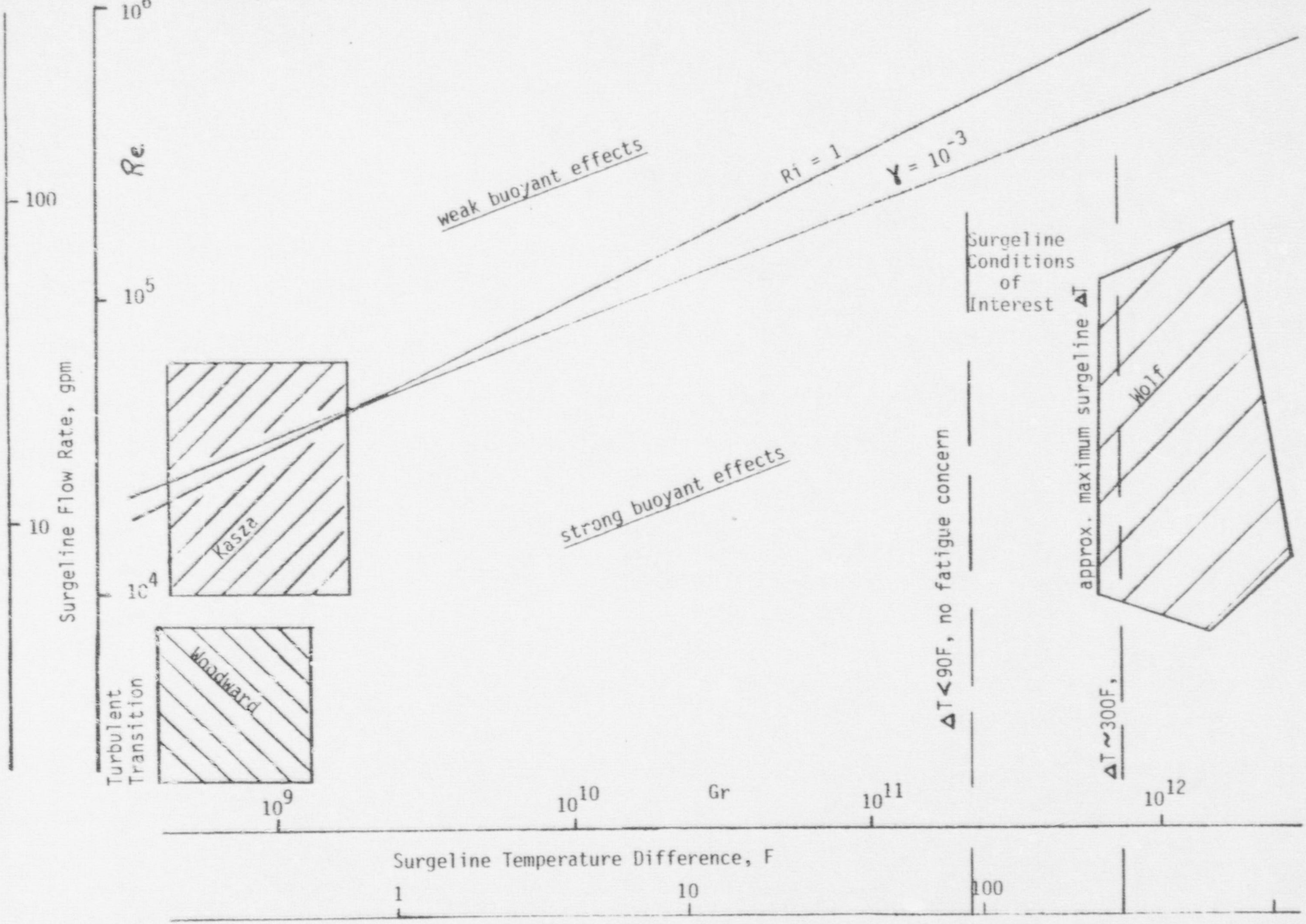
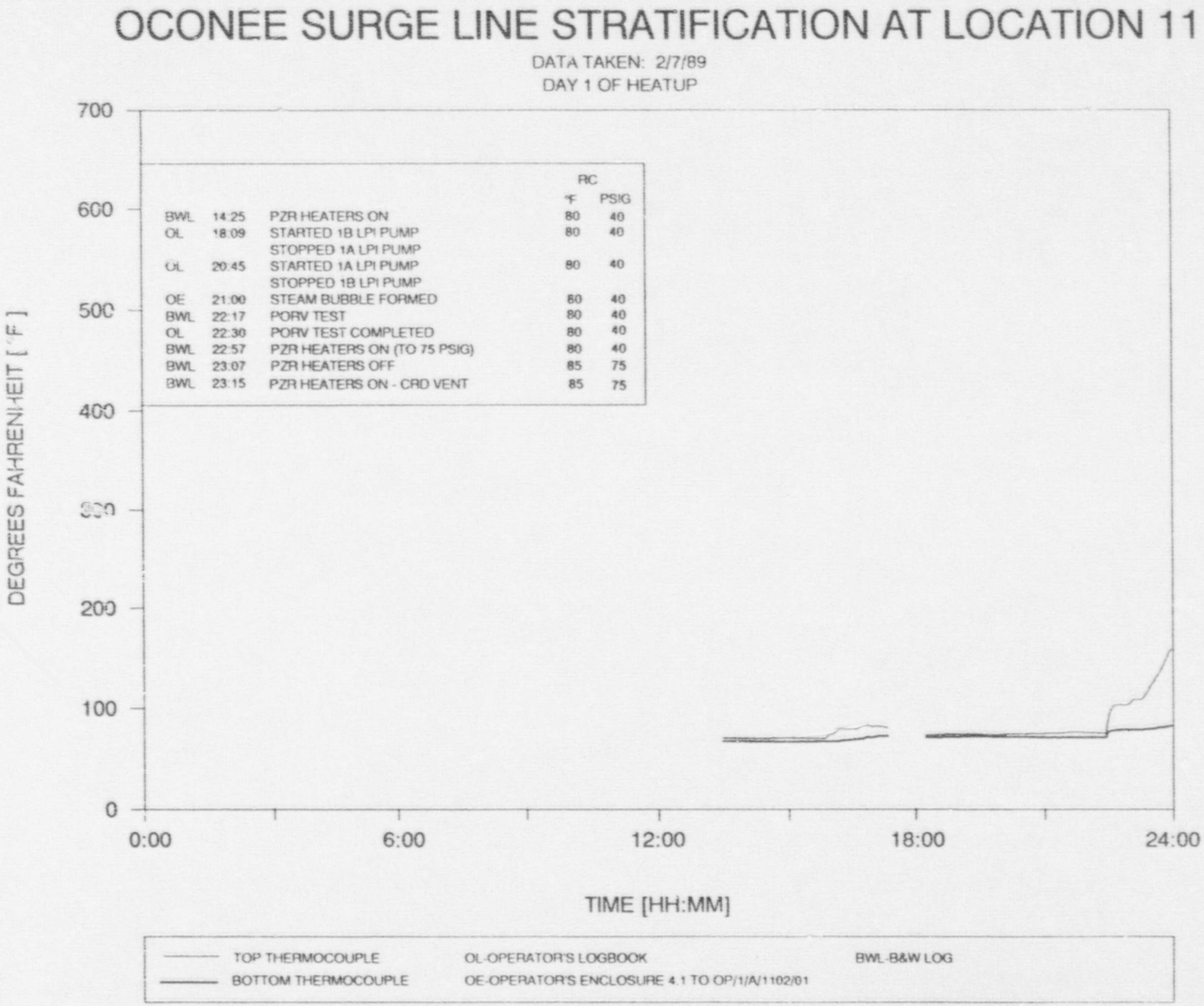


Figure 7.4



OCONEE SURGE LINE STRATIFICATION AT LOCATION 11
DATA TAKEN: 2/7/89
DAY 1 OF HEATUP
RC
°F PSIG
BWL 14:25 PZR HEATERS ON 80 40
OL 18:09 STARTED 1B LPI PUMP 80 40
STOPPED 1A LPI PUMP
OL 20:45 STARTED 1A LPI PUMP 80 40
STOPPED 1B LPI PUMP
OE 21:00 STEAM BUBBLE FORMED 80 40
BWL 22:17 PORV TEST 80 40
OL 22:30 PORV TEST COMPLETED 80 40
BWL 22:57 PZR HEATERS ON (TO 75 PSIG) 80 40
BWL 23:07 PZR HEATERS OFF 85 75
BWL 23:15 PZR HEATERS ON - CRD VENT 85 75
DEGREES FAHRENHEIT [°F]
700
600
500
400
300
200
100
0
0:00
6:00
12:00
18:00
24:00
TIME [HH:MM]
TOP THERMOCOUPLE
BOTTOM THERMOCOUPLE
OL-OPERATOR'S LOGBOOK
OE-OPERATOR'S ENCLOSURE 4.1 TO OP/1/A/1102/01
BWL-B&W LOG

Figure 7.5

OCONEE SURGE LINE STRATIFICATION AT LOCATION 11

DATA TAKEN: 2/8/89
DAY 2 OF HEATUP

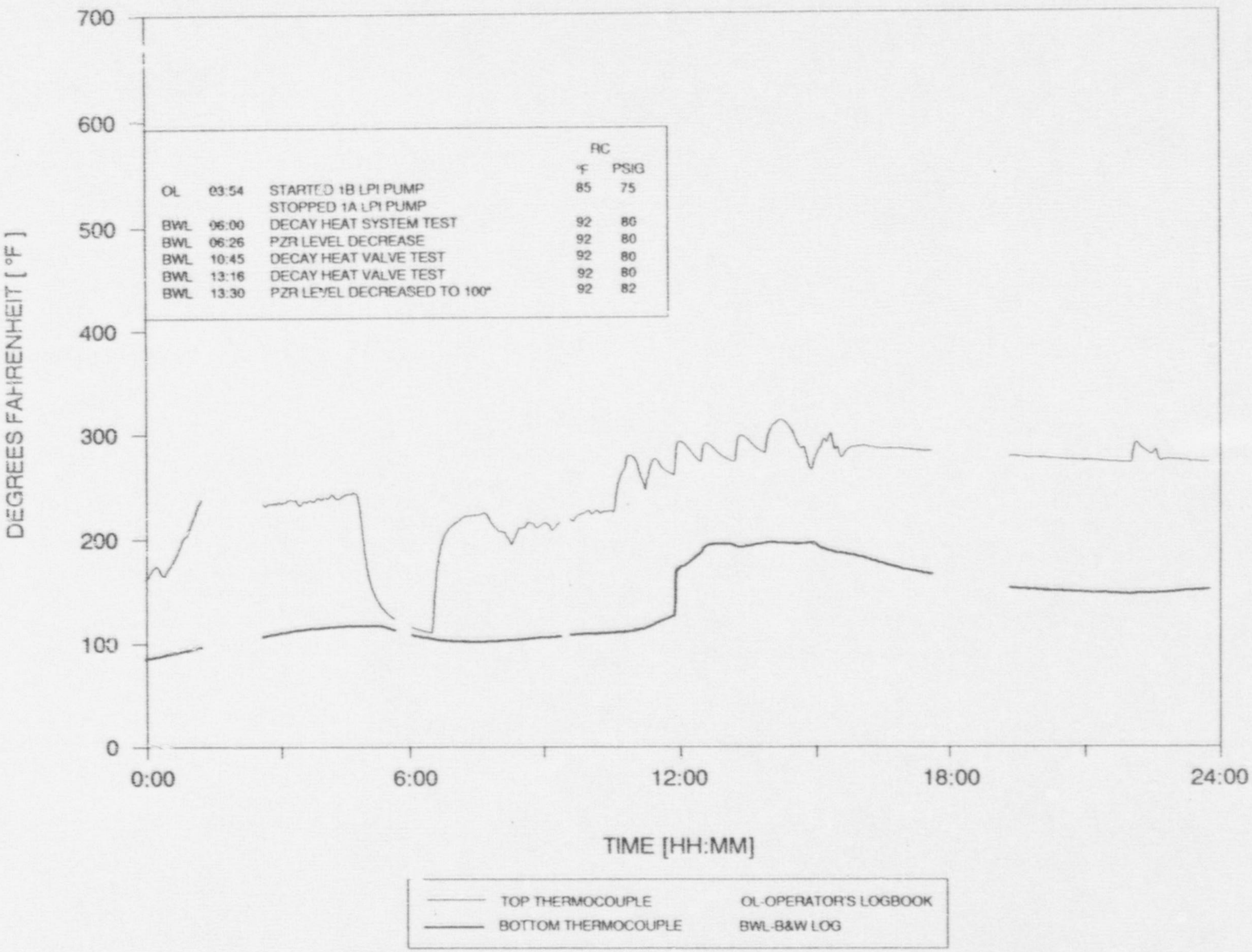


Figure 7.6

OCONEE SURGE LINE STRATIFICATION AT LOCATION 11

DATA TAKEN: 2/9/89
DAY 3 OF HEATUP

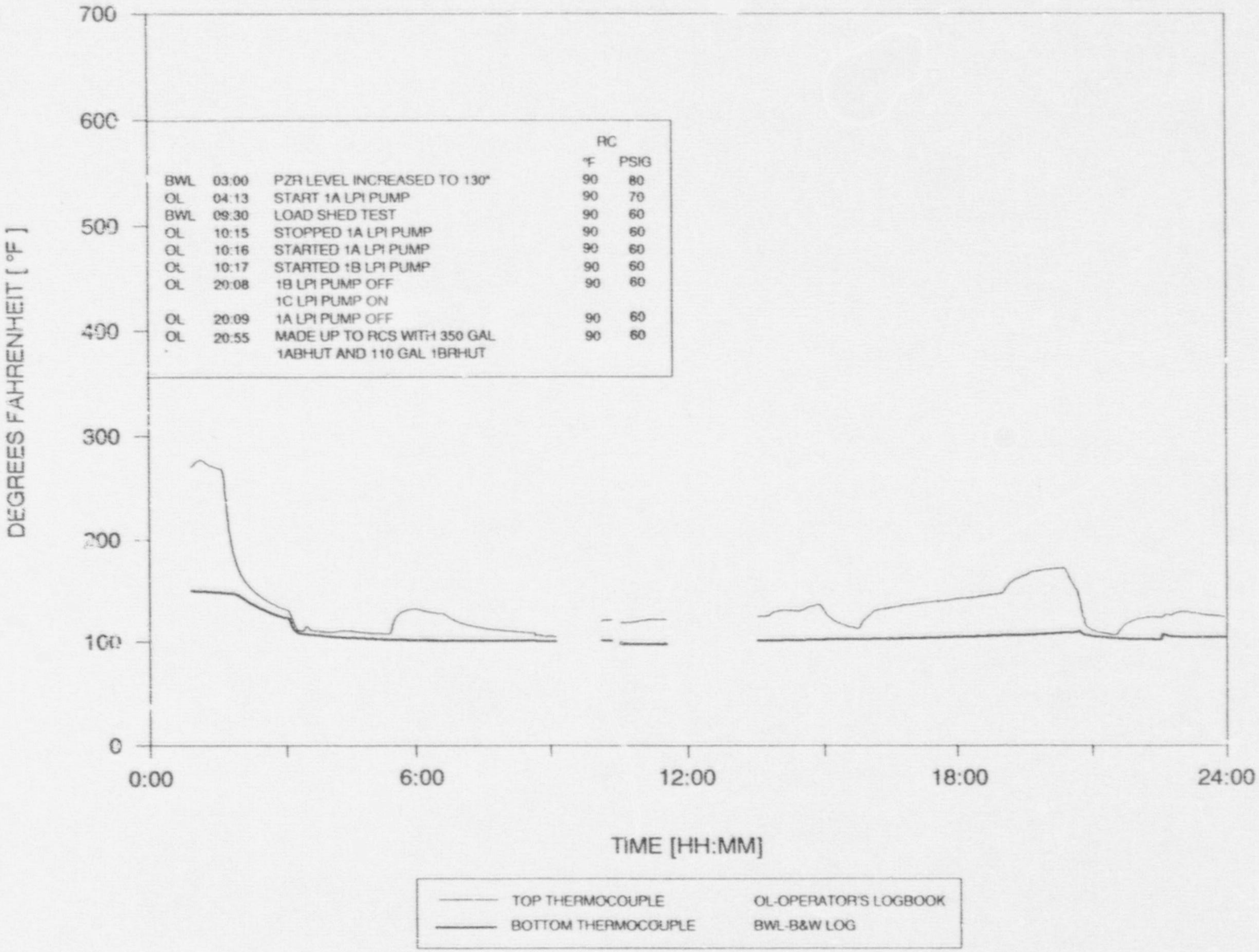


Figure 7.7

OCONEE SURGE LINE STRATIFICATION AT LOCATION 11

DATA TAKEN: 2/10/89
DAY 4 OF HEATUP

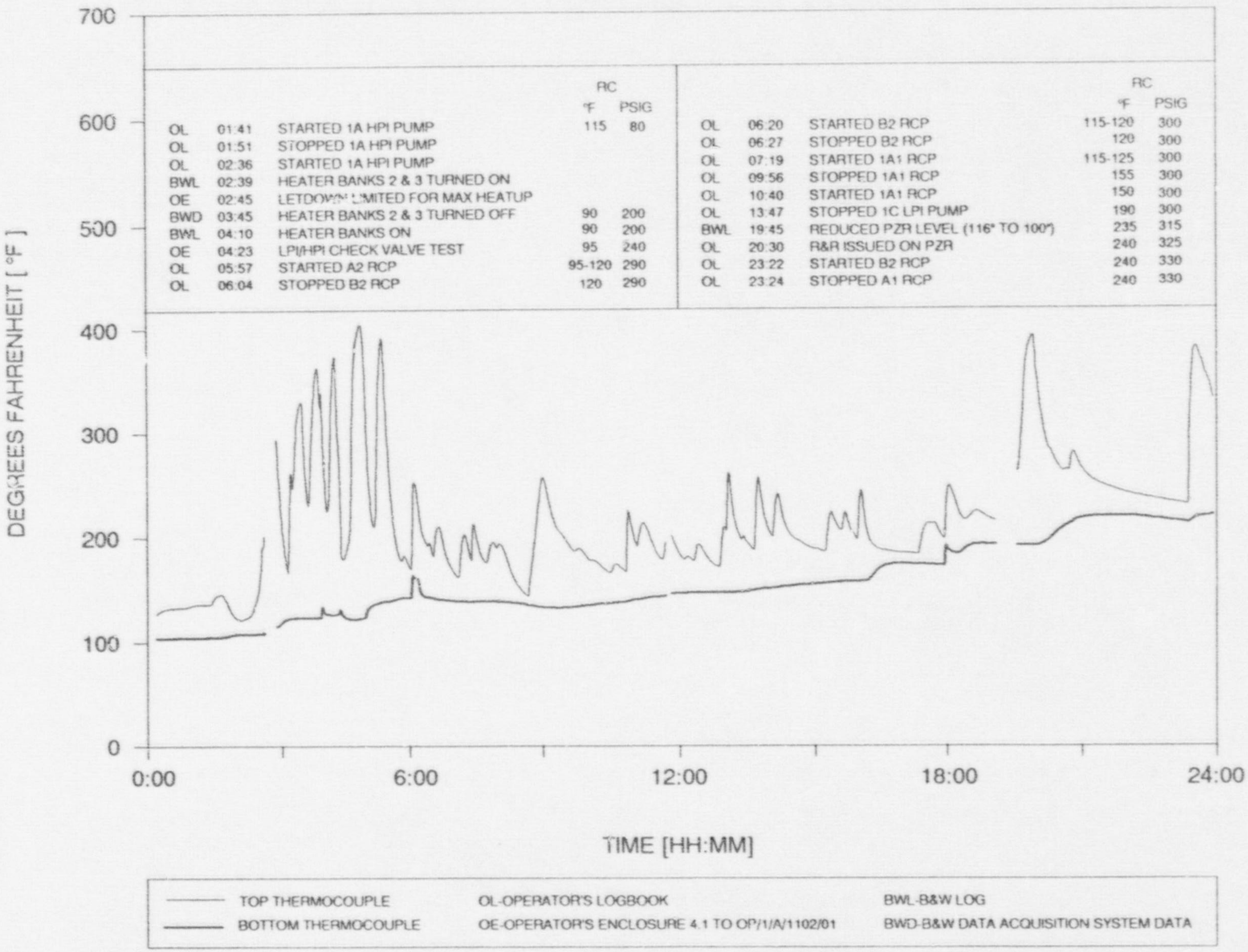


Figure 7.8

OCONEE SURGE LINE STRATIFICATION AT LOCATION 11

DATA TAKEN: 2/11/89
DAY 5 OF HEATUP

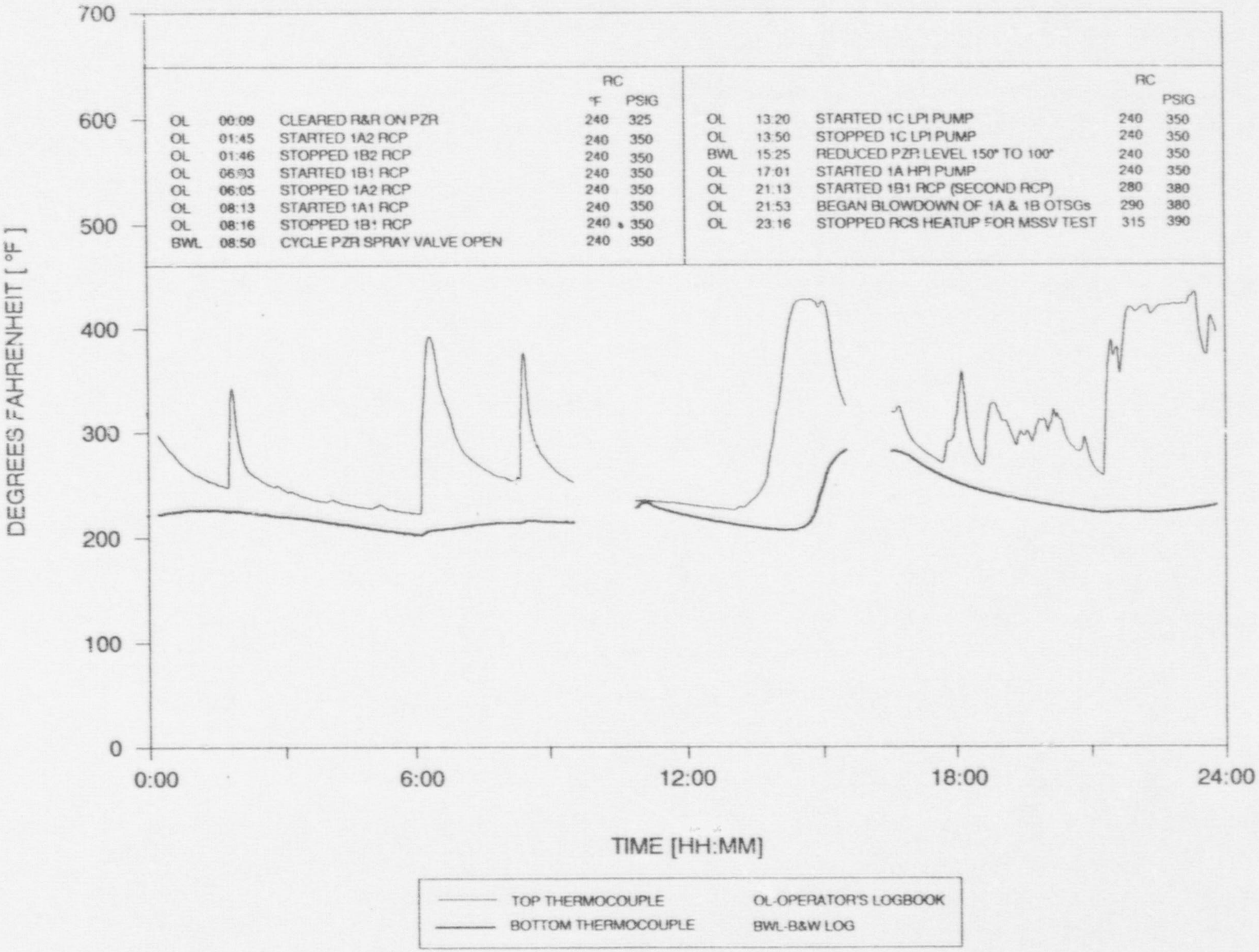


Figure 7.9

OCONEE SURGE LINE STRATIFICATION AT LOCATION 11

DATA TAKEN: 2/12/89
DAY 6 OF HEATUP

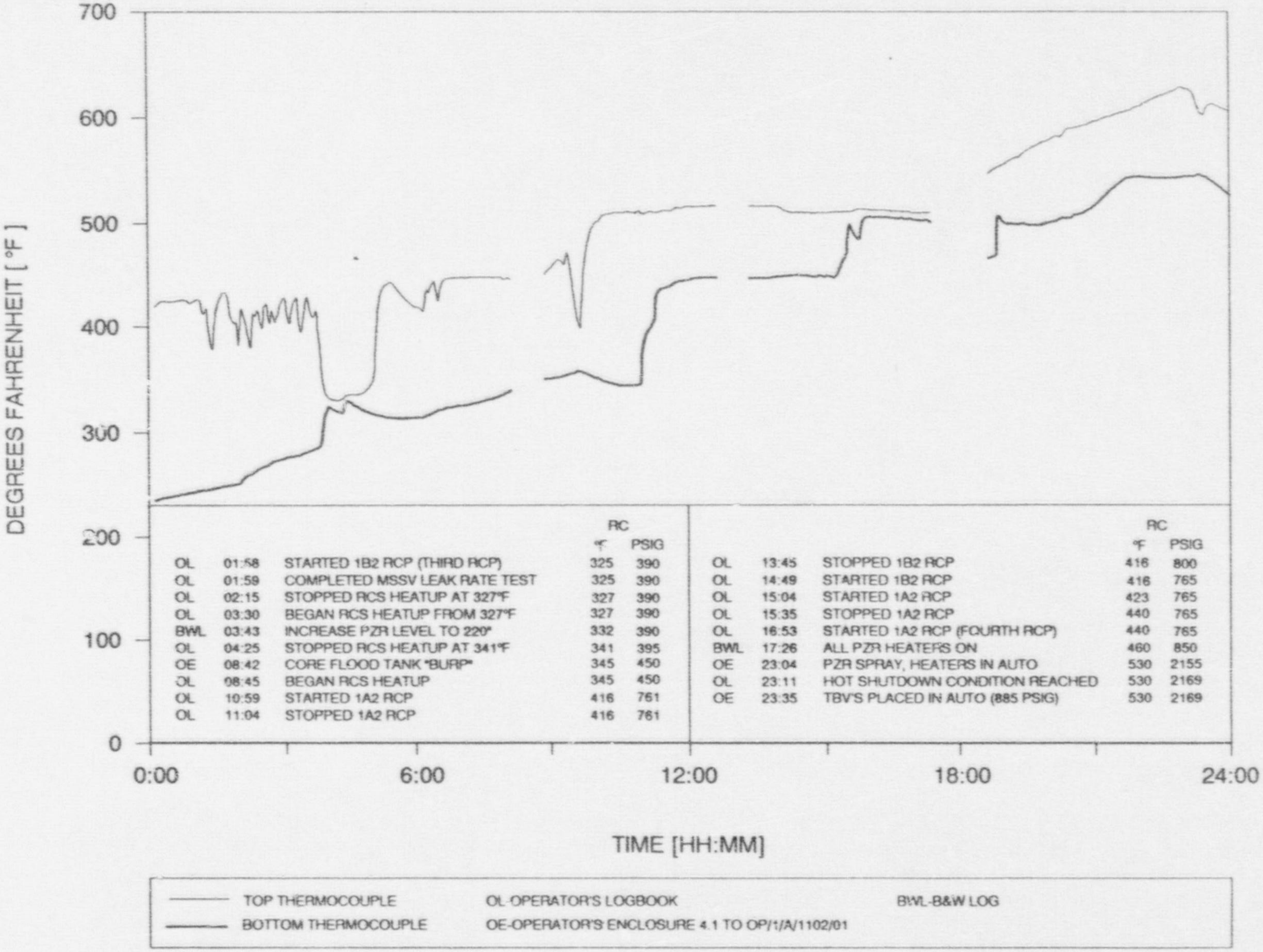
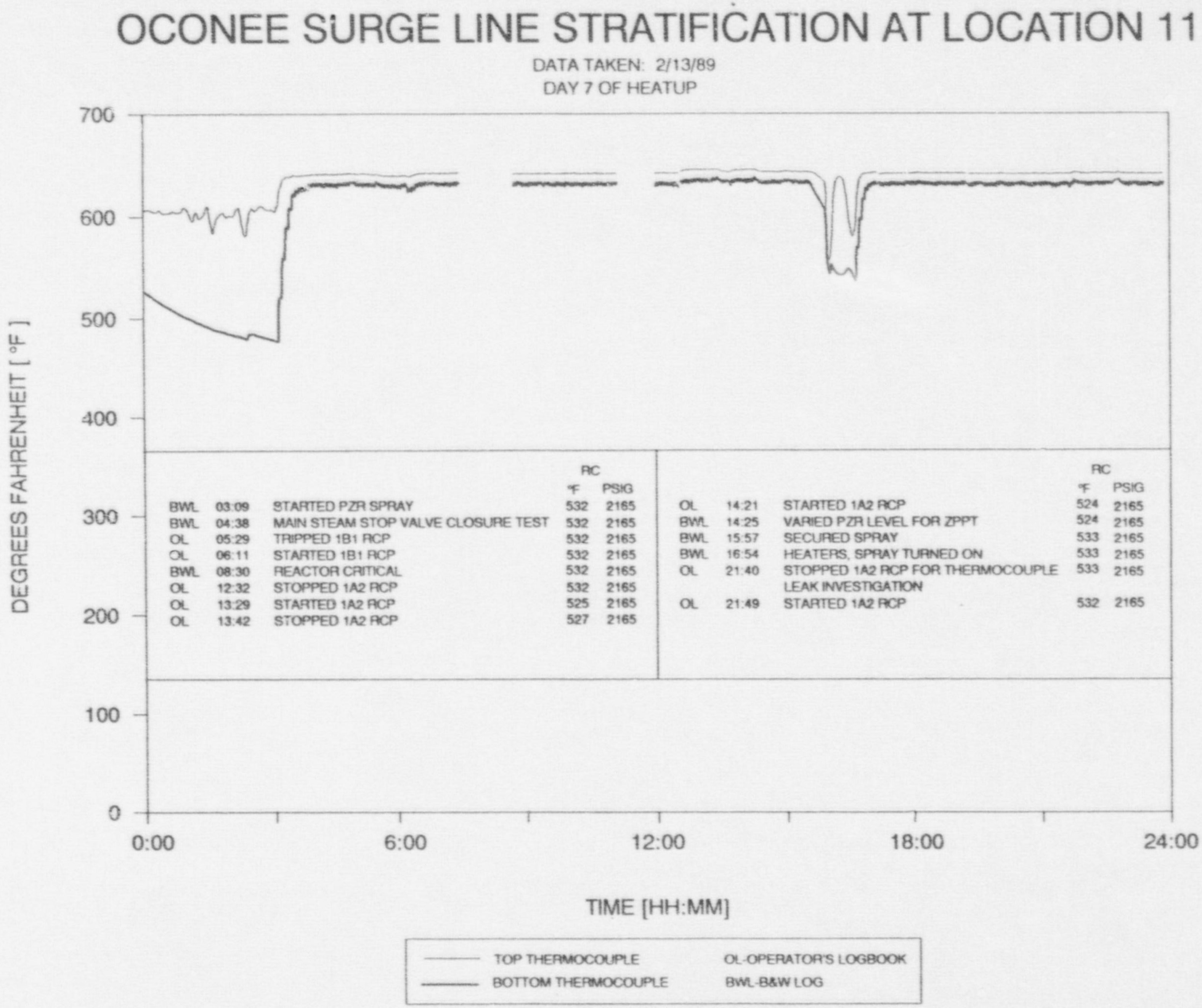
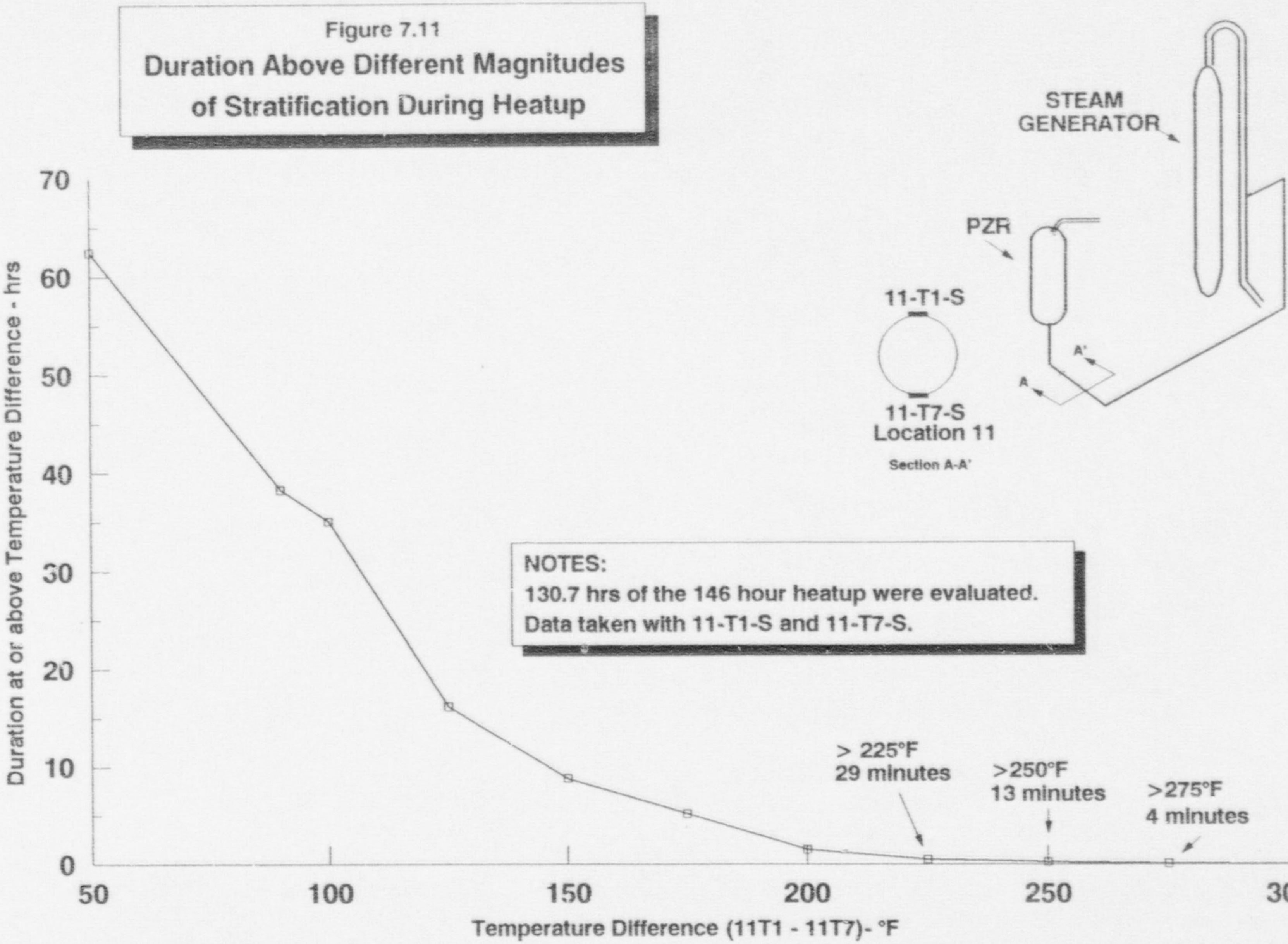


Figure 7.10



OCONEE SURGE LINE STRATIFICATION AT LOCATION 11
DATA TAKEN: 2/13/89
DAY 7 OF HEATUP
DEGREES FAHRENHEIT [°F]
700
600
500
400
300
200
100
0
0:00
6:00
12:00
18:00
24:00
TIME [HH:MM]
RC
°F PSIG
BWL 03:09 STARTED PZR SPRAY 532 2165
BWL 04:38 MAIN STEAM STOP VALVE CLOSURE TEST 532 2165
OL 05:29 TRIPPED 1B1 RCP 532 2165
OL 06:11 STARTED 1B1 RCP 532 2165
BWL 08:30 REACTOR CRITICAL 532 2165
OL 12:32 STOPPED 1A2 RCP 532 2165
OL 13:29 STARTED 1A2 RCP 525 2165
OL 13:42 STOPPED 1A2 RCP 527 2165
RC
°F PSIG
OL 14:21 STARTED 1A2 RCP 524 2165
BWL 14:25 VARIED PZR LEVEL FOR ZPPT 524 2165
BWL 15:57 SECURED SPRAY 533 2165
BWL 16:54 HEATERS, SPRAY TURNED ON 533 2165
OL 21:40 STOPPED 1A2 RCP FOR THERMOCOUPLE LEAK INVESTIGATION 533 2165
OL 21:49 STARTED 1A2 RCP 532 2165
TOP THERMOCOUPLE
BOTTOM THERMOCOUPLE
OL-OPERATOR'S LOGBOOK
BWL-B&W LOG

Figure 7.11
Duration Above Different Magnitudes of Stratification During Heatup



8. SUMMARY AND CONCLUSIONS

This report describes the B&W Owners Group program for addressing the surgeline thermal stratification and thermal striping issue and presents the results of the preliminary work done to justify continued operation until the final program results are available. The Owners group plan is the same as was presented to the Nuclear Regulatory Commission staff on September 29, 1988 and April 7, 1989. It consists of three parts: bounding calculations to justify near term continued operation, a measurement program to quantify the phenomenon, and the final analysis using the plant data.

The results to-date show that near term safe plant operation is assured. The measurement program results are currently being assessed. When this is completed, and the work on thermal striping is finalized, the entire program will be in place. The striping analysis to comply with the requirements of NRC Bulletin 88-11 Item 1.b is expected to be completed by October 31, 1989.

Preliminary results from the striping evaluation show that the fatigue impact on the surgeline is estimated to be approximately 0.10 of the usage factor. The evaluation shows that temperature differentials in the surge line with the plant at power are not large enough to affect the usage factor; those existing during the early parts of plant heatup are most significant.

Based on the interim results contained herein, it is concluded that the domestic B&W plants can continue operating safely in the near term until the final analyses are in place. Davis-Besse can be expected to operate for 3-1/2 to 7 more years without exceeding the ASME code limits while the oldest lowered loop plants can operate for an additional 5 years without exceeding the ASME code limits with the exception that the cyclic strain hardened yield strength was substituted for the virgin yield strength in the fatigue evaluation (see Section 5.1).

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2. (a) B&W Drawing No. 13191016 - Reactor Coolant Piping Assembly Plan

   (b) B&W Drawing No. 131911E9 - Reactor Coolant Piping Assembly Elevation

   (c) B&W Drawing No. 131912E7 - Assembly and Details for 10" Surgeline

3. (a) B&W Drawing No. 146615E12 - Reactor Coolant Piping Assembly Plan

   (b) B&W Drawing No. 146616E4 - Reactor Coolant Piping Assembly Elevation

   (c) B&W Drawing No. 146617E5 - Assembly and Details for 10" Surgeline

4. (a) B&W Drawing No. 141583E11 - Reactor Coolant Piping Assembly Plan

   (b) B&W Drawing No. 141584E8 - Reactor Coolant Piping Assembly Elevation

   (c) B&W Drawing No. 141585E4 Assembly and Details for 10" Surgeline

5. (a) B&W Drawing No. 131982E10 - Reactor Coolant Piping Assembly Plan

   (b) B&W Drawing No. 131983E6 - Reactor Coolant Piping Assembly Elevation

   (c) B&W Drawing No. 131984E2 - Assembly and Details for 10" Surgeline

6. (a) B&W Drawing No. 150142E13 - Reactor Coolant Piping Assembly Plan

   (b) B&W Drawing No. 150143E6 - Reactor Coolant Piping Assembly Elevation

   (c) B&W Drawing No. 150144E4 - Assembly and Details for 10" Surgeline

7. (a) B&W Drawing No. 143493E13 - Reactor Coolant Piping Assembly Plan

   (b) B&W Drawing No. 143492E10 - Reactor Coolant Piping Assembly Elevation

   (c) B&W Drawing No. 143503E6 - Assembly and Details for 10" Surgeline

8. (a) Reactor Coolant Piping Assembly Plan View - 152055E7

(b) Reactor Coolant Piping Assembly Elevation View - 152056E4

(c) B&W Drawing No. 152030E8 - Assembly and Details for 10" Surgeline

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10. DOCUMENT SIGNATURES

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This report has been approved for release.

F. R. Burke
Program Manager

APPENDIX A

Oconee Unit 1 Surge Line Measurement Program

1. PURPOSE

The purpose of the test program was to collect data on the pressurizer surge line during plant heatup, power operation, and plant cooldown in order to determine:

a. The magnitude and extent of thermal stratification in the pressurizer surge line, and

b. The magnitude and direction of surge line displacement.

The thermocouple data will be used to evaluate the need for redefinition of the design bases for the surge line and additional stress and fatigue analysis and/or modifications to operating procedures to minimize stratification effects. The displacement data will be used to confirm analytical predictions of the surge line displacements. Following this validation, computer simulations will be used to resolve concerns about closure of pipe whip restraint gaps and snubber travel.

2. TEST PLAN

The program was oriented toward maximizing the recording of data throughout the entire plant evolution taking the plant from a cold, depressurized condition to hot full power and back down to cold conditions. The recording of surge line sensor data began prior to initial energizing of the pressurizer heaters and continued through power escalation. Data will be recorded during upsets and cooldown to cold shutdown. No alterations to the plant's normal startup procedures were made because of this data recording program. During periods of steady operation, such as zero power physics testing, the data collection system was re-configured to record selected thermocouples at higher scan rates (from 0.6 to 1.2 seconds) than the configuration allowed with all temperature and displacement sensors being recorded. These selected data acquisition periods, called "striping runs", optimized the system's ability to detect temperature oscillations at the exterior of the surge line wall.

The data acquisition included major plant parameters to enable correlation of plant conditions to the surge line conditions, especially with regard to transients that occur in the surge line. Data was continuously recorded at a sample rate of 20 seconds for the entire data acquisition period. Short pauses in the data collection process to enable downloading of data were acceptable, but were done to the extent possible during steady state periods.

3. DATA REQUIREMENTS

3.1. Measurement Sensors

The locations of thermocouples along the surge line pipe are shown on Figure 6.1. A total of 54 thermocouples were installed as specified in the orientation key at each of the locations shown. The number and distribution of thermocouples are set to observe the temperature distribution in a plane perpendicular to the pipe length with emphasis on the bend closest to the pressurizer and the horizontal sections of the line.

The location of displacement sensors along the surge line are shown in Figure A1. In order to provide displacement measurements in all directions at each of the locations shown, a total of 25 sensors are installed. Two additional sensors at locations 6Y and 10Y are provided for redundancy and signal comparison between sensor types. The number and distribution of displacement meters along the surge line were set to observe the amount and direction of pipe displacement during plant heatup and cooldown.

3.2. System Data

Numerous reactor coolant parameters were recorded simultaneously during the data acquisition period. These included reactor coolant loop temperatures (hot and cold legs), pressurizer temperature, level, and pressure, reactor coolant pump status, and selected balance of plant parameters. This data will be correlated with changes in surge line conditions to identify what plant evolutions have significant impacts on stress conditions in the surge line.

3.3. Preliminary Results

A preliminary evaluation of the Oconee data has shown that the typical range of surge line temperatures with the plant at 100% power is 490 to 600 F with only one location registering temperatures as low as 490 F. All other locations show minimum values of 530 to 540 F. The evaluation of the data will explore the validity and significance of this deviation. In the meantime, a traditional fatigue assessment has shown that for the surge line material (stainless 316) that a zero-to-peak temperature fluctuation of 45^{o}F will result in an alternating stress equal to the endurance limit from the ASME design high-cycle fatigue curve. This result is derived using the relationship = 1.43E T and an endurance limit of 16,500 psi at 10" cycles. This temperature fluctuation is equivalent to a peak-to-peak temperature difference of 90^{o}F. Therefore, if thermal striping is an operative phenomenon at power, the maximum temperature differential in the surge line cannot cause the endurance limit of the material to be exceeded.

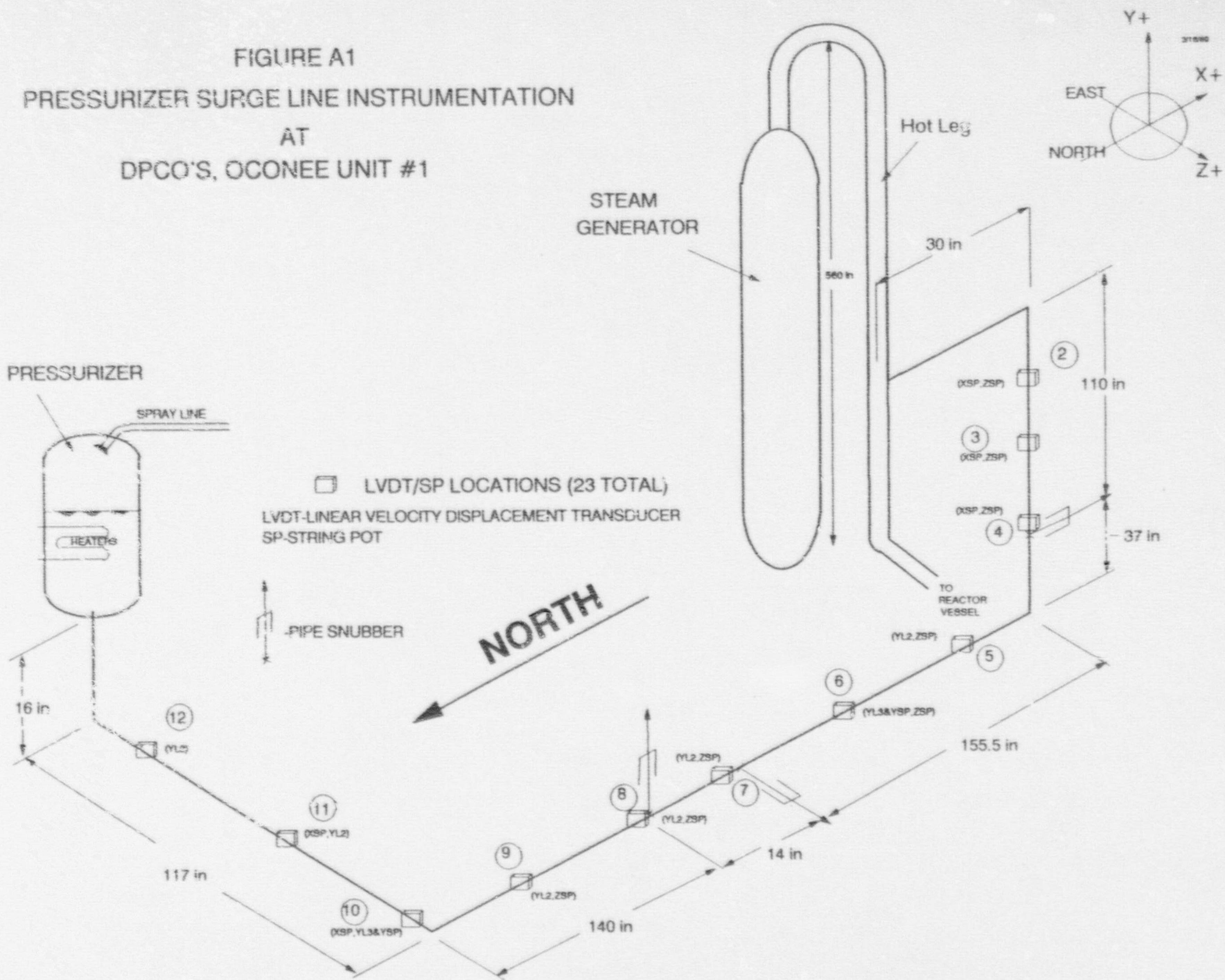
A more detailed evaluation of the data, as described in Subsection 7.2, is expected to show that the maximum temperature oscillation at any one point on the surge line inside surface is significantly less than the top-to-bottom temperature differential. During the vast majority of the plant's life, the reactor coolant system will either be at power conditions or at cold shutdown. At cold shutdown there are no thermal gradients in the surge line to contribute to a striping effect. At power, the temperature differential is stable and relatively small. These factors suggest that for the quiescent conditions which characterize the largest fraction of the surge line operating life the thermal striping effect may be rather small.

FIGURE A1

PRESSURIZER SURGE LINE INSTRUMENTATION

AT

DPCO'S, OCONEE UNIT #1



APPENDIX B

Verification of the Bounding Fatigue Analyses
by Using Oconee Unit 1 Temperature Measurements

In light of the surge line temperature measurements performed during the February 1989 Heatup of Oconee Unit 1, it was decided to review the bounding fatigue analyses previously performed (see description in Subsections 5.1 and 5.2).

First of all, the effect of the non-linear temperature profile was compared to the assumed linear temperature profile. The non-linear temperature profile which corresponds to the maximum top to bottom temperature difference was evaluated (403F at the top and 123 F at the bottom, for a difference of 280F). A finite element surge line model was built to determine an equivalent linear temperature profile. This surge line model was loaded respectively with a linear temperature profile varying from 403F at the top to 123F at the bottom, and with the actually measured non-linear temperature profile. It was found that the resulting rotation of the pipe cross-section due to the actual non-linear temperature profile is 25% higher than the one due to the linear temperature profile.

In a subsequent step, the temperature variations measured during the Oconee Unit 1 heatup have been scanned and 28 thermal stratification cycles have been counted and retained. The 28 peaks range from a 280F to a 65F top to bottom temperature difference. These 28 thermal stratification cycles represent a good picture of the temperature variations to be expected during a plant heatup. Table B-1 gives an overview of the temperature differences assumed in the bounding fatigue analyses, compared to the ones measured during the February 1989 Oconee Unit 1 heatup.

Table B-1. Top to Bottom Temperature Differences (Temperatures in F)

Bounding Fatigue Analyses		Measurements at Oconee Unit 1
Oconee Unit 1	Davis-Besse	(February 1989)
HEATUP:		
422	386	280
330	309	250
330	309	250
330	309	240
-	301	220
		+ 23 additional cycles with temperature differences ranging from 206F to 65F.
COOLDOWN:		
306	309	No complete cooldown data available to date.

The comparative study described below has been performed.

First, the peak stress ranges calculated in the bounding fatigue analyses for the maximum top to bottom temperature differences (422F and 386F respectively) are:

1. scaled down in accordance with the different top to bottom temperature differences measured at Oconee Unit 1,

2. multiplied by 1.25 to reflect the increased rotation due to the nonlinearity of the temperature profile,

3. added to the corresponding thermal striping peak stresses (described in Subsection 7.3.3).

An alternating stress range is then calculated for each measured top to bottom temperature difference, leading to an allowable number of cycles from the fatigue curves given in Appendix I of the Section III ASME Code. The heatup thermal stratification usage factor results from the summation of the products "number of heatups times number of cycles per heatup" divided by the allowable number of cycles (for each measured top to bottom temperature difference).

The revised cumulative usage factor is the sum of the usage factors from:

1. heatup thermal stratification (see above),

2. cooldown thermal stratification (from the bounding analyses),

3. stress reports for all functional specification transients (from the bounding analyses),

4. thermal striping (as described in Subsection 7.3.3).

The above described fatigue evaluation has been performed for both Oconee Unit 1 and Davis-Besse. The fatigue results from the bounding analyses (Subsections 5.1 and 5.2) were found to envelope the fatigue results using the Oconee Unit 1 temperature measurements for the most critical locations.

APPENDIX C

Justification for Use of Cyclic Strain-Hardened Yield Strength

1. PURPOSE

The purpose of this discussion is to provide a technical justification for the use of twice the cyclically strain-hardened yield strength in place of the limit 3 S_m specified in Section III of the ASME Boiler and Pressure Vessel Code.

2. SIGNIFICANCE OF $3S_m$ LIMIT

In the design of pressure vessels, the applied loads frequently result in stresses that exceed the yield strength of the material. This is particularly true of stresses that arise due to the constraint of the material when subjected to a temperature gradient. Since a reasonably exact analysis of such non-linear cyclic stresses would be a formidable and expensive task even with the analytical tools available today, simplified methods have been developed to ensure adequate design margins. The authors of ASME Section III chose the method of elastic strain invariance as the basis for the design procedures in Section III. This method permits the use of calculations based on elastic material behavior if certain requirements are satisfied. In Section III, this requirement is specified by limiting the range of primary plus secondary stress intensities to $3S_m$. The basic idea is to ensure that, after a few cycles of limited plastic deformation, the structure "shakes down" to elastic action, i.e., after a few cycles, a residual stress pattern will develop about which subsequent stress cycles behave linearly. The following description is taken from Ref. 1.

In the study of allowable secondary stresses, a calculated elastic stress range equal to twice the yield stress has a special significance. It determines the borderline between loads which, when repetitively applied, allow the structure to "shake down" to elastic action and loads which produce plastic action each time they are applied. The theory of limit design provides rigorous proof of this statement, but the validity of the concept can easily be visualized. Consider, for example, the outer fiber of a beam which is strained in tension to a strain value ϵ_1, somewhat beyond the yield strain as shown in Fig. 1 by the path OAB. The calculated elastic stress would be $S = S_1 = E\epsilon_1$. Since we are considering the case of a secondary stress, we shall assume that the nature of the loading is such as to cycle the strain from zero to ϵ_1 and back to zero, rather than cycling the stress from zero to S_1, and back to zero. When the beam is returned to its undeflected position, 0, the outer fiber has a residual compressive stress of magnitude $S_1 - S_y$. On any subsequent loading, this residual compression must be removed before the stress goes into tension and thus the elastic range has been increased by the quantity $S_1 - S_y$. If $S_1 = 2S_y$, the elastic range becomes $2S_y$, but if $S_1 > 2S_y$, the fiber yields in compression, as shown by EF in Fig. 1(b) and all subsequent cycles produce plastic strain. Therefore, $2S_y$ is the maximum value of calculated secondary elastic stress range which will "shake down" to purely elastic action.

3. FATIGUE ANALYSIS WHEN $3S_m$ LIMIT IS EXCEEDED

As explained in Paragraph II, a prerequisite for a valid fatigue analysis is satisfaction of the $3S_m$ limit for the range of primary plus secondary stress intensities. As further discussed in Paragraph II, the limit on these stresses is (1) to ensure that the use of linear elastic analyses will yield reasonably accurate results even though the yield strength of the material may be exceeded locally and (2) to ensure that shakedown occurs, i.e., that after a few cycles of limited plastic deformation, the structure behaves linearly with no progressive distortion during each load cycle. If the range of primary plus secondary stress intensities is in fact exceeded, neither of these goals can be assumed to have been met and the procedure for calculating the usage factor must be modified.

From the structural viewpoint, the principal concern with cyclic stresses in the plastic region is that high values of strain concentration can occur which, if not properly accounted for, can lead to fatigue failure earlier than would be predicted from a purely elastic analysis. Since an accurate means of calculating these strain concentrations was not available to the authors of the Code rules twenty years ago, simplified methods (Simplified Elastic-Plastic Fatigue Analyses) were developed to account for any strain concentrations that might occur. Each of these methods involves the calculation of a factor to be applied to the alternating stress before entering the fatigue curve. This factor represents a strain concentration factor and is applied in the fatigue analysis in a manner similar to a stress concentration factor. See, for example, Ref. 2, Para. NB-3653.6.

While the simplified procedure defined in Section III is conservative and easy to use, it includes simplifying assumptions with regard to the behavior of material in the inelastic range.

In deriving the $3S_m$ limit, use was made of a linear-elastic/perfectly-plastic (i.e., horizontal) stress-strain diagram (see Fig. 1) and it was shown that elastic behavior is assured provided that the stress range does not exceed 2 S_y, where S_y is the static yield strength. The use of a horizontal stress-strain diagram above S_y is conservative since the actual stress-strain curve for austenitic stainless steels exhibits strain hardening. More important, however, is the fact that austenitic stainless steels have a pronounced tendency to strain-harden under cyclic loading. The basic idea is that the strength of the material increases under cyclic plastic deformation so that the stress-strain curve shifts upward relative to the static curve. To take advantage of this effect, it would be reasonable to base the limit of elastic behavior on twice the cyclic strain-hardened yield strength ($2S_b$) rather than on twice the static yield strength ($2S_y$). This is not a new concept, as evidenced by the fact that it was incorporated in an earlier pressure vessel design code, "Tentative Structural Design Basis For Reactor Pressure Vessels and Directly Associated Components", December 1958 (Ref. 3).

The following definition of the limit of elastic behavior is taken from Ref. 3, Appendix B.

> "B.2.5 Limit of Elastic Behavior. The stress intensity S_b is defined as the limit of elastic behavior and is used in connection with the fatigue diagram to determine the reduction in mean stress produced by plastic flow. For some materials the yield stress, S_y, is higher than the endurance limit, Se. In these cases $S_b = S_y$. For materials which strain harden by appreciable amounts, the endurance limit may be appreciably higher than the yield stress of the annealed material. Thus S_y is not the true limit of elastic behavior and the endurance limit is a more realistic value to take for S_b. For this purpose, the endurance limit of polished specimens without safety factor is taken as the best estimate for S_b, because the use of too low a value results in an unconservative estimate of the alleviating effects of the plastic flow."

From this definition of the limit of elastic behavior, a reasonable approximation to the cyclically strain-hardened yield strength (S_b) may be obtained by using the alternating stress to failure at 10^6 cycles of polished specimens without safety factor. Since the value of S_a at 10^6 cycles on the design fatigue curve is equal to one-half the value of the actual failure S_a (the design fatigue curve contains a factor of two on the failure stress at 10^6 cycles), the cyclic strain-hardened yield strength may be approximated by twice the value of S_a on the design curve at 10^6 cycles. From the design curve (Ref. 4, Fig. I-9.2.1), $S_b = 2S_a = 2(26) = 52$ ksi.. The limit of elastic behavior, 2 S_b, would then be 2(52) = 104 ksi. Since the design fatigue curve is based on tests conducted at 70F, the value at 70F must be reduced by the ratio E_{550}/E_{70} to obtain the equivalent value at 550F. This results in a limit of elastic behavior at 550F of ($25.55 \times 10^6/28.3 \times 10^6$) X 104 = 93.9 ksi. Therefore, it can be concluded that the Code limit of $3S_m$ (= 58 ksi) could be replaced with the $2S_b$ limit (= 93.9 ksi) and still meet the stated intent of the limit, which is to ensure linear behavior after a few cycles.

Reference 5 summarizes the results of a study conducted for the ASME Code Subgroup on Fatigue Strength. The purpose of the study was to review the fatigue design methods and curves and to make improvements where possible based on new technology and data.

During the course of the study, it was necessary to make some assumption concerning the cyclic yield strength of austenitic stainless steels. Based on a review of test data, a value of 44 ksi was chosen to represent the cyclic yield strength over the range 70F to 800F. This implies a limit of elastic behavior equal to 2 X 44 = 88 ksi at 800F. To adjust this to 550F , the value 88 ksi is multiplied by the ratio $E_{550}/E_{800} = 25.55/24.1 = 1.06$. This results in 88 x 1.06 = 93.2 ksi., which is only 0.8% less than the value 93.9 ksi. derived above.

A quantitative verification that the use of $2S_b$ as the elastic limit is reasonable can be provided by estimating the plastic strain per cycle. The Coffin-Manson equation (Ref. 6, page 412) relating plastic strain range per cycle to the number of cycles to failure (N) is

$$\Delta\epsilon_p N^{1/2} = C \text{ (Constant)}$$

Assuming that the static tensile test consists of one-quarter cycle and that the total plastic strain is the true strain at fracture, one obtains,

$$\mathrm{Ln}\left(\frac{100}{100-RA}\right) \cdot (1/4)^{1/2} = C$$

where RA is the percent reduction in area at fracture. From Ref. 1, Fig 11, RA = 72.6%. Then

$$C = (1/4)^{1/2}\ \mathrm{Ln}\left(\frac{100}{100-72.6}\right)$$

Using Eq. 1 with $N = 10^6$ cycles,

$$(10^6)^{1/2} \Delta\epsilon_p = 0.6473$$

$$\Delta\epsilon_p = 0.0006$$

The quantity $\Delta\epsilon_p$ is the plastic strain range per cycle based on an elastically calculated stress range of 93.9 ksi. This is less than one-third the plastic strain used to define the 0.2% offset yield strength for steels (ϵ=0.002). This result shows that the plastic strain per cycle based on the use of $2S_b$ as the elastic limit is inconsequential and that the use of linear elastic mechanics is reasonable as long as the stress range does not exceed $2S_b$.

4. CONCLUSION

Use of twice the strain-hardened yield strength in place of the limit $3S_m$ specified in ASME Section III, while less conservative than the Code value, satisfies the intent of the Code limit.

5. REFERENCES

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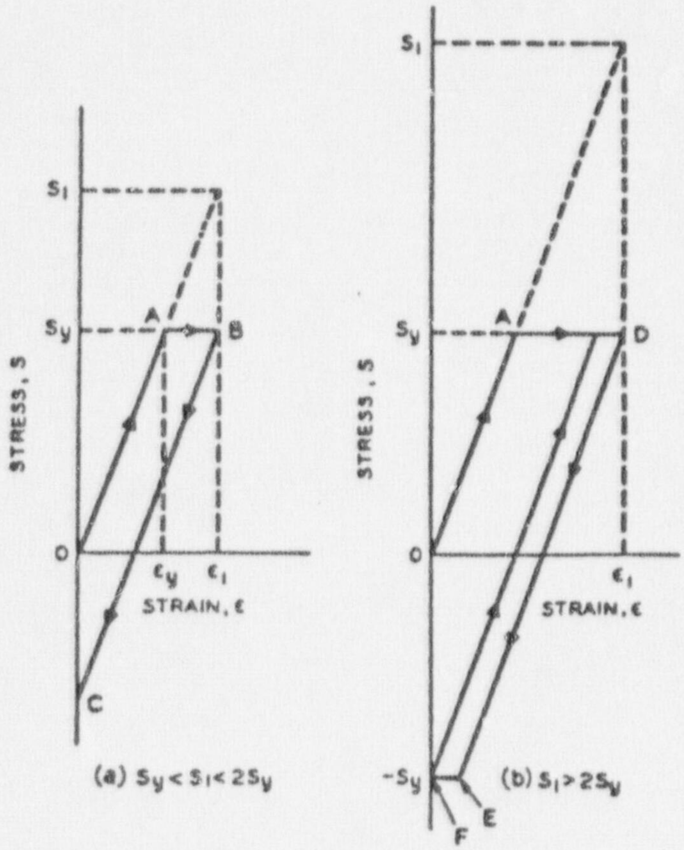

STRAIN HISTORY BEYOND YIELD

Figure C.1