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Battelle

Pacific Northwest Laboratories
Richland, Washington 99352

AEC Research and Development Report

DEFECT STUDY - PRESSURE VESSEL
CSE REACTOR VESSEL SIMULATOR

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June 1971

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Health and Safety

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Printed in the United States of America
Available from
National Technical Information Service
U.S. Department of Commerce
5285 Port Royal Road
Springfield, Virginia 22151
Price: Printed Copy \$3.00; Microfiche \$0.95

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ABSTRACT

A large laminar-type defect was detected by ultrasonic tests in the 150 ft³ pressure vessel used for coolant blowdown experiments, following a thrust support failure. Extensive testing and analysis of core samples indicated that the defect was an inclusion containing calcium and aluminum oxide and that the base metal surrounding the flaw had adequate strength and ductility. Analytical studies of potential crack propagation led to the conclusion that further growth into the sound metal was unlikely and the decision was made to continue the testing program. Careful monitoring of the defect region was carried on following each of the ~ 50 blowdowns made after detection of the flaw. No further growth of the defect region was noted.

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1.0 INTRODUCTION

This report presents the results of studies of a large laminar defect indication in a simulated nuclear reactor vessel used in experiments on coolant blowdown as part of a reactor safety research program. The reactor simulator vessel (Figure 1) was designed and fabricated to meet the requirements of Section III of the ASME Boiler and Pressure Vessel Code.

The vessel was used for a series of experiments in which water in the vessel at temperatures up to 600 °F and pressures up to 2500 psig was suddenly released to atmosphere by initiating rupture of a double rupture disc assembly installed on a blowdown nozzle on the bottom head of the vessel.

During one of these experiments, a thrust support structural member failed allowing the vessel to move within its support framework. Although no obvious damage to the pressure vessel was evident, a thorough nondestructive test program was performed to determine if the accident had caused any deterioration in the vessel integrity.

Ultrasonic testing disclosed a laminar type defect indication in the bottom head near the blowdown nozzle. The defect indication size exceeded that allowed by the Code.⁽¹⁾

Three core samples were removed from the defect region for extensive metallurgical tests, and a series of comprehensive stress analyses were performed to evaluate the effect of the defect on the vessel.

Based on the core sample test results and stress analyses, it was concluded that the risk of catastrophic vessel failure was sufficiently small to permit the blowdown experiments to continue without repair of the defect indication. The experimental program was broadened to include

NOTE: Blowdown Experiments
Conducted from
Nozzle A, B, or C.

FIGURE 1: CSE Blowdown Facility

defect surveillance by acoustic emission monitoring and extensive temperature and strain data from the defect region. The defect indication was also monitored by ultrasonic tests after blowdown tests.

This report presents details of the vessel fabrication history, service, results of the core sample tests and stress analyses on the defect, and results of the defect surveillance after the vessel was restored to service.

2.0 SUMMARY AND CONCLUSIONS

A large laminar type defect indication was found by ultrasonic tests following a thrust support failure accident on a 150 ft³ pressure vessel used for coolant blowdown experiments. The vessel had been designed and fabricated in accordance with the ASME Boiler and Pressure Vessel Code, Section III for Class A Nuclear Vessels with design conditions of 600 °F and 2750 psig. Material was A-212 grade B.

Extensive nondestructive test results were available from the inspections made during fabrication and before shipment from the vendor's plant, and the defect indication was not found before shipment.

Two unacceptable defect indications had been found and repaired in the bottom head plate for the vessel. The repaired plate successfully passed the ultrasonic tests, and no ultrasonic test defect indications were found in the bottom head during the final 100% coverage test on the vessel.

The defect indications found after the support failure accident exceeded the size allowed by the Code. In order to evaluate the nature of the defect indications, three core samples were removed from the defect region. Extensive metallographic examination of these core samples revealed that the ultrasonic test defect indications resulted from inclusions and that these inclusions were very probably a portion of those which were detected and repaired in the bottom head plate during vessel fabrication.

Tensile tests were performed on the core samples. Results showed that the base metal had adequate strength and ductility; however, in the region of the inclusions, flaws were so extensive that the nominal stress at failure was low, since only metal in the plane of the flaws was able to deform.

Fractographic and electron microprobe examinations revealed that the flaws were calcium and aluminum oxide inclusions. No evidence of other contributory substances was found. The inclusions formed separations (cracks) at very low strain levels. Except for the flaws, all metal fracture was by plastic dimpling. Hence, a crack could propagate along the inclusion flaw in a brittle manner, but the metal around the flaw was tough and ductile.

Stress analyses were performed in order to assess the effect of the defect on the integrity of the vessel, and thereby to investigate the possibility that the defect region might propagate in a manner which could lead to catastrophic vessel failure. If possible, with acceptable risk, it was desired to continue the blowdown experimental program without the delay and expense of repairing the defect indication. Further, the situation afforded a unique opportunity to monitor the propagation of a "real" defect in a pressure vessel under severe thermal load service conditions.

A finite element method was utilized for both the elastic and thermal load conditions in order to model the stress situation for use in a fracture mechanics analysis. An energy method was developed for computing the stress intensity factor rather than making use of the stress or displacement field predictions in the vicinity of the crack tip. Comparisons of the computed stress intensity factors for one-half and four inch laminar cracks with the values for unstable crack propagation showed there was negligible chance of unstable crack behavior if the metal surrounding the defect is indeed sound. Crack growth rates also were calculated to be so small that subsequent detectable growth into sound metal is very unlikely. If additional weakened areas exist, then additional crack growth could occur.

The possibility of the crack turning toward the vessel surface was examined, and it was calculated that this possibility could be ruled out. Finally, the most likely direction for propagation of the laminar crack was found to be away from the blowdown nozzle, and this was not considered dangerous.

Based on the core sample examinations and the stress analyses, it was judged safe to continue experimental work with the unrepaired defect in the vessel. However, temperature and strain sensors were installed on the bottom head to obtain data for further stress analyses, and an acoustic monitoring system was installed on the vessel to detect the acoustic noise emission resulting from defect propagation. In addition, the defect region was rechecked by ultrasonic test after each blowdown to determine if the outline or characteristics had changed.

No significant change in the size or characteristics of the defect region occurred during the ~50 blowdowns conducted since tests were resumed, based on ultrasonic tests. The acoustic emission system detected a few indications from locations in the general defect region. These regions were given careful ultrasonic examination, but no specific changes were found that could correlate with the acoustic emission information.

In conclusion, the defect indication has been stable since its discovery. It is almost certain that the defect originated in the inclusions which were not detected and removed at the time of repair of the unacceptable UT test indications during vessel fabrication. It is outside the scope of this report to comment on topics such as the inability of ultrasonic tests on the repaired plate to detect the remaining inclusions and the implications in terms of test criteria for nuclear vessels. It was reassuring to find the defect did not propagate, even under very severe thermal load conditions; however, the extent to which this behavior may be generally applicable is unknown.

3.0 VESSEL FABRICATION

Design specifications, service conditions and manufacturing history of the reactor vessel simulator are included in this section. The vessel was built under and conformed to Section III of the ASME Pressure Vessel and Boiler Code.

3.1 Procurement Specifications

The reactor vessel simulator was procured for use in experimental studies of reactor accidents. The vessel was specified to contain 150 cubic feet of water at 2750 psig and 650 °F and be serviceable for 500 blowdown cycles. Detailed specifications of the vessel are in Appendix A. Nozzles shown in Figure 1 are used for blowdown nozzles or instrumentation.

3.1.1 Vessel Operation Procedures

An operational cycle begins with the vessel being filled with water (70 °F). The fluid is then pressurized to a prescribed level normally between 1000 and 2500 psig. The water is then circulated through an external loop which contains a heater which will heat the water in the vessel to the test temperature, up to 600 °F, in about 6 to 7 hours. After the desired fluid conditions are established, the vessel is isolated from the loop and the rupture discs in the discharge nozzle are broken allowing the hot, pressurized fluid to be discharged. Following the discharge of the water, the vessel is allowed to cool in the ambient environment to its initial state. (The vessel is insulated and cooling takes several days). Different types of cycles are possible depending on the blowdown nozzle used, the diameter of the break, and the initial conditions of the fluid. All the water is not discharged every time. The amount of water remaining depends on the nozzle used and experimental conditions. Boiling of the residual fluid in the lower vessel induces a thermal gradient and the accompanying thermal stresses in the vessel head.

The discharging water creates a horizontal thrust force on the vessel which is restrained by structural members between the four thrust-restraint vessel trunnions and the supporting structure. Any rotational tendency of

the vessel about its axial centerline is restrained by four lateral stabilizer support bars normal to the axis of the blowdown nozzle.

3.2 Design Stress Analyses

Stress analyses were performed for the manufacturer of the vessel, Struthers-Wells, Inc. by Lessel's and Associates, Inc. Analytical methods varied from hand calculations to computer codes. A major digital computer code SOR II⁽²⁾ was used to determine the stresses and deformations in an axisymmetric shell subjected to mechanical and/or thermal loading. Stresses in the shell caused by external loads on the nozzles were found in accordance with Prof. Bijlaard's work, condensed by the PVRC Design Division and Subcommittee on reinforced openings and external loads.⁽²⁾

Thermal analyses of the vessel used for the Lessel's and Associate, Inc. stress calculations, were performed under subcontract by Foster-Miller Associates, Inc. The thermal analyses treated the vessel as a lumped parameter network of thermal capacitances and conductances.

The most conservative results from the thermal analyses were used in the stress code SOR II to obtain thermal stress intensities.

3.2.1 Stress Analyses Results - Summary

Summary results abstracted from the design stress analyses performed by Lessels and Associates are presented in this section.

Design Mechanical and Pressure Loads

Maximum stress intensities and locations together with the corresponding Code allowable stress intensities are as follows:

(a) Primary General Membrane Stress Intensity

Location: Vessel shell away from any discontinuity

Stress intensity:

maximum: 17,400 psi

allowable: $S_m = 18,200$ psi

(b) Local Membrane Stress Intensity

Location: Vessel shell 3 inches below junction with tapered portion of flange

Stress intensity:

maximum: 21,900 psi

allowable: $1.1 S_m = 28,000$ psi

(c) Primary Plus Secondary Stress Intensity

Location: Spherical head at junction with tapered portion of flange

Stress intensity:

maximum: 42,200 psi

allowable: 56,100 psi

3.2.2 Transient Thermal Loading

The shell, spherical head, instrument ring, and nozzles were considered for three transient thermal conditions:

- warm up
- 50 percent water loss blowdown; blowdown through nozzle B (Figure 1)
- 100 percent water loss blowdown; blowdown through nozzle C (Figure 1)

The warm up thermal loading condition causes the larger stress intensities. The maximum thermal stress intensities occur at 5.3 hours after the heat-up starts and are as follows:

(a) Location: Vessel shell at junction with tapered portion of flange

Stress intensity:

maximum: 32,700 psi

allowable: $3 S_m = 56,100$ psi

(b) Location: Spherical head at junction with tapered portion of flange

Stress intensity:

maximum: 28,200 psi

allowable: $3 S_m = 56,100$ psi

The 50 percent water loss blowdown case (blowdown from nozzle B) resulted in maximum stress intensities in the nozzles. Since at least one of each nozzle types (sizes and purpose; i.e. blowdown or instrumentation) are below the water line (a conservative case) in the 50 percent case, this

condition was used for the calculations. Maximum thermal intensities occurred at 6 hours after the 50 percent blowdown case took place. Results of calculations based on this condition are summarized below:

(a) Location: Nozzle O

Stress intensity:

maximum: 10,400 psi

allowable: $3 S_m = 53,400$ psi

(b) Location: Nozzle N

Stress intensity:

maximum: 13,700 psi

allowable: $3 S_m = 53,400$ psi

(c) Location: Nozzle A

Stress intensity:

maximum: 11,400 psi

allowable: $3 S_m = 53,400$ psi

Design of the reactor vessel simulator complies with the requirements for a class A pressure vessel of Section III of the ASME Pressure Vessel and Boiler Code on the basis of the stress analyses results.

3.3 Vessel Manufacture

The steel for the shell and hemispherical heads was manufactured by Lukens Steel Company. The nozzles were forged and machined by Tube Turns Inc. These sections were magnetic-particle and ultrasonically tested and shipped to Struthers-Wells Inc. for final forming and assembly. Prior to assembly, each part was again magnetic-particle and ultrasonically tested.

Several defects were noted during the pre-assembly inspection; however, only two defects exceeded the ASME Pressure Vessel and Boiler Code specifications. These defect indications were located in the bottom hemispherical head plate and are shown in Figure 2. The defective regions were gouged and ground out until magnetic-particle inspection

results indicated that the defect regions were completely removed. The defect regions were then weld repaired by adding successive layers of weld metal. The first layer of weld metal and every layer of weld metal thereafter was magnetic-particle tested. The head plate was maintained at 250 °F during the metal removal and welding processes. All repairs were made in accordance with procedures approved by Buyer engineers and inspectors. When the defect region was completely full and the outside surface ground down, the welded region was again ultrasonically inspected. Results from this inspection indicated that the repair was satisfactory and that the head plate met ASME Boiler and Pressure Vessel Code specifications.

FIGURE 2: Defect Regions in Bottom Head Noted and Repaired During Manufacture

Final assembly of the vessel shell, heads and nozzles was performed. The assembly was tested (hydro, ultrasonic, and dye penetrant) in accordance with ASME Pressure Vessel and Boiler Code specifications prior to shipment.

4.0 BLOWDOWN ACCIDENT AND POST ACCIDENT DEFECT INDICATION

The reactor vessel simulator was received from the manufacturer, installed and the first test was run on 2/7/67. Several blowdown experiments were then conducted as shown in Table 1. The fluid was discharged through the middle nozzle (Nozzle B) on acceptance tests B-1, and B-2, and through the bottom nozzle (Nozzle C, Figure 1) for the experiments B-3 and B-18, except as noted. All experiments consisted of filling the vessel with water; pressurizing and/or heating the water until the desired initial conditions were reached and then breaking the rupture discs allowing the hot pressurized water to be discharged. Horizontal thrust reaction forces were restrained by structural columns attached between the four vessel thrust restraint trunnions and the foundation.

Blowdown experiments B-1 through B-17 were conducted satisfactorily. Experiment B-18 was scheduled as an 8 inch (nominal diameter) break at 2400 psig and 600 °F. A structural failure occurred during this experiment when a thrust support column attached to a lower trunnion apparently buckled (Figure 3). This allowed the vessel to pivot about the upper trunnions until the vessel contacted the supporting structure. The structural failure was essentially complete approximately 100 milliseconds after the rupture discs were broken. Loads applied to the trunnions are unknown because the attitude of the vessel and the amount of bending applied on the load cells. Regardless of the magnitude of the forces applied to the trunnions, inspection of the trunnions, and the vessel shell immediately around the trunnions and the blowdown nozzle seemed prudent. Visual observations of the test facility after the accident indicated that the blowdown nozzle had not contacted the concrete foundation or any other structural member.

TABLE 1. Blowdown Summary

Run No.	Date	Break Area-ft ²	Initial Conditions	
			Pressure, psig	Temp. °F
B-1				
B-2		0.0156		
B-3	3/10/67	0.0156	600	Amb.
B-4	3/24/67	0.0643	600	Amb.
B-5	4/3/67	0.0643	1550	Amb.
B-6	6/2/67	0.146	1550	Amb.
B-7	6/22/67	0.0643	680	500
B-8	6/29/67	0.253	650	500
B-12	7/6/67	0.0643	1550	420
B-6B	8/18/67	0.253	450	Amb.
B-9	9/1/67	0.253	1600	575
B-10	9/6/67	0.0156	1250	575
B-11	9/8/67	0.0643	1250	575
B-12B	9/12/67	0.0643	1550	595
B-13	9/14/67	0.253	1550	615
B-14	10/17/67	0.0643	2160	500
B-15	10/23/67	0.0643	2185	560
B-16	10/24/67	0.0643	2384	600
B-14B	10/25/67	0.0643	2160	500
B-17	10/26/67	0.0156	2375	600
B-16B	11/7/67	0.0643	2385	600
B-18	11/10/67	0.253	2400	600
B-19A	12/16/68	0.0643	2150	600
B-19B	2/4/69	0.0643	2150	600
B-20A	2/20/69	0.0643	990	500
B-20B	2/28/69	0.0643	968	500
B-21	3/28/69	0.1460	999	500
B-23	5/7/69		2152	500
B-24	3/8/69	0.0643	810	545
B-27		0.0643	1000	545

Structural support failure occurred on experiment B-18

FIGURE 3: Photograph of Buckled Trunnion Arm

4.1 Post Accident NDT Inspection

NDT inspection results of the simulator vessel following the B-18 experiment indicated acceptable and unacceptable defects existed within the wall of the vessel. The significant defects were found during the ultrasonic tests. All tests and results⁽⁷⁾ from these tests are discussed below:

4.1.1 Radiography

Two small discontinuities were detected from these tests. Each of these appeared to be a small void or low density inclusions in the nozzle N wall. Both defects are observed on the original inspection reports and neither has apparently changed. These defects are within that allowed⁽¹⁾ and are therefore acceptable.

4.1.2 Dye Penetrant

Dye penetrant inspection revealed one surface defect in nozzle C and nozzle N. Each defect was noted by the manufacturer and accepted by the buyer. Each defect was in the stainless steel cladding and was probably caused by welding irregularities during the welding of the cladding. Neither defect is detrimental to the vessel. The cladding was neglected for stress calculations and was specified only for corrosion protection.

4.1.3 Magnetic Particle

Results from this inspection did not reveal any defects. This inspection technique could not be used on the inside surface of the vessel because of the nonmagnetic cladding material.

4.1.4 Ultrasonic

Ultrasonic inspection results were the more useful of the various NDT inspection techniques. Several indications were noted including one which was unacceptable. The defect area (shown in Figure 4) which exceeded ASME Boiler and Pressure Vessel Code Specifications was easily found during the UT tests. Results from normal and oblique radiograph tests did not indicate this defect area.

FIGURE 4: Photograph of Defect Area - CSE

The defect in the top hemispherical head initially located during final inspection of the vessel was "relocated" during the post accident inspection. This defect was not observed during ultrasonic inspection of the plate before forming into the hemispherical head. The original defect was 6.5 inches long by 0.75 inch wide with a 20% to 40% loss of back reflection and was acceptable.⁽²⁾ This defect had enlarged at the time of the post accident inspection to 7.9 inches by 1.5 inches with a 10% to 80% loss of back reflection. This enlarged defect is within specification limits and is acceptable.⁽³⁾

Three small (maximum diameter of 1/4 inch) defect indications were located in the vessel shell around nozzle D. These indications were not observed during manufacture and are acceptable.

The immediate question after the accident was whether the vessel was satisfactory for blowdown service or if the defect must be repaired before blowdown experiments could be resumed. Also of significance was the unique opportunity to study a laminar type defect in a pressure vessel manufactured and accepted under ASME Boiler and Pressure Vessel Code specifications. It was believed that valuable information regarding propagation of laminar defects in pressure vessels might be obtained from monitoring the defect indication when the blowdown experiments were resumed.

Continuation of the blowdown program without first repairing the defect area then depended on the strength of the bottom hemispherical head including the defect region. Evaluation of the strength of the bottom head was based on stress analyses results which are summarized in section 6.0 of this report and results from extensive tests made on the core samples removed from the defect indication region.

5.0 CORE SAMPLE EXAMINATION

Three core samples (denoted A, Y, and V) were removed from the defect indication region for testing. Sample A was removed from an apparently sound region but near the defect indication region. Sample V was removed

from an area with a partial loss of back reflection of the ultrasonic signal. Sample Y was removed from an area with a complete loss of back reflection. The core sample locations are shown in Figure 4.

Removal of the core samples was accomplished by first drilling a hole perpendicular to the outside surface inward approximately $3/4$ inch. A coring cutter was then used until the depth was well beyond the defect indication region. A hole was then drilled from the inside surface until the core was free. This procedure resulted in the cores as shown in Figure 5.

Only short portions of core samples A, V, and Y were examined. These portions were taken from the level at which ultrasonic inspection had indicated flaws. The specimens were first sectioned to reveal the flaws, studied ultrasonically, radiographically, metallurgically, then tensile tested, and finally examined by fractography and microprobe techniques to identify the flaw substance. Each of these aspects is discussed separately.

5.1 Metallography

Longitudinal cross sections of the core specimens A, V, and Y were examined; photomicrographs of typical structures are shown in Figures 6 through 15. The polished surfaces were approximately $1/16$ inch deep from the OD of the $5/16$ inch core specimens. The longitudinal axis of the cores was perpendicular to the plate surface so the banding and defects in Figures 6 through 15 are elongated in the rolling direction.

In core sample A there were no ultrasonic indications nor defects evident in the cross sections. The microstructure of sample A shown in Figures 6 and 7 reveals that the region has been weld repaired. The size of the weld bead was about $1/8$ inch.

In the areas from which core sample Y was taken there was a loss of ultrasonic back reflection. The cross section shown in Figures 8 and 9 reveals the defects. The flaws are elongated in the rolling direction of the plate. The length of the longest defect in Figure 8 is $1/10$ inch.

Scale: Full Size

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* Band Spacing Determined from Photomicrographs
** Metallographic Ultrasonic, Radiographic, Tensile, Microprobe, and Fracture Surface

FIGURE 5: CSE Simulator Vessel Core Sample Layout

FIGURE 6: Etched Macrograph of Longitudinal Section of Core Sample A Showing Weld Repair. 4X

FIGURE 7: Microstructure of the Weld Repair Sample A. 75X

FIGURE 8: Macrograph of Defects in the Longitudinal Section of Core Sample Y. 4X

FIGURE 9: A Portion of the Defects in Figure 8. 75X

FIGURE 10: Macrograph of the Banding Revealed by Etching of Core Sample Y. 4X

FIGURE 11: Banding Microstructure of Figure 10. 75X

FIGURE 12: Defects in Longitudinal
Section of Core Sample V.
4X

FIGURE 13: Photomicrograph of Some
Defects in Figure 12. 75X

FIGURE 14: Etched Macrograph of Longitudinal Section of Core Sample V Showing Banding and Weld Repair. 4X

FIGURE 15: Microstructure of Figure 14 Showing Location of a Defect in the Banding Rather than in the Weld Repair. 75X

The flaws are distributed over a distance of about 1/2 inch along the length of the core sample; this multi-layered defect structure probably caused destructive interference of the ultrasonic wave so that there was no recognizable back reflection from the flaws.

The banded structure of sample Y (Figures 10 and 11) revealed by the etching indicates that this region is original plate material (has not been weld repaired). This banded structure is not uncommon in low carbon steels and results from solute segregation in the ingot. It is not a deleterious condition.

In the areas from which core sample V was taken, there were ultrasonic back reflections from defects in the plate. The position of the defects in Figures 12 and 13 correspond to the depths indicated by the ultrasonic back reflection. The fact that the defects do not appear as extensive in sample V as in Y does not necessarily reflect the relative severity in the two areas because possibly either or both sections are not typical. However, the existence of back reflection in the V area does imply that the flaws are limited to only several specific levels. The macroetch shown in Figure 14 reveals that weld repair had been performed partway through. Correlation of the flaws (Figure 12) with the macroetch in Figure 14 (note bubbles in the plastic mount) demonstrates that the flaws are in the base material, just below the weld repair. One must conclude that the remaining flaws are just a portion of those which were to have been weld repaired in these areas of the vessel. Apparently then, these flaws were either overlooked, were not deemed significant, or developed to their present extent during blowdown service after vessel manufacture.

In the polished condition, the defects are voids, although remnants of inclusions are evident, such as in Figure 9. After tensile tests, the samples were sectioned and repolished only through 1- μ diamond to preserve inclusions. Electron microprobe analysis revealed calcium and aluminum (oxide). These results will be described in detail later.

5.2 Tensile Tests

Four tensile specimens were machined from core samples A, V, and Y. Two specimens were machined from A; one labeled A was taken from the portion that had been weld repaired, while the other was taken from the original plate material and labeled A'. The original diameter of the core samples was 5/16 in. OD. The core ends were threaded and a uniform reduced diameter of 0.20 in. was machined. The axes of the tensile specimens were perpendicular to the surface of the vessel plate; and the plane of the defects was perpendicular to the axis of the specimens, and parallel to the vessel plate surface.

The load-extension results from the tensile tests are plotted in Figures 16 through 19. An ordinate of axial stress is also included, but it must be emphasized that this stress is based on the original nominal area, rather than on net unflawed area. Figures 20 through 27 are photographs taken during the testing of specimens A, V, and Y. Specimen A' was not photographed during testing, but the fractured specimens are shown in Figure 28. The fracture surfaces of specimens A, V, and Y are shown in Figure 29.

Some of the pertinent tensile information are shown in Table 2.

TABLE 2. Tensile Test Data

	A	A'	V	Y	A-212B
Gauge length, in.	0.42	0.51	0.65	0.74	
Yield load, lb	1550	1050	1560	700	
Yield strength (avg.) psi	49,400	33,400	49,600	22,300	35,000
Ult. load, lb	2,000	2,200	1,940	960	
Ult. strength (avg.) psi	63,750	70,000	61,800	30,600	66-77 ksi
Extension at ult., in.	0.047	0.061	0.040	0.023	
Strain at ult. (avg.) %	11.2	12.0	6.1	3.15	23
Extension at failure, in.	0.175	0.124	0.105	0.063	
Strain at failure (avg.) 1%	42	24	16	8.5

FIGURE 16: Tensile Test of Core Sample A; Gauge Length 0.42 in.; the Numbers Refer to the Photographs Taken During the Test

FIGURE 17: Tensile Test of Core Sample A'; the Gauge Length Was 0.51 in.

FIGURE 18: Tensile Test of Core Sample V; Gauge Length 0.65 in.; the Numbers Refer to the Photographs Taken During the Test

FIGURE 19: Tensile Test of Core Sample Y; Gauge Length 0.74 in.; the Numbers Refer to the Photographs Taken During the Test

FIGURE 20: Deformation of Core Sample A (Weld Repair) During Tensile Test. 15X

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FIGURE 21: Deformation of Core Sample A (Weld Repair) During Tensile Test. 15X

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FIGURE 22: Deformation of Core Sample V During Tensile Test. 15X

FIGURE 23: Deformation of Core Sample V During Tensile Test. 15X

FIGURE 24: Deformation of Core Sample V During Tensile Test. 15X

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FIGURE 25: Deformation of Core Sample Y During Tensile Test. 15X

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FIGURE 26: Deformation of Core Sample Y During Tensile Test. 15X

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FIGURE 27: Deformation of Core Sample Y During Tensile Test. 15X

FIGURE 28: Longitudinal and End Views of Core Sample A' (Original Plate Material) After Fracture. 7X

FIGURE 29: Fracture Surfaces of Core Samples A, V, and Y. Sample A Is Weld Repair While V and Y Are Original Plate Material. 7X

Again it must be emphasized that the stress and strain values for specimens V and Y are not based on the net material tested; furthermore, the ratio of flaw size to gauge length and specimen diameter may not correctly model the vessel plate. The nominal properties of A-212B are a yield strength of 35 ksi, ultimate of 65-75 ksi, and 23% elongation. One must apply these specifications with caution since the tensile axis of these specimens was in the plate thickness direction, while the rolling or width direction is standard practice. Also the properties are more variable in this orientation.

Specimen A was weld repair material and ultrasonic, metallographic, and radiographic examinations had revealed no defects. The tensile test results (Figures 16, 20, and 21) further demonstrate the material to be satisfactory. Specimen A', which was from the original plate material below the weld repair in A, also performed satisfactorily (Figures 17 and 28). Although A' was not sectioned, one may be reasonably certain that there were no appreciable flaws because of the high ultimate strength, good ductility, and completely fibrous fracture surface. The yield strength is somewhat anomolous, however, in that it is so low compared to specimen V (which was flawed); this behavior is probably due to variable properties in the thickness direction.

No actual flaws were evident on the surface of specimen V before the test. A small flaw became evident at the yield point. Undoubtedly this had been a prior separation open to the sample surface because it was rusted. (There was no identifiable corrosion product on any of the other flaws which subsequently appeared.) By the time of photograph V-1 (Figure 22) a second flaw was evident above the first. Subsequent sectioning and metallographic examination revealed that these flaws were just into the base metal from the weld repair, as had been previously shown in Figures 12 through 15. From V-1 through V-4 (Figures 22 and 23) the length of the principal flaw has remained about 0.09 inches long, while opening to 0.02 inches wide. This fact plus photographs V-4 and V-5 (Figure 24) demonstrates the extensive ductility that was exhibited at the crack roots

of the flaw. A number of other defects opened up on the right half of the specimen from photographs V-3 through V-5. The large load drop between photographs V-4 and V-5 was due to the failure of the tensile ligament between the principal flaw and ones to the right. The left portion of specimen V was unflawed and still continuous in photograph V-5. It eventually failed by ductile shear in V-6 (Figure 24); this shear lip covered about one-half the fracture surface (Figure 29). The above considerations and the fact that the majority of the sample extension was confined to the flawed region, demonstrated that the metal, per se, was ductile in the condition tested. But the flaws or defects produced separations (cracks) at very low strains.

A large separation about 0.12 inches long was evident on the surface of specimen Y before testing, as shown in photograph Y-1 of Figure 25. At the yield point it grew to 0.16 in. as a relatively sharp crack as shown in photograph Y-2. From this point on, the crack did not grow, but continued to open while the metal at the crack root exhibited extensive ductility (Figures 26 and 27). Therefore, the 0.04 inch propagation at the yield point must have been along a flaw. And from this occurrence one could again infer that the mechanical properties of the flaw substance are such that it either fractures itself or debonds from the metal at the onset of plastic deformation in the local region. Separations on flaws at different levels are evident in photographs Y-4 through Y-5 (Figures 26 and 27). Only the metal in the plane of the flaws contributed to the sample extension. The fracture surface of specimen Y shown in Figure 29 is essentially all flat flaws with areas of ductile shear between them. Considering the net unflawed metal area, the yield and ultimate loads are not unreasonable.

5.3 Fractographic and Electron Microprobe Analyses

In brief, the tensile tests demonstrated that the metal itself was satisfactory, but that the flaws degenerated into separations (cracks) at low strain levels. The purpose of the fractographic microprobe examinations was to determine if there was evidence of anything but ductile

(metal) fracture and to identify the substances or structure responsible for the flaws. Only specimens A and V were extensively examined; undoubtedly these contained the same conditions as the other specimens, although not to the same degree.

Figures 30 through 32 are scanning electron microscope fractographs of the fracture through the weld repair of specimen A. The entire fracture surface was dimpled with no flaws or cleavage, indicating that great amounts of plastic shear deformation had occurred to produce the fracture. Thus this dimpling appearance is corroborative evidence of the ductile behavior exhibited in the tensile test of A. The equiaxed dimples resulted from normal forces only in the specimen center. The elongated dimples resulted from the operation of both normal and shear forces at the periphery of the specimen.

Figure 33 shows the elongated dimples on the shear lip of specimen V, which demonstrates the ductile behavior of the metal. As evidenced by Figure 29, this ductile behavior extended over about one-half the specimen area. The initial flaw in photograph V-1 of Figure 22 covered about one-quarter of the area. Under the scanning electron microscope idiomorphic particles, similar to those in Figure 35, were heavily distributed across the entire area. The entire flaw was essentially on one level. The remaining one-quarter of the fracture is characterized by Figures 34 and 37. The surface was covered with small flawed regions which appeared bright, as in the center of Figure 34 and in several areas of Figure 37. These bright areas were covered with the particles shown in Figure 35. The electron microprobe analysis of the flaws revealed that calcium and aluminum were the principal elements in the particles; the results for the scan over the area in Figure 34 are shown in Figure 36. A number of flawed areas were specifically analyzed for silicon, manganese, and sulphur, and none was found. Several areas were analyzed for all elements beyond oxygen, and in each case only calcium, aluminum, and iron were present. In general the flaws were flat plateaus at different levels; the metal between these plateaus failed in a plastic manner under a combination of normal and shear forces, thus producing elongated dimples. Figure 38 shows such a structure

FIGURE 30: Fractograph of Equiaxed Dimples in the Cup Portion of Tensile Specimen A. 1,000X

FIGURE 31: Higher Magnification View of the Same Area as in Figure 30. 10,000X

FIGURE 32: Fractograph of the Elongated Dimples on the Cone or Shear Lip Portion of Tensile Specimen A. 5,000X

FIGURE 33: Elongated Dimples on the Shear Lip of Specimen V. (See Figure 29) 5,000X

FIGURE 34: Fractograph of Typical Flawed Area in Specimen V. The Bright Portion Is Particles Shown in Figure 30. The Surrounding Region Is Dimpling. 100X

FIGURE 35: Higher Magnification of the Particles in the Flaw in Figure 34. 3,000X

FIGURE 36: Results from the Electron Microprobe Scan for Calcium and Aluminum on the Fracture Surface Shown in Figures 29 and 30. There Were No Indications of Silicon, Manganese, or Sulphur. 1,000X

FIGURE 37: Fractograph of Specimen V with Several Flaws Surrounded by Bands of Plastic Dimpling. See Figure 35 for a Higher Magnification View of the Center Small Hole. 100X

FIGURE 38: Elongated Dimples in a Plastic Band Between Flaws on Different Levels. 3,000X

formed on a very steep ridge between flaws at different levels. This result is more evidence that the metal itself had undergone ductile fracture. There was no evidence of cleavage (brittle) fracture in any area examined.

Figure 39 is a stereo view of inclusions which provided the local fracture initiation. The resulting hole is evident in Figure 37. The inclusion morphology is similar to those in the flaws shown in Figure 35. The net effect on the specimen toughness of the inclusions shown in Figure 39 is that they prohibited a small area of the metal from participating in the energy-absorbing dimpling fracture process. Single, isolated spots such as shown in Figure 39 are of minor concern; however, a distribution of just such inclusions over a larger area, as shown in Figure 35, does indeed constitute a flaw which produces a separation or crack at low strains. And much less fracture energy is expended in the flaw than if the same area of metal had fractured by plastic dimpling.

An atypical structure found in one area on the fracture surface of specimen V is shown in Figures 40 and 41. From the platelet morphology (Figure 41) one may surmise that they are MnS. This structure shown in Figure 40 should not be considered a flaw as detrimental as those in Figures 34 and 37. Even though these (MnS) inclusions were distributed across a wide area, a continuous separation or crack did not form and extensive plastic deformation occurred throughout the area, as evidenced by the dimpling in Figure 41. Possible explanations for this less detrimental situation are that either the inclusions shown in Figure 40 were not so dense and continuous as those in Figures 34 and 37, or that they did not fracture or debond until larger amounts of metal strain had occurred.

The other half of tensile specimen V was longitudinally sectioned and examined by metallography and electron microprobe. The flaws shown in Figure 42 were near the fracture surface and contained calcium and aluminum (oxide) as indicated by Figure 43. An extensive examination for other elements revealed only silicon in the one position. Several microcracks are evident in 42. These indicate that the yield stress was achieved and some plastic deformation occurred here during tensile testing.

FIGURE 39: Stereo Fractograph of the Small Hole in the Center of Figure 32. The Inclusions Were the Source of Local Fracture Initiation. The Inclusion Morphology Is Similar to Those in Figure 30. The Surrounding Dimpling Demonstrates the Ductile Metal Behavior. 3,000X

FIGURE 40: Stereo Fractograph of a Structure on Specimen V Which Was Not Typical. Also a Flaw Due to Calcium and Aluminum (Oxide) Inclusions Is Evident at the Top. 100X

FIGURE 41: High Magnification of the Structure in Figure 35 Showing the Platelets Responsible for Appearance. The Lack of Idiomorphic Character to These Inclusions Is Indicative of MnS. 3,000X

FIGURE 42: Photomicrograph of Flaws Near the Fracture of Specimen V, Revealed by a Longitudinal Section. Microprobe Analysis of the Right Half Is Given in Figure 38. Note the Microcracks in the Center. 250X

FIGURE 43: Electron Microprobe Scan for Calcium, Aluminum, and Silicon on the Flaws in Figure 37. Note the Association of Calcium and Aluminum with the Flaws, While Only One Spot Contained Silicon. 350X

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5.4 Summary and Conclusions - Core Sample Tests

The sectioning and metallographic examination of core samples A, V, and Y exposed the flaws in samples V and Y which had been responsible for the ultrasonic indications. Also it revealed that weld repair had been performed in the vessel plate in these areas.

This conclusion is supported also by Figure 44 which shows an overlay of the flat pattern of the head with the repaired defects at the time of manufacture and a flat pattern of the existing defective head with the core holes. Angular position of the original defect is not known; therefore one cannot be absolutely sure that the overlay is correctly oriented. However, the figure does show that core samples A and V could have come from the weld repair region; then core sample Y would have been removed from base metal which is in agreement with the metallurgical results. The portion examined of core sample A was entirely weld repair, V was one-half weld repair, and Y was entirely base metal. Each core portion examined was only about 1-1/4 in. out of the 4 in. plate, so that this information on the extent of weld repair is very limited. The flaws in V were in the base metal just below the heat affected zone of the weld repair. Therefore, one may conclude that the flaws are a portion of those in the bottom head plate which were to have been repaired by welding.

Samples A, V, and Y were tested in tension. Also Sample A', which was base metal from below A in the plate, was tested. The metal, per se, has adequate strength and ductility. However, in specimen Y especially the flaws were so extensive that the nominal stress was low and only metal at the plane of the flaws was able to deform.

Fractographic and electron microprobe examinations revealed that the flaws were calcium and aluminum (oxide) inclusions. No evidence of other contributory substances was found. The inclusions formed separations (cracks) at low strain levels (approximately the strain at metal yield). Except for the flaws, all metal fracture was by plastic dimpling. Hence, a crack may propagate along a flaw in a brittle manner, but the metal is tough.

FIGURE 44: Overlay of Defect Areas

6.0 STRESS ANALYSIS OF DEFECT REGION

The primary objective of the stress analysis was to predict the behavior of the defect due to the predicted and measured pressure loads and thermal shock and to attempt to understand why the defect occurred.

Because of the serious consequences of catastrophic failure and more importantly because of the research nature of this investigation, an extensive analytical investigation was undertaken in an attempt to model the actual defect and its loadings as accurately as possible within the framework of the present state-of-the-art and time limitations.

6.1 Analysis Methods

Since the defect lies in the immediate vicinity of a nozzle, the stress state is a complex one. Therefore to provide an adequate model

of the stress situation for use in a fracture mechanics analysis, it was necessary to resort to numerical methods. In particular, the finite element method was utilized in this regard for both the elastic and thermal portions of the study.

6.1.1 Finite Element Method

The finite element method is a very versatile numerical procedure which may be viewed as a generalization of the well known Rayleigh-Ritz method. However even the finite element method has practical limitations with regard to the geometries it can treat and in particular full three dimensional simulations are difficult. The complete problem here is clearly three dimensional but it can be seen that the vessel-nozzle intersection can be approximated rather well if considered as axisymmetric about the nozzle centerline since the bottom head is spherical. The defect, of course, does not extend completely around the nozzle centerline although it shows extension in that direction. Also one of the thermal loading situations is clearly not axisymmetric. Nevertheless it was deemed necessary and considered to be the best approach possible to consider the vessel as an axisymmetric three dimensional solid with comparable loading. The cross section of the nozzle-vessel intersection then appears as in Figure 45 with appropriate dimensions shown.

The particular finite element approach and theory for the three dimensional axisymmetric solid elastic behavior is given in Reference (4) and will not be repeated here. Similarly, the thermal code is described in Reference (5). However, for the convenience of the reader, a brief summary of the capabilities and limitations of the procedure will be given.

First of all the geometrical configuration is approximated by an assemblage of rings. This can be visualized most easily by considering only a cross section of the axisymmetric body created by passing a plane through the axis of symmetry. Only one "side" of the cross section need be considered, however, as for example in Figure 45. This cross section then is approximated by an assemblage of triangles and quadrilaterals

as, for example, in Figure 46. Each triangular or quadrilateral "element" is in reality a ring which is itself axisymmetric about the axis of symmetry of the complete solid.

FIGURE 45: Nozzle C Geometry

Since complete axisymmetry is assumed then the displacements, stresses, temperatures, etc. show no variation in the θ direction. In the R and Z directions, however, (in the plane of the previous cross section) the displacement or temperature field is free to vary linearly within each element. For the elasticity problem this produces a state of constant strain except for ϵ_θ which can vary as $1/R$ with comparable stresses within each element.

FIGURE 46: Element Idealization of Nozzle C-Vessel Intersection with No Defect Present

In theory the error associated with a given problem may be reduced to a value as small as desired by reducing the element size appropriately. There are practical limits imposed both by the computer code and time available for data preparation. In general the element networks employed in this study were considered to be small enough to produce stresses within 10% of the correct value except at points of singularity such as at crack tips and these problems are met in a different way as will be seen in the discussion on fracture mechanics.

For either the elastic or the thermal code the various boundary conditions that can be treated are numerous and will not be enumerated here. The particular boundary conditions employed are described in the appropriate sections of this report.

For the thermal shock portion of this analysis the thermal problem was solved first and temperature distributions at various times were written on tape. After some study only a selected few were utilized in the stress analysis. The element nets were generally different between the thermal and elastic problem; in which case suitable interpolation was used to transform the data from one code to another.

6.1.2 Fracture Mechanics Approach

To make use of linear fracture mechanics as presently formulated, it is necessary to deal with a singularity in the elastic stress solution which appears at the crack tip. Since the method of stress analysis is basically a Rayleigh-Ritz approximation, the accuracy of the solution is highly dependent upon the closeness of fit of the trial solution versus the actual solution. Also the singularity is not, of course, modeled directly in the present formulation. These factors result in a very poor characterization of the stress state at the crack tip. This problem led to the development of an energy method for computing the stress intensity factor rather than making direct use of the stress or displacement field predictions in the vicinity of the crack tip. This procedure is given in detail in Reference (6).

Briefly, however, the stored strain energy is computed for two slightly different crack lengths and these results numerically differentiated to produce the strain energy release rate, G , which is directly related to the stress intensity factor, K .

The usage of the stress intensity factor, once it is obtained, is the essence of linear fracture mechanics and no attempt will be made here to present the entire subject. Reference (7) is suggested for further reading in this area and also serves to indicate the aspects of fracture mechanics which will be pursued here, namely, critical crack propagation and more importantly crack growth rate.

The next portion of this report will deal with the problem of obtaining the stress intensity factor, K , whereas the final section will deal with the consequences of the magnitude of this factor as predicted by linear elastic fracture mechanics.

6.2 Stress Analyses of Defect Regions

Two types of loading are immediately evident which could possibly result in defect growth. First of all there is the presence of the internal pressure loading. Preliminary estimates [Reference (8)] of this form of loading indicated a low undefected structure stress level in the vicinity of the defect. Nevertheless this type of loading was considered first because it is of a non self-relieving type and could lead to catastrophic failure. A pressure of 3000 psi was taken as nominal even though operating pressure rarely exceeds 2400 psi.

A second type of loading considered was that created by thermal gradients set up because of the rapid drop of the fluid temperature from an initial temperature of 600 °F or less down to 212 °F after completion of the blowdown. This loading will be referred to herein as thermal shock loading. Depending on the blowdown nozzle size, placement of blowdown nozzle (bottom, middle, or top) and assumptions used on the heat transfer coefficient, different thermal loading conditions exist. These are enumerated in the thermal analysis section.

A final loading not considered in this report is one caused by dynamic loads of the blowdown extension nozzles on the vessel (applies only when blowdown nozzle is nozzle C). The primary reason for not considering this loading is the degree of complexity and corresponding analysis time required. On the other hand the initial estimates [Reference (8)] indicated that the stress level associated with this dynamic loading to be comparable to the thermal loading condition so the decision was made to only consider the thermal condition at this time.

6.2.1 Assumed Defect Size

An examination of the ultrasonic mapping of the defect indicates that the overall dimensions of an ellipse which would encompass all the area was about 16 inches by 5 inches with the longer dimension "wrapping" itself around nozzle C or in terms of the previously mentioned coordinate system, the long dimension is in the θ direction. The plane of the defect was generally laminar, i.e., parallel to the shell mid-plane and at a depth of about 1.5 inches. Within the defect ellipse defined above the actual defect area is "spotted" with the largest single area of dimensions 12 inches by 3 inches.

When the defect was core sampled, it was discovered that the apparent "solid defect" regions were actually made up of many small defects still generally laminar but none (in the core samples) exceeding a maximum dimension of about one-eighth of an inch. This would lead one to suspect that it would be safe to say that none of the actual small defects exceeded one-half inch in length in any direction. Although the small defects were generally laminar, they were scattered over a depth of about one-half of an inch.

After some consideration it was decided that two situations would suffice for the study. First on the assumption that the general area was weakened by the presence of the small defects, an axisymmetric (i.e., extending completely around the nozzle) crack will be studied, parallel to but 1.5 inches in from the outside surface, and of length four inches.

Second, a similar crack but only one-half inch in length will be considered. This latter configuration is probably the most realistic.

6.2.2 Pressure Loading

Consider first the unperturbed stress field due to pressure loading. The finite element net used for the undefected configuration is shown in Figure 46. The stress components along the failure plane, i.e., 1.5 inches in from the outer surface, are shown in Figure 47. The primary conclusions one can draw from these results are first that it is very unlikely that pressure type loading caused the defect originally because of the very low shear stress and the compressive stress normal to the failure plane. Second because of the large tensile membrane type stress, one would expect flaw growth to occur radially, i.e., normal to the defect plane, provided the introduction of the defect does not drastically change the overall stress field. This, of course, contradicts the present defect shape unless it has always existed in its present form or else there exists a "weakened" plane in that area. If growth does occur in the laminar direction due to this pressure loading it most certainly is associated with a Mode II type stress field.

Consider now the situation where the defect is introduced. The element idealization for the one-half and four inch defects are shown in Figures 48 and 49. Before discussing the stress intensity factors involved, consider the stress state along and at each end of the defect. The results for the 1/2 inch and 4 inch defects are shown in Figures 50 and 51.

First of all one should note that the stress values near the crack tips must be interpreted carefully due to the singularities present in the exact elastic solution at those points. However there is some useful information one can extract from these data.

Both cracks and the stress perturbations show indications of the Mode II type stress field. This is most evident from the large jump in magnitude of the σ_s component that occurs across the defect face near the crack tip. The one-half inch crack indicates that growth, if it occurs

FIGURE 47: Stress Components Occurring on Failure Plane - Undefected Structure; 3,000 psi Internal Pressure Loading

FIGURE 48: Element Idealization of Nozzle C-Vessel Intersection with One-Half Inch Defect Present

FIGURE 49: Element Idealization of Nozzle C-Vessel Intersection with Four Inch Defect Present

FIGURE 50: Stress Components in Elements Near Defect One-Half Inch Crack

FIGURE 51: Stress Components in Elements Near Defect, 4 Inch Crack

in a laminar direction, will most likely occur in a direction away from nozzle C although there is little difference. The four-inch defect shows a definite preference for growth toward nozzle C.

Growth of the defect into the nozzle appears to be the more serious situation. Therefore stress intensity values associated with growth toward the nozzle will be examined.

Consider first the one-half inch crack. The element mesh was altered in the region near the crack tip as shown in Figures 52B and 52C. Following the procedures outlined in Reference (6) the following values for the stress intensity factors were found:

$$7/16 \text{ inch crack } K = 0.56 \text{ ksi} \sqrt{\text{in}}$$

$$17/32 \text{ inch crack } K = 0.61 \text{ ksi} \sqrt{\text{in}}$$

Although the crack behavior under these conditions will not be discussed until later it is apparent that, although growing with crack extension, these are very low values.

A. 1/2 INCH CRACK ELEMENT MESH

B. 3/8 INCH CRACK ELEMENT MESH

C. 9/16 INCH CRACK ELEMENT MESH

FIGURE 52: Element Meshes Used in Vicinity of Defect for Nominal One-Half Inch Crack

The same procedure for the four-inch crack gives:

$$3 \frac{15}{16} \text{ inch crack } K = 3.89 \text{ ksi } \sqrt{\text{in}}$$

$$4 \frac{1}{32} \text{ inch crack } K = 4.00 \text{ ksi } \sqrt{\text{in}}$$

where the extended crack meshes for the four-inch crack size are similar to those in Figures 52B and 52C. Comparison of these results with the formula $K = \tau \sqrt{\pi a}$ where τ is taken from Figure 47 as 0.60 ksi shows that one-half inch results compare well whereas the four inch results are underestimated by a factor of three.

In all fairness to the approximate method, however, it should be pointed out that there is a contribution to K in the finite element method from the compressive stresses across the failure zone. This contribution could have been eliminated but to do so was not in the interest of conservatism in addition to the fact that crack growth may indeed be influenced by compressive stress.

There is, of course, the possibility of the crack "turning" and growing into either the inner or outer surface. Inspection of the two possibilities reveals that the worst situation is growing into the inside since upon penetration, the nozzle would be "held on" only by a thin portion which would be highly vulnerable to complete failure by tearing (see Figure 53). In addition inspection of the stress field associated with the flaws shows that the highest tensile stress occurs on the surface towards the inside of the shell at the end of the crack toward nozzle C (for the four-inch crack). This stress is seen to be about 10 ksi at the center of the first element beyond the crack tip.

For the turning situation a slightly different finite element net was employed to gain "room" for the crack extension. Figure 54 shows the element net employed in the crack region.

The stress intensity factors come out as follows (loading is 3000 psi internal pressure; basic 4 inch laminar crack):

FIGURE 53: Consequences of Crack "Turning" and Extreme Growth

FIGURE 54: Element Idealization in Vicinity of Defect Used for Crack "Turning" Study. See Figure 49 for Remainder of Element Net.

<u>Length of 90° turn extension</u>	<u>ksi $K\sqrt{\text{in.}}$</u>	<u>$\sigma\sqrt{\pi a}$ ksi $\sqrt{\text{in.}}$</u>
1/16	3.90	1.9
3/16	6.19	3.3
5/16	7.66	4.2

It is interesting to note that if the membrane stress existing at this point in the shell (6.9 ksi) and the "turned" length (not the total crack length) are used in conjunction with the usual approximate formula $K = \sigma\sqrt{\pi a}$ where a is one-half the crack length, fair agreement results with the finite element procedure. The results from such a calculation are shown in the third column above. Although somewhat low these approximate predictions are not grossly so which says that in this situation the presence of the laminar crack has only a moderate affect on crack behavior at 90°. In addition to being of interest in itself this also obviates the need to examine the tendency of the one-half inch crack to turn since the results will be very similar and somewhat less than the above.

6.2.3 Thermal Stress Analysis

At the initiation of any given blowdown, the vessel temperature is close to uniform throughout and is approximately the same as the fluid. For the nominal hot blowdown this is about 600 °F. As soon as blowdown commences and the pressure drops to saturation, a rapid fluid temperature drop begins where, by the mechanism of flashing, the fluid temperature remains at the saturation point while the pressure drops to atmospheric in 8 to 100 seconds depending on the nozzle break size. This, of course, means that the vessel experiences a severe thermal shock as the fluid quickly drops to 212 °F.

The phenomenon of heat transfer across the vessel-fluid interface is a complicated one. Among others, temperature drop across the interface, pressure, surface character and orientation, type of fluid and metal involved are parameters that enter into the situation. A rather common

model which was utilized herein was to employ the approximation that the heat flux across the interface is linearly proportional to the temperature drop across the interface, i.e., $\frac{q}{A} = h\Delta T$

$$\text{where } \frac{q}{A} = \text{heat flux } \frac{\text{Btu}}{\text{ft}^2 \text{hr}}$$

h = heat transfer coefficient

ΔT = temperature drop across the interface.

The constant of proportionality, the heat transfer coefficient, is then treated as a constant with respect to time although it may be changed each time increment in the step by step solution of the thermal transient.

A simplified formula, developed by Jakob and Hawkins, Reference (9), was used herein to estimate h .

It is:

$$h = 0.64(q/A)^{3/4} \left(\frac{P}{P_{\text{atm}}} \right)^{0.4}$$

$$5000 < q/A < 75,000 \quad q/A \sim \frac{\text{Btu}}{\text{hr-ft}^2}$$

$$P = \text{absolute pressure} \quad h \sim \frac{\text{Btu}}{\text{hr-ft}^2 \text{ } ^\circ\text{F}}$$

P_{atm} = standard atmospheric pressure.

Using the upper limit of applicability of this formula, $q/A = 75,000 \frac{\text{Btu}}{\text{hr-ft}^2}$, gives an h of $2900 \text{ Btu/hr-ft}^2 \text{ } ^\circ\text{F}$ for a pressure of one atmosphere. This value is most likely much too high since the maximum flux possible under these conditions according to Zuber, Reference (14), is approximately $35,000 \text{ Btu/hr-ft}^2$. On the other hand the presence of about one-half inch of stainless steel cladding on the CSE pressure vessel inner wall in the vicinity of nozzle C provides a pseudo poor conductor skin

relative to the carbon steel. This effect acts in series with the interface coefficients and results in a low sensitivity of the bulk of the vessel to changes in h provided it remains above about 500 Btu/hr-ft². Also from a stress analysis point of view, maximum stresses are usually associated with maximum thermal gradients so that using the larger value for h is conservative.

Since the coefficient and fluid temperature are highly dependent on pressure it was considered necessary to adjust the value of h in the initial portion of the blowdown. The time between the start and end of the active blowdown is therefore divided into several periods called time sequences in which the value of h and environmental temperature are based on the fluid pressure and temperature existing at the midpoint in the time sequence. Each time sequence is made up in turn by several time increments. This provides a "stepping down" of the coefficient and environmental temperature which is considered accurate enough.

After considering all of the possibilities of various combinations of nozzle sizes, nozzle locations, initial conditions, etc., two situations were isolated as being the most severe and were modeled. The first of these, referred to as thermal case (a), corresponds physically to the blowdown where a small nozzle (two inches) located in the top or middle of the vessel is used (so that water remains in the bottom of the vessel completely covering the nozzle C area) and where the initial temperature is maximum (600 °F). The reason for the small nozzle is to provide maximum time with the higher values of h . This condition also represents one found on the bottom blowdown situation. Since the bottom blowdown requires the longest time to complete, a two-inch bottom blowdown was used as the model in this case.

The other thermal condition considered, referred to as thermal case (b), corresponds to the situation where a bottom nozzle is used and the blowdown nozzle diameter is maximum (8 inches). Again an initial temperature of 600 °F is used. In this case it is assumed that after the active portion

of the blowdown is over then only dry steam is in contact with the inner nozzle area whereas the bottom of the vessel remains in contact with water. Since the analysis is constrained to be axisymmetric, this in effect puts all of the remainder of the vessel in contact with water. Since the heat transfer to steam is much smaller, the assumption is made that the inner portion of the nozzle becomes insulated after active blowdown ceases which leads to higher axial gradients than existed in case (a). In reality this case only represents one extreme for the bottom blowdown as actual thermocouple data showed some cooling in the nozzle area.

The actual boundary condition data used are summarized in Table 3.

TABLE 3. Thermal Boundary Condition Data for Thermal Analyses

Sequence Number	Time at End of Sequence (sec)	Gauge Pressure at Midpoint of Sequence (psi)	Fluid Temperature During Sequence °F	h Used During Sequence Btu/ft ² hr (°F)
Thermal (a)				
1	30	1290	580	17,400
2	60	1010	550	15,800
3	70	620	490	13,100
4	80	245	400	9,100
5	95	50	300	5,200
6	500	0	212	2,900
Thermal (b)				
1	2	1420	590	18,100
2	4	1050	552	16,100
3	6	540	478	12,300
4	8	65	312	5,700
5	500	0	212	2,900

Blowdown run B-19B thermocouple data showed that complete boil off occurred at 420 seconds after blowdown initiation or about 325 seconds after the active blowdown ceased. Nevertheless the thermal simulations were carried out to 500 seconds as though water were still present.

6.2.3.1 Temperature Distribution

As stated earlier, a finite element formulation and computer code, Reference (9), of the heat conduction equations was used to solve the thermal portion of the problem. The element mesh used was almost identical with that used for the solid shell stress solution, Figure 46, except that some variation in the location of the interior nodes was made to account for the one-half inch of stainless cladding. The first two elements made up the cladding leaving the other elements for the remaining 3 3/4 inches of carbon steel (in the 4 inch section, of course). This provided for elements of dimensions one-fourth of an inch in the direction of maximum gradient for the cladding and a nominal 0.6 inch thick element for the rest of the vessel. This, of course, gave minimum element size where the temperature gradients were maximum which is desirable.

To gain some confidence in the boundary conditions selected for the analysis a comparison of predicted temperature on the outside surface of the shell versus time is compared with some actual thermocouple data. This particular thermocouple data was obtained by averaging data from three thermocouples, TB06, TB09 and TB013, all exterior surface gauges which are far enough away from the nozzle as to be virtually unaffected by the presence of the nozzle. The comparison is shown in Figure 55.

Even though the initial fluid temperature for this run was close to 600 °F the outer shell temperature was initially only about 580 °F due to nonequilibrium or thermal leakage. After about 180 seconds the thermocouple data shows an abrupt downward turn and from then on is quite close to the predicted temperature. The temperature-time gradient is also shown and even though the thermocouple data shows considerable scatter the correlation is quite good after about 200 seconds.

FIGURE 55: Comparison of Temperature at Outer Surface of Shell, Away from Nozzle C, Thermal Solution A with Average of Thermocouples TB06, TB09, TB013 Run B19-B

Finally consider a comparison of the analytical and experimentally observed thermal profiles through the thickness of the shell, Figure 56. These profiles are given at 180 seconds after blowdown initiation which turns out to be the critical one. The correlation is seen to be good.

6.2.3.2 Critical Time Selection

By looking at such parameters as the root mean square of the temperature variance from uniform, temperature gradient radially across the failure zone, or temperature gradient axially in the failure zone, several critical temperatures were selected. Stress analyses of the undefected shell were then run using these several temperature distributions. Using stored elastic strain energy and the normal or shear stress components at the failure surface as guides, a final critical temperature was selected. For thermal case (a) this clearly occurred at a time of 180 seconds after blowdown commenced.

The critical situation for thermal case (b) was a little more difficult. First of all the maximum axial gradient had not been reached at $t = 500$ seconds. Continuation of boil off after 500 seconds is considered so unrealistic that it was not worth the computer time to continue any further. In addition the axial gradient was apparently nearing the maximum. Comparison of the same parameters as above then led to the conclusion that the critical temperature gradient occurred at $t = 140$ seconds.

6.2.3.3 Thermal Stress Analysis

The finite element nets used in the thermal stress analyses are identical with those used previously. The resulting stresses in the undefected shell along the failure zone for thermal case (a) are shown in Figure 57. The stresses associated with thermal case (b) were similar in shape and almost everywhere less severe. Several observations can be made based on these figures. First one notes that the dominant cracking mechanism possible is the tensile stress across the failure plane and that this stress is dropping to zero and going negative at the end of the failure zone toward the nozzle. Second one notes that the stress component parallel

FIGURE 56: Comparison of Experimental and Analytical Thermal Gradient in Bottom Head of CSE Pressure Vessel. Experimental Data from Run B19-B.

FIGURE 57: Stress Components on Failure Plane; Undefected Structure; Thermal Loading A

to the defect plane is compressive and therefore there is no need to even consider the turning phenomenon discussed earlier in connection with the pressure loading. Finally, one can conclude that there is no need to consider thermal case (b) any further as it is less severe than case (a). This final observation was confirmed by checking the stress intensity factors for the four inch crack.

Introduction of the one-half inch crack in the same position as before (see Figure 48) puts the end toward nozzle C into a zone of compressive stress as can be seen in Figure 57. The one-half inch size is small enough not to disturb the gross field much and therefore the possibility of crack-ing in the direction toward nozzle C becomes remote. (The question of crack propagation under alternating compressive stress is not well understood, however.) Therefore it suffices for the one-half inch defect to say that there is a greater tendency for extension to occur in a direc-tion away from nozzle C and to obtain an estimate of the stress intensity factor from a straightforward application of $K = \sigma \sqrt{\pi a}$ using the maximum value of σ (within the failure zone) from Figure 57. The resulting K for the one-half inch defect is 2.3 ksi $\sqrt{\text{in.}}$.

On the other hand, when the four inch defect is introduced into the failure zone as before, tensile stresses develop ahead of the crack toward nozzle C. This can be seen in Figure 58 where the stress components in the defect vicinity are given. In addition it can be seen that there is a substantial Mode II shear contribution in the end towards nozzle C. This is evidenced by the rising value of τ_{sn} as it approaches the crack tip from ahead and by the large jump in σ_s as one moves across the defect on the other side of the crack tip.

Application of the finite element technique as before results in the following values when the crack extension is toward nozzle C:

<u>Crack Length (inches)</u>	<u>K ksi $\sqrt{\text{in.}}$</u>
3 15/16	7.97
4 1/32	7.51

FIGURE 58: Stress Components on Failure Plane with Four Inch Defect Present. Thermal Case A $t = 180$ sec.

Note that the stress intensity factor is reducing in this case as crack extension proceeds.

Consideration of crack extension in the direction away from nozzle C results in considerably larger values for K which do increase with crack extension:

<u>Crack Length</u> <u>(inches)</u>	<u>K</u> <u>ksi√in.</u>
3 15/16	10.20
4 1/16	10.56

These values represent the largest stress intensity factors found.

6.3 Defect Behavior

First of all consider the possibility of unstable crack growth. Because of the axisymmetric nature of this structure, the crack tip area is under a high degree of constraint such that clearly a condition of plane strain normal to the crack leading edge is much more realistic than plane stress. Reference (11) gives estimates of the minimum fracture toughness of the material in question which is SA212B. However this property is highly temperature dependent and estimates are given only for the 20-140 °F temperature range. In reality this presents no problem since the lowest value given, corresponding to a temperature of 20 °F, is $K_{Ic} = 47. \text{ ksi}\sqrt{\text{in.}}$ which is considerably greater than any of the stress intensities found. One word of caution here is that all presently available data on critical stress intensity values are based on Mode I behavior whereas the K estimates above sometimes include K_{II} contributions. This is not considered cause for concern because of the wide margin of safety indicated.

Perhaps the most worrisome situation is the possibility of the defect turning and growing due to internal pressure at high temperature which accelerates growth after which pressure could be imposed (and in fact has been) at low (40 to 60 °F temperature) where the possibility of unstable growth is maximum. However projecting the K data found herein for the

turning situation on a $\sqrt{a}$ and net section stress basis indicates that the crack would have to grow to a length of about 2.3 inches normal to the failure plane before unstable growth would occur.

Consider next the crack growth possibilities. Reference (11) shows that elevated temperature increases the crack growth rate for SA212B and therefore the data given in Reference (11) for that metal and the 600 to 650 °F temperature range will be utilized.

The stress intensity ranges for the data are higher in Reference (15) than are needed here so a variation of form

$$\frac{da}{dN} = C (\Delta K)^n$$

was utilized to project the data back into the desired stress range. Such extrapolation could lead to large error but is considered acceptable here since the extension is to be used for smaller values of ΔK . This produced the formula:

$$\frac{da}{dN} = 6.9 \times 10^{-7} (\Delta K)^{4.95}$$

where

$$\frac{da}{dN} = \text{growth rate in } \mu\text{in. cycle}$$

$$\Delta K = \text{fluctuation range of stress intensity factor.}$$

To apply this formula to the 90° turn situation for the pressure loading, it is first of all necessary to estimate the initial flaw size. Since the core samples indicated laminar flaws spread over a depth of about one-half inch, a reasonable initial flaw size may be taken as one-fourth of an inch. Again using the previous data for the four inch laminar crack with a "turn" of 5/16 inch to provide a base point on which

to project K, estimates may be made of the growth behavior of the crack. This produces a prediction of 9×10^6 cycles necessary for the flaw to grow from one-fourth to one-half inch long. Using the same procedure, the estimate for growth from one-half to one inch is $2. \times 10^6$ cycles. Since the maximum frequency that could be expected is about 100 cycles per year, the above margin appears safe.

Consider next laminar growth of the four inch defect due to thermal case (a). Using the stress intensity value given previously (one cycle is taken here to be equivalent to one blowdown; heat up is assumed to be slow enough that significant gradients do not develop) with the above propagation law gives the following estimates for the number of cycles required for the defect to grow one-quarter inch in each direction:

toward nozzle C: $17. \times 10^6$ cycles
away from nozzle C: $2. \times 10^6$ cycles

As can be seen these are very low growth rates and for that reason the one-half inch crack will not be considered.

6.4 Summary and Conclusions - Stress Analysis

Stress intensity factors associated with one-half and four inch laminar axisymmetric cracks in the vicinity of nozzle C have been determined for pressure and thermal loading on the vessel. Comparison of these values with critical values for unstable crack behavior has shown that there is negligible chance of unstable crack behavior for the conditions investigated assuming that the metal surrounding the defect is indeed sound. Crack growth rates also are so small that subsequent detectable growth into presently sound metal is very unlikely. If additional weakened areas exist then additional growth could occur. Since there is no way of estimating any material properties for such a weakened zone neither stability nor growth estimates can be made.

However some comments may be made which are meaningful although partly speculation. As stated before, the one-half inch defect size studied in this report is probably much more closer to reality than the larger defect.

The tensile tests on the removed cores indicated considerable strength remaining in the defect zone itself so that gross weakening of the area leading to pseudo large defect behavior seems very unrealistic. If this can be accepted then it seems likely that the only possibility for laminar extension is away from nozzle C even if considerable weakened material exists. Intuitively this does not seem particularly harmful since laminated pressure vessels are sometimes built intentionally. Therefore the presence of "weakened" material outside of the one-half inch laminar zone becomes a primary question with regard to unsafe behavior of the defect.

7.0 EXPERIMENTAL PROGRAM - DEFECT INDICATION SURVEILLANCE

Continuation of the blowdown experiments was desired if at all possible in order to obtain blowdown data and information regarding possible propagation of the defect indication. Additional precautions would be required to pursue this plan of action because of the defect in the bottom head. These precautionary measures included:

- continuing stress analyses
- acoustic monitoring
- ultrasonic inspections.

Stress analyses methods and results are as described in section 6 of this report. Strain gauge data obtained from the outside and inside surfaces of the vessel during and after the blowdown experiments are used as boundary conditions. These data, of course, include mechanical and thermal components. Thermal stress levels are calculated by using temperature data obtained from thermocouples mounted on the inside and outside surfaces and also on the plug inserted into the "Y" core hole during repair of the core holes.

Acoustic emission monitoring is provided during and after the blowdown experiments. Suspect noise source locations are defined which may be ultrasonically inspected. Results of acoustic emission monitoring are described in this section.

Ultrasonic inspections of accessible portions of the defect region are made after nearly every blowdown experiment. These tests are made to ascertain as nearly as possible with current technology that the defect region has not enlarged.

7.1 Acoustic Emission Monitoring Effort

An acoustic emission monitoring system has been used to monitor the vessel during several pre-blowdown tests and eight blowdown experiments. This system and results obtained from the acoustic emission data are discussed below.

Acoustic emission is defined as the pressure wave produced in metals by the energy released as the metal deforms or fractures. The pressure waves are detected and converted to electronic signals by transducers mounted on the metal surface. The signals are conditioned and analyzed to produce real time indications of crack growth. Information on time of arrival at various transducers plus known propagation velocity of the pressure waves in the metal permits locating the source of flaw growth.

7.1.1 The Monitoring System

The acoustic emission monitoring system schematically shown in Figure 59 was fabricated and installed in order to monitor known flaws in the vessel heads and determine any flaw growth on the Containment System Experiment (CSE) reactor vessel simulator. The monitoring system consists of ten PZT-5 transducers (six located on the bottom head shown in Figure 60 and four on the top head), ten preamps, eight emission rate detectors, and strip chart recorders as shown in Figure 61. In addition, the system is supplemented with a seven channel tape recorder to store data for use in locating source of emission signals.

System calibration is accomplished by using a 2 MHz ultrasonic transducer and a pulse generator to produce acoustical pulses in the vessel heads. Sensitivity of each sensor and time of calibration signal arrival is recorded. Figure 62 shows the propagation time and pulse amplitude of the calibration pulse arriving at sensor No. 3 from an input point over a known flaw in the vessel head.

FIGURE 59: Block Diagram of Acoustic Emission Monitoring System

FIGURE 60: Installed Acoustic Emission Monitoring System (Preamplifiers and Sensors Not Shown)

FIGURE 61: Sensors 2, 3, and 4 on the Bottom of the Test Vessel (Indicated by Arrows)

1V/cm Vertical Deflection
50 μ s/cm Horizontal Deflection

FIGURE 62: Calibration Pulse on Channel 3

7.2 Acoustic Emission Test Results

A series of vessel proof tests were performed following repair of damaged support braces and of the holes in the bottom hemispherical head where the core samples were removed. Acoustic emission monitoring was included as part of these tests.

7.2.1 Test 1 - Axial Nozzle Test

The first test consisted of applying a force of 100,000 pounds axially against the face of the discharge nozzle C. The loading was repeated three times. During the first load cycle a significant number of emissions were recorded. Fewer were recorded the second and fewer yet the third time. Analysis indicated that many of the emissions originated from the initial loading of the braces on the side of the vessel. The emissions decreased with each successive load due to the Kaiser effect. This is a phenomenon which is attributed to stress redistribution on a micro scale which occurs when metal is first loaded. There was some noise detected during each loading cycle resulting from the test vessel moving against the support framework. It is concluded that there was no acoustic emission due to flaw growth during this test.

7.2.2 Test 2 - Hydrostatic Test

Test 2 was a cold hydrostatic test. The test vessel was pressurized in 500 psi increments to 3500 psi. A small number of acoustic emissions were detected over the entire test with a noted increase between 1500 psi and 2000 psi. Another small increase was noted between 2500 psi and 3100 psi. Analysis indicated that all emissions which occurred between 1500 psi and 2000 psi came from an area just above the center of the vessel. Investigation showed that a blank flange of 1/4 inch boiler plate was installed in that area and had deformed 5/16 inch during the hydrostatic test. This is significant from the standpoint of indicating the detection sensitivity of the acoustic emission monitor. A statistical sample of emission signals near and at the maximum pressure (3500 psi) indicated emissions came from several sources: (1) a 1/4 inch thick blanking flange

FIGURE 64: Bottom View of Test Vessel

The acoustic emission rate was used for real time indication of emission activity. The recorders were turned on early in the test and continued to operate for 16 hours after the blowdown was completed. The record showed low acoustic emission rates before the blowdown. A significant increase in indicated emission was recorded immediately following the blowdown initiation. This is attributed to rupturing of the diaphragms and high velocity steam flow noise. The level decreased markedly within a few minutes. The count rate charts showed little change for the remainder of the monitored time. Figure 65 shows the emission rate chart of Channels 5 and 6 during and immediately following blowdown B-19.

A sample of data stored on the magnetic tape just before the blowdown occurred showed emissions originating from areas on the sides of the vessel with exact sources indefinite. It is believed they were generated by thermal expansion of the vessel support structure. A three minute tape sample taken just after the blowdown was complete showed emission signals from the sides of the vessel, the discharge nozzle flange and Areas 1 and 2 (Figure 64) on the bottom of the vessel. Two emissions originated from Area 1 and eight from Area 2. The last tape sample was taken 12 minutes after the blowdown and analysis showed no significant emission data.

7.3.2 Blowdown B-19B, B-20B, and B-21

The emission rate charts showed the usual major indication from diaphragm rupture and high velocity fluid flow at blowdown initiation. Samples of data recorded on magnetic tape immediately before and after blowdown B-19B and B-20B showed all emissions to originate from areas other than the vessel heads.

Analysis of magnetic tape samples from blowdown B-21 showed three emissions originating from the bottom head, each from a different location. These are considered to be insignificant. Preamplifier No. 2 on the bottom head was replaced with a tuned preamplifier which reduced mechanical and flow noises during the later two tests.

FIGURE 65: Channels 5 and 6, Blowdown 19

7.3.3 Blowdown B-22 and B-22B

Analysis from magnetic tape samples taken during these tests showed a few emissions originating in the vicinity of the inlet nozzle located in the center of the bottom head. The exact origin of these emissions is indefinite--they may be from the flange blank, flange bolts, or the nozzle to vessel wall interface. Ultrasonic examination may not expose the source of these emissions because the indicated flaw growth would be expected to be slight. Close examination of the nozzle to vessel wall interface is warranted, however.

7.3.4 Blowdown B-23

No significant emissions were found during analysis of recorded data from this test. Additional filters were used during this test which eliminated most of the high velocity flow noise and mechanical noises.

7.3.5 Blowdown B-24

Additional high pass filters set at 600 kHz were used during this test to evaluate their ability to eliminate mechanical and flow noise interference. Recent laboratory tests have shown that this should not compromise detection of acoustic emission. Flow noise was essentially eliminated and cleaner data were obtained. Data analysis showed two emission signals originating from the area noted in Figure 63 and several signals originating from Area 2 as illustrated in Figure 64. The total number of signals originating from Area 2 warrants close ultrasonic examination of this area. Figure 66 shows a detected signal originating from this area and shows time displacement as it arrived at various sensors. Analysis also showed several signals originating from an indefinite location on the vessel wall adjacent to sensors 5 and 6. Their origin is out of the intended monitoring zone and accurate source determination is difficult.

7.4 Conclusions from Acoustic Emission Results

No major change in flaw areas in the vessel heads have been indicated by acoustic emission detected during these tests. Sufficient indications

FIGURE 66: Signal from Area 2 Figure 64 During Blowdown 24

from specific locations in the defect region warranted careful ultrasonic examination of these areas although a positive correlation between acoustic emission and flaw growth has not been established to date. Results of the subsequent ultrasonic tests did not indicate any growth of the defect.

The monitoring system functioned as expected but some problem has been experienced with mechanical and flow noise interference. A logical requisite of the initial system was that only tested component concepts and designs should be used. This dictated a relatively low monitoring frequency (200 kHz pass filtering). Subsequent laboratory study has demonstrated the validity of monitoring acoustic emission in the low megahertz frequency range (1-3 MHz) which allows much greater discrimination against unwanted noise signals. The installed system was designed to permit upgrading with a minimum of modification.

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APPENDIX A

APPENDIX A

HWS-7247
Rev. 1

SPECIFICATION FOR PROCUREMENT
OF REACTOR SIMULATOR VESSEL

BUILDING 221-T-HE

CONTAINMENT SYSTEMS EXPERIMENT

OCTOBER 27, 1964

Prepared by General Electric Company
Hanford Laboratories

Prepared by

12-9-64
Date

Approved for General Electric Company

Facilities Engineering

DEC. 9, 1964
Date

Chemical Effluents Technology

12/9/64
Date

PREFACE

The purpose of this revision is to update the original issue of March 17, 1964.

Since the seller's original proposal of May 21, 1964, numerous changes to the proposal have accumulated. These changes have been included in this specification for the simplification of reference to both seller and buyer. It is not intended that this revision change any of these previous agreements.

This specification was issued originally on March 17, 1964, shortly after the ASME Section III, Code for Nuclear Vessels was issued. Since that time additional study of this Code has revealed requirements which were detailed in the specification. This specification requires conformance with Section III, therefore all details which have been found to be included in the Code have been deleted in the specification.

SPECIFICATION FOR SIMULATOR VESSEL-CONTAINMENT SYSTEMS EXPERIMENT

1.0 General

The equipment required by this specification is a model of a primary vessel of a nuclear power reactor. The vessel will be installed in a small containment vessel which is appropriately scaled. These vessels, along with other necessary equipment, will be used to study the effectiveness of containment systems subjected to conditions resulting from credible accidents of water cooled reactors.

The primary vessel will supply high-pressure high-temperature water. The water will be released suddenly to the containment systems to simulate complete pipe rupture conditions. Release of the water will be through any one of the 8 inch nozzles.

The primary vessel will not contain any equipment for sustaining a nuclear chain reaction and no radiation damage to materials of construction will be encountered.

- 1.1 The vessel shall be designed, fabricated, tested and stamped in accordance with the ASME Code, Section III, Code for Nuclear Vessels for Class A Vessels.
- 1.2 The vessel shall conform to the requirements of SK-2-3580, Rev. 2 and this specification. (Refer to SK-2-3582, Rev. 3, SK-2-3584, Rev. 3 and HWS-7248 for additional information).

2.0 Operational Information

The vessel will be required to operate in accordance with the following description:

- 2.1 The vessel will be filled with a maximum of 150 cubic feet of demineralized, deoxygenated water. It will be pressurized by an inert gas to a maximum of 2500 psig. The water will then be heated to the desired temperature (600F maximum) in an external recirculating loop. Water recirculation will be a 100 GPM through a 300 KW Dowtherm to water heat exchanger for about six hours. (The external loop equipment is not part of this specification).

When the vessel and contents have reached a maximum temperature of 600F (or other test temperature) the external loop will be valved off. The vessel will then be depressurized by rupturing a rupture disc in one of the 8 inch blowdown nozzles. (Refer to SK-2-3584, Rev. 3 for blowdown schedule and Table I).

3.0 Design

- 3.1 Design shall be in accordance with 1.1.
- 3.2 The maximum allowable working pressure for the vessel shall be 2750 psig at a temperature of 600F.
 - 3.2.1 The nozzles shall be fitted with ASA 1500-lb class flanges.
- 3.3 The vessel shall be designed to operate with or without the instrument collar in the head to shell connection.
- 3.4 The support trunnions shall provide restraint for the vessel during blowdown for the forces given in the table on drawing SK-2-3584 Rev. 3. The trunnions shall also be used for lifting and handling the vessel. Reinforcement of the vessel wall adjacent to the trunnions shall be provided if needed.
- 3.5 Operating life of vessel shall be 500 cycles as shown on Table I.
- 3.6 The following items are provided to inform the designer of conditions that will exist during operation.
 - 3.6.1 The support trunnions will be unrestrained for movement parallel to the vertical axis of the vessel.
 - 3.6.2 The simulator legs shall be fixed to force transducers which shall be bolted to the support beams in the drywell assembly.
 - 3.6.3 All external piping will be arranged to produce forces and moments no greater than those given in Table II.
 - 3.6.4 Blowdown nozzles A, B, C, D, E shall operate attached to a rupture disc assembly, weighing 1000 lb, which has a center of gravity 12 inches from the nozzle face.

4.0 Materials

- 4.1 The vessel materials shall be vendor choice subject to requirements of 1.1.
- 4.2 All wetted surfaces shall be clad with vendor choice austenitic stainless steel. Cladding shall be a minimum of 1/16 inch thick.

5.0 Fabrication

- 5.1 Fabrication shall be in accordance with 1.1.
- 5.2 The cladding for the blowdown nozzles shall be applied as deposited weld metal. The welded surface shall be refinished to a nominal pipe finish.
- 5.3 All nozzle flanges shall be equipped with mating carbon steel blind flanges. Gaskets shall be stainless steel - asbestos spiral wound type.

6.0 Testing and Inspection

- 6.1 Testing and inspection shall be in accordance with 1.1.
- 6.2 In addition to the testing required by 1.1, the following tests shall also be conducted by the seller.
 - 6.2.1 Shell material shall be radiographed at points of trunnion attachment before holes are drilled for nozzles. Trunnions shall not be located at points where radiographs or ultrasonic tests disclose material flaws which will result in stresses higher than those allowed by the Code.
 - 6.2.2 The ultrasonic inspection of the materials required by 1.1 shall include 100% coverage. The reference specimens shall include a defect 50% smaller than standard and also one 50% larger to permit greater accuracy in estimating size of actual defects. All defects shall be recorded on a map of the exterior wall of the vessel using a linear coordinate system referenced to an appropriate and permanent origin.
 - 6.2.3 The final inspection as required by 1.1 shall include an ultrasonic inspection in compliance with the specifications of N-321.1 of the ASME Code for Nuclear Vessel as amended by 6.2.2 of this specification. In addition the liquid penetrant test shall include 100% coverage of the vessel shell.
 - 6.2.4 Vessel cladding material shall show no separation from the base plate during the shear test in ASTM A264 Par. 7 (b) (2).
 - 6.2.5 The vessel cladding material shall be inspected ultrasonically for unbonded areas. Any defective areas of 1 sq. inch or greater shall be repaired by welding. The

reference specimen shall be similar geometry and like materials with a nominal 1/2 sq. inch unbonded area. All defects shall be recorded on a map of the interior wall of the vessel using a linear coordinate system referenced to an appropriate and permanent origin.

- 6.3 Buyer inspection is required. The inspectors shall have access to shop areas and material records concerned with the vessel in this specification.

7.0 Documentation

- 7.1 Six copies of complete design calculations including stress analysis report before start of fabrication for buyer information.
- 7.2 Six copies of drawings showing all wall thicknesses, nozzle locations with details for attachment, flange, bolted head details and trunnion attachment details before start of fabrication for buyer information.
- 7.3 At the completion of the order the following items shall be furnished by the vendor:
- 7.3.1 Six copies of completed Code forms.
 - 7.3.2 Six copies of notarized certificates listing physical and chemical analysis for all materials used in the manufacture of the vessel.
 - 7.3.3 A certified set of reproducible "As Built" drawings.
 - 7.3.4 Six copies of certified results for all tests required in Section 1.1 and 6.0 of this specification.
 - 7.3.5 Six copies of certified post weld heat treatment procedure.
 - 7.3.6 One set of radiograph negatives of the vessel welds.

TABLE NO. I
SIMULATOR OPERATING SCHEDULE

SIMULATOR OPERATING SCHEDULE						Vapor Condition After Blowdown (2)	
Simulator Nozzle	No. of Cycles (1)	Initial	Volume	Discharge	Time to	Simulator & Dry Well Vessels	
		Temp (°F)	Discharged (%)	Time (sec)	Condition (sec)	Temp (°F)	Pressure (psig)
<u>Pressure Suppression Concept</u>							
A	30	600	50	1.5	4	254	17
B	30	600	100	2.5	5	254	17
C	30	600	100	2.5	5	254	17
D	30	600	100	2.5	5	254	17
E	30	600	50	1.5	4	254	17
A	20	500	50	2.4	5	230	5
B	20	500	100	4	6	230	5
C	20	500	100	4	6	230	5
D	20	500	100	4	6	230	5
E	20	500	50	2.4	5	230	5
<u>Dry Containment Concept</u>							
A	30	600	50	1.5	1.5	195	15
B	30	600	100	2.5	2.5	240	30
C	30	600	100	2.5	2.5	240	30
D	30	600	100	2.5	2.5	240	30
E	30	600	50	1.5	1.5	195	15
A	20	500	50	2.4	2.4	160	13
B	20	500	100	4	4	200	13
C	20	500	100	4	4	200	26
D	20	500	100	4	4	200	26
E	20	500	50	2.4	2.4	160	26

- For all cycles listed as 30 in this column the initial simulator pressure shall be 2500 psig for 15 cycles and 2000 psig for 15 cycles.
 - For all cycles listed as 20 in this column the initial pressure shall be 1500 psig.
- These are estimated values.

TABLE II
EXTERNAL PIPING FORCES AND MOMENTS

SIMULATOR NOZZLE	FORCE (LBS)			MOMENT (IN-LBS)			REMARKS
	FX	FY	FZ	MX	MY	MZ	
N	6.7	-514.0	272.2	-6355.9	-823.2	-301.5	Y-axis vertical and normal to nozzle N
L	-2.8	-44.7	-16.0	-77.8	362.1	-629.2	
O	-28.4	-515.8	-83.1	375.5	1867.1	-11,771.0	X-axis, horizontal and normal to nozzles, Y-axis vertical
Q	-12.9	-396.3	-48.4	11.3	1139.6	-9,393.1	
S	-115.5	-475.0	-268.5	-1697.6	5859.1	-9,692.8	

NOTE:

1. Moments counter-clockwise about axis.

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