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AN EVALUATION OF PWR STEAM GENERATOR WATER HAMMER

Final Technical Report

June 1, 1976 - December 31, 1976

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Creare, Inc.

for

U. S. Nuclear Regulatory Commission

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AN EVALUATION OF PWR STEAM GENERATOR WATER HAMMER

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J. A. Block P. H. Rothe
C. J. Crowley G. B. Wallis
L. R. Young

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Creare, Inc.
Hanover, NH 03755

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ABSTRACT

An investigation of waterhammer in the main feedwater piping of PWR steam generators due to water slugs formed in the steam generator feedring is reported. The relevant evidence from PWR operation and testing is compiled and summarized. The state-of-the-art of analysis of related phenomena is reviewed. Original exploratory modeling experiments at 1/10 and 1/4 scale are reported. Bounding analyses of the behavior are performed and several key phenomena have been identified for the first time. Recommendations to the Nuclear Regulatory Commission are made.

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EXECUTIVE SUMMARY

The feedwater spargers of Pressurized Water Reactor (PWR) steam generators can uncover and drain during abnormal operating transients such as main feedwater pump trips. To restore the water level and maintain adequate heat transfer between the secondary and primary coolant, cold auxiliary feedwater is introduced into the main feedwater piping. This water is normally pumped at a relatively low rate such that it flows as a layer along the bottom of the horizontal feedwater sparger and adjacent piping. Under some circumstances, this water can form a slug that blocks the pipe cross section and traps a steam void upstream. If this occurs, a rapid sequence of events follows: the steam in the void condenses, the void pressure decreases to near zero, the water slug is accelerated upstream through the piping by the pressure difference acting on it, the slug impacts the first elbow or pipe bend, a pressure wave propagates through the entire piping system, and some piping, supports or components may be overstressed.

Background

At the outset of this study, twenty incidents believed to be of this type had been reported to the Nuclear Regulatory Commission. These involved fifteen of the thirty-four operating PWR plants. Four of these plants had sustained extensive permanent pipe deformation and pipe support or component fracture or damage requiring extensive repairs. At Indian Point #2, an eighteen-inch feedwater pipe bulged at one location and fractured at another (inside containment) leading to deformation of the steel containment liner from the thermal shock of the impacting hot water. At Calvert Cliffs #1, feedwater control or isolation valves were rendered inoperable on both of their loops and feedwater flow control was lost altogether on one loop, resulting in a rapid flooding of the vessel.

Various hardware modifications and operating procedures have been recommended by the PWR vendors and by U.S. and foreign utilities. These approaches have been based almost entirely on qualitative descriptions of the phenomena. At various plants, pipes have been shortened, loop seals or internal water traps have been installed, holes in the feedwater sparger have been oriented up instead of down, vent holes have been drilled in the sparger, and a maximum limit on the feedwater flow rate has been employed.

Experience with all of these fixes has been equivocal. For example, there was no waterhammer reported in the first two tests of a shortened pipe at Indian Point #2, but there was a waterhammer event in the third test with only the pretest power level changed. Top discharge J-tubes have been tested successfully at several plants, but there was a waterhammer event during tests of functionally equivalent internal standpipes at Calvert Cliffs #2. Vent holes were tested successfully at Doel #2, but not at Ringhals #2. Feedwater flow rate limits were established by tests at Indian Point #2 and Doel #2, but the Zion plants experienced three separate waterhammer incidents during the first five months of our study even though these two plants have piping layouts that are within the Westinghouse guidelines and plant operators were apparently adhering to the flow rate limit recommended by Westinghouse.

Creare Conclusions

The present state of knowledge is so limited that means to reduce the frequency or intensity of water slug impact must be simple and overpowering in their implementation and subject only to the most unsophisticated success criteria. Available analytical procedures are insufficiently mature for confidently predicting water slug behavior in PWR systems during abnormal operating transients.

The eleven possible combinations of the four interdependent means recommended by the PWR vendors to mitigate steam-generator waterhammer are ranked subjectively from best to worst in Table 19 (page 218). The highest-ranked combination is to employ all of the recommended means, namely to: discharge from the top of the feedwater sparger, make the feedwater piping as short as possible, initiate feedwater flow as soon as possible during an event that uncovers the feedwater sparger (with top discharge only), and limit the feedwater flow rate until the feedwater sparger is refilled with water. Although experience with this full combination is very limited, even lower ranked combinations have a successful operating and test history and have clear qualitative merits. The lowest ranked approaches are the individual means alone; these are of questionable merit and reliability.

Steam generators supplied by B&W already incorporate the best of the approaches and implement them in a particularly positive manner. Most other new plants now coming on stream are implementing the highest ranked approaches. However, many operating plants presently employ lower ranked approaches. Accordingly, the most immediate means available to reduce the frequency and severity of steam generator waterhammer is for operating reactors to upgrade their system within the framework of the present PWR vendor recommendations. The utilities decision to upgrade their approach should, however, be based on additional economic, plant operation, and safety considerations beyond the scope of this project.

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There are many alternatives to the set of approaches presently recommended by the PWR vendors. However, these alternative approaches have not yet been designed, tested or analyzed appreciably and cannot be recommended for use at the present time. It should be recognized that the problem is solved if the feedring does not drain so that no steam void forms. If means are employed to supply a flow in excess of the drainage rate whenever the steam generator level drops below the feedring (e.g., a small pump on line at all times, controls to ensure that existing pumps are on before the feedring is uncovered, or the use of suitable accumulator systems) then the feedring will not drain. (Such approaches are practical with top discharge J-tube systems where the drainage rate is already reduced dramatically.) Only the reliability of such means needs to be evaluated; further analysis and testing of complex hydraulic and thermodynamic phenomena is unlikely to be necessary.

Verification by tests on a facility of intermediate scale, by further analytical model development, and ultimately by PWR tests, becomes increasingly desirable as lower ranked means are employed. Indeed, one recommendation of this report is that the Nuclear Regulatory Commission immediately plan an intermediate scale test program that would be expected to 1) verify present and alternate approaches, 2) guide and reduce the number of PWR tests needed, and 3) provide quantitative data suitable for the development of empirical modeling coefficients in "best-estimate" analyses for predicting criteria for slug formation and the characteristics of resultant pressure waves in PWR feedwater piping systems.

Basis for Creare Conclusions

Our findings are based on a comprehensive review of the relevant PWR operating and test evidence, a study of the literature treating the phenomena, an examination of the few preliminary analytical and experimental model investigations of this problem performed previously, and our own experiments and analyses. Creare has developed original analytical models of the component phenomena and has supported the analytical model development by experimental modeling at 1/10 and 1/4 scale. Although significant advances in understanding the phenomena have been made, this type of work is still at an early stage and can only be described as exploratory. Key findings are reviewed below.

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A hydraulic instability in the feedring responsible for water slug formation in bottom discharge systems has been identified for the first time and supported by air-water experiments at 1/10 and 1/4 scale and steam-water experiments at 1/10 scale. A first-order analysis of this instability has been developed and compared successfully with the quantitative data. Two other mechanisms for water slug formation have been identified and examined. This work demonstrates that it is possible for water slugs to form in the feedring as well as in the feedpipe. Since most of the drained volume is in the feedring in PWRs, modifications that only shorten the feedpipe may be expected to be ineffective.

Modeling experiments at 1/10 scale have explored slug formation, motion, and impact behavior and its dependence on feedwater flow rate, water temperature, vessel pressure, water subcooling, noncondensible gas content, feedwater pipe length, and sparger hole pattern and orientation. A threshold flow rate below which water slugs do not form was determined as a function of water subcooling. Pressure histories of void depressurization and slug impact overpressure were recorded as functions of all the above parameters. A major empirical finding was that the combination of top discharge and a very short external run of feedwater pipe reduced the overpressure magnitudes by a factor of 5 to 10 relative to the overpressure measured with bottom discharge and a long pipe run. Furthermore, neither top discharge nor a short pipe alone reduced the overpressure magnitudes within the data scatter.

First-order bounding analyses were developed to describe rapid steam condensation, void depressurization, water slug dynamics, and water slug impact. Of these, steam condensation rates are most uncertain; however, the available data support the use of an extreme model that assumes an instantaneous reduction of the pressure in the void to zero. The dynamics of water slug motion and pressure wave propagation at impact are then straightforward to analyze, but rely on presently arbitrary assumptions of initial water slug size, amount of water initially in the pipe, and so on. When the need for such assumptions is eliminated by direct measurements, as in some of our experiments, the calculations agree with the data

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within the data scatter. Furthermore, the uncertainty introduced by these assumptions is likely to be much less than that due to poor prediction of condensation rates or due to related and previously unidentified phenomena, such as the potential for rapid condensation and depressurization on the trailing face of the water slug, instabilities that tend to break up the slug and three-dimensional flow phenomena at impact. These three phenomena tend to reduce the intensity of the waterhammer pressure waves.

The propagation without attenuation of pressure waves through piping systems and the calculation of the resultant stresses are well developed engineering disciplines. However, the underlying physics in typical codes for the behavior of complex piping systems are limited by crude modeling assumptions that may need to be refined to treat forcing functions of the slug impact type. First-order analyses of simple piping systems have been conducted as part of the present work to derive order of magnitude estimates, to illustrate typical modeling assumptions, and to permit comparison with the limited available pipe deformation data from PWR experience.

Based on several conservative assumptions, impact overpressures of 16,000 psi or more are estimated, whereas typical piping is overstressed and bulges permanently at 3000 to 6000 psi. Moreover, ASME codes specify a stress safety factor of two or more for such applications. The available data from exploratory tests at 1/10 scale and vessel pressures near atmospheric pressure are a factor of three to six below the upper limit of 2200 psi estimated for those conditions by the same means. Therefore, there is a preliminary indication that the present estimates are quite conservative predictions of overpressure magnitude. Bending and other modes of deformation that depend on more complex parameters including the pressure impulse, the piping geometry, and the location and type of restraints must also be treated. The predictions of highly idealized models indicate that typical piping systems may also be overstressed in bending, as has been demonstrated by the damage reported at several commercially operating plants. Empirical evidence from extensive modeling experiments will be needed to improve the available calculation methods.

These are the reasons that our main recommendations are based on implementing means to reduce the probability and intensity of waterhammer. Techniques are not yet available with sufficient precision to permit confident calculations that waterhammer will not occur in present systems, and conservative calculations predict overstressing of the piping. Therefore, the development, verification, and implementation of simple and overpowering means has been, and should remain, a priority item.

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ACKNOWLEDGEMENTS

The direct contributions of Professor C. Samuel Martin to this report are gratefully acknowledged. Professor Peter Griffith also provided many critical comments and helpful suggestions for which we are thankful.

The support of this work by the Nuclear Regulatory Commission, Division of Systems Safety is acknowledged. In particular the guidance and suggestions provided by Mr. Victor Benaroya and Mr. Donald Fischer have contributed significantly to the value of our efforts. The assistance rendered by Mr. Peter Hearn and Mr. Robert Giardina is appreciated.

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1 INTRODUCTION AND SUMMARY

This report responds to the need of the Nuclear Regulatory Commission, Division of Systems Safety, for improved understanding of the mechanics of waterhammer in the feedwater piping systems of Pressurized Water Reactor (PWR) steam generators. The report describes a six month study completed in December of 1976.

In agreement with the workscope specified in the contract award, Creare's efforts have been divided into three parts:

1) A review of available information describing PWR waterhammer incidents, analytical models and test data.

   This review has led to a general understanding of the sequence of events associated with steam generator waterhammer and a tabulation and comparison of conditions under which these events did or did not occur.

2) Investigation of the physical phenomena involved, the important thermodynamic and fluid mechanical processes and their interactions.

   These studies have led to quantitative descriptions of key phenomena, limiting analyses for predicting the possible range of identified effects and an understanding of the major causes of uncertainty in cases where the phenomena are poorly defined.

3) Recommendations for actions to be taken by the Nuclear Regulatory Commission.

   An assessment of previous understanding and the original experiments and analyses conducted during this project have led to a ranking of various combinations of means to reduce the probability and severity of steam generator waterhammer. Based on this, various alternative and parallel recommendations and strategies of action are presented and discussed herein.

1.1 Background

The damaging potential of waterhammer caused by water slug impact in the steam generator feedwater systems of PWRs was first demonstrated conclusively by the study following the major

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incident at Indian Point #2 on November 13, 1973 [1].* Damage on this date included a 180° circumferential fracture of the 18 inch diameter main feedwater pipe to the #22 steam generator at the point where the pipe penetrated the reactor containment structure, gross thermal deformation of the metal containment liner near this juncture due to water sprayed from the ruptured pipe, and a large bulge in the main feedwater pipe in the horizontal run of pipe to the steam generator nozzle. Water level could not be reestablished in #22 steam generator and it was isolated from the system during reactor cooldown. Over three hours passed between the initiating event and complete isolation of #22 steam generator.

Since the incident at Indian Point #2, and up to December 1, 1976, there have been at least 16 reported events believed to involve water slug impact in the steam generator feedwater systems of U.S. PWRs. At least five similar waterhammer incidents were also identified prior to that at Indian Point #2 (the earliest recorded event was at Yankee Rowe in 1966), according to the available evidence. Several waterhammer events have also occurred at various PWR plants during system tests intended to demonstrate the absence of waterhammer.

These waterhammer incidents are triggered by unusual operating transients such as unexpected reactor or feedwater pump trips, which occur infrequently and do not necessarily lead to waterhammer. Thus, there is a considerable element of statistical randomness about these occurrences which is itself a major cause of present uncertainties.

Evaluation of the safety implications of possible steam generator waterhammer is beyond the scope of the present program. However, it is our understanding, based on meetings held with the PWR vendors [2,3,4] and on examination of piping and instrument drawings, that the steam generators are typically the only readily available means (i.e., without activating safety systems) with adequate heat transfer capacity to remove all decay heat in the event of a reactor trip from 100% power. Accordingly, loss of function of all steam generators at a plant has potential safety consequences requiring careful evaluation. Moreover, there has been considerable loss of power generation and revenue arising from plant downtime and delays due to waterhammer events. To cite a prominent example, Indian Point #2 was not operated (except for test purposes) for over four months following the major incident at that plant. For these reasons there is considerable incentive to obtain a prompt and effective solution to the "steam generator waterhammer" problem.

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*Numbers in brackets designate references listed at the end of this report.

Over the years various "fixes" have been proposed for this problem, including hardware design changes and regulated operating procedures. Often, the fixes have been described or interpreted as means to "prevent" waterhammer from occurring. Relative to this criterion, the success of the fixes has been poor. Even those which were expected to preclude waterhammer based on qualitative arguments or appeared to pass some preliminary tests have often failed to prevent waterhammer in the later, more severe, tests or under operating conditions. During the first five months of this project, three waterhammer events were reported at the Zion plants, which were apparently following two independent guidelines, specified by the steam generator manufacturer, for avoiding waterhammer.

It is important to question the "success" criterion as well as the hardware design. It is probably impossible to prevent waterhammer absolutely. Small steam bubbles may be expected to develop, collapse, and induce pressure waves, noise and visible pipe motion. Pipe system deformation and component fracture are undesirable, but all waterhammer events are not unacceptable. With this perspective, part of the solution to the steam generator waterhammer problem may lie in the development of more sophisticated criteria for what is "unacceptable".

The base of quantitative technical data that has been available for providing accurate evaluation of analytical models of these phenomena is scanty. Only one PWR system (Tihange) has had instrumentation in place to provide a recording of the pressure transient associated with a significant waterhammer event that occurred during a test at operating conditions. Only one test was run at Tihange and during the test the pressure transducers failed. No damage was reported subsequent to this event. Extensive instrumentation installed at Indian Point #2 after the November 1973 incident and at Doel yielded little useful information apparently because the three waterhammer events recorded at Indian Point #2 and the two reported at Doel during their test programs were mild; no damage was reported subsequent to any of these events. A scattering of tests have been conducted on PWR installations in which a limited number of parameters was varied; in these tests, only whether or not a significant noise was heard or damage observed was recorded. A small scale modeling study by Westinghouse [5] did not achieve anywhere near the potential impact pressures, apparently because of the presence of reported large fractions of noncondensible gases in the steam. For these reasons it has been necessary for Creare to perform some diagnostic model studies to provide a sound basis for a technical description of the phenomena.

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A comprehensive review of the evidence available from incident reports, published studies, and meetings between Creare and personnel from the Nuclear Regulatory Commission, vendors, utilities and architect-engineers is contained in Appendix A of this report. This review is summarized in Section 2.

1.2 Description of Steam Generator Waterhammer

Only a particular type of waterhammer event in the feedwater system of PWR steam generators has been considered in the present study and will be termed "steam generator waterhammer" throughout this report. This waterhammer is always preceded by an unusual system transient that causes uncovering of the feedring that supplies cold make-up water to the secondary side of a steam generator (Figure 1).* If the water level in the steam generator falls below the feedring, water may drain out of the ring and allow steam to enter; in some designs it may take several minutes for significant drainage of water from this ring to occur. Steam entering the ring can form a layer above the water lying, or flowing, on the bottom of the ring and any associated piping at the same level (Figure 2). The hypothetical sequence of events subsequent to draining is described below.

When cold feedwater is added to a drained or partially drained ring it has hydraulic and thermodynamic effects. The hydraulic effects include raising the level of water in the piping and ring, formation of "open channel" transient waves in the piping, and interaction with any steam flow that may be occurring. The thermodynamic effects include thermal stratification, steam condensation, resultant steam flow, and changes in the average temperature of the water in the ring and horizontal piping (Figure 3). These effects are coupled since the steam condensation rate depends on the mixing and turbulence occurring in the water; the steam flow itself interacts with the water surface to cause waves and mixing.

A critical "event" occurs when the various disturbances to the water surface cause it to rise locally and block off the entire cross-section, forming a water slug and trapping a steam void (Figure 4). Alternatively, a steam void can be trapped when the steam generator vessel water level rises to seal off the bottom drainage holes on the feedring. Since this steam void is surrounded by water below saturation temperature, condensation will occur, dropping the pressure in the void.

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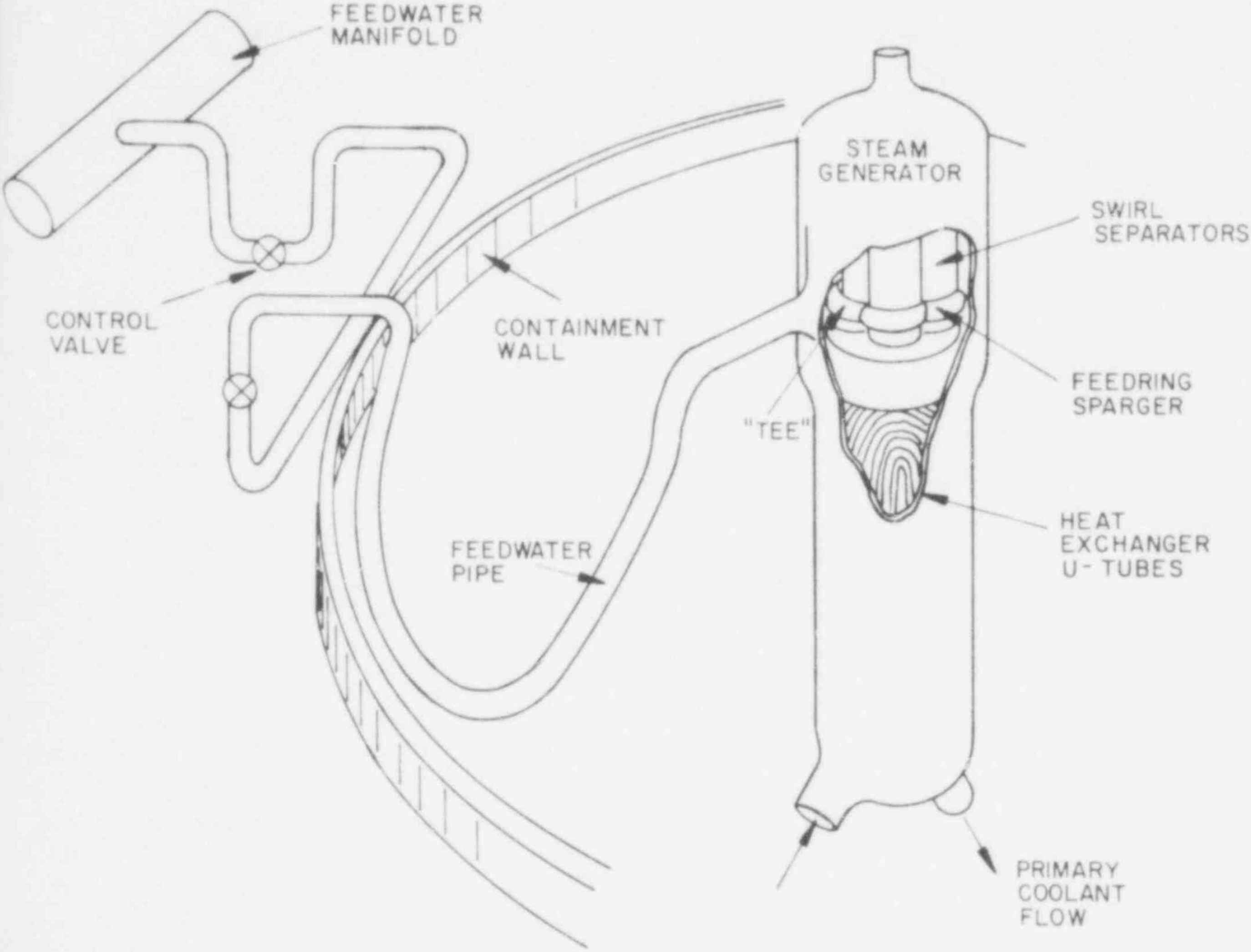
*In Babcock and Wilcox "once-through" steam generators the feedwater nozzles are designed to remain uncovered during normal operation.



GENERAL LAYOUT OF A WESTINGHOUSE FEEDWATER LINE [5]

FIGURE 1

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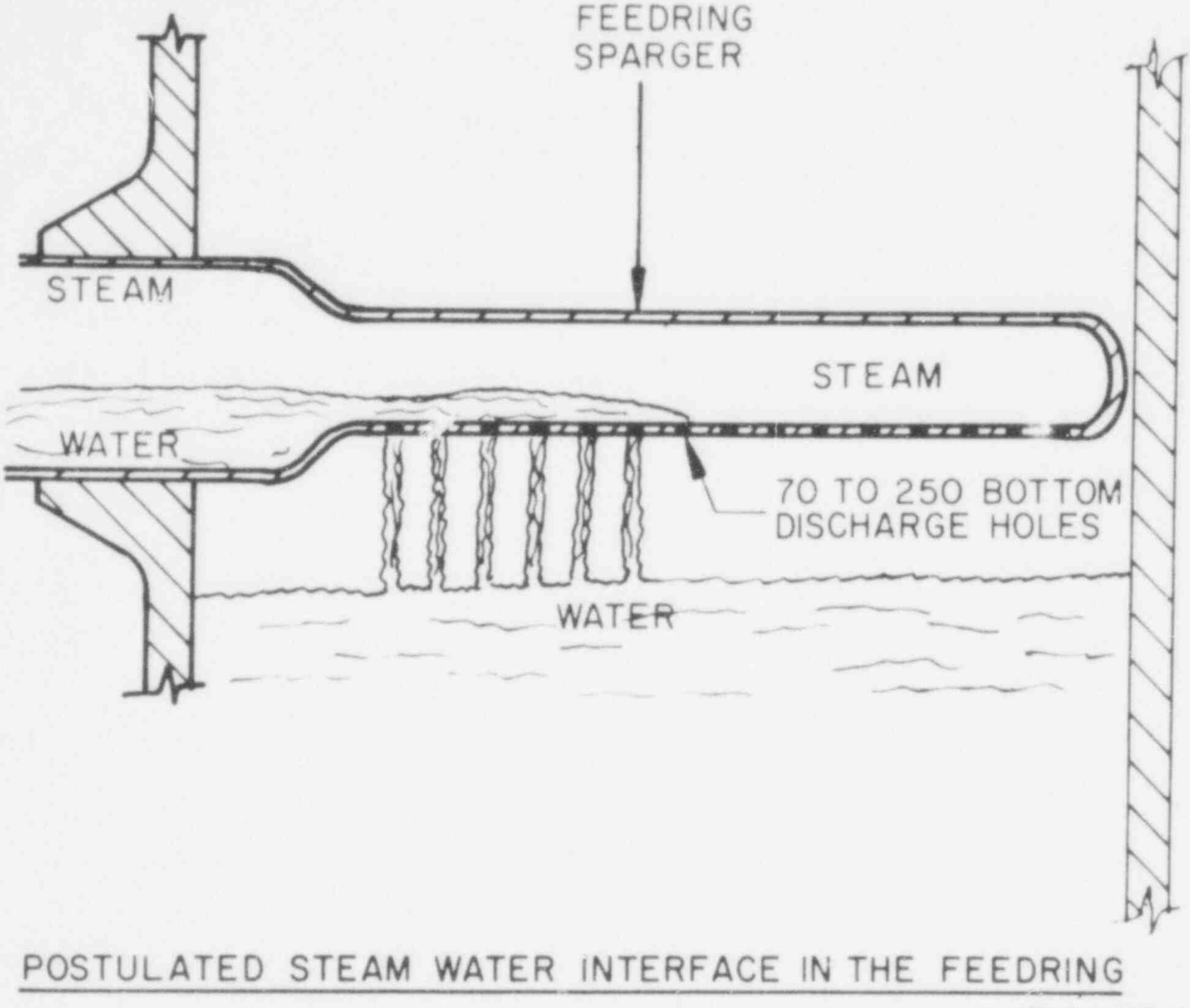

POSTULATED STEAM WATER INTERFACE IN THE FEEDRING

FIGURE 2

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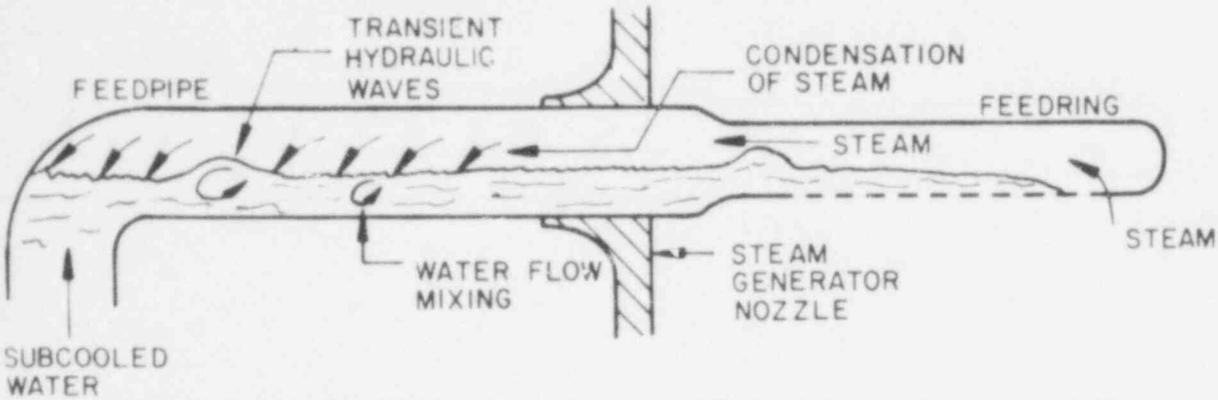
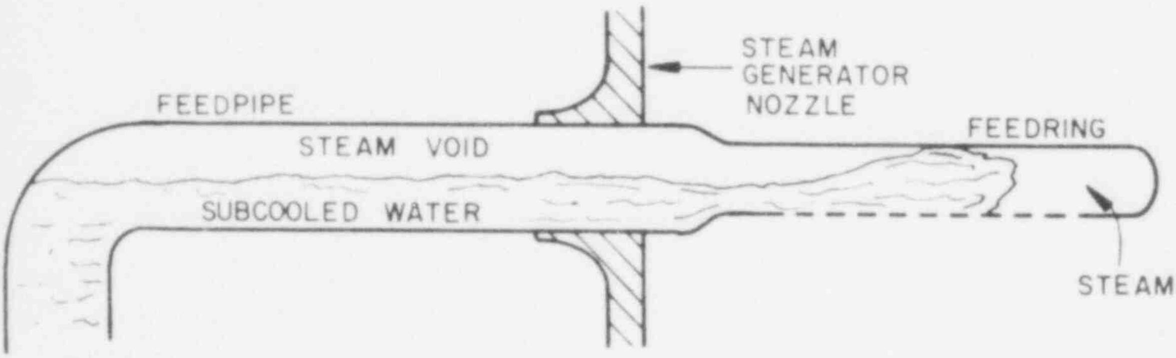


POSSIBLE STEAM-WATER MIXING PHENOMENA IN THE FEED SYSTEM

FIGURE 3



POSSIBLE TRAPPING OF A STEAM VOID

FIGURE 4

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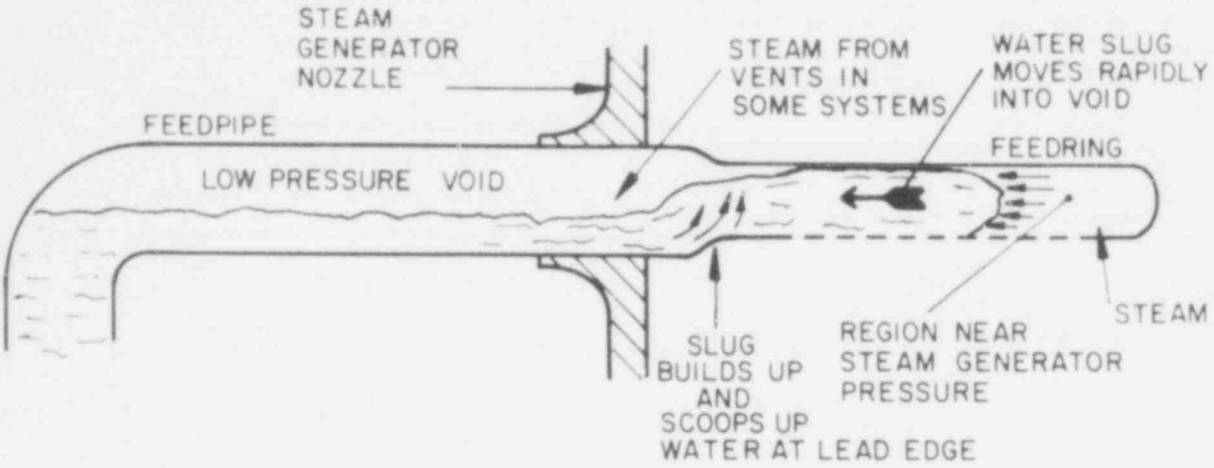
The pressure difference between the steam generator and the collapsing void accelerates the water "slug", which is blocking the pipe cross-section. As it moves, this slug scoops up water lying on the bottom of the pipe. The motion of this slug depends on several factors such as the rate of condensation, the water depth, the piping geometry, any vents or other source of steam to the void, and so on (Figure 5).

Waterhammer occurs if the steam void collapse is rapid enough and if the trapped steam essentially disappears. The water slug, which may be traveling at tens or even hundreds of feet per second, impacts on the water filling the upstream side of the pipe and sends large hydraulic pressure waves (possibly thousands of psi in amplitude) through the system (Figure 6).

Damage to the piping may occur by at least two mechanisms. The first is the local overpressure which may exceed the yield stress of the material in the hoop direction and cause the pipe to grow like a balloon and possibly rupture. The second is the response of the entire piping network to being wrenched violently by strong short impulses as the hydraulic pressure waves travel around bends, reflect from junctions or valves and so on.

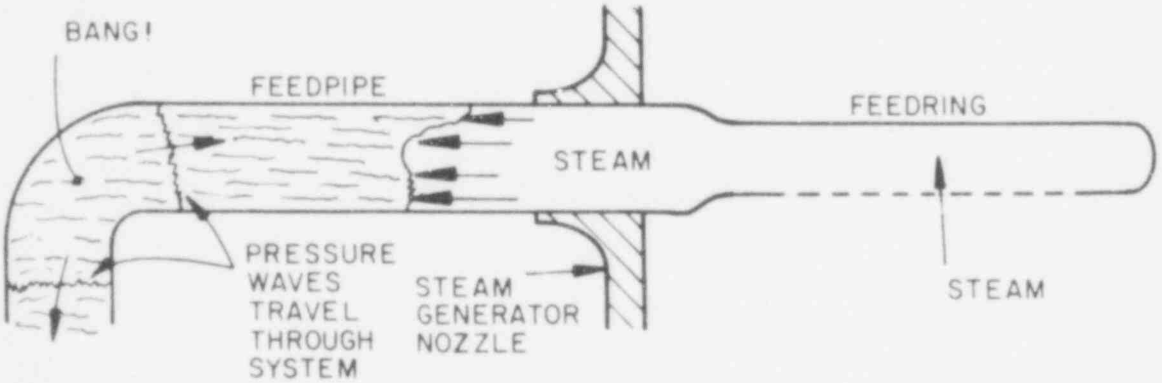
This scenario is in essential agreement with qualitative descriptions provided by all of the parties with whom Creare has been in contact. It obtains support from the small scale experiments by Roidt [5], and the diagnostic work performed under this contract. It must be admitted, however, that almost no direct evidence exists for such a sequence of events in a PWR system; it can merely be stated, at present, that if one assumes such a scenario and develops reasonable analytical models based upon it, the results are not incompatible with the very limited full-scale evidence that is available.

It is clear that a sequence of several sub-events is necessary in order for damaging waterhammer to occur. The sequence may be interrupted if any one of the individual steps is inhibited. Moreover, each sub-event is a function of several variables, including the initial conditions, the system geometry and some parameters that may be under the control of the PWR operator. Since plant conditions vary, it would be lucky, and generally not to be expected in advance, if it were possible to develop some "simple" universal descriptions of the entire event and criteria for its severity. Before reaching conclusions about how far the description can be simplified it is necessary to examine the individual phenomena in detail, as we shall do in Sections 3, 4, and 5 of this report.



POSSIBLE SLUG ACCELERATION INTO VOID

FIGURE 5



POSSIBLE WATER SLUG IMPACT

FIGURE 6

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1.3 Phenomena Contributing to Waterhammer

We have found it useful to identify six separate physical phenomena, roughly in accordance with the stages in the scenario discussed in the previous section, which need to be adequately understood if an analytical model is to be developed. These are:

1) initiating mechanisms,

2) steam void collapse,

3) water slug dynamics,

4) pressure pulses from water slug impact,

5) transmission of pressures and forces throughout the piping,

6) mechanisms that may overstress the piping system.

Subsequent sections of this report will consider the technical aspects of each of these categories with emphasis on the first four items.

1.4 Summary of Results

The major results of this work in the order in which they can be found in this report are summarized below.

Section 2, supported by the comprehensive survey in Appendix A, reviews PWR experience. Background information is supplied including a description of typical systems. A comprehensive history of reported U.S. PWR operating experience is presente in the context of a hypothetical sequence of events during a "steam generator waterhammer" incident. Vendor recommendations and plant tests are described. It is found that the rate of reported steam generator waterhammer incidents has increased since the archetypical incident at Indian Point #2 on November 13, 1973. The vendor recommendations and their underlying rationale is described. These recommendations are based largely on hypothetical descriptions of the behavior which draw some support from very limited qualitative experiments at 1/10-scale. Little quantitative evidence of any kind has been developed either in subscale experiments or during PWR tests intended principally to assist in verifying hardware or operating procedures at the particular plants tested. Premature tests of hardware modifications directly on PWRs without an adequate analytical basis or confirmation on scale models has tended to be ineffective and has reduced the credibility of current recommendations. Current operating plants have not adhered to the vendor recommendations uniformly and a variety of operating procedures and hardware is currently employed.

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Section 3 reviews previous research and related technology on each of the phenomena identified in the hypothetical sequence of events comprising a steam generator waterhammer incident. Although hydraulics and some aspects of two-phase flow are well developed engineering disciplines, little development work has been conducted on such hydraulic behavior as feedring drainage and no prior analytical work on slug formation in steam generator feedrings was identified during the course of the present study. The basic physics of rapid condensation and resultant steam flow and steam void collapse during steam-water mixing with a free interface are controversial. There is evidence to support an extreme model of condensation rate limited only by gas dynamic effects in the vapor. Water slug dynamics is one of the best understood phenomena and can be predicted accurately from first principles if the initial water interface and the pressure in the void are known. Water slug impact, pressure wave propagation without attenuation, and the calculation of stresses in piping and components are well developed engineering disciplines that do not significantly increase the uncertainty in the calculated behavior relative to that introduced by other features of the problem, although improved analysis techniques are desirable.

Westinghouse has performed model experiments at 1/10 scale and has conducted analyses of void collapse, slug motion, and slug impact. Unfortunately, the Westinghouse report by Roidt [5] indicates that the experiments were performed with an appreciable air content in the steam, which largely mitigates the value of the results. (Tests in a similar facility at Creare, but with negligible air content, gave impact pressures 100 times the typical values reported in Reference [5].) The Westinghouse analysis contains questionable assumptions that tend to decrease the predicted overpressure at impact by as much as a factor of 50 relative to alternative assumptions that can be made. Limited tests at 1/10 scale have also been conducted by the Framatome group and scattered analyses have been reported by various architect-engineers. The other PWR vendors have not published any analyses or reports on experimental model studies of this behavior.

Together, Sections 2 and 3 of this report provide for the first time a thorough summary of technical information available to assess steam generator waterhammer. Creare concluded based on this information that additional work would be required in order to identify and describe the major phenomena realistically. Accordingly, Creare mounted original experimental and analytical efforts in order to raise the level of understanding of the relevant phenomena.

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Section 4 presents the results of exploratory tests conducted at Creare using 1/10 scale models of the feedwater system. Extensive quantitative data were obtained to contribute to confirmation of analyses. Overpressures up to 700 psi and 1300 psi respectively were recorded in experiments with system pressures of 16 psi and 75 psi. Flow visualization was performed using transparent test sections in order to identify the mechanisms of slug formation during these tests and to measure slug velocity using high speed motion picture films. Extensive quantitative hydraulic and two-phase flow data were also established by experiments at Creare using a 1/4-scale model of a feedring in an air environment. Empirical curves were developed to delineate the threshold flow for slug formation as a function of water temperature, system pressure, and feedwater sparger geometry. The effects of noncondensible gas content were investigated. Experiments with various hardware modifications recommended by the vendors for use in PWRs demonstrated that while top discharge alone did not prevent waterhammer, it tended to suppress slug formation somewhat even in initially drained systems (it increased the threshold flow rate by 50%). A shortened feedwater pipe alone reduced slug impact overpressure only 20%. Top discharge and a shortened pipe were much more effective together than separately; in combination they reduce slug impact overpressure by a factor of 5 to 10 and appreciably reduced the frequency of slug impact occurrence in our exploratory experiments.

Section 5 presents new analyses and phenomenological descriptions developed by Creare. The calculations are compared with the data described in Section 4. A previously unsuspected hydraulic instability stemming from feedring hole effects and multiphase flow interactions in the feedring was identified conclusively during the tests. Analysis of this instability led to a quantitative hydraulic criterion for slug formation that agreed with our air-water data within $\pm$ 10%. This part of the work demonstrates that it is likely for water slugs to form in the feedring (as well as the feedpipe) and explains why shortening the feedpipe (alone) was ineffective in our experiments. Prediction of condensation rates introduced significant uncertainty into the extension of the hydraulic analysis to predict our steam-water data; the analysis and data agreed within a factor of two. A first-order analysis was also conducted of a countercurrent flow instability that might cause slug formation in the feedpipe (particularly in top discharge systems where the hydraulic instability is suppressed). First-order analyses of void collapse, slug dynamics, and slug impact were conducted and the calculations were compared with the available data. A simple model assuming that the condensation rate is limited only by compressible gas dynamic behavior in the void (in essence a sonic velocity limit) is appropriate to describe the depressurization recorded at Tihange, but overpredicts the rate of depressurization in our experiments at 1/10-scale by a factor of five. Using the actual pressure measured in the void, the calculated slug motion and impact generally agrees with our data within the data scatter, which is approximately a factor of two. First order pipe stress analyses were conducted and shown to be consistent with data from the Indian Point #2 incident and the Tihange test. The prime utility of the analytical efforts described in Section 5 of this report is to provide an overall quantitative perspective on the relevent phenomena, including the effects of various parameters, quantitative predictions where appropriate, and an illustration of major analytical uncertainties.

Together, Sections 4 and 5 of this report present the first thorough work on the entire spectrum of phenomena associated with steam generator waterhammer. The understanding derived is rudimentary, but this is in keeping with the complexity of the phenomena.

Section 6 assesses the state of knowledge, evaluates the present situation, and recommends necessary further work.

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2 PWR EXPERIENCE WITH STEAM GENERATOR WATERHAMMER

This section of the report summarizes the comprehensive review of the available evidence from PWR operating and test experience that is provided as Appendix A of this report. Vendor recommendations and utility actions on means to mitigate steam generator waterhammer are evaluated based on PWR experience. Quantitative evidence potentially useful for confirmation of analytical models is identified. The reader familiar with the background of this problem may wish to proceed directly to the conclusions derived in Section 2.6.

At the outset of the present study, Creare was supplied with the extensive body of relevant evidence available to the Nuclear Regulatory Commission, Division of Systems Safety. In addition, meetings were held with each of the PWR vendors [2,3,4], and with various utility, plant, and A/E personnel in order to ensure that Creare was aware of virtually all documentation of PWR experience, analyses, and scale model studies of the behavior. Needed documents were obtained. A "position statement" was solicited and obtained from each PWR vendor. This documentation is summarized here to provide a comprehensive base for this and future work.

2.1 Steam Generator Waterhammer During PWR Operation

Relatively little attention was given to the occasional reports, during the first roughly 100 reactor-years of commercial operation, of minor pipe support damage and pipe deformation due to waterhammer in the secondary coolant system. "Bumping" and audible, but non-damaging waterhammer are events that are routinely tolerated in steam-water systems ranging from home radiators to nuclear power plants. However, incidents involving appreciable pipe deformation and support damage in the last four years (roughly another 100 reactor years of commercial operation) have demonstrated generic phenomena with significant economic and potential safety consequences. This experience is summarized in Tables 1 and 2.

Although the survey and technical work in this report is limited to behavior in PWR steam generators, it is important to appreciate that similar phenomena may occur in other components or in Boiling Water Reactors (BWRs). To illustrate this and to provide a broader perspective on the present investigation of phenomena, some of the reported incidents in BWRs are listed in Table 3.

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TABLE 1

DOCUMENTED U.S. HISTORY OF FEEDWATER/STEAM GENERATOR WATERHAMMER*

Date	Plant	PWR Vendor	Comments
1966	Yankee Rowe	W	damaged feedring
?	Haddem Neck	W	"several" waterhammer incidents minor piping support damage
04/29/72	San Onofre #1	W	safety injection triggered
10/01/72	Surry #1	W	main feedpipe displaced ten inches, extensive piping support damage, check valve gasket blown.
07/22/73	Robert Ginna	W	feedwater pipe vibration
1973	Oconee #1	B&W	very minor incident
11/13/73	Indian Point #2	W	prominent incident; fractured 18 inch main feedwater pipe, deformation of containment liner.
?	Turkey Point #3	W	leaking check valve, unit 3B
?	Turkey Point #4	W	leaking check valve, unit 4B
01/74	Turkey Point #4	W	prominent incident; inspection uncovered piping support damage, and pipe elbow deformation during fuel outage, unit 4A.
01/14/74	San Onofre #1	W	damage to three supports and one restraint
?	Fort Calhoun #1	CE	vibration and instrument damage on auxiliary feedwater piping on several occasions.
08/29/74	Zion #2	W	two broken snubbers loop 2C, safety injection signal due to low pressure in loop 2D.
12/30/74	Zion #2	W	no damage reported, safety injection due to low pressure in loop 2C.
01/13/75	Maine Yankee	CE	valve failed**
03/18/75	Zion #2	W	broken air line loop 2D, motion and broken snubber loop 2B.
04/12/75	Calvert Cliffs #1	CE	prominent incident; all main feedwater isolation valves vendored inoperable; other valve damage; extensive piping support damage.
06/1/75	Praire Island #2	W	damaged check valve
06/17/75	Robert Ginna	W	sheared instrument lines evidence of pipe motion
1975	Donald C. Cook	W	no damage reported

The above incidents are the complete set of those identified as possibly due to steam generator waterhammer in the responses to the May 13, 1975 NRC questionnaire [6]. Below are listed a few recent incidents that have come to our attention.

05/28/76	Zion #2	W	no damage reported, waterhammer in loop 2C implicated in safety injection.
06/20/76	Zion #2	W	no damage reported, waterhammer in loop 2C implicated in safety injection.
09/27/76	Zion #1	W	safety injection, loops 1A and 1C 8 hangers broken, loop 1D
11/05/76	Beaver Valley	W	no report received yet.**

*Waterhammer incidents reported to have occurred in the feedwater piping of operating U.S. PWR plants. Although these incidents are believed to be of the steam-generator waterhammer type described in the Introduction, there is little or no evidence available to specify the actual causes in most cases, as described in Appendix A. Source: response to the May 13, 1975 Utility Questionnaire [6] and incident reports listed in detail in Appendix A of this report.

**Some evidence suggests that this incident may be unrelated to the sequence of events termed "steam generator waterhammer" in this report.

TABLE 2

U.S. PWR EXPERIENCE WITH FEEDWATER/STEAM GENERATOR WATERHAMMER*

A. Westinghouse PWRs

Plant	Reported Incidents
Beaver Valley	1**
Donald C. Cook	1
Robert Ginna #1	2
Haddam Neck	"several"
Indian Point #2	1
Kewaunee	none
Point Beach #1	none
Point Beach #2	none
Praire Island #1	none
Praire Island #2	1
H. B. Robinson #2	none
Salem #1	none
San Onofre #1	2
Surry #1	none
Surry #2	1
Trojan	none
Turkey Point #3	1
Turkey Point #4	2
Yankee Rowe	1
Zion #1	1
Zion #2	5

B. Combustion Engineering PWRs

Plant	Reported Incidents
Calvert Cliffs #1	1
Calvert Cliffs #2	none
Fort Calhoun #1	?
Maine Yankee	1**
Millstone #2	none
Palisades	none
St. Lucie #1	none

C. B&W PWRs

Plant	Reported Incidents
Arkansas #1	none
Oconee #1	1
Oconee #2	none
Oconee #3	none
Rancho Seco	none
3 Mile Island #1	none

*Source: Table 1.

**Some evidence suggests that this incident may be due to causes unrelated to the sequence of events termed "steam generator waterhammer" in this report.

TABLE 3

SOME REPORTED WATERHAMMER-LIKE INCIDENTS IN BWR PLANTS

Date	Plant	Component Involved
May 1970	Dresden #2	HPCI Piping
May 1971	Oyster Creek #1	Containment Drywell
March 1971	Dresden #2	Containment Spray
March 1971	Dresden #2	Core Spray Piping
Sept. 1971	Dresden #2	Shutdown Coolant Piping
April 1972	Quad Cities #1	RHR Piping
June 1972	Millstone #1	LPCI Piping
Oct. 1972	Browns Ferry #1	HPCI Piping
Oct. 1972	Millstone #1	Blowdown Condensor
April 1973	Browns Ferry #1	HPCI Piping
June 1973	Duane Arnold	HPCI Piping
June 1973	Dresden #3	Feedwater Regulating Valve
Nov. 1974	Dresden #3	Core Spray Piping
July 1975	Fitzpatrick	RHR Piping

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It is important to recognize that Table 1 cites only the reported waterhammer incidents in PWR experience. Evidence of mild waterhammer is routinely tolerated as a gremlin that haunts all steam-water plants. Such behavior is rarely reported unless there is pipe or support deformation or a related reportable abnormal event. During the course of our study, informal discussions identified internal plant memoranda documenting at least one feedwater system waterhammer incident that was not reported to the NRC. Conversely, some of the "incidents" cited on Table 1 may not qualify as reportable occurrences, but have been described by the utility for completeness. Table 1 summarizes the reports directly without attempting to make an independent judgment of severity.

Comprehensive review has, of necessity, been limited to U.S. plants. Although Creare is aware of similar occurrences at some foreign plants, such as Mihama #1, Doel #2, Tihange #1, Beznau #1, and Ringhals #2, descriptive documentation is generally lacking. The limited available information is described in Appendix A in the context of tests conducted at these plants.

It must also be pointed out that the evidence of waterhammer is often scanty. Here the information displayed in Appendix A is summarized. In major incidents, there have been loud audible "bangs" and subsequent inspection revealed measurable deformation or obvious fracture of the piping system or its supports. In other cases there may only have been a noise or a meter fluctuation without detectable damage or permanent effects on the system. (The difficulty of hearing or isolating a noise over normal background noise levels at a PWR plant should be appreciated.) Alternatively, in several cases permanent deformation of the feedwater piping or supports found during periodic inspection has been reported as a possible steam-generator waterhammer incident even though no direct audible or visible evidence of waterhammer had been recorded at any previous time. No PWR has had dynamic instrumentation in place suitable to record waterhammer characteristics during any of the incidents cited in Table 1. (A few plants have had such instrumentation installed during special tests.) Quantitative information is generally limited to steam generator water level records and meter readings recalled by the operator, and even that information is scanty. Thus, the true cause and nature of most of the reported incidents is uncertain.

Only the incident at Indian Point #2 has received careful exhaustive study by the utility. With few exceptions, repairs have of necessity been made promptly following an incident, in order to resume power generation, and only a cursory document has been issued to note that an incident had occurred, to describe the repairs, and to indicate the utility's view that safe operation could be resumed.

With the above as background, any evaluation must be limited to very general, overall conclusions. A detailed understanding of steam generator waterhammer behavior is not possible from PWR operating experience alone, although it provides some evidence for testing any speculations, and some indication of the frequency and severity of the problem.

2.2 General Review of Action by the Industry

The previous section of this report identified a generic problem that emerged in the early 1970's when several PWR plants suffered pipe system deformation and component fracture due to feedwater flow instabilities. The nuclear reactor industry responded to this situation in several ways. Major programs of "research" were conducted at a few plants, notably Indian Point #2 and Doel. Of necessity, such research using PWRs was limited by the prime need to return the plant to service, but the desire for safe and reliable service with high confidence was influential in dictating careful, thorough studies at some plants. The vendors, particularly Westinghouse, conducted analyses and scale model studies and advised the utilities on system design and on verification test programs. The Nuclear Regulatory Commission has recognized the generic basis of the phenomena and has encouraged the development of an objective base of technical information on which strategic decisions can be founded. The broad-based questionnaire of May 1975, the present overall evaluation study, and internal evaluation studies now underway, are typical of several NRC efforts to develop generic technical information. In addition, clear regulatory action was taken. The utilities have been required to evaluate the hardware and procedures at each operating plant relative to the available information, and new plants coming on stream have in addition been required to test the hardware and procedures at those plants.

Creare is qualified principally to make a purely technical evaluation of the phenomena. Strategic decisions, however, should be founded on a broader spectrum of information including plant safety studies and cost-benefit analyses. In order to provide a broader perspective within which the present work may be viewed, the following paragraphs summarize actions and views taken by several other informed parties.

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The several parties involved have universally indicated that steam generator waterhammer can be tolerated without unacceptable safety or economic consequences. The Nuclear Regulatory Commission has permitted present PWR plants to continue operation and has licensed new PWR plants. Each vendor has stated that steam generator waterhammer is eliminated or can be tolerated as described in Sections 2.3 and 2.5 of this report. A representative of each utility has stated that steam generator waterhammer cannot occur or has been mitigated acceptably at their plants in their response to the NRC questionnaire of May 1975. Several architect engineers have published analyses certifying that the plants under study cannot experience waterhammer or will not exceed allowable stress levels during a credible steam generator waterhammer event. Thus, there is general agreement that steam generator waterhammer does not require urgent corrective action in order to ensure safe and economic operation of PWRs.

Differences in the views taken by the several parties are also evident. The vendors have strongly taken the position that the steam generator waterhammer problem will be eliminated altogether--in the sense that any waterhammer that occurs will not damage the pipe system or exceed allowable stress levels--if their recommendations are adopted. The basis for these recommendations has been questioned by the NRC, however, due to unfavorable experience with earlier hardware recommendations that were not clearly formulated and were adopted prematurely without adequate confirmation. Additional questions have arisen because adequate quantitative evidence to confirm analyses does not exist. New plants coming on stream have usually implemented the most generally accepted hardware configurations based on present evidence (e.g., J-tubes and short horizontal pipe runs), but older operating plants have tended to justify a myriad of other hardware configurations and operating procedures rather than incur the cost of retrofits without clear justification of their need. Several architect-engineers and plant personnel have questioned the Westinghouse analysis from first principles [5] and the Westinghouse empirical forcing function. Some architect engineers have conducted independent analyses. In recognition of the controversy surrounding some of the technical issues, the Nuclear Regulatory Commission did not feel that an adequate technical information base existed, and acted by funding the present independent evaluation and thermal-hydraulic work as well as comparison studies by other groups, such as an analysis of the response of typical pipe systems to hypothetical forcing functions.

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In the remainder of this section of the report, system design and test experience are reviewed. Section 2.3 describes the positions taken by Westinghouse and Combustion Engineering and their supporting rationale. Section 2.4 summarizes the results of tests of PWRs supplied by Westinghouse and CE. B&W steam generators differ significantly in design and are discussed separately in Section 2.5. A summary and evaluation of the vendor recommendations relative to the evidence is supplied in Section 2.6.

2.3 Westinghouse and Combustion Engineering Recommendations

Steam generator designs developed by Westinghouse and Combustion Engineering are similar and are discussed together in this report. Typical systems are described in Appendix A. This section of the report describes the position taken by these vendors in terms of recommended approaches to preclude unacceptable waterhammer. Extensive discussion is supplied herein to clarify the terse position statements and to present our understanding of the rationale for these vendor recommendations. Action taken by the utilities on these recommendations is summarized.

Westinghouse Position

The Westinghouse position on existing feedring steam generators was presented to the NRC and to Creare at meetings held in Bethesda on July 23, 1976 and in Pittsburgh on September 1, 1976 [2]. Several follow-up discussions have been held. Certain recommendations have been quantified in the earlier Westinghouse bulletin by Bennett [7]. The Westinghouse position is:

1) "Maintain adherence to main feedwater pipe layout criteria.
2) a) Administrative or auto control to limit auxiliary feedwater flow rate is satisfactory or,
   b) The addition of J-tubes to feedwater ring replaces the need to limit auxiliary feedwater flow rate.
3) Feedwater/steam lines design should consider the effects of waterhammer."

It is our understanding that this position is taken on "existing" steam generators now in operation (or planned) as distinct from future "preheat" steam generators. Although similar phenomena are expected to be involved, fundamentally different and as yet unknown effects may occur in the new "preheat" steam generators.

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Pipe Layout Criteria

The Westinghouse Technical Bulletin by Bennett [ ] provides sketches, shown as Figure 7 of this report, of several possible arrangements including downward facing elbows, loop seals and pipe elevation changes with the common objective of minimizing the horizontal run of pipe just outside the steam generator. A maximum permissible run of eight feet is indicated. The pipe layout guidelines are simply intended to minimize the length of pipe that can drain through the feedring.

Throughout this report the Westinghouse definition of horizontal pipe run dicated on Figure 7 has been adopted. The length of horizontal feedwater pipe (exclusive of the feedring) <u>inside</u> the steam generator is approximately two feet in both Westinghouse and Combustion Engineering steam generators.

There is a broad qualitative basis for minimizing the horizontal run of piping. An analysis of steam void collapse and a subscale model test reported by Westinghouse [5] both indicate that the overpressure at slug impact tends to increase as the length of drained pipe is increased. Rudimentary analyses and scale model data presented in Section 4 of this report display a similar effect. Creare knows of no analytical or test basis for the specific eight foot limit recommended, however. It is our understanding that an early survey of incidents by Westinghouse indicated that there had not been any incidents in systems where the horizontal run was less than eight feet. (This is no longer true.) On the basis of the evidence, we feel that a fairer statement of the present Westinghouse position is that the horizontal run should be kept "as short as possible". Unfortunately, no one is yet in a position to indicate what is "short enough".

Evidence of misinterpretation of the analytical basis for the Westinghouse recommended limit to an "eight foot horizontal run" is available in several documents issued by the utilities. Typical of these is the statement made in the Point Beach response to the NRC questionnaire:

> "The length of the horizontal steam generator inlet pipe for Point Beach feedwater piping is shorter than the maximum allowed, which will prevent shock wave propagation from exceeding the allowable limits."

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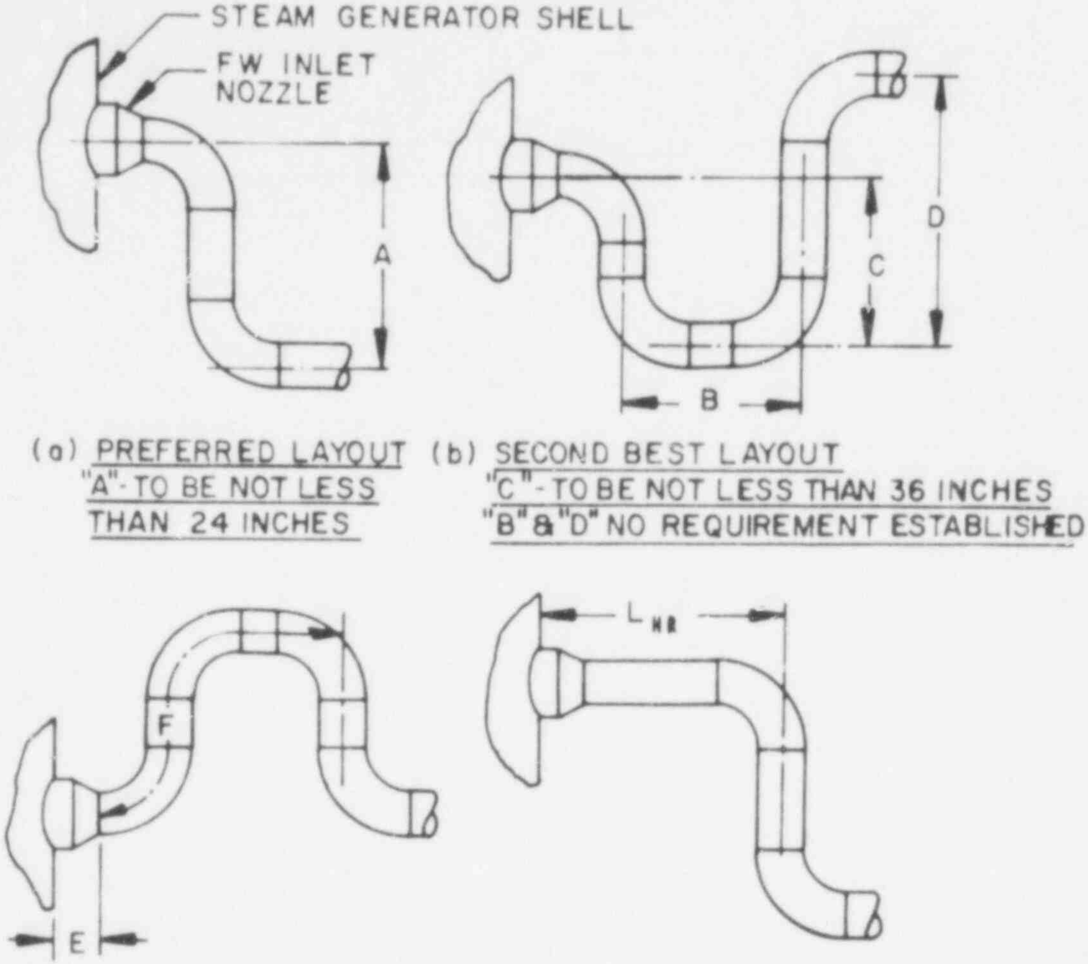

(a) PREFERRED LAYOUT
"A" - TO BE NOT LESS
THAN 24 INCHES

(b) SECOND BEST LAYOUT
"C" - TO BE NOT LESS THAN 36 INCHES
"B" & "D" NO REQUIREMENT ESTABLISHED

(c) ACCEPTABLE ALTERNATIVE
LAYOUT
"E" + "F" NOT TO EXCEED
8 FEET

(d) ACCEPTABLE ALTERNATIVE
LAYOUT
HORIZONTAL RUN L** TO BE
MINIMUM DIMENSION
NOT TO EXCEED 8 FEET

WESTINGHOUSE RECOMMENDED PIPE LAYOUTS [7]

FIGURE 7

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Feedwater Flow Rate Control

Based on the Westinghouse bulletin by Bennett [7], this recommendation is in essence a "threshold-flow" requirement that the auxiliary feedwater flow rate to any steam generator remain below 150 gpm whenever the water level in that steam generator drops below the feedwater sparger. No requirements on other parameters such as steam generator pressure, the rate of change of flow rate, or feedwater temperature have been stated.

Many phenomena can be speculated and qualitative arguments can be invoked to suggest that if the feedwater flow rate is low enough, a water slug may not form. Assume that the feedring has drained and accept for the moment that the flow rate is so low that the pipe is only partially full of water. Imagine that the incoming water is raised to saturation temperature at equilibrium by condensation of steam. Then the steam flow rate is proportional to the water flow rate. Accordingly, the growth of wave instabilities due to countercurrent flow of steam over the water layer can be suppressed by supplying sufficiently low water flow rates, due largely to the concomitant reduction in steam flow within the feedring. Further, the water flow rate has hydraulic effects: the water level in the sparger is higher, which provides a greater heat sink for condensation. More discharge holes in the sparger tend to be covered at higher water flow rate. Finally, the rate of water level rise in the steam generator vessel and the distribution of water temperature in the sparger and the vessel depend on the water flow rate.

It is our understanding that such qualitative arguments led to the Phase II tests at Indian Point #2 which were conducted over a range of auxiliary feedwater flow rates from 75 to 240 gpm. (Available PWR test evidence is summarized in Section 2.4 of this report.) In brief, there were two waterhammer events recorded out of four tests at 240 gpm and no waterhammer events recorded in nine tests at 200 gpm and below. According to Bennett [7], the 150 gpm limit is based on these data (with a 50 gpm safety factor).

At the meetings with the vendors, we were told that there were no analyses conducted prior to the present study in order to determine a threshold flow or identify the relevant physical mechanisms of the many that might be involved. Relevant dimensionless parameters (i.e., scaling laws) had not been identified and other potentially relevant parameters such as water subcooling, feedwater flow rate transients, system pressure, or pipe size had neither been identified comprehensively nor quantified in any published report prior to the present work.

One difficulty with applying a feedwater flow rate limit is that the present threshold flow, even if supplied to all loops, is less than the flow required to remove decay heat at several plants.

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Top Discharge Devices

This generic term indicates means by which the feedwater is constrained to flow from the top of the feedring. J-tubes in conjunction with plugging the bottom discharge holes are one such device. A typical arrangement is sketched in Figure 8.

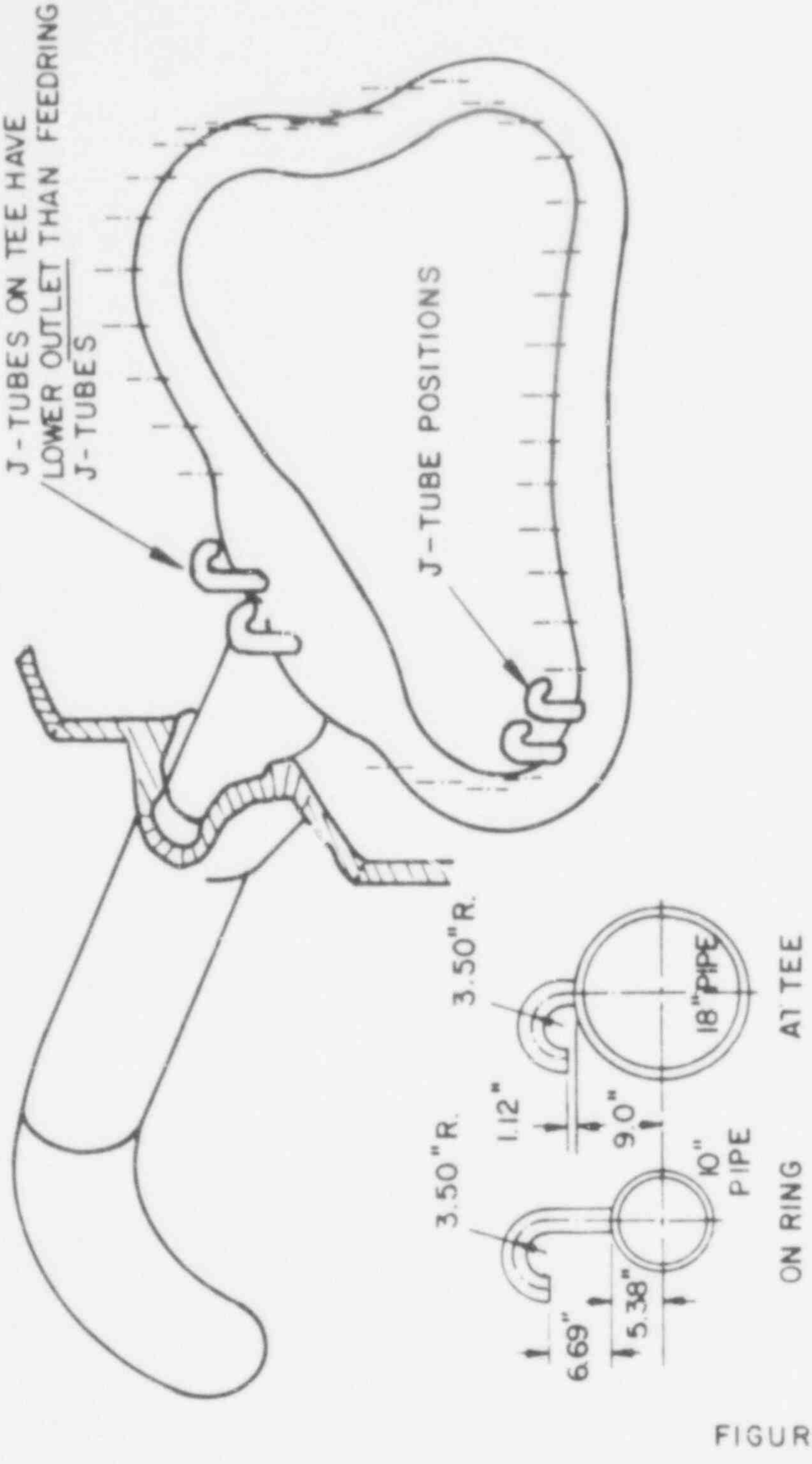
The main rationale for top discharge is that in the event that the water level drops below the feedring, the J-tube design might ideally prevent the feedring and adjacent horizontal feedpipe from draining and thence prohibit steam from entering the ring. Figure 8 shows that the J-tubes (or a comparable top-discharge pipe) are necessary to accomplish this. If top discharge holes are employed alone (i.e., with bottom holes plugged but without J-tubes), then the upper part of the feedpipe can drain rapidly. In this report "top discharge" means, 1) plug the bottom holes, 2) drill top holes, and 3) install top discharge pipes (e.g., J-tubes).

Some drainage occurs in practice because all Westinghouse (and Combustion Engineering) feedrings have a built-in leak at the thermal sleeve where the feedring assembly joins the feedpipe. (This sleeve is intended to accomodate thermal and pressure expansion.) Thus, the J-tube modification can only reduce the drainage rate, not prevent drainage. A quantitative estimate of leakage rate is needed in order to determine the steam void that might develop during various hypothetical operating transients.

Unfortunately, critical data are lacking to quantify the possible range of leakage rates or even the sleeve clearance geometry. Although "shop floor" dimensions and tolerances are available for the cold metal assembly, the actual clearance gap under hot, pressure conditions can only be estimated.* Futhermore, the clearance gap may either erode or plug up due to chemical action and deposits. Up to December 1, 1976 direct measurements of feedring leakage rate had been made (and reported to Creare) only during cold shut-down conditions at one plant (Indian Point #2). These measurements and our analysis of feedring hydraulic behavior and leakage rates are given in Section 5 of this report. In brief, without feedwater flow a normal bottom discharge feedring is likely to drain more than halfway in only ten seconds, whereas a top discharge system should remain largely undrained for a minute or more and may require ten minutes to many hours to drain almost fully, depending on a clearance gap that cannot be specified with any confidence.

The reduction in drainage rate possible with a J-tube system is potentially significant because it may greatly decrease the size of the steam void developed in the feedwater system during the anticipated time required for the automatic control or operator to respond and reestablish feedwater flow.

*Calculations have been reported to Creare informally by personal communications with C. Fredricksen, Nuclear Plant Safety, Westinghouse Electric Corporation, January 12, 1977. This information is supplied in Section 5.1 where a drainage analysis is developed.



J-TUBE FEEDRING CONFIGURATION AT INDIAN POINT # 2

FIGURE 8

POOR ORIGINAL

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Because quantitative information is lacking, it has been the policy of the Nuclear Regulatory Commission to require a two hour drainage period in tests of PWRs with top discharge systems. It is expected that two hours is a very long time period relative to operator response and is probably long enough to drain the feedring fully.

The J-tubes may serve other functions in addition to decreasing the feedring drainage rate. For example, qualitative arguments can be invoked to suggest that the J-tubes will act as vents. (Indeed, literal vent holes in the top of the feedring have been suggested previously as a fix.) Vents may permit steam to enter the feedpipe between the water slug and the upstream water column as suggested by Figure 5. This effect, if it occurs, is expected to suppress slug formation and to decrease the rate of steam void collapse dramatically; vents can thereby prevent slug impact or reduce the slug velocity and subsequent impact pressure. The vendors [2,3] were unable to describe or identify any analysis or scale model study of this possible vent effect, however. PWR tests have been run in systems with top discharge devices and in bottom-discharge systems with vent holes, as described in Section 2.5 of this report.

Since in a top discharge system the feedring te to refill before appreciable water is supplied to the ve sel, any effect of rising water level blocking off holes to trap a steam void is expected to be eliminated by a top discharge system.

Feedwater Piping Design

The criterion that the "feedwater/steam lines design should consider the effects of waterhammer" is the last recommendation made by Westinghouse. The Westinghouse bulletin by Bennett [7] clarifies the intent of this recommendation.

> "Although the control of feedwater flow below the waterhammer threshold when the feedring is uncovered should minimize waterhammer, methods by which feedline waterhammer can be completely prevented have not been verified. Thus, a low number of waterhammer events should be anticipated, and an evaluation of the effect on the structural capabilities of the system may be required. An important input to these analyses is the waterhammer forcing function. The forcing function is a time dependent mathematic quantity representative of the energy released by steam water slugging (waterhammer) in the feedwater piping connected to PWR steam generators. In order to provide an estimate of the energy released, an elemental forcing function is being developed. The forcing function provides a typical time history of the pressure which results from the acoustic shockwave generated by a steam water slug.

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"Examination of test measurements made at the Tihange reactor site, Belgium, indicates that a forcing function may be developed which can be used to predict the displacement of the feedwater piping system when subjected to a waterhammer event. Subject to confirmation of this forcing function by application of calculation method to the desired results at Tihange and results from a test at another plant, it is anticipated that Westinghouse can propose to provide similar forcing functions by no earlier than late July 1975, if requested.

"Steps may be taken which will reduce the magnitude of a waterhammer slug, or to reduce the amplitude of the resulting pressure wave. This is accomplished by reducing to the minimum achievable length, the straight pipe connection to the feedwater pipe nozzle on the steam generator. Where it is possible, appropriate pipe modifications may be necessary to obtain a more desirable piping arrangement.

"To date, our observations indicate that the probability of damage from waterhammer can be kept to a minimum by elimination of long pipe runs at the inlet nozzle which can drain into the steam generator when water level in the generator drops below the feedwater inlet. This will not prevent the formation of water slugs but it has been demonstrated that this will ensure that the energy of the slug will be well below those values which could cause pipe damage. An analysis of the piping system including supports and restraints may be necessary by the Utility/A-E to show that major damage to the system will not result."

It is our understanding from the above that the utility or architect-engineer is expected to analyze the piping system, including supports and restraints, in order to ensure that allowable stresses will not be exceeded, presumably in the hypothetical event of the worst credible slug impact in the feedwater systems induced by two-phase flow behavior within the steam generator. What is not clear in the Westinghouse position is what the utility is expected to assume for the slug impact pressure characteristic or whether the results of such an analysis will be tolerable in the context of common piping system design practice.

The quote above from the 1975 bulletin by Bennett [7] mentions a "forcing function" under development. However, at our September 1, 1976 meeting with Westinghouse personnel [2], no one was able to recommend a forcing function or analysis for use in the design of PWR piping to preclude unacceptable steam generator waterhammer. Westinghouse had previously reported an analysis by Roidt [5] of steam void collapse, slug motion and impact intended to provide such a forcing function from first principles. The wording in the quote above from the later technical bulletin appears to recognize that the forcing

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function developed by Roidt in Reference [5] is inadequate for use in designing PWR piping systems. However, that fact is not made clear in the analysis report itself ard has led to wide spread misinterpretation by utilities and architect engineers. (The Roidt report is critiqued in detail in Appendix B of this report.) No analysis reports subsequent to the January 1975 Roidt report [5] have been issued by Westinghouse up to January 31, 1977. The point here is that no forcing function developed from first principles is presently confirmed and mature enough for use in the design of PWR piping systems.

An alternative approach is to provide an empirical forcing function, such as the one recommended by Westinghouse to Bechtel in August 1975 [8]. Unfortunately, the available data are so limited that there is no hope they are representative of anything near a credible, severe impact. To our knowledge, high response measurements of the pressure transient associated with impact have not been made during a damaging incident in any PWR prior to January 31, 1977. High response pressure, acceleration, and strain measurements have been recorded at Indian Point #2, Trojan, Tihange, and Doel. With the exception of one measurement at Tihange, all the data exhibit mild pressure fluctuations of only a few hundred psi or less (most of the data indicate nothing at all). It is concluded that these were very mild waterhammer events useless for the purpose of deriving a strictly empirical forcing function representative of severe impacts. (These data may be useful to help verify analytical models of the slug behavior.) During the only severe pressure excursion ever recorded--the non-damaging incident at Tihange--the two pressure transducers failed, apparently due to overpressure. The detailed test data is summarized in Appendix A. The point here is that the evidence is insufficient to permit construction of any realistic empirical forcing function.

Therefore, the information available up to January 3 77 does not supply a credible forcing function useful for the design of PWR feedwater piping systems. Accordingly, the Westinghouse recommendation--that the utility (or A/E) can analyze the piping to ensure that stresses in the piping system will remain within allowable limits--lacks a critical component and cannot be effected at presented.*

*Some of the general characteristics of the forcing function can be described qualitatively based on available analyses and data. For example, the pressure pulse is likely to have large amplitude (thousands of psi) and sh duration (milliseconds). This information is of some use to ect-engineers in order to identify the types of restraints n ued (e.g., axial rather than perpendicular to the pipe) and to identify weak points in the system. Competent absolute prediction of the worst credible event is not possible at present.

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Combustion Engineering Position

The Combustion Engineering Position on feedring steam generators was presented to the NRC and to Creare in a meeting at Windsor, Connecticut on September 22, 1976. Several follow-up discussions have been held. The Combustion Engineering position on measures to prevent waterhammer is

1) "Top discharge holes on the feedring reduce frequency of preconditions for initiating waterhammer.

2) A downward sloping 90° elbow from the steam generator feed nozzle reduces consequences of waterhammer.

3) In-plant testing indicates that a 90° elbow induces mild and acceptable waterhammer. For current plants, this configuration is required.

4) For existing plants, with long horizontal runs, 2-fold action is recommended:

   a) in-plant testing to validate procedure for restoration of water level following feedring exposure,

   b) implementation of procedures in plant technical specifications."

This position statement is similar to the Westinghouse position, but distinctly different in key respects. A detailed discussion of each point is supplied below.

Top Discharge Devices

An early design recommended by Combustion Engineering is the "standpipe" configuration consisting of straight pipes screwed into the bottom discharge holes and standing in the feedring. The tops of the pipes are 0.25 inches from the top of the feedring. This design is not literally a top discharge device since the holes are still at the bottom. (Convenience in installation led to this design.) However, the clear intent of the standpipes is to reduce the rate of feedring drainage in a manner similar to the J-tubes recommended by Westinghouse. The standpipes may also function as vents unless the bottom of the feedring is covered by water. It should also be noted that all Combustion Engineering feedrings have one to three 1.0 inch top vent holes, one of which is at the tee with the feedpipe.

The standpipes are less effective than the J-tubes because they permit part of the feedpipe to drain rapidly. (So do the vent holes.) Further, in the event that the top half of the feedring has been uncovered for a long time so that the ring will have drained half-way (through the thermal sleeve), the J-tubes might work as vents whereas the standpipes would not. This second difference is minor, however.

At this writing, the standpipe design has been rejected by Combustion Engineering and the three plants with standpipes installed (Calvert Cliffs #2, St. Lucie #1 and Millstone #2) have removed or will remove them. Operating experience at St. Lucie revealed that three standpipes failed in service. The Combustion Engineering evaluation of these failures implicated fatique due to water flow induced vibrations encountered in normal operation (rather than slug impact failures.) Creare has no reason to doubt this evaluation, but has not independently evaluated the cause of standpipe failure during the present study because this design will not be used in the future.

Alternative top discharge designs are presently being evaluated by Combustion Engineering. It is our understanding that prior to December 1, 1976, a system similar to the J-tubes had been installed at some, but not all, of the plants that had standpipes previously.

Since top discharge devices merely "reduce the frequency of preconditions for waterhammer", these devices are clearly not taken to be an absolute fix in the Combustion Engineering position, in contrast to the stated Westinghouse position. However, the view expressed at the meeting [3] was that top discharge devices should be expected to reduce the frequency of waterhammer incidents dramatically, perhaps to the point where this type of incident will no longer be a problem.

90° Elbow at the SG Nozzle

The installation of a downward sloping 90° elbow from the steam generator nozzle is equivalent to the Westinghouse recommendation that the horizontal pipe run from the nozzle should be minimized. No other pipe configurations are specified in the CE position although other configurations may have a comparable or superior effect.

Again, the qualitative effect of "reducing consequences" is stressed and there is no quantitative evidence that the potential impact pressures are low enough that pipe system stresses will remain within allowable levels.

Operating Procedures

It is our understanding that the present interpretation of "operating procedures" is entirely general. If a utility is not willing to install top discharge devices and 90° elbows, their alternative is to establish their own means to preclude unacceptable waterhammer.

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The Combustion Engineering position is that any procedure proposed by the utility should be tested and that the procedure should be implemented in the plant technical specifications. One difficulty in this approach is that it is impossible to test any procedure or design under all potential operating conditions. Excessive testing of PWRs is also undesirable on economic grounds and because such testing raises the probability of incidents. Specific details of the Combustion Engineering view of necessary test compromises are unclear. To our knowledge, neither Combustion Engineering nor Westinghouse have published any document recommending general test guidelines, procedures or criteria although this subject was discussed at meetings held with Creare and it is our understanding that Combustion Engineering personnel have advised utilities when requested to do so.

Creare presents general test guidelines in Appendix C of this report.

Combustion Engineering Analyses

At the meetings with Creare [3], Combustion Engineering personnel presented analyses comparable in scope and assumptions to those published by Roidt of Westinghouse [5]. The individuals responsible for these analyses stated their opinion that their analyses were at too early a stage of development to warrant publication. CE personnel pointed out strongly that they felt that these analyses were insufficiently mature for application to PWR systems.

Utility Action on Vendor Recommendations

The commerical plants supplied by Westinghouse and Combustion Engineering have generally not made piping modifications since the incident at Indian Point #2. The status of currently operating plants with respect to the major hardware and procedural recommendations of the vendors is presented in Table 4 based primarily on responses to the NRC questionnaire of May 13, 1975.

In brief, the description indicated in the utility responses of the overall status of each of the 28 presently operating plants (supplied by Westinghouse or Combustion Engineering) with respect to means to preclude unacceptable waterhammer is:

- six plants had J-tubes installed or planned, (two of these also have a short horizontal pipe run),

- four plants had installed or planned to install standpipes,

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TABLE 4

SURVEY OF PLANT GEOMETRY AND PROCEDURES

(Main Source: Response to May 13, 1975 NRC Questionnaire)

PLANT	TOP DISCHARGE	FLOW LIMIT* (gpm)	EXTERNAL HORIZONTAL PIPE RUN** (feet)	NOTES	UTILITY STATED OVERALL STATUS
WESTINGHOUSE SUPPLIED STEAM GENERATORS			A B C D / 1 2 3 4		
Beaver Valley	J-tubes	NO	max 3 (?) (loop seal)	No response to questionnaire test procedure.	Will test J-tubes
D. C. Cook #1	NO	NO	max 4.4 (?)	Scaled from sketch, may be atypical.	Acceptable based on analysis.
Robert Ginna #1	NO	150 gpm	max 3 (?)		Evaluating alternatives.
Haddam Neck	NO	?	?		Acceptable based on analysis.
Indian Point #2	J-tubes (4/74)	NO	7 4 12 10	SG #22 piping had 17 ft horizontal run prior to January 14, 1974.	J-tubes verified by test.
Kewaunee	NO	200	max 3.5 (?)	May be atypical.	Waterhammer cannot happen based on analysis.
Point Beach #1	NO	200	max 12 (?)	May be atypical.	Meets Westinghouse pipe guidelines. (?)
Point Beach #2	NO	200	max 12 (?)	May be atypical.	Meets Westinghouse pipe guidelines. (?)
Prairie Is. #1	NO	?	max 8 (?)	May be atypical.	Not indicated.
Prairie Is. #2	NO	?	max 7.6 (?)	May be atypical.	Not indicated
Robinson #2	NO	?	max 5 (?)	May be atypical.	Meets Westinghouse pipe pipe guidelines.
Salem #1	J-tubes (planned)	NO	11 10 13 12		J-tubes verified elsewhere.
San Onofre #1	NO	?	max 5 (?)	Lengths include loop seal.	Meets Westinghouse pipe guidelines.
Surry #1	J-tubes Planned (10/76)	YES	10 8 5 -	Flow limit unspecified: lengths are from SG wall to end of loop seal.	J-tubes verified elsewhere.
Surry #2	J-tubes Planned (10/76)	YES	max 8 (?)	Flow limit specified.	J-tubes verified elsewhere.
Trojan	J-tubes (9/75)	NO	<3	May be atypical.	J-tubes verified by test.
Turkey Point #3	NO	NO	5 5 9 -	pipes shortened after incidents.	meets Westinghouse pipe guidelines.
Turkey Point #4	NO	NO	5 5 9 -		
Yankee Rowe	NO	?	max 3	May be atypical: may include loop seal. Pipe shortened after incident.	Nine years of successful operation.
Zion #1	NO	?	4 4 8 6		meets Westinghouse pipe guidelines
Zion #2	NO	?	4 4 6 4		
COMBUSTION ENGINEERING SUPPLIED STEAM GENERATORS					
Calvert Cliffs #1	Standpipes (planned)	Yes	10 10 - - (MAIN) ≈1(?) - - (AUX)		Limit on level rise, 1.2 in/min.
Calvert Cliffs #2	Standpipes (installed)		10 10 - - (MAIN) ≈1 - - (AUX)		Will test standpipes.
Fort Calhoun #1	NO	NO	≈1 - - (MAIN) ≈5(?) - - (AUX)		Short pipes
Maine Yankee	NO	?	≈3(?)	3 loops, no aux. nozzle.	Analysis results being evaluated.
Millstone #2	Standpipes (installed)	NO	≈7(?)	No response to questionnaire, no. aux. nozzle	Evaluate standpipe alternatives.
Palisades	NO	NO	28 28 - -	(MAIN), feedwater pipe.	Acceptable based on plant history.
St. Lucie #1	Standpipes (installed)	NO	≈0(?)	No response to questionnaire, elbow on feedring.	Evaluate standpipe alternatives.

*A "?" in the flow limit column indicates that there was no unequivocal statement of any flow limit. Probably there is no flow limit.

**Where drawings supplied by the utility have been inadequate, the maximum value from surveys by Westinghouse and Combustion Engineering are reported.

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* nine plants had indicated short horizontal runs of piping within the Westinghouse eight foot guideline,

* two plants were indicated to be acceptable based on plant history,

* three plants were indicated to be acceptable based on analyses,

* the overall status of four plants was unstated or was under evaluation (these plants have short to moderate horizontal pipe runs and at least three plants have flow limits in effect).

Because the information in Table 4 was approximately a year and half old, and because several statements were vague or absent, an informal survey was conducted by the NRC in November 1976 and was supplemented by informal discussions with vendor and plant personnel. This survey is reported in Appendix A and the most current assessment of plant overall status is listed below (for 29 plants):

* 4 plants have J-tubes installed. Of these, 2 plants also have short horizontal pipe runs within the Westinghouse guideline, and 3 plants are in addition applying a flow limit of some kind.

* 4 plants still have J-tubes planned. In the interim, these plants are relying on feedwater flow limits with bottom discharge.

* 2 plants that previously had standpipes are also expected to employ J-tubes. In the interim one plant is using a flow limit and the other still has standpipes.

* 9 plants with bottom discharge employ a limit on the maximum feedwater flow when the feedring is uncovered. 7 of these plants also have a short horizontal pipe run within the Westinghouse guideline.

* 2 plants with bottom discharge (Calvert Cliffs #1 and #2) employ a limit on auxiliary feedwater from a separate auxiliary sparger such that the water level in the vessel rises less than 1.2 inches per minute.

* 6 plants with bottom discharge have only a short pipe run within the Westinghouse guideline and do not employ a feedwater flow limit. One of these (Yankee Rowe) has over 10 years of favorable plant history.

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* 2 plants with bottom discharge have long or unknown horizontal pipe runs and do not employ a flow limit. One of these (Palisades) has the longest installed pipe run of any PWR (28 feet), but has not reported any waterhammer incidents in over four years of commerical operation.

Clearly, the situation is still in a state of flux and periodic surveys will be required to maintain an accurate and up-to-date information base.

The plants now being brought into commerical operation will generally incorporate the current vendor recommendations of top discharge from the feedring and short horizontal pipe runs. Some of these plants and most proposed plants not yet under construction are expected to employ steam generators of the "preheat" or "economizer" type now under development and test. New effects involving similar phenomena should be anticipated in these new configurations

2.4 Summary of PWR Test Experience

Over 30 full-scale tests have been conducted, largely to verify the hardware and operating procedures at six of the 28 U.S. commerical operating PWR plants supplied by Westinghouse or Combustion Engineering. At least 20 more tests at three foreign plants have been reported. The evidence from these tests is summarized in Table 5 and is reviewed in the context of PWR operating experience in Section 2.6 of this report. A detailed review of each of these tests is given in Appendix A of this report.

In the course of verification testing at least four of the plants were instrumented entensively. Unfortunately, such instrumentation has had little pay-off to date. The only compelling quantitative evidence is the Tihange depressurization history (prior to the failure of the two pressure transducers) during one non-damaging waterhammer event at that plant.

The one test of steam generator waterhammer conducted on a PWR plant supplied by B&W is not included on Table 5. It is discussed in the context of B&W system design in the following Section 2.5.

TABLE 5

TESTS AT PWRs SUPPLIED BY WESTINGHOUSE OR CE

PLANT	INSTALLED HARDWARE Feedring	INSTALLED HARDWARE Horizontal Pipe Run(s) (feet)	Pretest Feedring Draining (Minutes)	Feedwater Flow Rate (GPM)	Number of Tests	Reported Test Results
Indian Point #2						
• Phase I	Bottom Discharge	7, 4, 12, 10	≈ 1	?	3	Non-damaging waterhammer at 35% power
• Phase II	Bottom Discharge	7, 4, 12, 10	?	75, 100 200, 240	13	Two non-damaging waterhammer events at 240 gpm
• Phase III	J-tubes	7, 4, 12, 10	10 (Typ) 30 (Max)	≈ 140(?)	7	No Waterhammer
Trojan						
• Subcritical	J-tubes	≈ 3	1 to 120	220, 440	8	No waterhammer
• decay heat	J-tubes	≈ 3	30	275	1	No waterhammer
Calvert Cliffs #1	Bottom Discharge	10, 10	30 to 100	≈ 175	2	Noise (Separate Auxiliary Feed)
Calvert Cliffs #2	Standpipes	10,10	≈ 60	≈ 600 (?)	1	Non-damaging waterhammer (SG Pressure ≈ 145 psig)
St. Lucie #1	Standpipes	≈ 0	≈ 136	≈ 300	1	No waterhammer
Millstone #2*	Standpipes	7	≈ 13	≈ 300	?	No waterhammer
Tihange #1	Bottom Discharge	8	?	176*	?	Non-damaging waterhammer
Doel						
• Vented	Bottom Discharge	≈ 50	?	15 to 530	15	No waterhammer
• Unvented	Bottom Discharge	≈ 50	?	65 to 260	5	2 non-damaging waterhammer events at 260 gpm
Ringhals*						
• Vents	Bottom discharge	8(?)	?	≈ 200	1 (?)	Waterhammer(?)
• J-tubes	J-tubes	8(?)	?	?	?	No waterhammer(?)

*Based on informal discussions. Documentation of these tests is unavailable to Creare or is incomplete in significant respects.

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2.5 B&W Supplied Systems

It is helpful to examine the design features of steam generators supplied by B&W because these systems have been relatively free of steam generator waterhammer.

The position stated by B&W at the meeting held with the NRC and Creare [4] is:

Destructive waterhammer does not appear to be a credible phenomena in B&W plants since:

1) "The B&W steam generator/feedwater systems are designed to eliminate causes of waterhammer,

   a) Geometry of feedwater inlet piping prevents steam from entering the pipe.

   b) Mechanisms for collapsing the small amounts of steam are minimized:

      - Auxiliary feedwater injection directly on steam generator tubes rather than into feedwater lines.

      - Modulated main feedwater flow control using cascaded 6" and 16" flow control valves.

2) Waterhammer has not been detected in operating B&W plants with the exception of the Oconee problem which was corrected."

The B&W "once through steam generator" (OTSG) is described in Appendix A. In brief, separate external rings are supplied for each of the auxiliary and main feedwater piping. A set of connecting pipes emanates from the top of each ring header and attaches to a set of steam generator nozzles (32 for main feed, seven for auxiliary feed). The feedwater piping connects to each ring header at its lower surface and effectively forms a trap to prevent direct drainage into the steam generator vessel.

Considerable emphasis was placed in the meeting on the fact that precisely the phenomena involved in steam generator waterhammer were considered (at least qualitatively) during the initial design stages of the B&W OTSG.

Main Feedwater System Drainage

The B&W generic position is based primarily on the assertation that the main feedwater piping will always be filled with water right up to the nozzles and hence no steam (or negligible quantities of steam) can be trapped in the pipes. The nozzles are uncovered during normal operation and the main feed system was clearly designed to prevent appreciable draining into the steam generator due to uncovered nozzles.

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During normal operation there is also a steady flow through a one inch bypass pipe which is sufficient to maintain the system nearly full. It was indicated at the meeting [4] that an estimated flow of 30 to 50 gpm is supplied through this pipe. That would be expected to keep the ring header and vertical portions of the connecting pipe full, but is not sufficient to run the (32) horizontal piping sections full. Thus, there is likely to be a small steam bubble lodged in the end of the pipes if main feed flow is secured.*

The B&W design is functionally equivalent to Westinghouse J-tubes, but is much more positive in its execution. Specifically, there are no built-in leaks and all joints are exposed for easy verification. Moreover, the main feedpipe joins the ring header from below in such a way as to form a trap that acts to prevent drainage into the vessel.

During occasional abnormal conditions involving a loss of main feedwater flow, at least one check valve is available to prevent backflow and reverse draining of the system.

A prominent question concerns anticipated system performance in the event of an unanticipated and relatively unlikely occurence such as the check valve leak to the ambient environment that occurred at Surry #1 (a Westinghouse supplied plant) on October 1, 1972. Alternatively, an improperly closed check valve subsequent to a main feed pump trip in conjunction with a pressure differential between two steam generators could establish a backflow. (Note, however, that any such differential pressure will usually be small since the steam generators are coupled on the steam side and the steaming rate should be low in these circumstances.) These and other relatively unlikely means can be postulated to cause back draining of B&W systems.

Feed System Design Features if Drained

For the purpose of discussion, it is postulated that the main feedwater system has drained or partially drained at the time that main feedwater flow is reestablished. Under these unlikely circumstances the potential exists for a water slug

*No quantitative analysis was presented [4] to demonstrate that forces generated by collapse of the small steam bubble that could be trapped in the end of the connecting pipes could be neglected althogether. We believe that this is likely to be the case. However, the potential for mild waterhammer stemming from bubble collapse should be appreciated in the planning and execution of any tests.

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to form and trap a steam void anywhere in the piping system and whether or not this occurs will depend on the detailed timing and transient rate of main feedwater flow and on the pipe system layout. To prevent this positively (if drainage is detected) the drained region should be isolated and the plant returned to "cold" conditions.

If such drainage is limited to the region near the steam generator, however, the B&W design has several features that may suppress slug formation. The ring header is a "clean" design that is vented regularly along its entire periphery by the connecting pipes. The feedpipe connects to the ring header in a vertical run which also acts to suppress slug formation. There is no horizontal pipe run (connected directly to the ring header) for a void to be trapped in if a slug forms in the ring or pipe tee.

When auxiliary feed is employed to refill the steam generators, the main feedwater nozzles are sometimes recovered by the rising water level in the vessel [4]. In the unlikely circumstance that the main feedwater system has previously drained, this action can trap a steam void in the main feedwater piping. The water in the vessel and that remaining in the main feedwater piping will tend to be hot but somewhat subcooled. Whether or not a slug will form or the void will collapse potentially depends on several interacting phenomena and parameters and cannot be established definitively by qualitative arguments.

Thus, even in the highly unlikely event that the main feedwater system has backdrained, the B&W design exhibits features that are superior to Westinghouse and CE systems as means to suppress slug formation and reduce slug impact intensity.

Auxiliary Feedwater System

A steam void will usually exist in the auxiliary piping due to evaporation [4]. Thus, when auxiliary feedwater is established there is the potential for slug impact and waterhammer in the auxiliary feedwater piping system. The question here is how extensive such evaporation can be and what region might be affected. We suspect that this effect may be slight if there is any reasonable heat transfer from the auxiliary feedring header to the ambient environment. However, information needed to establish the actual situation is unavailable to Creare.

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Operating Experience of PWRs Supplied by B&W

Several plausible, but unlikely, circumstances have been postulated which might cause slug formation leading to waterhammer in B&W steam generator feedwater systems. In fact, such an event has been reported only once at a plant (Oconee #1) supplied by B&W. In this instance, the steam generator pressure was near atmospheric pressure (reactor coolant temperature was 275°F, during reactor start up) and only a mild impact would be expected. No damage was reported.

The essence of the situation is that during reactor start-up the main feedwater control valves intermittently open and close during start-up so that is possible for a steam void to form. The specific means by which a steam void might have been formed and trapped are not speculated on in the brief statement by the utility [9].

To prevent such an incident from recurring a small (one inch) bypass line is employed at all B&W plants to maintain at least a small flow (≈ 30-50 gpm) during start up.

Tests at Oconee #1

Although Creare is aware of tests conducted at Oconee #1 to identify the phenomena in the prior incident and to verify the procedures developed at Oconee #1, the test report is unavailable to Creare at this writing.

Summary of B&W Experience

Although it is possible to postulate events that could lead to slug formation and impact in the feedwater systems of B&W steam generators, these circumstances are highly unlikely relative to those circumstances involved in the several incidents reported to have occurred in Westinghouse or CE steam generators.

This is a situation where A/E adherence to vendor recommendations and utility attention to proper procedures is more likely to be important than fundamental improvement in the steam generator design.

Uncertainties exist as described above and quantitative information is lacking. Some clarification on these issues should be sought, but evaluation of systems developed by the other vendors should be given clear priority.

2.6 Conclusions from PWR Experience

Based solely on operating history it is possible to draw several very general conclusions concerning the behavior termed "steam generator waterhammer" in this report. The two main conclusions are, 1) that the problem is generic, potentially affecting all PWRs, and 2) that the problem is not yet "solved" in the sense that there have been several incidents in the past few years involving extensive damage to piping and pipe system components. With respect to the latter conclusions, it is encouraging, but by no means definitive evidence, that in the last eighteen months such incidents have been reported only at two plants (Zion #1, #2) which may be atypical in some unknown way.* The infrequence of the triggering occurrences and the whimsical nature of the phenomena make it impossible to draw more definitive general conclusions.

Several specific conclusions on details of the events have also been made:

1) The hypothetical, but generally accepted, sequence of events in a steam generator waterhammer incident--as described for example in the Introduction of this report--is consistent with the evidence, but the evidence from PWR operating experience is too scanty to afford adequate support or confirmation of detailed hypotheses.

2) There are several possible precursor events such as reactor trips or feedwater pump trips which are unlikely to be eliminated.

3) No incident has been reported to have occurred without uncovering the feedring.

4) No incident has been reported at the high flow rates necessary to run the feedwater pipe full.

5) Some incidents have occurred with reported flow rates less than 100 gpm.

6) Although records are lacking, we believe that highly subcooled feedwater was supplied during the major incidents.

*A report on the November 5, 1976 incident at Beaver Valley is unavailable at this writing, but is claimed to implicate other causes than those involved in the present inquiry [10].

7) The hypothesis that slug impact and waterhammer must be triggered by recovery of the feedring is not supported by the evidence. Accordingly, special procedures designed to work only as the sparger is recovered may be ineffective.

8) Quantitative data of any kind useful for confirmation of analyses are very limited.

Review of Vendor Recommendations

The available operating and test experience in support o each of the hardware or procedure recommendations made by the PWR vendors is summarized below. This review is intended to be simply an objective summary of the facts. Subjective evaluation of these recommendations is deferred to Section 6 where the understanding of phenomena derived from our work can be applied.

Top Discharge Devices. No incidents of steam generator waterhammer have been reported during commerical operation of any plant with J-tubes or standpipes installed. It must be recognized, however, that such operating experience is very limited, essentially three reactor years of operation with J-tubes at Indian Point #2 plus roughly a year at Trojan, and scattered brief experience at eight other plants. (A report on the experience at Ringhals is unavailable to Creare.) It has been reported to Creare [11] that in the past three years, the feedrings at Indian Point #2 have never been uncovered more than five minutes, and generally for much shorter periods, during the occasional events that have uncovered the feedrings at that plant.*

This experience speaks favorably of operating experience and procedures at Indian Point #2, but it is therefore not a severe test of J-tubes.

Favorable results were reported during seven tests at Indian Point #2, nine tests at Trojan, and also at Ringhals #2. The tests at Trojan explored the potential effects of operating pressure (400-1100 psig), flow rate (220 and 440 gpm) and drainage time (1 to 120 minutes). However, Trojan has a very short horizontal pipe run. One test in a system with a relatively long horizontal pipe run (10 feet) was conducted at Calvert Cliffs #2 which had standpipes installed. A strong, but non-

*At Indian Point #2 if a drainage period in excess of five minutes is incurred, the operating procedures call for automatic establishment and subsequent manual limitation of the auxiliary feedwater flow to 150 gpm, at least until feedring refill is assured.

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damaging waterhammer was experienced. (In this instance, the standpipes were expected to be functionally equivalent to J-tubes.) Interpretation of this result is complicated by the fact that the system was at lower pressure than tested elsewhere (145 psig) and probably at higher flow rate than tested elsewhere (600 to 1200 gpm). Favorable tests of standpipes were conducted at Millstone #2 and St. Lucie #1.

The ability of J-tubes by themselves to prevent slug formation if significant drainage has occurred is questionable and has not been confirmed over a representative range of feedpipe horizontal runs, operating pressure, feedwater flow, and drainage times. The limited evidence suggests that J-tubes tend to suppress slug formation in some circumstances, however.

The J-tubes have unquestionable merit as a means to reduce drainage rate and as a means to ensure that the feedring refills before the water level in the vessel rises to cover the discharge holes. In conjunction with appropriate automatic controls or technical specifications to limit the drainage time, it may be possible to limit the drained volume stringently in most instances and thereby suppress slug formation and reduce the severity of any slug impacts. Unfortunately, little quantitative evidence exists to establish a basis for the prediction of drainage rate.

Short Horizontal Pipe Runs. A plausible qualitative rationale has been developed to suggest that decreasing the length of pipe adjacent to the steam generator (i.e., the pipe that is susceptible to draining into the vessel, see Figure 7) tends to reduce the severity of slug impact and, particularly in conjunction with J-tubes or vents, also tends to suppress slug formation. Unfortunately, quantitative evidence from PWR experience is lacking and only speculative conclusions can be drawn. The best available guideline is to make the horizontal run "as short as possible".

The four incidents involving the most pipe system damage to date have occurred in systems with moderate to long horizontal runs of feedpipe adjacent to the steam generator, namely Indian Point #2 (SG22: 17 feet, shortened to four feet after incident), Calvert Cliffs #1 (SG12: 10 feet), Surry #1 (SGA: 10 feet) and Turkey Point #4 (SGA: 21 feet, shortened to five feet after incident). Although these pipe runs all exceed the Westinghouse guideline, they are not appreciably longer than typicaly pipe runs on present operating reactors. The system with the longest pipe run (Palisades: 28 feet) has not yet experienced a reportable waterhammer.

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Supposedly short pipe runs, perhaps three feet outside the steam generator, are not "short", if the piping inside the steam generator is accounted. Typically, this piping includes two feet of feedwater pipe, a tee of some sort, and a feedring that has a volume approximately equivalent to 15 feet of 16 inch feedwater pipe in Westinghouse systems and 30 feet of feedwater pipe in CE systems. Whether any or all of the feedring volume should be included is controversial and depends on the nature of the multiphase flow in the feedring, particularly the effectiveness of the discharge holes as vents. However, it is a fact that a major fraction of the potentially drained system volume resides in the feedring and is not amenable to size reduction. We shall return to this point in Section 4 which describes model tests at Creare.

The evidence from PWR experience is too limited and scattered to support the claim that a short horizontal pipe run will by itself mitigate the severity of slug impact appreciably or reduce potential waterhammer pressures to tolerable levels.

Feedwater Flow Limit. A plausible qualitative rationale for a maximum limit on feedwater flow when the water level is below the feedring derives support and quantification from 13 tests at Indian Point #2 and five tests (unvented) at Doel #2. These tests gave the consistent result that waterhammer did not occur at either plant with auxiliary flow rates of 200 gpm, but did occur at flow rates of 240 gpm or greater.

Several plants (Kewaunee, Ginna #1, Point Beach #1, #2, Praire Island #1, #2) with bottom discharge and short to moderate horizontal pipe runs have been operating at least since mid 1975 (and generally somewhat longer) without a reported incident. Although the overall status of these plants, as indicated by the utilities (see Table 4), ranges from evaluating alternatives to meeting the Westinghouse pipe layout guidelines, it is our understanding from informal discussions that careful feedwater flow control is being practiced at these plants, apparently with some success.

Some conflicting evidence is provided by the reports of at least two incidents involving "C" loop of Zion #2 (six feet horizontal run) while feedwater flow was being controlled below 100 gpm. In addition, incidents have been described at Doel #2 at feedwater flow rates of 40 gpm a[illegible] less (the latter occurred at low operating pressures). A waterhammer apparently occurred at Ringhals #2, during a ramp to 200 gpm flow in an early test of vents, and the waterhammer event at Tihange occurred at a reported 176 gpm.

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The available evidence does not support the universal applicability of a feedwater flow rate limit. It is likely that some dimensionless parameter(s), and not flow rate alone, govern the behavior. Moreover, one suspects that the flow processes are too whimsical to be amenable to a simple limit on flow rate without regard for piping geometry, imposed flow rate transients, fluid subcooling, and other parameters.

Vents. Although vents are not currently recommended specifically by any vendor, all CE plants have at least one, and often three, one inch vent holes in the top of the feedring and tee. Tests at Doel #2 suggested that vents alone were an adequate hardware modification to prevent slug formation, but subsequent tests at Ringhals #2 repudiated this claim. A prominent incident involving extensive pipe system damage occurred at a CE plant with vents (Calvert Cliffs #1).

The evidence indicates that vents alone are unlikely to prevent slug formation in general. However, the evidence also suggests that vents may tend to suppress slug formation and reduce potential slug impact intensity, particularly when employed in systems with short horizontal pipe runs.

The case history of the experience with vents illustrates how a proposed hardware modification can rapidly gain and lose favor based on very limited evidence.

B&W Systems. Relative to Westinghouse and Combustion Engineering systems, the B&W system exhibits superior design features and a significantly superior operating experience. To our knowledge there have been no damaging steam generator waterhammer events on any B&W system. Based solely on qualitative arguments, the B&W system already uniformly incorporates the best features of the hardware modifications recommended by the other vendors, and is generally appreciably more positive in accomplishing their design intent.

It is possible to speculate second-order events involving failure of pipe system components that as a result back drain the piping and defeat the design intent of the B&W system. Whether or not slug impact would occur or lead to stresses in excess of allowable stress in these circumstances is controversial. However, such events are unlikely, and are beyond the scope of the present first-order generic evaluation.

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Quantitative Evidence

The prime quantitative evidence developed as a direct result of PWR operating and test experience is:

- 200 gpm threshold flow at Indian Point #2 and Doel #2,
- feedring drainage data, Indian Point #2,
- measurements of pipe bulge, Indian Point #2,
- crude estimate of pipe stress at fracture, Indian Point #2,
- depressurization trace prior to slug impact, Tihange.

The quantitative data listed above are a significant fraction of the complete set and are the data most likely to be useful to confirm analyses. These data are provided in Appendix A. Other data available include:

- instrumented records from Indian Point #2 tests,
- instrumented records from Doel #2 tests,
- displacement traces from Tihange test.

Utility of PWR Experience

It is worthwhile to assess the general utility of past PWR experience in order to guide future testing and reporting of operating occurrences.

The main role of PWR tests has been to provide a limited full-scale verification of installed hardware and intended operating procedures. Such a role is widely accepted as essential, but is costly on the one hand and subject to criticism for being incomplete on the other hand.

PWR operating experience is the ultimate test of the hardware and procedures. Although the pipe system damage incurred in the past has promoted some concern about public safety in the future, there has been no direct impact on public safety in any past incident. The damage has been costly to repair in some cases, however. Continued occurrence of incidents involving steam generator waterhammer suggests a need to solve the problem unequivocally in order to save money and enhance confidence in plant safety.

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PWR tests and operating experience has had relatively little value beyond the above major objectives of problem identification and plant verification. Fundamentally, past experience illustrates that a PWR is ineffective as a research or development tool. Virtually all of our present understanding of phenomena and relevant parameters stems from qualitative arguments, analyses from first principles, empiricism in the literature, and scale model studies. Premature evaluation of fixes on PWRs without adequate analytical basis or confirmation on scale models has been ineffective and has reduced the credibility of current recommendations.

PWR tests can provide critical quantitative data, such as the depressurization trace at Tihange, that are important to the verification of analyses or modeling ideas. However, PWR tests have universally been conducted as verification tests in the expectation that little or nothing would occur. Although this context is appropriate, it is not conducive to acquisition of critical quantitative data. In our view PWRs have been instrumented and tested excessively relative to the data likely to be derived. Our general recommendation would be to test PWRs primarily as required to assist in plant verification, although we encourage the use of appropriate instrumentation at a few key points in order to obtain critical quantitative data. It may be appropriate to leave a few rugged standby instruments (such as maximum displacement indicators) in place during commercial operation.

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3 SURVEY OF PREVIOUS RESEARCH AND RELATED TECHNOLOGY

The purpose of this section is to review previous scientific work that may help explain some of the physical mechanisms, described in Sections 1.2 and 1.3, and provide a basis for analytical development.

Though many of these processes are interdependent, it is convenient at this stage to itemize the available knowledge in the categories listed in Section 1.3; these are considered in order below.

3.1 Initiating Mechanisms

The two important events that must occur in order to initiate a waterhammer event are the partial draining of the feedring, to admit steam, and the formation of a water slug.

Feedring Drainage

The feedring can drain in three ways:

1) Through the holes in the bottom of the ring, if this method of water distribution is part of the design.

2) Through the "slip fit" seal between the feedring tee and the thermal sleeve where the feedpipe enters the steam generator.

3) Back through the feedwater piping.

Items 2 and 3 involve single phase hydraulics that are discussed in many standard texts and should be straightforward to apply as long as sufficient details of the system geometry are known. A difficulty with item 2 is that the slip fit clearance is not very accurately known.

There does not seem to be any previous study of the possibility of drainage through path 3. This could conceivably occur through a leak to the ambient environment or through a leaky check valve if there were sufficient difference in pressure between different steam generators. Here again, the phenomena are well understood, but component reliability data are lacking.

Drain path 1 provides a very low resistance path for water drainage which should be described by a simple hydraulic model. A possible consideration is the effect of countercurrent steam flow which can limit the rate of water discharge by the mechanism known as flooding [12]. Information on countercurrent flow through orifices is available in the chemical engineering literature and a recent thesis by Hagi [13].

Water could also be removed from the feedring by evaporation or flashing. These phenomena are well understood and have not been shown to play a role in recorded events to date.

Slug Formation

Initial slug formation could occur either as a result of purely hydraulic phenomena (of the "open channel flow" type) or from the effect of steam flow on the water surface or from the closing off of the drain holes by the rising water level in the steam generator.

The hydraulics of open channel flow are an established science and proven analytical techniques exist for handling many transient one-dimensional flow problems [14,15]. Flow in a duct with a varying inlet flow rate can be analyzed by the method of characteristics. The numerical computation may be quite considerable, especially if "surge waves" of finite amplitude develop and are transmitted or reflected from junctions, such as the feedring tee. The drainage of an open channel flow through holes in the bottom of the channel can be handled through the equation of continuity if the discharge characteristics of the holes are known. We have not been able to discover previous work describing flow through holes beneath a flow with a considerable horizontal component of velocity; such a flow is not one-dimensional and should not be expected to obey the normal orifice equations.

We have not found any study of feedring hydraulics in the literature and discussion with vendors revealed that they do not have any analyses or quantitative research studies of any of these initiating phenomena.

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Because of condensation on the incoming cold water, a steam flow is set up counter to the open channel water flow in the pipe and feedring. This steam flow produces two-phase flow interactions both near the holes in the feedring (in the ring itself) and in the horizontal pipe run. The condensation rate at a steam-water interface is usually governed by heat transfer on the water side, though in extreme cases it may be limited by kinetic theory effects in the steam, gas dynamics or the presence of non-condensibles [16]. The heat transfer mechanism on a large stream of water is much more likely to be dominated by turbulent mixing than by transient conduction effects. The jet condenser literature, e.g., [17] indicates that direct contact condensation is subject to interface instabilities that may lead to almost instantaneous thermodynamic equilibration across what could be described as a "vapor implosion" or "condensation shock". Previous studies by Creare of transient behavior near the ECC injection point of a model PWR cold leg [18,19] (where cold water is suddenly exposed to steam) showed that this almost instantaneous equilibration could be assumed to occur and that the rate of condensation was essentially limited by the gas dynamics of the steam flow. These topics will be considered again in Section 3.2 where the effect of condensation on steam void collapse is treated.

The ability of countercurrent steam flow to form a water slug was demonstrated by Roidt [5] who also performed similar air-water tests. Movies taken by Westinghouse reveal an interaction between the gas flow and large waves on the water surface (that may result from a surge generated by a previous slug). These studies were not quantitative and no criteria for slug formation were derived.

Slug formation in rectangular horizontal ducts was studied by Wallis and Dobson [20] who developed a criterion for slug formation from a relatively quiescent pool in the form

$$j_g^* > \alpha^{3/2} \tag{1}$$

with

$$j_g^* = j_g \, \rho_g^{1/2} \, (gH\Delta\rho)^{-1/2} \tag{2}$$

where H was the overall duct height, j_g the gas volumetric flow rate divided by the total duct cross-section, and α the fraction of the cross-section occupied by the gas. One would expect a similar criterion, modified to account for geometry, to apply to a circular pipe. A similar result was derived in slightly different form by Taitel and Dukler [21]. Wallis and Dobson also found that slugs could be created at lower gas flow rates by any method that would produce large surface waves while the gas was blowing over the liquid.

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The third possible mechanism for slug formation occurs when the water level in the steam generator rises to the level of the drain holes (as it can if they are on the bottom of the pipe). The steam in the feedring and horizontal pipe run is now entirely surrounded either by water or by a pipe wall; further condensation will lead to sucking of water through the drain holes into the ring and possible slug formation (though the surface depression of the water level around the holes may allow further steam to enter the ring). Some waterhammer incidents have occurred at about the time that the steam generator water level reached the feedring as described in Appendix A of this report.

3.2 Steam Void Collapse

Spherical Bubbles

The effect of the collapse of small vapor bubbles near or away from boundaries has been a subject of intense interest in the field of cavitation. Following the simple model of Rayleigh that predicted an infinite pressure at the time of collapse of a spherical bubble, attempts have been made to improve the analysis by considering effects of viscosity, nonspherical bubble shape, heat transfer, presence of gas in the bubble, proximity of boundaries, and liquid compressibility. Hickling and Plesset [22] obtained impact pressures of the proper order of magnitude required to cause the observed extent of physical damage to boundaries. The analytical model developed for Florschuetz and Chao [23] predicted bubble collapse rates that compared favorably with measurements. By adding the analytical refinement of a finite velocity of sound in the liquid Biasi et al [24] show that the pressure pulses are less than those for an incompressible liquid. Board and Kimpton [25] experimentally and analytically investigated bubble collapse for the range of subcooling for which full coupling of inertia and heat transfer occurs. Measured bubble collapse rates compared reasonably well with theory for subcoolings up to 110°F. A summary of the available theories of nonequilibrium bubble collapse is provided by Theofanous et al [26].

Although analytical models for small spherical bubble collapse rates have been reasonably well confirmed by experiment, the magnitude of the bubble collapse pressure can still not be adequately predicted. The difficulties of measuring these high intensity pressures away from a boundary are obvious. Jones and Edwards [27] extrapolated pressures measured on a pressure-bar gauge to 10,000 atmospheres at collapse. The effect of the presence of a solid boundary on collapse pressures was discussed by Benjamin and Ellis [28] and by Plesset and Chapman [29].

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The latter investigators numerically estimated waterhammer pressures on a solid boundary from an impacting jet to be 2000 atmospheres.

A recent review of the literature by Richter [30] reveals that solutions only exist for small spherical bubbles and not for large bubbles which may not have a simple interface geometry. Several authors mention the possibility of enhanced condensation rates as a result of breakup of the liquid at the interface.

Steam Voids (Long Bubbles) in Ducts

The collapse of voids in ducts has been studied as part of the "column separation" problem. Almost all authors report that results can be predicted quite successfully by ignoring heat conduction and assuming that the steam is always at the equilibrium vapor pressure. Attempts to model thermal effects have used transient conduction theory [31,32] which may not be appropriate near a turbulent or non-planar interface.

Kisky and Henwood [33] devised an experiment for simulating vapor bubble collapse. By collapsing vapor cavities in the frustrum of a cone they generated peak pressures up to 18,700 psi. Using the same apparatus Hawtin et al [32] found that heat transfer effects were important for water temperatures between 130° and 185°F, but for temperatures less than 130°F (80°F subcooling) inertia effects were dominant. Notwithstanding the fact that collapse occurred in a frustrum of a cone, that the conduit was vertical, and that the liquid-vapor interface was initially horizontal, the experiments of these investigators are probably more similar to the slug-impact problem than any others that exist and they do reveal that very high pressures (several thousand psi) can be generated.

Kinetic Theory Limits

Vreeland [34], studying the Tihange waterhammer tests, concluded that condensation "rates" (actually mass fluxes, i.e., mass flow rate divided by the area of the governing interface) were orders of magnitude larger than would be predicted from heat transfer data obtained from conventional condensors. He suggested that the condensation rate might be determined from the kinetic theory of gases. According to a simple version of this theory [16] the condensation mass flux at an interface where the liquid and vapor have different temperatures and effective pressures is

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$$G_{gf} = \sigma \sqrt{\frac{1}{2\pi R_g}} \left[\frac{P_g}{\sqrt{T_g}} - \frac{P_f}{\sqrt{T_f}}\right] \quad (3)$$

σ is a "condensation coefficient" and R_g the gas constant. If the liquid is highly subcooled, Equation (3) appears to predict an upper limit to condensation flux set by the flux at which vapor molecules impinge on the interface. This is the expression cited by Vreeland [34].

In fact, Equation (3) only describes the kinetic theory limit to condensation flux when the differences in temperature and pressure at the interface are small. With large amounts of subcooling the condensation flux is so high that a bulk motion of the vapor molecules is added to the flux that would be obtained from a stationary gas [16]. At very high condensation rates the bulk flow terms dominate and there is no apparent upper limit to the molecular flux across a surface.

In practice, the gas flux due to net flow is likely to be limited by fluid mechanics effects and will be scaled by an appropriate Mach Number. For a perfect gas,

$$M = \frac{V_g}{\sqrt{kR_g T_g}} \quad (4)$$

and the mass flux due to flow is

$$G_g = \rho_g V_g = M\rho_g \sqrt{kR_g T_g} = \frac{M P_g \sqrt{k}}{\sqrt{R_g T_g}} \quad (5)$$

The similarity between Equations (5) and (3) indicates that an explanation based on either theory may predict the trends in a set of data.

3.3 Slug Dynamics

Roidt [5] analyzed the motion of a liquid slug propagating over a pool of stationary water. A similar analysis by Vreeland [34] appears to ignore the water picked up by the front of the slug but considers water sucked in (from the steam generator) at the back of the slug. Both of these analyses involve some idealization but they are straightforward applications of a momentum balance and resemble some of the equations of liquid column motion used to describe "column separation" (see Section 3.4). Informal discussions with CE indicate that they have performed a similar analysis, but it is unpublished.

The pressure difference driving the liquid slug has to be derived from the thermodynamics and continuity relationships for the collapsing void. Thus, these two processes are coupled, as discussed in the previous section. Vreeland [34] found that the transition from "inertia dominated" collapse to "heat transfer dominated" collapse of the void occurred over a small range of effective condensation mass flux. By examining the Tihange data he concluded that condensation rates had to be orders of magnitude greater than would be predicted from heat transfer data obtained from conventional condensers and may be limited by kinetic theory considerations. He also suggested that the interface between steam and water was not placid but disturbed by "waves, sprays and splashes", an observation qualitatively confirmed by high speed movies taken by Westinghouse in their small scale, atmospheric pressure, steam-water model.

Similar work by Roidt [5] using a small scale model at nearly atmospheric pressure gave slug motion essentially limited by condensation rate, due mainly to the large air content of the steam. However, calculations made by Sargent and Lundy [35,36] to model the Zion plant show condensation rates that are so high that the bubble pressure is almost negligible compared with the steam generator pressure over most of the transient with the results that inertia effects are essentially dominant.

These various models of slug dynamics do not seem to have been compared with detailed data (of slug front motion versus time, for example) but only with a few measured pressure transients. Fitting the pressure transient involves choosing two or three arbitrary parameters (initial slug length, water depth below the bubble, condensation coefficient) and it may not be possible to separate the various effects very accurately by such a comparison.

3.4 Slug Impact and Pressure Wave Propagation

It is the impact of the water slug on the tube wall and stationary water at the end of the horizontal pipe run that causes the actual "waterhammer". Since this is one of the more important features of the entire transient we have attempted a thorough review of the published theory.

Waterhammer is an elastic phenomenon normally associated with sudden flow deceleration in a conduit. Although the most severe pressure rise occurs with a nearly instantaneous flow cessation, waterhammer effects frequently occur with more gradual flow changes, especially in long pipe lines. In the event of very gradual flow changes, the unsteady flow may become independent of the liquid elasticity (the pressure changes associated with the resulting inertia and friction dominated flow would no longer be called waterhammer as pressure wave propagation effects are not important). Waterhammer or pressure wave propagation in a conduit is not only affected by flow changes and liquid compressibility, but also by the elasticity of the conduit walls and the constraint of the pipe against movement.

Wave Speed

Since waterhammer is a wave propagation phenomenon the speed of the pressure wave must be known. In an unconfined medium the speed of sound a is

$$a = \left(\frac{K_f}{\rho_f}\right)^{1/2} \tag{6}$$

in which K and ρ are the bulk modulus of elasticity and the mass density of the fluid, respectively. For water free of any gas or vapor the value of a is approximately 4700 ft/sec. Pressure-wave propagation in a conduit is also affected by the elasticity of the pipe wall. The wave speed in a conduit is reduced from the value given in Equation (6), as demonstrated by Streeter and Wylie [37].

$$a = \left(\frac{K_f/\rho_f}{1 + \frac{K}{E}\frac{D}{\varepsilon}}\right)^{1/2} \tag{7}$$

in which E is Young's modulus for the pipe wall material, D is the inside pipe diameter, and ε is the pipe wall thickness. For typical metal pipes over a range of diameters the wave speed computed on the basis of Equation (7) will vary from about 3000 ft/sec to 4500 ft/sec if no free gas is present. For thin-walled plastic pipes the wave speed may be reduced to 1000 ft/sec to 1500 ft/sec.

Pressure Change

Although waterhammer need not be associated with rapid flow changes, the Joukowsky equation is usually introduced to relate pressure change to velocity change. If the flow velocity in a conduit is abruptly varied by an amount of ΔV the pressure change Δp across the resulting pressure wave is given by

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$$\Delta p = -\rho_f a \; \Delta V \qquad (8)$$

where a reduction in velocity is associated with a rise in pressure. For an instantaneous closure of a downstream valve against a flow velocity of V_o the pressure rise is

$$\Delta p = \rho_f a \; V_o \qquad (9)$$

For a known wave speed the factors that may produce deviations from Equation (9) are line packing (as in long oil pipe lines) caused by a large initial pressure gradient, and axial motion of the valve and/or pipe. Yielding of the pipe wall material will also influence the wave speed and the resultant overpressure magnitude.

For the collapsing of a large vapor cavity adjacent to a valve, the resulting pressure rise would be given by Equation (9) if V_o were the slug velocity at impact and there were no valve or pipe motion at collapse. The idealized collapse of a vapor cavity within a conduit resulting from the impact of two liquid slugs cannot be directly computed by Equation (8) or Equation (9) because of the fact that the final compressed liquid is not brought completely to rest. Because of the resulting motion of the impacted liquid slug as a consequence of the force impacted to it, the idealized pressure rise is initially only one-half of that given by the usual Joukowsky expression, Equation (9).

Rapid Versus Gradual Flow Changes

The relatively short length of liquid slug in the horizontal portion of the feedwater line coupled with a finite time elapsed between initial and final collapse of the steam pocket may correspond to a gradual flow change rather than a rapid one even though the entire event takes only milliseconds. Therefore, it is considered appropriate to discuss the analogous differences between rapid and gradual closure of a downstream value in a pipe line.

For simplification an initial example is chosen for which the valve closure is assumed to be instantaneous, the initial flow is assumed to be small enough so that the pressure in the pipe is essentially constant, and there is no friction loss during the transient. An initial condition of steady flow in a pipe leading from a reservoir is considered. Following rapid valve closure at t=0 the sequence of events is as follows.

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Initially only a few layers of water near the valve are brought to rest. The deceleration necessary to instantaneously stop layer by layer of the water leads to a pressure wave propagating toward the reservoir at the speed of sound a. If L is the length of the pipe, all of the water in the downstream half of the pipe will be compressed and at rest at $t = L/2a$. As more and more of the water is brought to rest the wave finally reaches the reservoir at $t = L/a$. At this instant an unstable condition exists as the pressure or head in the pipe is greater than that in the revervoir. Since according to Equation (8) a pressure change must be accompanied by a velocity change, the water begins to flow back in and toward the reservoir layer by layer, resulting in a negative wave (or pressure drop) propagating downstream. The progress of such an idealized wave can be followed indefinitely. Because of the assumption of no friction or boundary resistance in the pipe during the transient and the assumption of no loss of kinetic energy as the water is flowing into the reservoir, the phenomenon continues forever, having a period of $4L/a$. In reality, no valve closure is exactly instantaneous and friction always tends to resist and to damp motion.

The instantaneous closure assumption does allow for a prediction of the maximum possible pressure rise due to closure of a downstream valve. For closure times t_c greater than 0 but less than one round-trip wave travel from the valve to the reservoir and back, defined as $2L/a$, the maximum pressure is equal to the instantaneous closure value if the initial pressure gradient is small. The time of occurrence of the maximum pressure at the valve is less than $2L/a$.

Valve closure is considered gradual if $t_c > 2L/a$ as the reflected wave from the reservoir provides pressure relief at the valve. The gradual closure of a downstream valve will always result in pressure rises less than those predicted by Equation (9). If the time of closure is in fact, much greater than $2L/a$, say $t_c > 10L/a$, the resulting pressure changes may be governed only by inertia effects. In this case, the phenomenon is no longer waterhammer.

In order that the pressure rise associated with the impact of a liquid slug can be predicted by $\Delta p = \rho_f a \, \Delta V/2$, the closure must occur before the first pressure wave caused by column deceleration can travel to the end of the slug and back, or $t_c < 2L/a$, where L is the length of liquid slug, and t_c is the time of closure. The degree of pressure reduction from that predicted by $\rho_f a \Delta V/2$ will depend upon the ratio $t_c/(2L/a)$.

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Wave Reflection

The reflection and transmission of pressure waves from pipe junctions, around bends, and through valves and machines is fairly well understood for single-phase flow. Coefficients of reflection and transmission can be easily derived for simple junctions, Rouse [38]. If the cross-sectional areas at the junction are A_1 and A_2 and the corresponding wave speeds a_1 and a_2, where section 1 corresponds to the pipe with the incident wave, the transmission coefficient defined as the ratio of the transmitted pressure change to the incident pressure change is given by

$$s = \frac{2\,\frac{A_1}{a_1}}{\frac{A_1}{a_1} + \frac{A_2}{a_2}} \qquad (10)$$

The reflection coefficient is given by

$$r = s - 1 \qquad (11)$$

If $a_1 = a_2$ and $A_2/A_1 = 2$, $s = 2/3$ and $r = -1/3$, meaning that 2/3 of the wave is transmitted as a wave of like sign while the reflected wave is of different sign but 1/3 of the amplitude of the incident wave. The limiting case of a constant pressure source (reservoir) or $A_2 = \infty$ yields $s = o$ and $r = -1$. In this instance none of the wave is transmitted, but all of it is reflected with a change in sign.

The pressure pulse can be itensified if an incident wave approaches a junction for which $A_2 < A_1$, and/or $a_2 > a_1$. For $a_1 = a_2$ and $A_2 = 0.5\ A_1$, $s = 4/3$ and $r = 1/3$. The limiting case of a dead end ($A_2 = 0$) results in $s = 2$ and $r = 1$, meaning that the reflected wave is completely reflected with the same sign, resulting in a doubling of the pressure, near the dead end.

The reflection and transmission of pressure waves from hyrdaulic machinery has not been well documented. Instead, analyses are usually based upon the assumption of applicability of steady-flow characteristics to the unsteady flow phenomena. A similar approach is employed for calculation of wave reflection from partially closed valves. As demonstrated by Contractor [39] and Safwat and Polder [40] the use of steady-flow loss characteristics produces reasonable results for junctions and valves, respectively.

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For 90° pipe bends Swaffield [41] showed by measurement that the transmission coefficient would not be less than 0.96 if the ratio of radius of elbow curvature to pipe diameter exceeded a value of 5. Somewhat greater attenuation was measured in limited tests by Cagliostro et al [42] who also found that peak pressure (but not impulse) was reduced after passing a filled standpipe, that the overpressure pulse was annihilated at an empty (gas filled) standpipe and that the pressures added simply whenever two pulses met.

Pipe Motion

If the pipe in question is not adequately supported or fixed not only will the wave speed be affected slightly, but also the pressure change predicted by Equation (9) will not be correct. As shown by Jones and Wood [43] the pressure change associated with the instantaneous closure of a downstream valve that is free to move axially oscillates about the Joukowsky value. Subsequent to valve closure the extension of the pipe produces a pressure rise less than $\rho_f a V_o$, but as the pipe shortens a pumping action causes the pressure to then rise above the Joukowsky value. The latter deviation from Equation (9) is determined by Jones and Wood [43] to be

$$\Delta(\Delta p) = \alpha\, e^{-\beta} \tag{12}$$

in which

$$\alpha = \frac{A[p_o + \rho_f a V_o]\;[\omega^2 + q^2]}{k\omega V_o} \tag{13}$$

and

$$\beta = \frac{3\pi q}{2\omega} \tag{14}$$

in which ω is the natural frequency of the pipe end and k the spring constant for the pipe. The parameter q is defined to be

$$q = \frac{\rho a\ A}{2m} \tag{15}$$

where m is the mass of the valve. In any experiments for which cavity collapse occurs against a closed valve or pipe dea[illegible] the possible effect of axial motion of the pipe should be a[illegible]ed.

Liquid - Column Separation

Liquid-column separation is the generation of large vapor cavities as a result of rapid deceleration of liquid columns. Frequently, the cavity occupies the entire pipe cross section, allowing for the complete separation of the liquid column from

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a valve, or from another column of liquid. Column separation may occur due to flow deceleration subsequent to pump shutdown or as a result of closure of an upstream valve. Obvious locations for cavity formation are near the source of the transient and at high points in the pipe profile where the ambient pressure is lower due to gravity. Column separation can also occur following the rapid closure of a downstream valve. If the flow in the pipe cannot be completely arrested by the reflected negative pressure wave then the liquid column will separate from the valve, leaving a vapor pocket (and possible evolved gas) behind. Column separation will occur for rapid closure of a downstream valve if $\rho_f a V_o > p_o + p_a - p_v$, where p_o is the reservoir pressure, p_a is the barometric pressure, and p_v is the vapor pressure. Column separation occurs occasionally in water supply lines, in aviation fuel lines, and petroleum pipe lines. Usually the liquid in the pipe is subcooled to such an extent that the rate of condensation of the vapor is not heat transfer limited, but rather controlled by inertia effects until collapse occurs.

Notwithstanding heat transfer effects, cavity collapse in horizontal pipelines as a result of liquid column separation is not unlike slug impact in feedwater lines of PWRs. The ratio of t_c to $2L/a$ would be expected to be much greater for the collapse of a slug in a feedwater line than for the slamming of a liquid column against a valve. This expectation may not always be realized, however, as both the time of collapse t_c and $2L/a$ are large for liquid-column separation problems, while both are small for the slug-impact problem.

Closure of Downstream Valve

A literature search will reveal that liquid-column separation has not only been investigated for initial depressurization resulting from upstream valve closure, but also for the closure of a downstream valve, for which the initial transient pressure change is positive, not negative. In this case, at time $t = 2L/a$ the entire liquid column flowing toward the valve will have to be completely arrested or the liquid will pull away from the valve, creating a cavity whose site will depend upon the initial velocity in the pipe, length of pipe, valve closure, ambient pressures, and vapor pressure.

Most investigations of downstream valve closure have been laboratory studies of column separation in horizontal conduits. LeConte [44] probably conducted the first controlled test of this nature, observing a series of cavity formations and collapses,

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or a multiple number of rebounds with diminishing resurge pressure peaks. For horizontal pipes the volume and shape of the cavity depends upon the initial pressure and velocity. As observed by Baltzer [45] rapid valve closure in conduits with relatively low initial velocities V_o will result in elongated cavities on the top of horizontal conduits. In this case the maximum possible resurge pressure may not be realized if the time of complete collapse is greater than 2L/a for the pipe.

Upstream Valve Closure

A typical pressure trace measured for the sudden closure of an upstream valve resembles the depressurization and slug-impact record reported by Vreeland [34] for water-hammer in the feedwater line of the Tihange plant. In nearly all of the investigations of liquid-column separation and cavity collapse the subcooling of the liquid is so great that the effect of heat transfer is negligible. As demonstrated by Swaffield [46,47] the effect of dissolved gases is more important because of the cushioning effect. Li [31] considered the shape of the cavity and concluded that it was not a factor regarding the collapse pressure.

The usual analysis allows for a single cavity adjacent to the valve. The volume of the cavity is continuously computed from momentum and continuity considerations while the cavity is maintained at vapor pressure. Impact of the liquid column against the valve occurs in the solution whenever the cavity volume becomes zero. In nearly all analyses the shape of the liquid-vapor interface is not accounted for. Measurements by Safwat and Polder [40] suggests that a model with a vertical interface overpredicts the final pressure because of the additional time necessary to collapse the final vapor in an actual elongated cavity on the top of a horizontal pipe. The effect of a sloping interface on the rising pressure trace during collapse is also apparent when viewing the recording with synchronized high-speed motion picture of cavity growth and collapse, Safwat [48].

In summary, waterhammer pressures caused by liquid-column separation can probably be predicted to within 10-20 per cent accuracy using the assumption of an idealized cavity if no free or dissolved gases are present. Because of the shortness of the liquid slug the same degree of accuracy would not be expected for the slug-impact problem in feedwater lines.

3.5 Potential for Pipe Damage

This section of the report outlines current procedures for evaluating the damage potential of a postulated waterhammer event. When the water motion is changed abruptly, the momentum equation requires a concomitant large pressure increase. In this situation, instantaneous local yielding or rupture can occur either to the flow obstruction (e.g., valve) or to the pipe walls. If the energy of the impact is not completely absorbed by local deformation, a pressure wave propagates through the pipe with the potential for remote damage. Such a pressure wave applies a force to each pipe bend in its path. Excessive loads on pipe flanges and hangers can result from relatively low pressure changes. Under some circumstances, the initiating pressure wave can also reoccur at a frequency near the natural frequence of the pipe structure, enhancing the prospect of excessive pipe stress due to resonance.

The forces acting to damage pipes belong to three categories of analysis. Described below are:

- local hoop stresses exceeding the yield strength of the pipe,
- pipe network loads, the response of a system of interconnected pipes to loads applied at specified locations, and,
- resonances, the response of a pipe network to a repeated stimulus.

Internal Local Loading

The prediction of whether or not a pipe subjected to an internal static pressure will yield is a fully developed engineering discipline. In the simplest waterhammer event, the pressure inside a pipe is raised as a pressure wave passes subsequent to a water slug impact. The question "can the pipe withstand such a pressure without suffering fracture or plastic deformation?" can be answered readily once the pressure, the dimensions of the pipe, and the pipe material properties are specified. For engineering purposes, any gun barrel designer, pressure vessel designer, or the nearest copy of Roark and Young [49] can answer this question accurately and completely. If the slug impact occurs near the end of a pipe, then longitudinal as well as circumferential stresses must be considered. Also, the pipe may have flanges and gaskets subject to high pressure, but for engineering analysis, these are only minor complications.

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To provide a first-order estimate, consider the circumferential (hoop) stress σ in a thin-walled tube, due to internal pressure p:

$$\sigma = pr/\varepsilon \qquad (16)$$

where

r is pipe radius
ε is pipe wall thickness.

Waterhammer overpressure of 4000-6000 psi will cause stresses approaching the elastic yield point of a steam-generator's steel feedwater line. Accurate knowledge of possible overpressure history everywhere is therefore imperative.

The modification of the pressure pulse as it propagates is germane to the issue of pipe deformation. Overpressure increase can occur if the wave enters a constricted portion of a pipe, or a stiffer (less elastic) pipe. Such effects are easily calculable and are unlikely to increase the overpressure by more than a factor of two. Unfortunately, the pipe system designer cannot depend on pressure wave attenuation to simplify his problem. Pressure waves travel great distances through water-filled inelastic pipe with virtually no attenuation, and negotiate rigid bends handily. The means to extract wave energy are mechanical damage and pipe motion. That is, plastic deformation or gross movement of the pipe structure at one point can protect distant pipes. As a corollary, any prediction method gets weaker as the point of interest gets farther and farther from the initial point, and the intermediate pipe compliances accumulate.

The last variable to be discussed here is time. Specifically, the pressure pulse to this point has been assumed to exist forever, with no consideration of whether the pipe deformation might be less if the pressure pulse were short enough. The pressure pulse resulting from a postulated slug motion sequence is idealized as a square wave formed by the superposition of an initial depressurization, a compression wave, and the subsequent rarefaction wave. If the pulse width is short enough, the dynamic effects tend to "strengthen" the pipe. The inertia of the pipe walls can be sufficient to prevent a significant amount of motion (strain) before the applied pressure returns to its base value. (This effect is a well-known limit to production rate in the High Energy Rate Forming (HERF) field, see [50,51]). In the present case, "short" times can be defined by comparison with the natural frequency of the pipe walls ω:

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$$\omega^2 = E/r^2\rho_p \tag{17}$$

E = modulus of pipe wall
r = radius of pipe wall
ρ_p = density of pipe wall

The radial vibration period for a typical feedwater line is 0.3 millisecond. This is short compared with expected pulse widths, so that the pipe walls "track" the pulse and the designer cannot count on inertia effects for saturation. Indeed, if the pulse rise time is rapid, as in a square wave, then dynamic amplification of up to a factor of two could occur. The HERF literature would be a good starting place for refining the analysis if pulses with duration $\tau < 1$ ms (slugs with length less than 2.5 feet) are expected.

To summarize, if impact overpressure is given, current technology is adequate to verify with high confidence that the hardware will withstand the resulting direct, local, internal loads. However, the theory to deduce the loading implied by a known plastic deformation is not so well developed because emphasis is placed on remaining within elastic limits. The above discussion has emphasised means to predict pipe behavior. Although comparable theory may be applied to stress in pipe system components (e.g., valves) their geometry is more complex and accordingly quantitative predictions are more uncertain.

Piping Networks

The actual piping systems under consideration (feedwater lines, and power plants in general) are so complicated that computer codes are required for their analysis. However, the codes in current use do not generally employ any physical models individually more complicated than standard pressure wave propagation or beam bending subject to various boundary conditions. The process of assessing the ability of a proposed set of pipes, hangers, snubbers, and restraints to withstand waterhammer effects is currently divided into three tasks. A waterhammer analysis is first performed to predict the time and position dependence of the fluid pressures throughout the system. Existing codes such as WHAM [52] are adequate for this work. Second, by application of the momentum equation, the pressure waves are resolved into time-dependent forces on each of the nodes of the system. Finally, when the node forces have been determined, structural codes are used to determine the implied stresses due to bending and deformation. Many codes of this third type exist, such as ADLPIPE [53] and NUPIPE [54].

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The structural codes are of two distinct types. There is a group of codes designed specifically to analyze complex piping networks. The programs are based on linear elastic stress/strain theory and require discretization of the system based on junctions and uniform beams. A more general class of programs (of which NASTRAN [55] is the archetype) contains general finite-element stress capabilities. Some codes contain limited plastic deformation capability but this appears to be unnecessary for the present design task and is not yet a well developed tool.

The major difficulty that arises in the application of the network codes is the proper inclusion of time-dependent forces of high frequency.

Reference [8] is an example of the analysis of structural response to a waterhammer event. This reference reports the application of the programs ANSYS and STARDYNE to a feedwater pipe at Trojan. In general, the use of these codes by architect-engineer analysts is routine (but not trivial). The specific analysis of waterhammer loads has, however, some unusual features. First, the high-pressure square-wave pulse of a waterhammer may be of very short duration, thus exciting high-frequency vibrational modes of a pipe network. Appropriate analysis may require some finesse. The high-frequency modes can contribute significantly to the pipe stress, thus failure to include enough modes (or, in the alternate computational method, to integrate with sufficiently small time steps) results in underprediction of the peak stress.

Contemporary analyses of potential waterhammer loads in nuclear power plant piping systems reported in the open literature include the work of Fox and Stepnowski [56], Thorley and Twywan [57], Larsen [58], and Harper et al [59].

A major uncertainty in all of this analysis is the coupling between the waterhammer dynamics and the pipe motion. Techniques for analyzing waterhammer wave propagation in complex compliant pipe systems are not well developed. Unless the piping system can be treated as effectively rigid, as far as the water is concerned, significant errors may result. It may not be possible to measure high pressure magnitudes at points distant from the impact because the intervening piping will attenuate the pulse by bending and limit the impact by bulging.

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Resonance

Vibration response of a pipe network can be calculated by the computer programs now in existence. The system response to a repeated pulse can be computed, so that by varying the frequency at which the analysis occurs, a complete frequency response spectrum can be developed. Unfortunately, this information is not sufficient to identify self-induced (feedback) oscillations which may lead to pipe network damage.

The simpler problem can be handled. If a pipe system has known natural frequencies, it behooves the analyst to be sure that there are no potential forcing functions at these frequencies. In general, the natural frequency of the system ω_0 will be very low compared to the frequency of a disturbance that can be generated by an isolated waterhammer, i.e., roughly the speed of sound in water divided by a typical pipe length. In many practical cases literally a mile of pipe would be necessary before the organ-pipe frequency approached the system kinematic frequency.

Since the slug-impact event itself has been demonstrated to be repetitious at a low frequency, some consideration of possible resonance is appropriate. (After each impact, the water can wash back down into the feedring and reinitiate the event until the water is heated sufficiently or the pipe runs full.) However, repeated slug impacts are likely to be few in number.

The case wherein pipe motions due to a single waterhammer event are the trigger for subsequent slug impact is not credible because the flow in the steam generator and nearby piping is uncoupled from the piping response unless a pipe or component ruptures. In the latter case an unlimited range of potential coupling exists.

Damage Criterion

We have concluded that stresses and strains can be computed, but have neglected to describe the criterion by which potential damage can be assessed. The ASME has established complete guidelines for allowable stress, but some clarification is needed with respect to waterhammer incidents. The ASME code has different standards for different categories of incidents (e.g., normal, upset, emergency, fault). For example, Reference [8] classifies feedwater pipe waterhammer as "an unplanned accidental condition". Apparently, this is not a universally accepted classification, however, so that statements such as "predicted stresses are within allowable limits" are vague. Uniform application of the standards should be ensured; the standards themselves are likely to be adequate, once they are uniformly applied. Some consideration of the potential of low-cycle fatigue as well as one-time deformation is necessary.

Except possibly for coupling between the pressure waves and the structure, the present tools seem capable of doing the necessary analysis to assess the potential for pipe deformation and fracture. The prime uncertainty resides in postulating a waterhammer forcing function.

3.6 Previous Analyses and Model Tests of Steam Generator Waterhammer

Previous analytical work on this problem identified during the course of this study includes:

- Westinghouse slug analysis [5],
- recent Westinghouse slug analysis in progress (to be published early in 1977),
- unpublished Combustion Engineering slug analysis [3],
- Trojan pipe stress analysis by Bechtel [8],
- Kewaunee fluid/thermal analysis by Fluor [61],
- Westinghouse analysis of pipe integrity subsequent to the 4/30/72 incident at San Onofre [62],
- Zion analysis of slug behavior and pipe stress by Sargeant and Lundy [35,36],
- Maine Yankee analysis of pipe stress by Yankee Atomic Power Company [63],
- Evaluation of energy to fracture pipe at Indian Point #2 by Con Ed and Westinghouse [1],
- Comparisons with Tihange pressure data by Vreeland [34], Batchelor et al [64], Westinghouse (unpublished), CE (unpublished),
- Tihange Pipe Stress evaluation by Batchelor et al [64].

The prime work relevant to the present generic inquiry has been reviewed in the text of this section of the report and in Appendix B where a detailed critique of the Westinghouse analysis by Roidt [5] is provided.

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Scale model tests have been conducted by Westinghouse in two 1/10-scale facilities [5]. Only qualitative information was obtained in tests with a "straight-pipe" model tested in an air environment. Steam-water tests were conducted with a steam generator model, but suffered from a high air content in the steam. The only other scale model mentioned at meetings with the vendors [2,3,4] is the study of Framatome in a 1/10 scale steam generator model using steam at approximately 50 atm. At this writing the Framatome report is unavailable to Creare.

3.7 Summary of Previous Efforts

The main conclusions from this review of previous work are:

1) The mechanisms that initiate slug formation are not well understood. Some qualitative observations have been made at small scale but no descriptions of the sequence of events or quantitative criteria for making predictions exist.

2) The collapse of large steam voids cannot be predicted from existing information. Classical theories based on simple interface geometry and laminar transient heat conduction probably do not apply. There is evidence, in the Tihange data, for condensation rates limited only by compressible gas-dynamic effects in the vapor.

3) The motion of a one-dimensional water slug seems to be one of the best understood phenomena. The mechanics are straightforward; however existing analyses are based on assumptions which are so far unconfirmed by experimental results.

4) Waterhammer theory is an established science that can give good estimates of peak pressures and wave propagation if the properties of the impacting liquid slug are known. It should be possible to make upper bound estimates of the waterhammer intensity.

5) Calculations of the forces in pipes resulting from waterhammer involves a very complicated dynamic interaction problem requiring the use of large computer models. However, it should be possible to obtain reasonable estimates of some key factors, such as the maximum hoop stress near the slug impact point and the forces on simple bends near the point where the wave originates.

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These conclusions helped to provide a perspective of the nature of the technical problems to be considered in the present work. In particular, three major efforts where model development is needed can be identified.

1) Initiating Mechanisms. This study should involve feedring drainage, hydraulic transients in the feedring and attached piping, and the mechanism of slug formation. A considerable practical incentive for this work is that if the initiation of the whole sequence of events leading up to waterhammer can be avoided, the problem may be solved without worrying about the detailed consequences of a slug impact.

2) Void Collapse and Slug Dyanamics. This is needed as an improvement on the existing models of Roidt [5] and Vreeland [34]. It should contain better evidence for the component assumptions in the model and, if possible, a comparison with more extensive data.

3) Waterhammer Intensity. Based on (2) above, a method of predicting the forcing function applied to the piping system is needed. It is desirable to compare this with experimental data, including research results and any evidence from full-scale PWR plants, in order to obtain confidence in the validity of the procedure.

Scale model studies and analyses along these lines are described in the following Sections 4 and 5 of this report.

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4 EXPLORATORY SUBSCALE EXPERIMENTS

A general lack of experimental evidence, particularly quality quantitative data, is identified in the preceding sections of this report. Because some of the phenomena are exceedingly complex, analyses to date have been simple and have of necessity involved crude bounding assumptions of key phenomena. In such a situation, experiments of all kinds are critical to the development of even a first-order understanding of the phenomena. To this end, limited exploratory experiments were conducted in two 1/10-scale facilities using steam and water and in a 1/4 scale "hydraulic" facility in an air environment.

For presentation purposes, the experimental work at 1/10 scale has been lumped in this section of the report largely without comparison with any analysis. The analytical development is presented in the following Section 5 of the report. There the hydraulic data from the 1/4 scale experiments are also presented in the context of the analysis.

The reader is cautioned to avoid "scaling up" the results of these experiments. Such data are useful primarily to provide preliminary limited verification of analytical models, to display potentially relevant phenomena, and to suggest qualitative trends of key parameters. These data alone are inadequate for confirmation of analytical models to be employed to predict PWR behavior.

4.1 Water Cannon Model

The purpose of this model study was to gather data to examine the underlying assumptions of the slug motion and impact analysis in a simple, "clean" geometry chosen to remove some complicating phenomena. Areas where the analysis performed well were identified and some questionable assumptions were isolated.

A vertical pipe model (Figure 9), termed the "water-cannon model" in this report, was chosen in order to eliminate some of the complicating effects of horizontal piping such as slug-interface shape effects and the effects of water lying in the pipe on the slug behavior. In addition the slug was a water column of known length at impact in this model. The model consisted of a straight length of vertical metal pipe or transparent plastic tubing with steam introduced into the upper end (figure 9). The lower end of the pipe was submerged several inches in a large reservoir of water (at 65°F) open to the atmosphere. The pipe was stiffly braced axially at the upper end.

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In the operation of this model, the pipe would be full of liquid after the previous cycle. Steam introduced at a constant rate by choked flow from a fitting at the top of the tube would warm the water and gradually void the tube as the water warmed up and the rate of condensation decreased. As the water level in the tube reached the bottom end of the tube, the sudden exposure of the steam to the cold water in the reservoir resulted in rapid condensation of the steam, a depressurization in the tube, and subsequent acceleration of liquid entering the tube due to the pressure difference between the steam in the tube and the atmospheric environment. The water column in the tube rose upward rapidly. In a fraction of a second it impacted against the closed upper end of the tube causing a loud noise and visible shaking of the apparatus. The cycle repeated.

The depressurization preceding the slug impact and the pressure on the face of the pipe cap on the top of the pipe (i.e., the overpressure due to slug impact) were the primary measurements during repeated tests of the same event. In addition, flow visualization studies were conducted in the transparent arcrylic tube, slug velocities measured, the effect of pipe material noted, and various triggering mechanisms for initiating the slug motion investigated.

One piezoelectric crystal pressure transducer was flush-mounted in the upper end of the pipe and used to measure the the overpressure due to the slug impact.* A second transducer of the same type measured the pressure in the pipe just prior to the impact. Mounting the latter transducer on a standoff resulted in a major, but nonetheless negligible, loss of frequency response (with the response time about 1-2 msec rather than 0.001 msec in the flush-mounted case). However, this mounting was necessary to avoid thermal drift of up to 40 psi associated with this type of transducer which obscured the low pressure signal when the transducer was flush-mounted.

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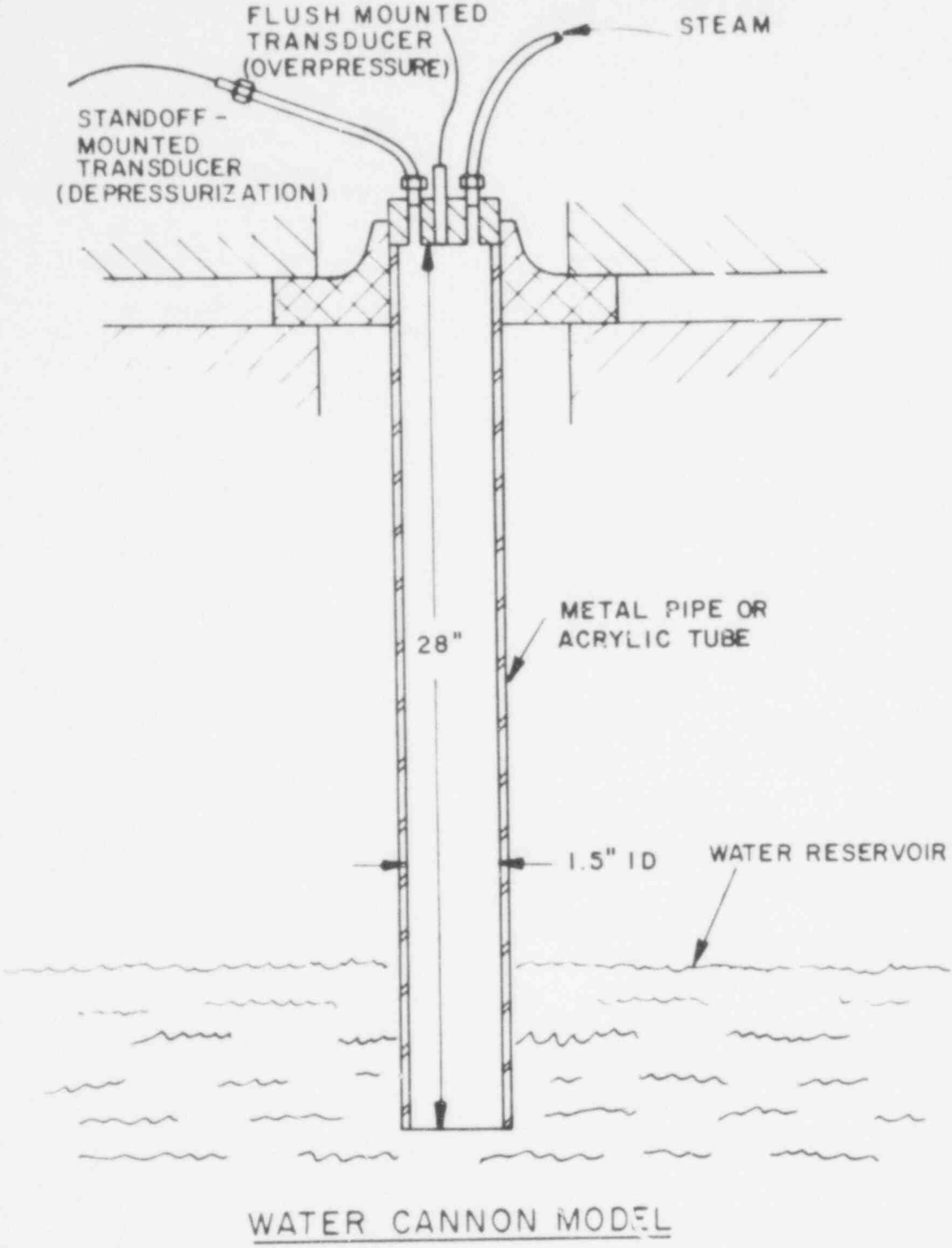
*The sensitivity to vibration of these instruments is specified as 0.002 psi/g. Order of magnitude calculations readily demonstrate that the momentum exchange due to slug impact would induce an acceleration of only a few tens of g's even if the pipe were unrestrained axially, but subjected to the measured impulse. This corresponds to a small fraction of a psi which is a negligible effect in the present experiments.



WATER CANNON MODEL

FIGURE 9

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Pressure Traces

Sample pressure traces from a cycle of operation of the water cannon model are shown in Figure 10. The precursor depressurization is shown in the upper trace (standoff-mount transducer) and the concomitant slug impact in the lower trace (flush-mount transducer) from a single event. At time 50 msec the system was at atmospheric pressure. The upper trace shows that the depressurization decreased gradually to about -6 psi (-6 psig) by 100 msec and remained near -5 psi to 200 msec; thus a differential of 5 to 6 psi acted on the water column for approximately 150 msec. Due to the oscilloscope gain setting used for pressure measurements, the prime overpressure spike was off-scale although its initiation was indicated at 200 msec. The lower trace had a much larger scale and showed the overpressure spike recorded at the time of the impact. The indicated overpressure was 1100 psi. There was a second depressurization after the impact, and 40 msec after the first impact a second overpressure spike of 300 psi amplitude was recorded.

Figure 11 shows depressurization traces (left) and corresponding time-expanded traces of the overpressure pulses (right) for several events. The depressurization characteristics were similar to the example described in Figure 10. The time-expanded overpressure pulse traces show that the duration of the slug impact was approximately 1 msec. This is uniform for each of the traces shown. The shapes of the pulses were essentially rectangular, though somewhat variable and perhaps sloped by a few hundred psi across the peak.

Figure 12 typifies the scatter obtained in measuring the overpressure spikes for many events. The range of observed overpressures was from 500 to 1300 psi, with the most frequent measurements occurring in the 800-1000 psi range.*

A quantitative evaluation of these data and comparison with a slug dynamic analysis are contained in Section 5.3 of this report. These demonstrate that the overpressure and depressurization impulses are self-consistent and that the pulse duration is consistent with the pipe length. The scatter in overpressure is due principally to erratic variation in the depressurization pulse.

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*The steam flow input into the model ranged from 0.0[illegible]1 - 0.007 lb/sec. The rate did not affect the depressurization or overpressure appreciably over this range.

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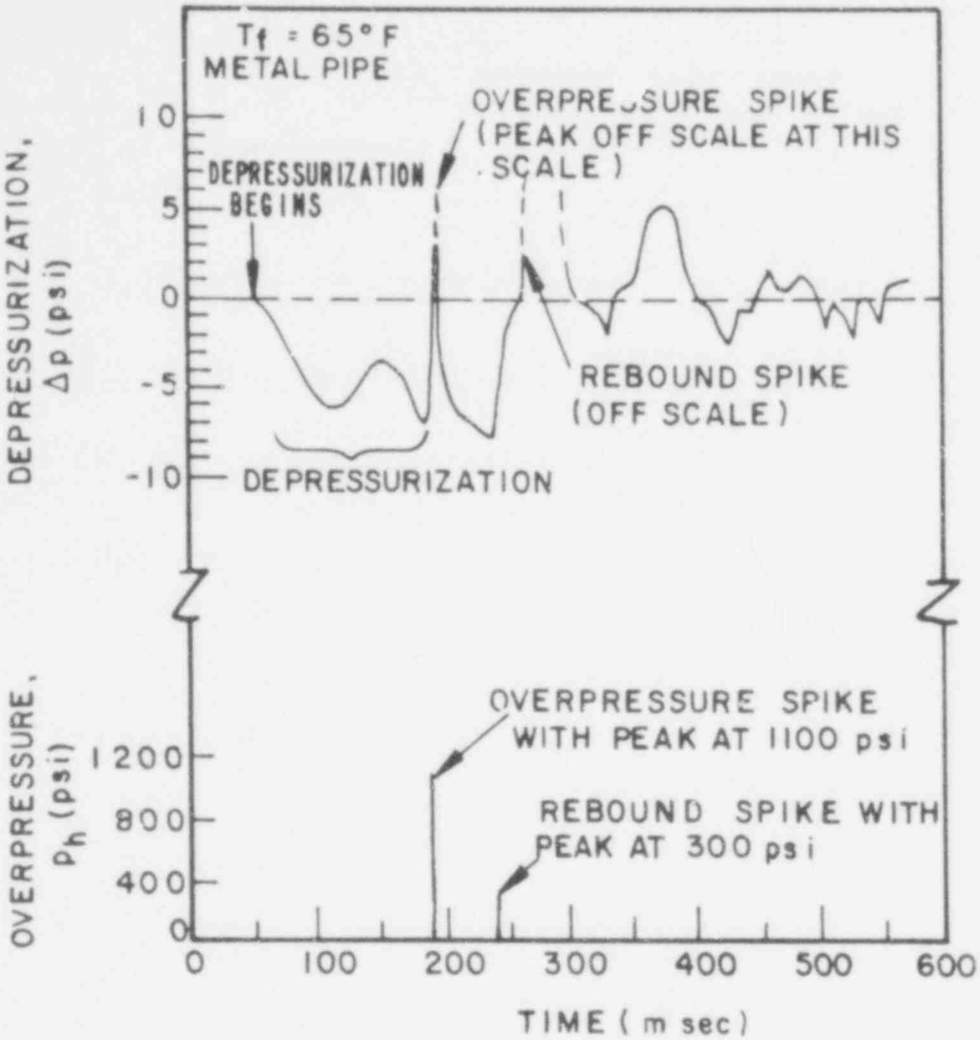

SAMPLE PRESSURE TRACES FROM WATER CANNON MODEL

FIGURE 10

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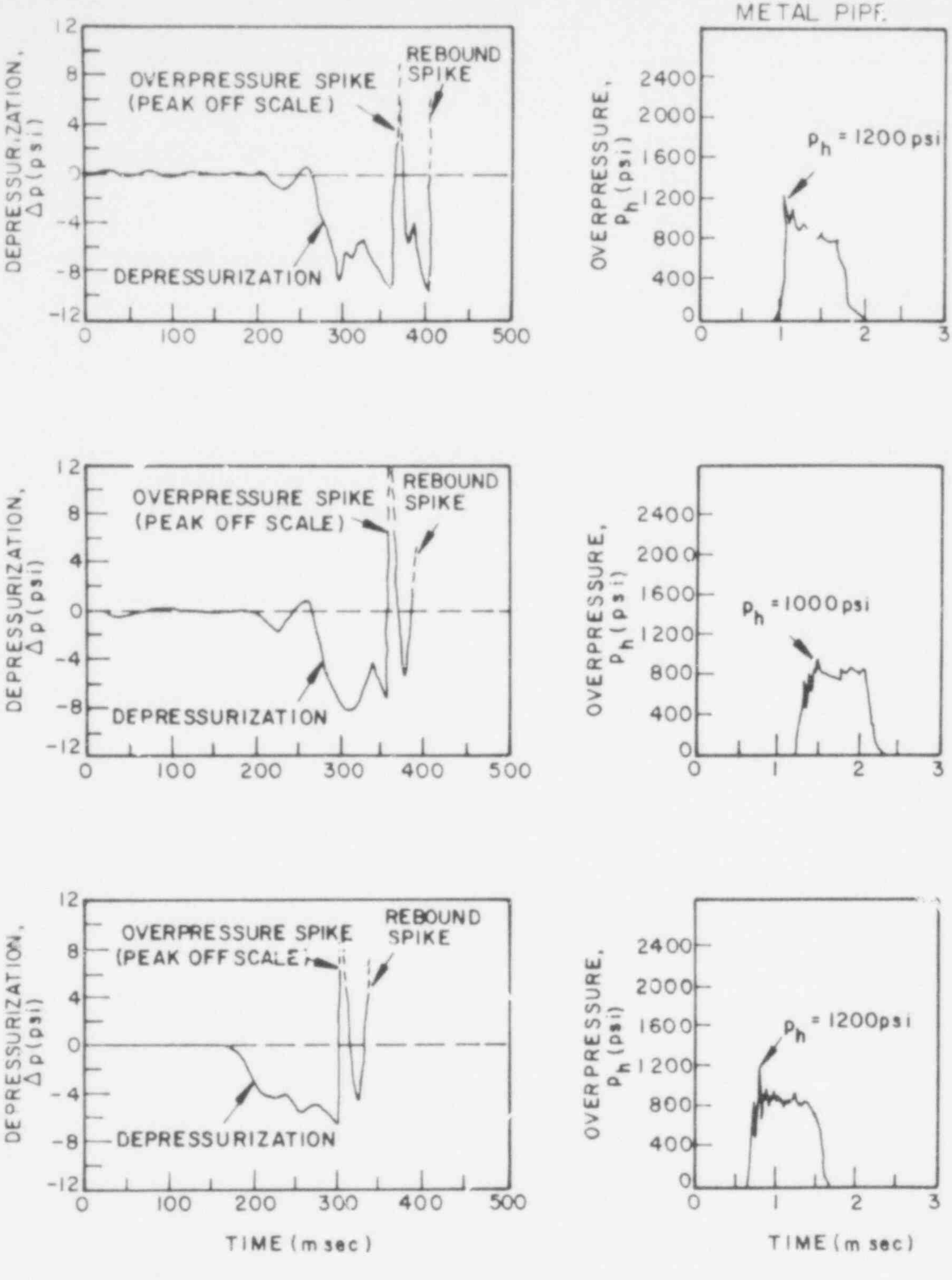

SIMULTANEOUS PRESSURE TRACES IN WATER CANNON MODEL

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FIGURE 11

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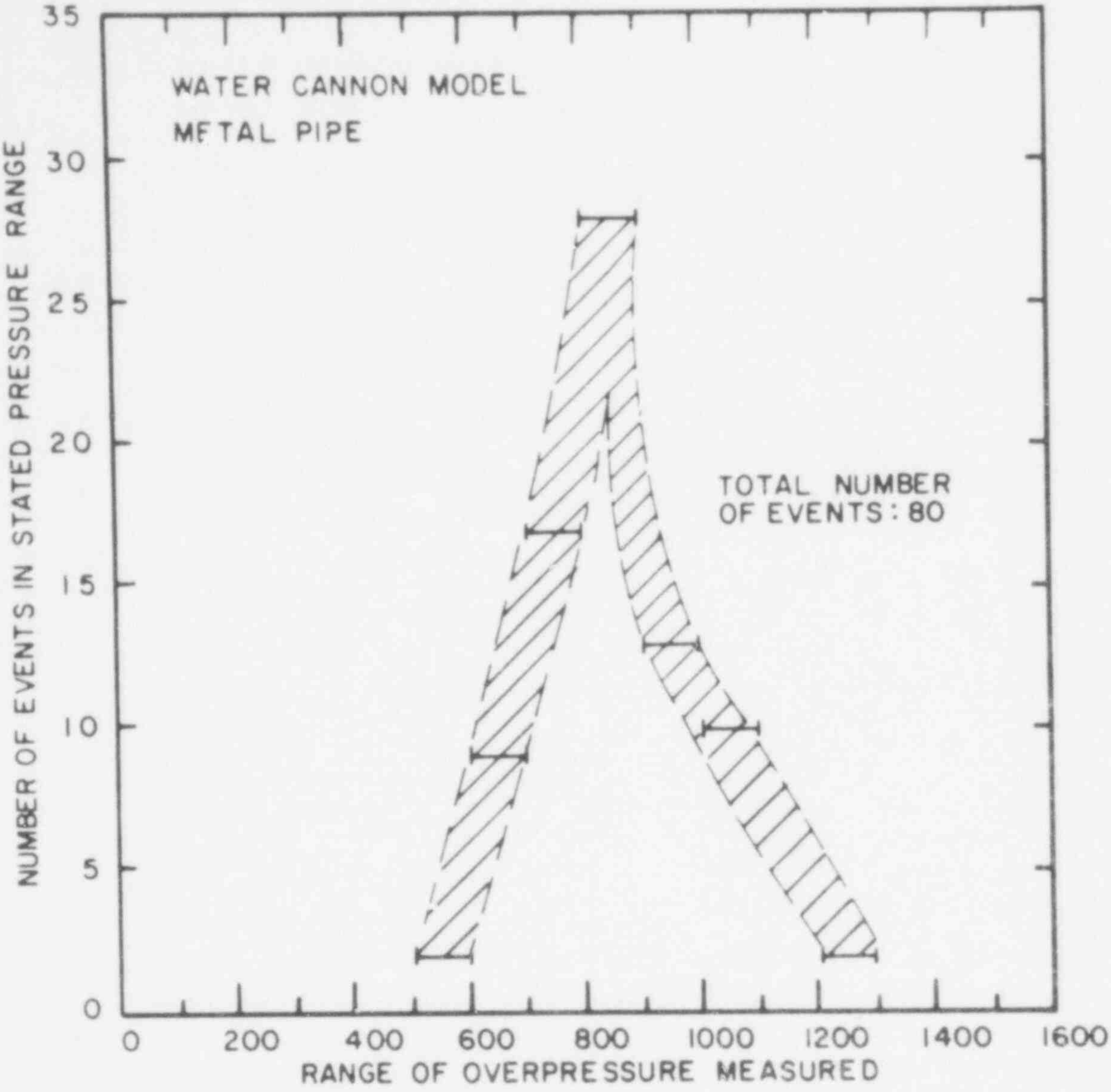

SCATTER OF OVERPRESSURE DATA

FIGURE 12

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Flow Visualization

High speed motion pictures (200-800 frames/sec) were taken in the water cannon model with an acrylic tube. The movies provided a clear visualization of the movement of a slug during an event and allowed the slug velocity to be determined. Simultaneous recordings of the depressurization were made.

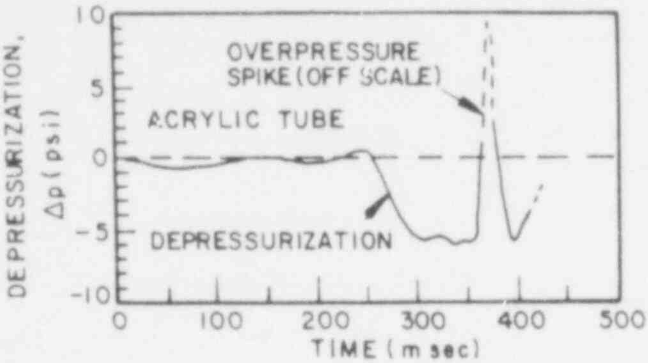
The depressurizations from two events are shown in Figure 13 along with the average velocities of the slug determined by timing the progress of the slug over a one foot length of the tube using a frame-by-frame analysis of the motion pictures. Typical slug velocities were on the order of 20 ft/sec and the velocity was nearly constant as the slug progressed up the tube as well as could be determined from the limited number of frames (about 20). The corresponding depressurizations were of the same magnitude and duration as the examples in Figures 10 and 11.

Variations in slug velocity in different experiments were much larger than the 10% uncertainty in determining slug velocity. Figure 14 shows an atypical trace for the depressurization. The pressure fell sub-ambient by several psi for a time, returned to ambient, and then fell sharply again, quickly followed by the slug impact. The depressurization was thus weaker than in a typical case and it is seen that the slug velocity was less--11 ft/sec rather than 20 ft/sec. So the weaker depressurization is qualitatively consistent with the lower slug velocity. Comparisons in Section 5.3 demonstrate close quantitative agreement as well.

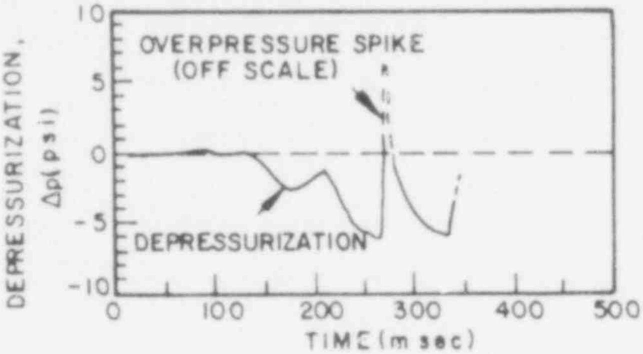
The films also show that the slug interface was not always flat or smooth. Often, splashes which extended one to two inches upward along the walls of the tube preceded the slug interface and, therefore, made the slug interface uncertain by the length of the splashes. This evidence of mixing at the slug interface is best viewed using a motion picture projector.

Pipe Material

During the course of the flow visualization tests it was observed that the magnitude of the overpressure traces was less in the acrylic tube than in the metal pipe. For a typical event it is seen (Figure 15) that the depressurization behavior was about the same with the acrylic tube as that observed with the metal pipe (Figures 10 and 11). On the other hand, the 270 psi overpressure spike was approximately one-third of that recorded for the metal pipe (Figures 10 and 11). Figure 15b shows



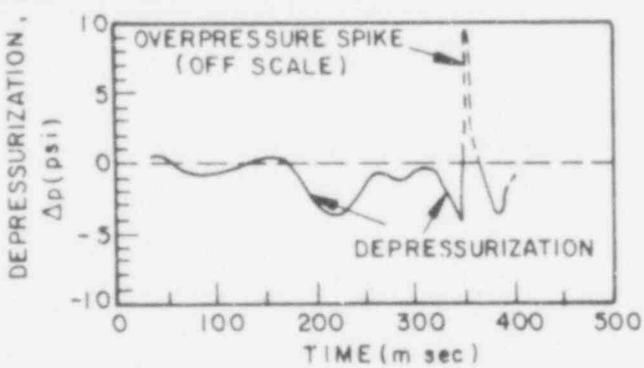
(a) V = 21 ft/sec



(b) V = 22 ft/sec

MEASURED SLUG VELOCITIES AND CORRESPONDING DEPRESSURIZATIONS

FIGURE 13



V = 11 ft/sec

DEPRESSURIZATION IN AN ATYPICAL CASE

FIGURE 14

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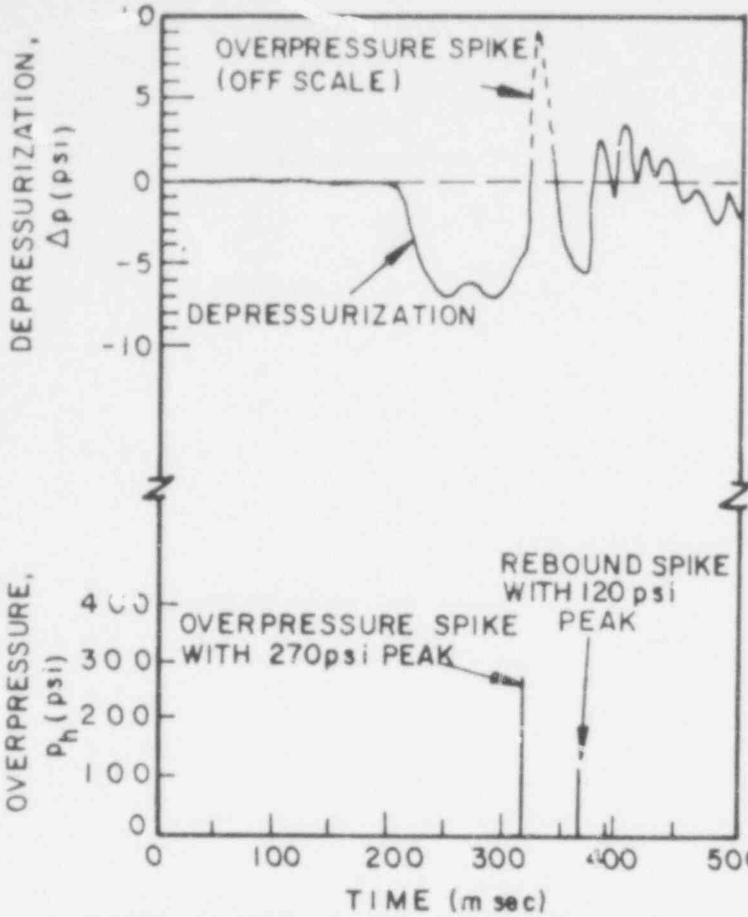
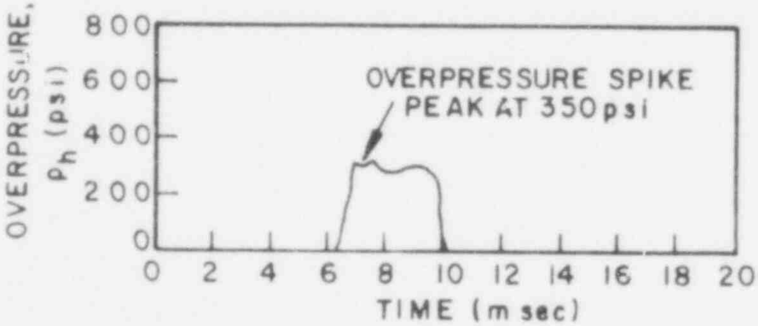


(a) SAMPLE PRESSURE TRACES WITH ACRYLIC TUBE



(b) SAMPLE OVERPRESSURE PULSE WITH ACRYLIC TUBE

FIGURE 15

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that the duration of a typical overpressure spike was 3.5 msec, or over three times longer than for the metal pipe (Figure 11). Thus, the impulse (which is proportional to the integrated area of the overpressure pulse) was approximately unchanged. The reason for this reduction in the overpressure peak amplitude at fixed impulse is due to the relative elasticity of the acrylic material versus steel, as discussed in Section 5.3. Note that the overpressure pulse shapes were roughly rectangular in both the acrylic tube and the metal pipe.

Initiating Mechanisms

Attempts were made to generate stronger overpressure spikes by inducing stronger depressurizations or reducing losses in three ways; 1) by reducing entrance loss in the model for the water column, 2) by inserting a spray nozzle into the model to improve steam condensation, and 3) by decreasing the steam flow into the model at various rates at the time the event was occurring. Each procedure is described briefly below.

In order to minimize losses at the sharp edged inlet end of the pipe (and thus, increase the slug velocity and resultant overpressure at impact), a conical inlet section was attached to the end of the pipe. The magnitudes of the depressurizations and overpressures were observed for a number of events. The depressurizations were not discernably different from those previously described. A plot of the measured overpressures (Figure 16) shows that typical pressures were approximately 1000 psi, and ranged from 500 to 1400 psi. Thus, the most frequent overpressures were slightly higher than without the conical inlet Figure 12), but only about 25% higher, while the overall range remained about the same.

A nozzle which sprayed enough water to condense all of the steam input into the model was inserted into the top of the model. Initiating spray with the nozzle triggered a depressurization and slug motion, but a distinct waterhammer was measured only if the water level was initially at the bottom of the tube. In that case, the slug impact was of the same magnitude as those observed without the spray.

The inlet conditions were also altered by manually decreasing the choked flow rate of steam into the model prior to and during the depressurization. During earlier work, in a 1/20-scale model of a PWR cold leg injection section, overpressures were enhanced in this way. In this model, the magnitude of the overpressure was the same as previously described, even in extreme tests where the steam was turned on and off rapidly.

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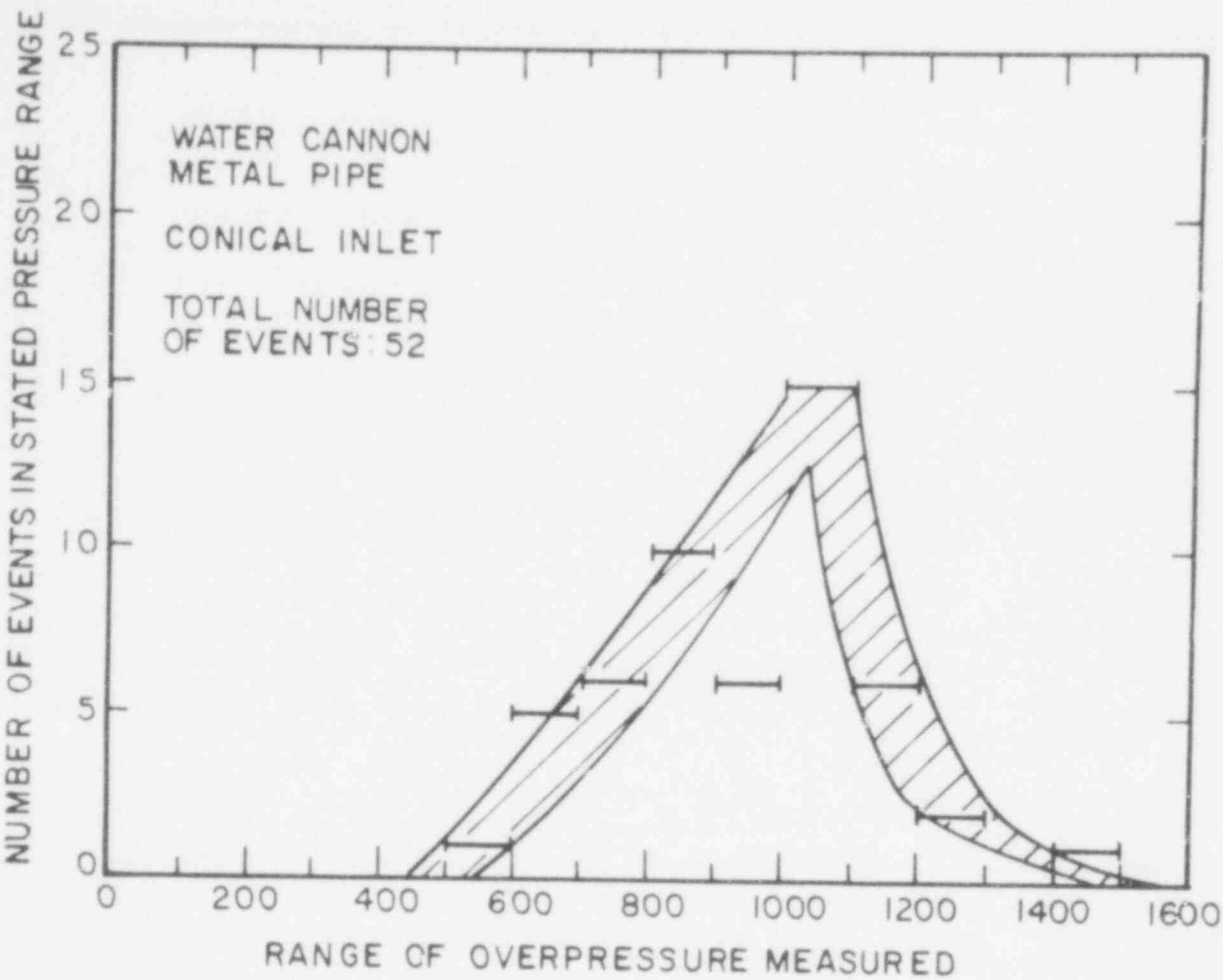

SCATTER OF OVERPRESSURE DATA WITH CONICAL INLET

FIGURE 16

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Direct Conclusions from Water Cannon Model Data

- Typical depressurizations were in the range 7-10 psia over a time period of 100-150 msec.
- The magnitude of the typical overpressure spike was 1000 psi (± 30%) for the metal pipe and 300 psi (± 30%) for the acrylic tube.
- Overpressure pulses were of uniform duration in this model, approximately 1 msec (± 10%) for the metal pipe and 3.5 msec (± 20%) for the acrylic tube.
- Flow visualization motion pictures showed that the typical velocity of the water column was approximately 20 ft/sec in these experiments.

4.2 Steam Generator Model Study

Flow behavior in a 1/10-scale model of the feedwater system in and near to a PWR steam generator was investigated. The reader is reminded that the purpose of this model study was to provide limited confirmation of analytical models under development and to suggest relevant phenomena. Scaling the quantitative results to full scale is unreliable.

The scale model study included acquisition of quantitative data for a range of the main geometric and thermal/hydraulic parameters including feedwater flow rate, feedwater subcooling, vessel pressure, and feedpipe length. The effect of air content was also studied. Phenomenological flow visualization experiments were conducted using various simulated feedwater sparger geometries in order to investigate the process of slug formation. Although most tests were conducted in the bottom discharge configuration typical of operating PWRs, special tests with top discharge were also conducted.

Apparatus

The model (shown in Figure 17) consisted of an existing 12-inch ID steel vessel simulating the steam generator vessel and a segmented length of straight pipe entering the vessel which simulated the feedpipe and the feedring. The baseline simulated feedpipe and sparger geometry used in the model consisted simply of a four foot section of 1.5-inch ID (approximately 1/10 linear scale) transparent acrylic tubing (or 1 1/2

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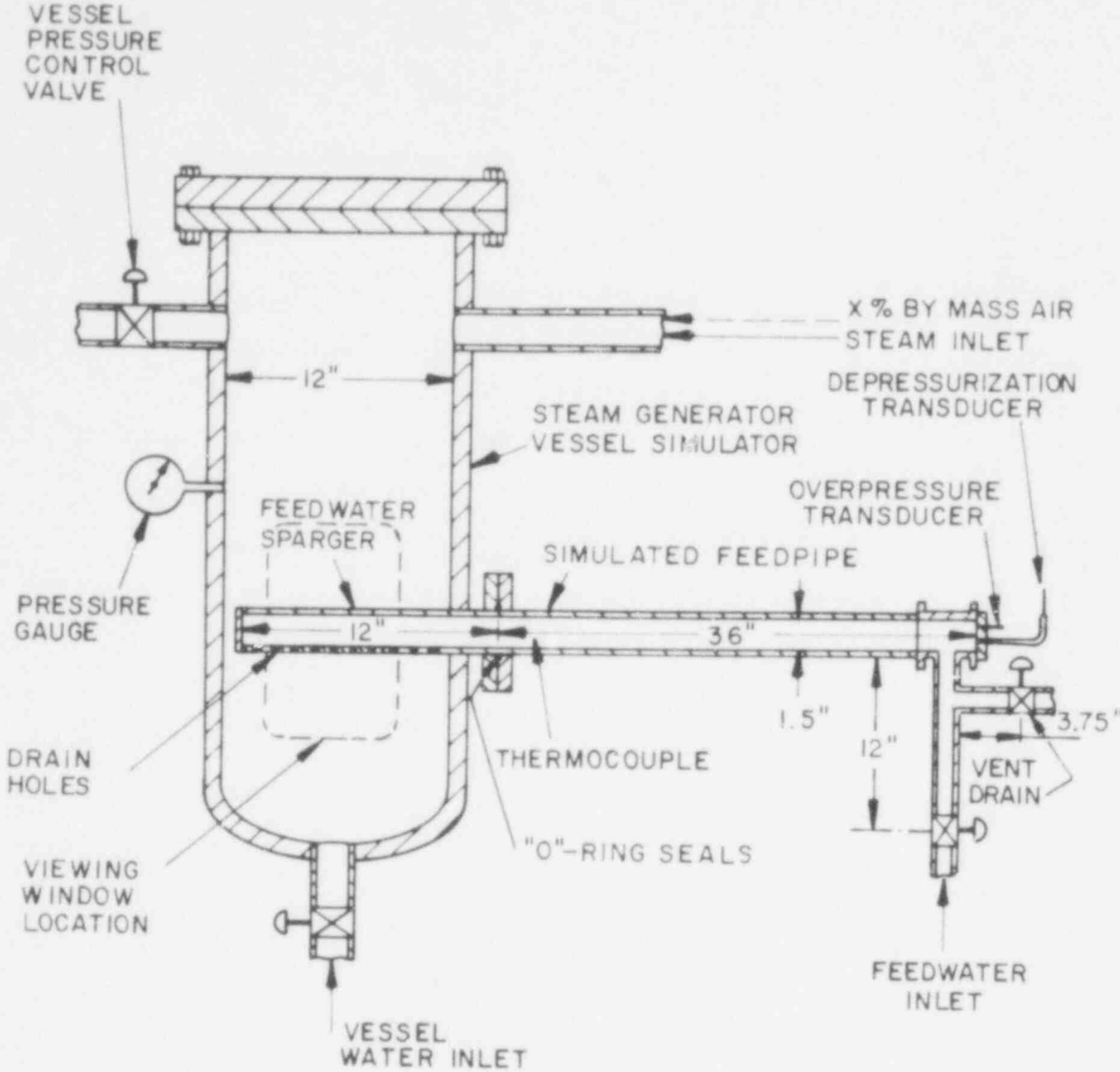

BASELINE STEAM GENERATOR MODEL

FIGURE 17

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inch sch. 40 steel pipe) with ten 0.375-inch diameter downward-facing holes spaced on one inch centers.* The end of the tube in the steam generator was capped. Nearly a one foot length of the tube was inside and the remaining three feet were outside of the vessel. Viewing windows on the vessel were employed for flow visualization with vessel pressure near ambient pressure.

This basic geometry was tested over a broad range of operating parameters. The geometry of the feedpipe and sparger sections could be changed easily in a variety of ways, and several other configurations were studied, although generally over a more restricted range of the operating parameters than was examined in the baseline configuration. The changes consisted of the number and size of the drain holes, the feedpipe length, the orientation of the drain holes, and the feedpipe material (acrylic or steel).

Water was introduced at a metered rate into the bottom of the simulated feedpipe at the far end from the vessel. The water level in the vessel itself could also be raised by injecting water directly into the bottom of the vessel at a known rate. (This could be done independently of the feedpipe water flow.) The water was supplied from an open 300 gallon trough maintained at desired temperatures in the range 65 - 205°F by heating with bypassed steam. A known, choked flow of steam was introduced near the top of the vessel and a valve on the discharge from the vessel was used to control the vessel pressure. This steam flow rate was ample to maintain the pressure at the desired value when water was introduced and provided a convenient means to purge the system of noncondensible gases. If desired, air could be mixed with the steam at a controlled rate.

As in the water cannon model, the primary measurements were the pressure history in the feedpipe, and the overpressure due to a slug impact. The same instrumentation was employed. The pressure transducers were mounted in the capped end of the feedpipe (away from the vessel) and the output was

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*The main rationale for this sparger hole configuration was to approximate the ratio of total hole area to cross-sectional area of feedpipe typical of PWRs. This area ratio impacts the hydraulics directly to first order. The area ratio ranges from 60-90% in PWRs and was 62% in these experiments. Fewer holes were used, and these were individually much larger than linearly scaled holes in order to avoid extreme surface tension effects at the very small scale of these experiments. A straight pipe of constant area was used for simplicity. This configuration should in no way be construed as "realistic", although it is a reasonable idealization that is useful to examine the fundamental flow behavior.

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again recorded on an oscilloscope and by photographs. Some temperature measurements were also made with a thermocouple located in the bottom of the feedpipe, near the vessel, as shown in Figure 17.

Experimental Conditions With Steam Generator Model

The baseline configuration and the baseline experimental conditions are listed in Table 6. The following Subsection 4.3 presents qualitative observations and quantitative data obtained with the baseline configuration and baseline experimental conditions. In some of these experiments, waterhammer events occurred. These tests have been separated for discussion in order to clarify test procedures and observations that are common to all of the later results. The remaining test conditions are summarized below in the order in which the tests are described in successive subsections of the report.

Section 4.4 contains the results from parametric studies made in the baseline configuration. These include:

- water flow rates of 0.52 to 6.2 gpm,
- feedwater temperatures from 65°F to 190°F,
- pipe material either acrylic tube or metal pipe,
- vessel pressure from 16.5 psia to 75 psia, and
- noncondensibles with 0.05-2% by mass of air mixed with the steam entering the vessel, and
- steam generator vessel water level rise rates of 2 to 96 in/min.

Section 4.5 describes experiments conducted to observe the process of slug formation in our facility (with flow visualization) using various sparger geometries. The geometry changes consisted of the number or size of the drain holes:

- twenty 0.375-inch diameter holes,
- ten 0.750-inch diameter holes, and
- an open-ended pipe without drain holes.

TABLE 6

STEAM GENERATOR MODEL BASELINE CONFIGURATION AND EXPERIMENTAL CONDITIONS

BASELINE CONFIGURATION	
Feedpipe Length	4.0 feet
Feedpipe ID	1.5 inches
Drain Hole Diameter	0.375 inches
Drain Hole Position	DOWN
Number of Drain Holes	10
Pipe Thickness	0.125 inches
BASELINE PARAMETERS	
Feedpipe Water Flow	VARIABLE
Water Temperature T_f,	$65 \pm 2°F$
Vessel Pressure, p_o	16.5 ± 0.5 psia
Air Addition Rate	ZERO
Vessel Water Flow	NONE
Vessel Water Level	Well Below Feedring

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Finally, Section 4.6 presents the results from experiments in which the recommended vendor hardware-modification recommendations were modeled. The modifications scoped were:

- Top-discharge of drain holes,
- Shortened feedpipe length, and
- Top-discharge with a shortened feedpipe length.

4.3 Description of Baseline Test Results

Flow Patterns

Preliminary experiments were conducted to observe the flow patterns in the baseline configuration first in air, without any steam input into the model. The depth of the liquid in the tube and the number of holes observed to be draining were recorded as a function of the water flow rate, and are displayed in Figures 18 and 19. At a flow rate of 2 gpm, all ten holes drained. The water level in the tube increased steadily with increased flow rate.

During each of the baseline experiments, after steam was introduced into the model, the vessel pressure was set to 2 psig by closing the vent drain and adjusting the valve on the steam exit as required. The pressure remained within 0.5 psi of this value throughout the test. Then, water flow was initiated into the feedpipe at a preset rate. (The water level in the vessel remained well below the feedring at all times.) In the transparant model, a water layer was observed to propagate slowly down the feedpipe to the sparger section. Some time after the lead edge of the water layer reached the sparger and began flowing over the drain holes, a loud noise was heard and water was observed to be piled up at the water-inlet end of the feedpipe. After the first noise, the water layer washed down again and the cycle repeated so that a succession of similar noises was heard (with a period of two to three seconds) and the water was observed to fill a greater and greater length of the feedpipe as this happened. The noises ceased when the pipe became full.

More detailed observations of the flow patterns in the sparger were made and are illustrated in Figure 20. At low water flow rates (less than about 0.70 gpm) only three holes or less in the straight sparger section drained water and the flow was quiescent and steady as sketched in Figure 20a. When the water flow was increased such that four holes would

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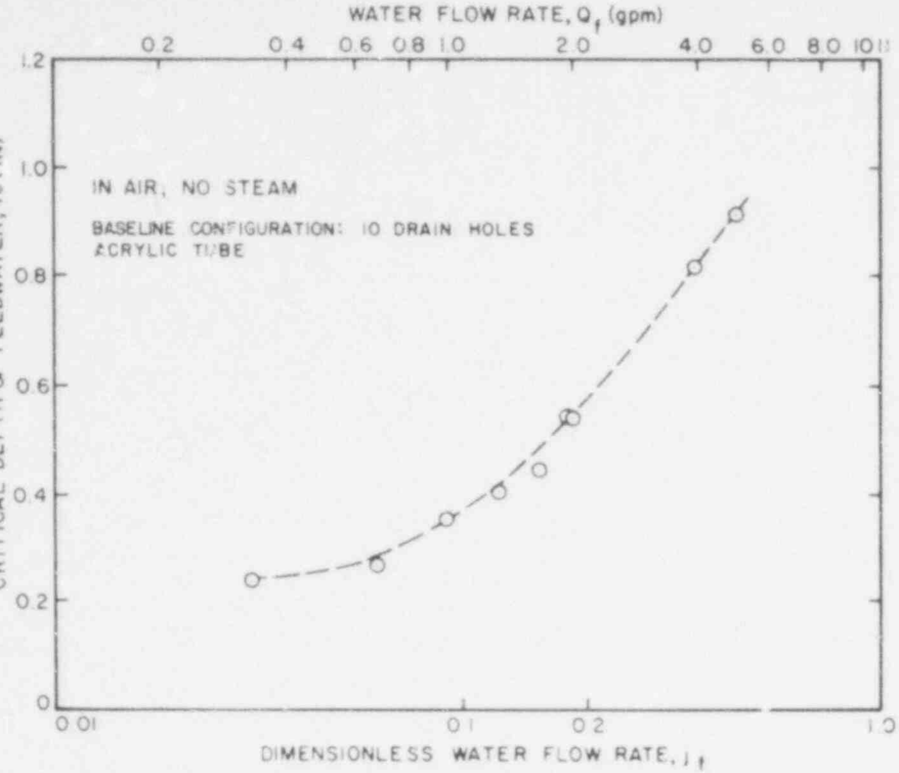
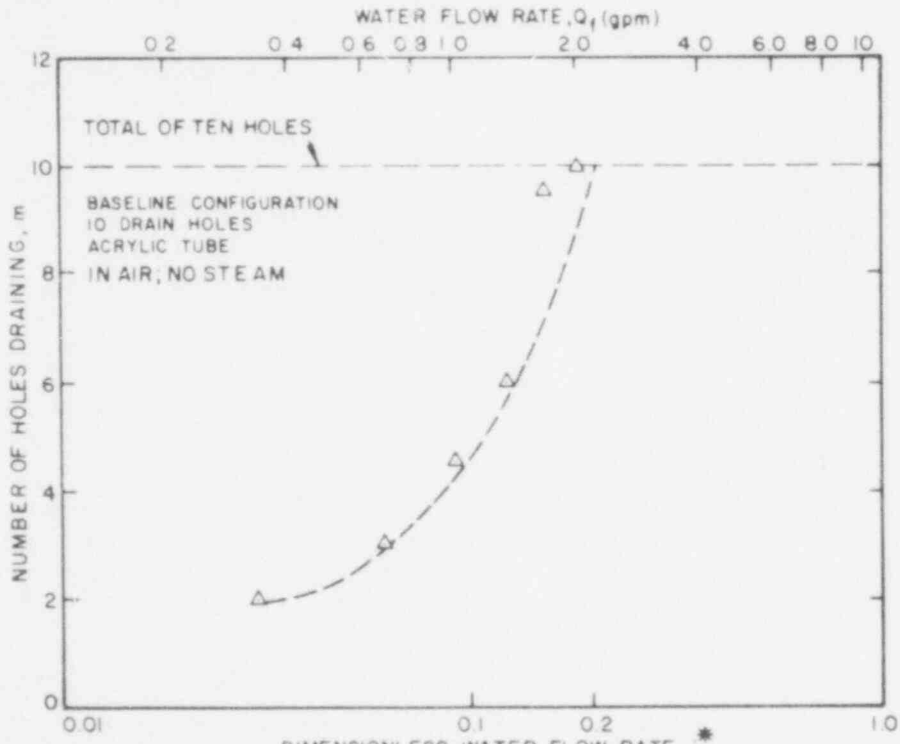


CRITICAL DEPTH OF FEEDWATER VS. WATER FLOW RATE

FIGURE 18



NUMBER OF HOLES DRAINING VS. WATER FLOW RATE

FIGURE 19

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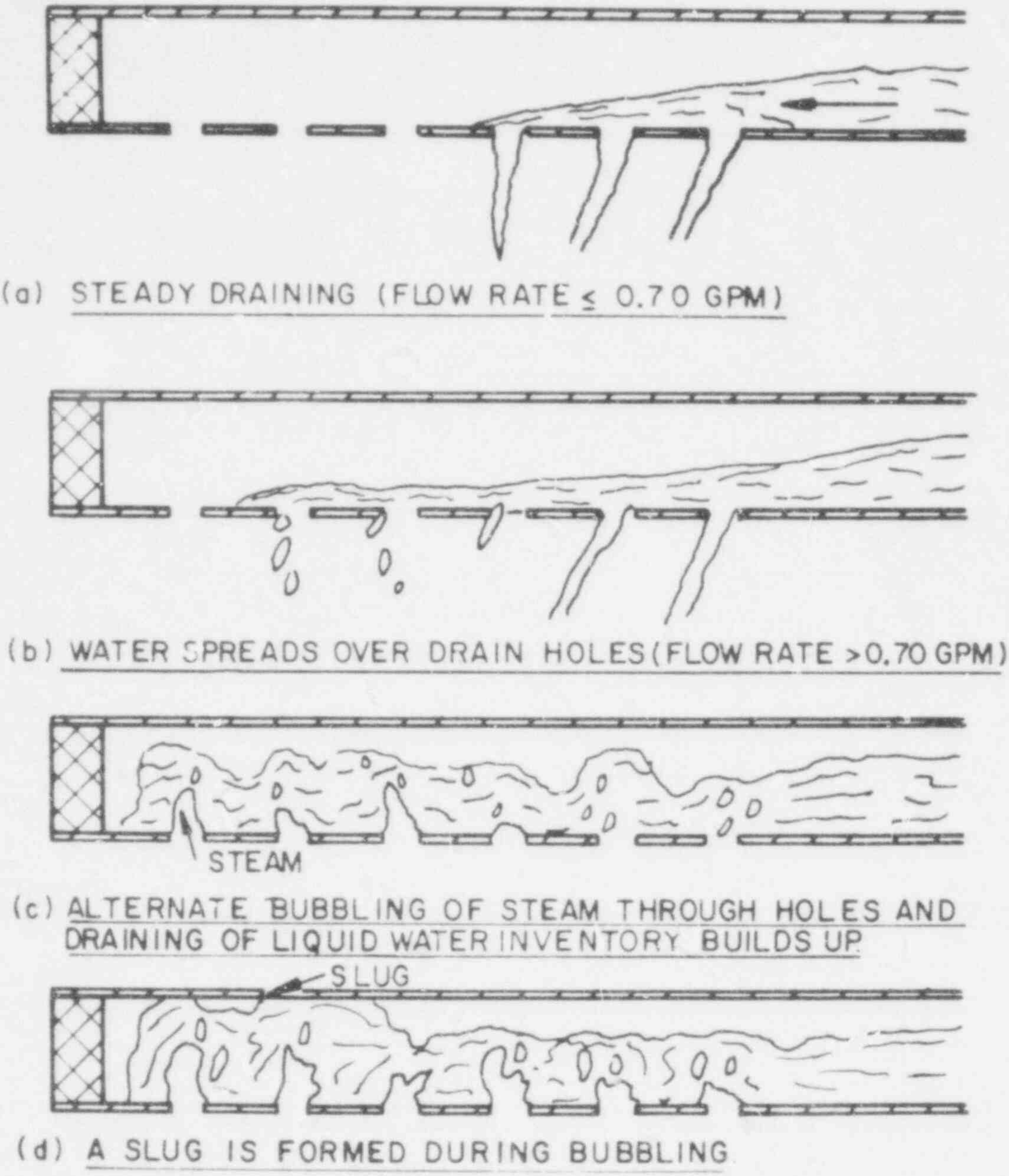

SKETCHES ILLUSTRATING SLUG FORMATION PROCESS IN STEAM GENERATOR MODEL

FIGURE 20

POOR ORIGINAL

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drain, the water began to spread along the bottom of the tube (Figure 20b). Eventually, the water covered all of the holes and steam began to bubble up through some of the holes (Figure 20c). This bubbling of the steam through the holes prevented the water from draining through them. Sometimes the bubbling stopped. The bubbling also caused mild waves to travel up the length of feedpipe outside of the vessel. Oscillations between bubbling and draining were observed (with all holes in one mode or the other) until the bubbling led to formation of a slug in the tube (in the region near the drain holes, Figure 20d), and a waterhammer event occurred rapidly thereafter. This "percolation" before a slug was formed lasted several seconds at low flow rates and occurred very quickly at high flow rates.

Pressure Traces for Baseline Configuration

Figure 21 shows typical pressure traces obtained with the baseline configuration. The precursor depressurization (upper trace of Figure 21a) reached 8 psi in a time period of about 100 msec. The overpressure spike (lower trace in Figure 21a) was about 300 psi for the first slug impact. The rebound spike 40 msec later was about 80 psi.

Figure 20b shows a time-expanded trace of a typical overpressure pulse. The pulse lasted about 1.5 msec ($\pm$ 30%), and the overpressure p_h was 300 psi. The magnitudes of this overpressure spike was in the same range as those observed in the water cannon model with an acrylic tube (Figure 15). However, the duration of the pulse was approximately one-half of the time in the water cannon model, indicating that the slug in the steam generator model was shorter than the water column in the water cannon. The depressurization behavior was similar in both models. The shape of the overpressure pulse was somewhat less rectangular than comparable pulses recorded with the water cannon model (Figure 11). We did not attempt to isolate specific mechanisms for the departure from an ideal rectangular pulse although experience with the water cannon oriented at a small angle relative to the vertical direction implicates slug interface geometry as a likely cause.* Alternatively, the delayed collapse of bubbles trapped by the water lying initially on the bottom of the pipe--an effect not present in the water cannon--is quite plausible.

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*Experiments with the water cannon oriented 15° to the vertical direction gave pulses of shape similar to those in Figure 21. The overpressure peak was statistically the same as with a vertical orientation, however.

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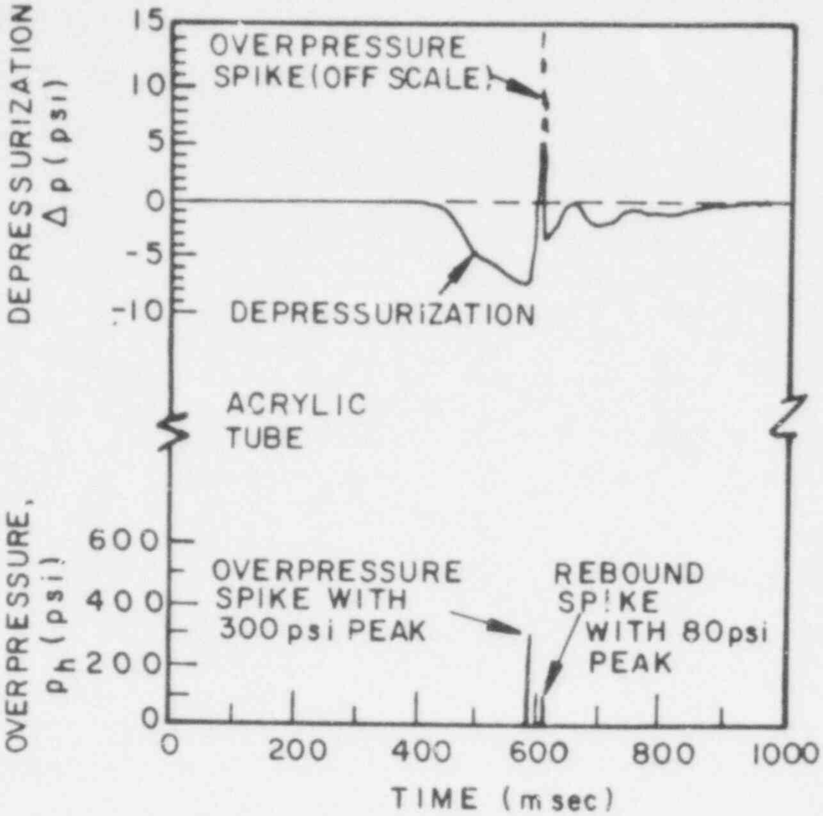
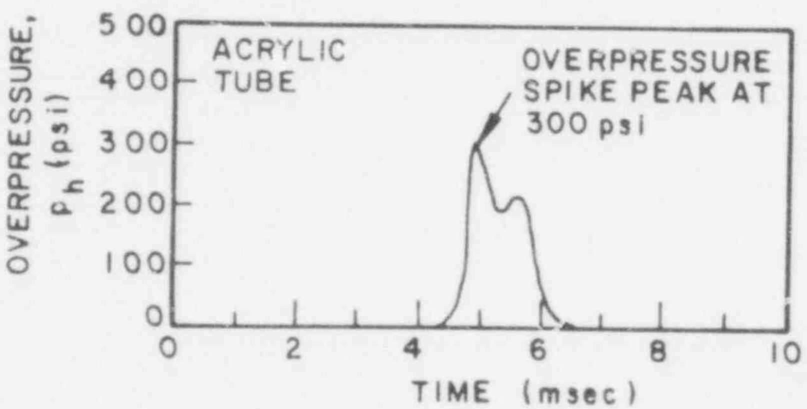


(a) SAMPLE PRESSURE TRACES IN BASELINE CONFIGURATION



(b) SAMPLE OVERPRESSURE PULSE IN BASELINE CONFIGURATION

FIGURE 21

POOR ORIGINAL

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Despite second-order differences, the depressurization and overpressure records obtained in the steam generator model with baseline experimental conditions are quite similar to the records obtained with the water cannon. The water cannon behavior provides a more realistic standard for evaluating the steam generator model data than would an idealized press re history with a complete depressurization of 0 psia followed by a sharp-edged rectangular pulse at the time of impact.

Temperature Traces for Baseline Configuration

Sample temperature traces from the thermocouple in the feedpipe are presented in Figure 22. The thermocouple location is shown in Figure 17; it penetrated 0.25 inches into the feedpipe from the bottom. The behavior at several water flow rates is shown. At 0.63 gpm, no waterhammer event occurred, and as seen in Figure 22a, the temperature gradually dropped from the steam temperature to about 130°F. (The inlet water was at 67°F.) At 0.76 gpm, a waterhammer event did occur and it is seen that the temperature decreased to 100°F but then oscillated between the steam temperature and some water temperature as the succession of waterhammer events took place (as the feedpipe filled with water). Eventually, the water warmed up, the tube filled with water, and the behavior stopped. At 1.89 gpm, nearly the same behavior is seen.

4.4 Parametric Effects, Baseline Configuration

In this section the effects of varying the parameters of feedwater flow rate, water temperature, pipe material, vessel pressure, and the amount of noncondensibles in the baseline configuration are discussed.

Feedwater Flow Rate: Baseline Configuration

A flow rate of approximately 0.70 gpm (± 10%) was determined to be a threshold value for slug formation and subsequent impact for cold (67°F) water in this model. Waterhammer events consistently occurred at flow rates that were higher and were not observed during any tests at flow rates that were lower. In addition to the audible and visible evidence of slug impact, overpressure measurements were evidence of slug impacts. Some typical results are presented in Figure 23, where it is shown that overpressures did not occur below about 0.70 gpm but did occur above it. Again at 4.84 gpm (± 10%) no overpressure measurements were observed. So, there was also an upper threshold on the water flow rate,

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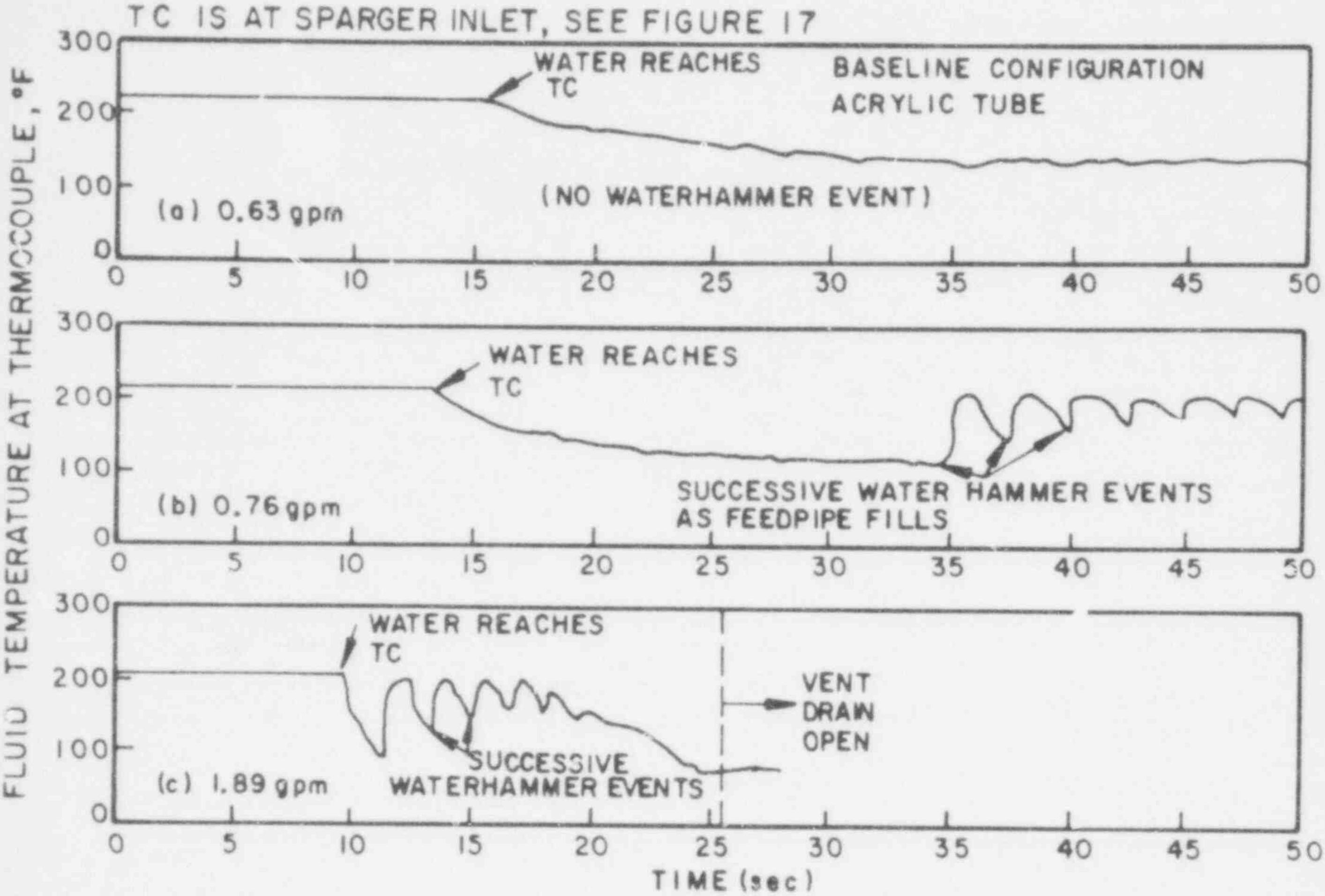

TEMPERATURE TRACES FOR VARIOUS WATER FLOW RATES

FIGURE 22

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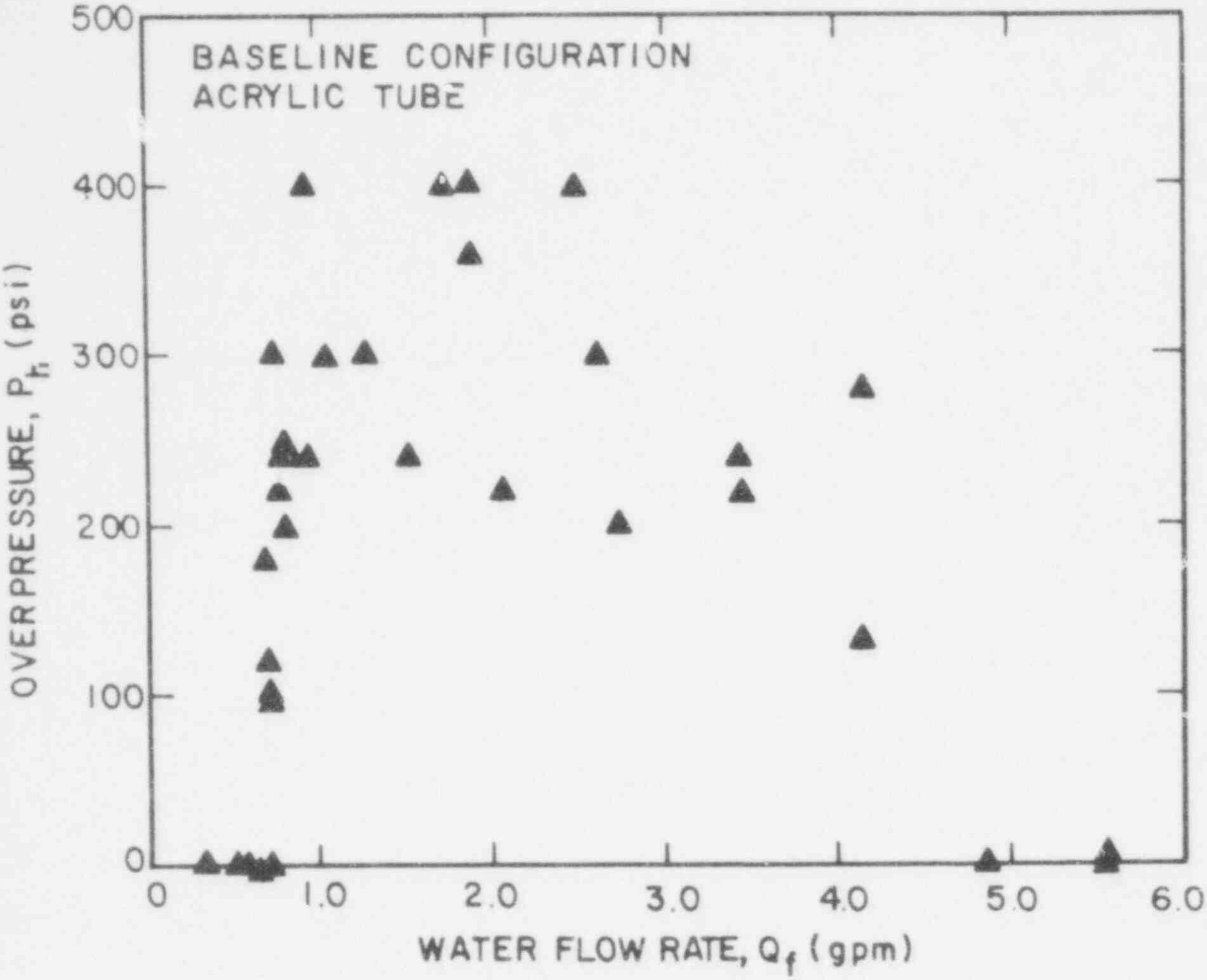

OVERPRESSURE FOR VARIOUS WATER FLOW RATES

FIGURE 23

corresponding to flow rates at which the pipe ran nearly full of water, and thus a slug did not form. Subsequent work emphasized the low flow threshold because the mechanism of the high flow threshold is relatively well understood.

Overpressure spikes typically ranging from 200-400 psi were measured during the experiments conducted at intermediate water flow rates. The depressurization traces and the overpressure spikes showed the same magnitude at the different flow rates within the considerable scatter of the data. The series of traces in Figure 24 illustrates this. So, the only perceptible effect of water flow rate was the observed threshold water flow rates for waterhammer events to occur only between 0.70 and 4.8 gpm. The scatter of the overpressure data entirely obscures any possible trend as a function of water flow rate in this range.

Water Temperature: Baseline Configuration

The dependence of the water flow rate threshold (at low water flows) on the temperature of the injected water was studied in this model. Water temperatures of 120, 160, and 190°F were investigated in addition to the above tests at 67°F. The results are summarized in Figure 25. The lowest water flow rate at which waterhammer events occurred was similar with water temperatures of 67°F, 120°F and 160°F, but was somewhat higher at 190°F (up to 1.1 gpm rather than 0.7-0.8 gpm). The high flow threshold is not shown. The bars on the graph indicate the transitional range of water flows at each temperature where the behavior was not consistently "waterhammer" or "no waterhammer". Near the low flow threshold at each elevated temperature, the same number of drain holes (four) were covered at 67°F. At 190°F, up to one more hole (five) was observed to be draining steadily without waterhammer.

Both the overpressure and depressurization characteristics were roughly independent of water temperature in the range 67°F to 190°F. Figure 26 shows sample traces at 2.52 gpm with 160°F water which may be compared with Figures 24. The depressurization amplitude was 4 psi over 100 msec and the overpressure spike was 260 psi. These values are similar to those of Figure 24. The time-expanded trace of the overpressure pulse (Figure 26b) shows that the shape and duration of the overpressure pulse was also similar to the shape and duration at 67°F (Figure 21).

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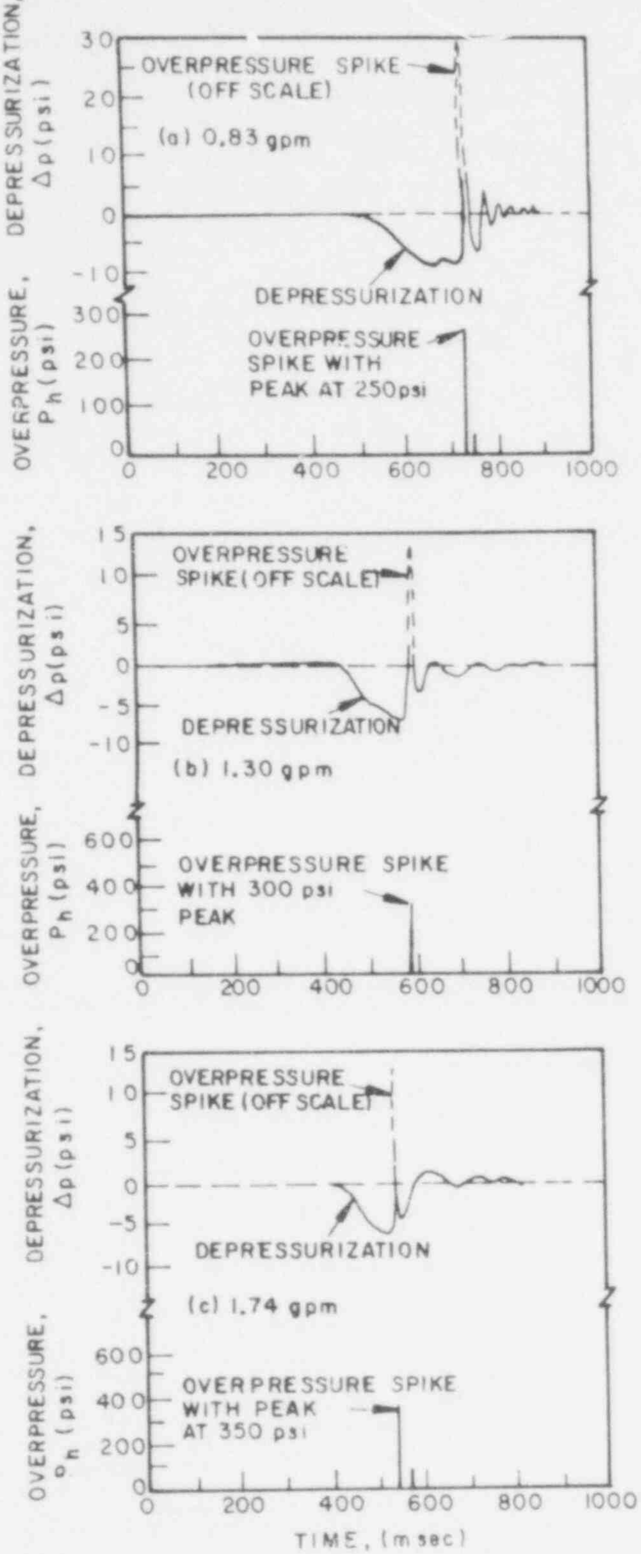

SAMPLE PRESSURE TRACES VS WATER FLOW RATE IN BASELINE CONFIGURATION, ACRYLIC TUBE

FIGURE 24

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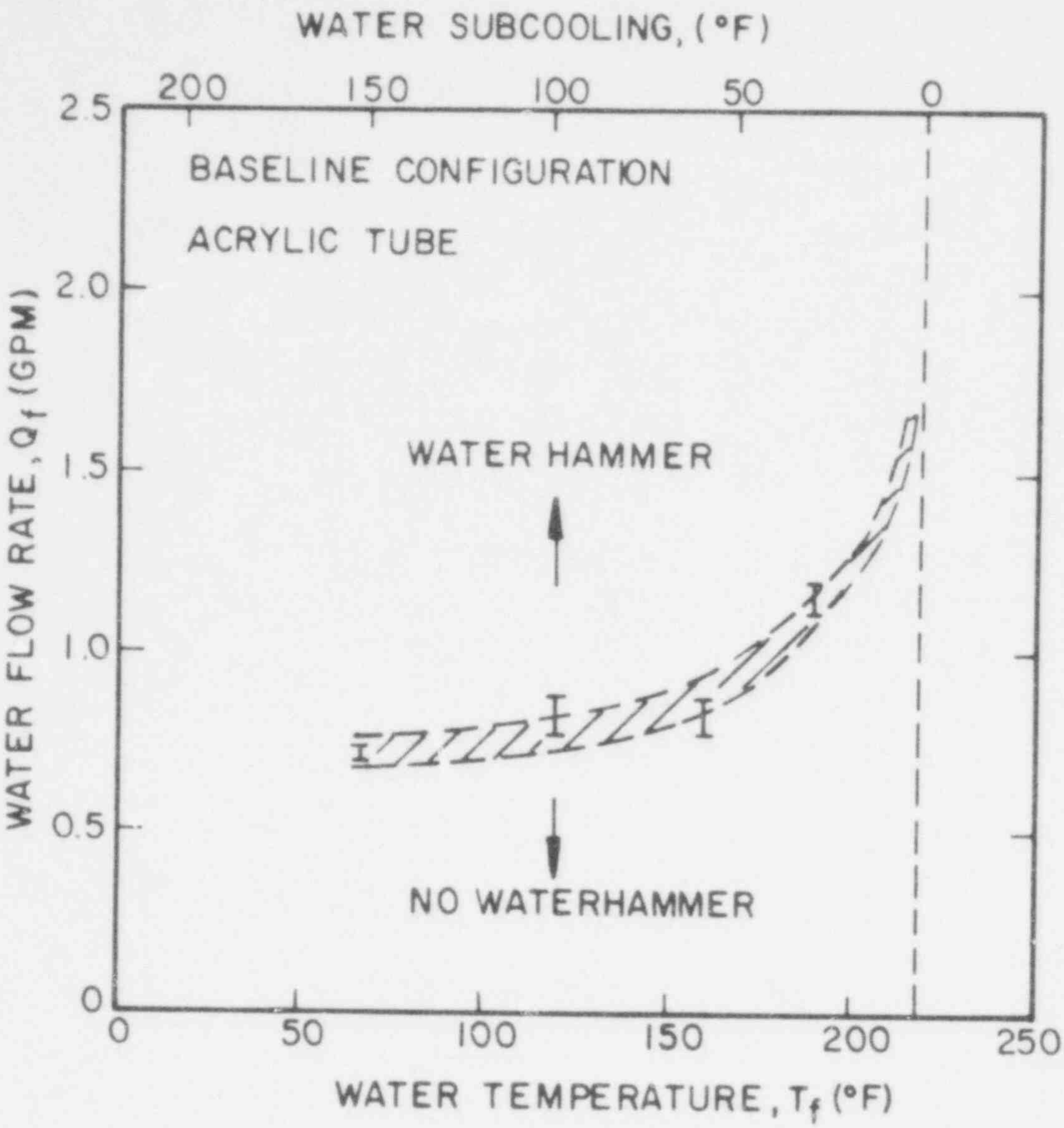

THRESHOLD WATER FLOW RATE VS WATER TEMPERATURE

FIGURE 25

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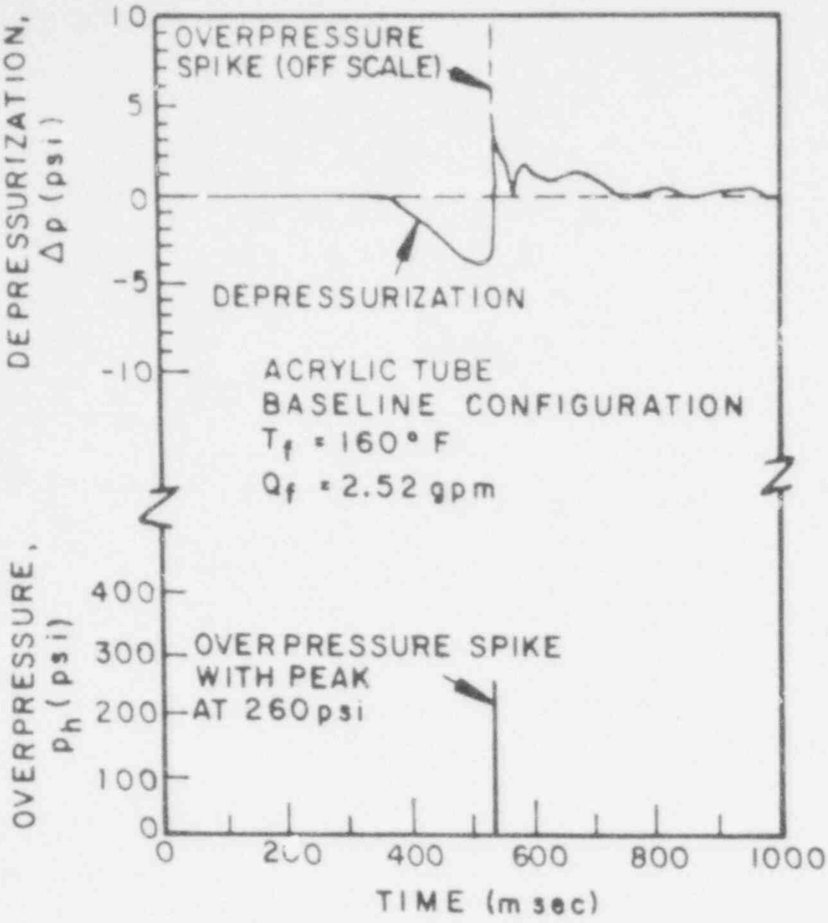
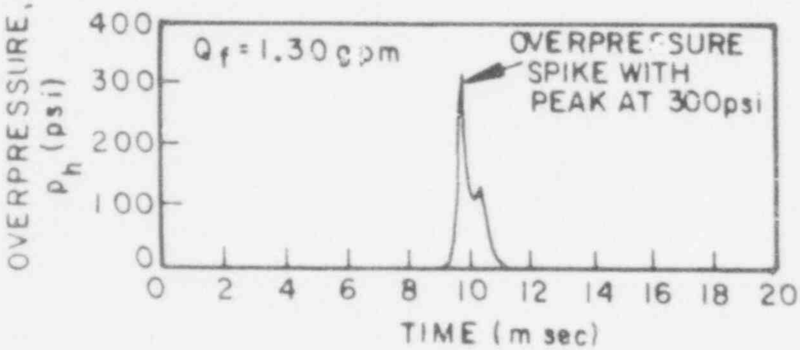


(a) SAMPLE PRESSURE TRACES WITH 160° F WATER



(b) SAMPLE OVERPRESSURE PULSE WITH 160° F WATER

FIGURE 26

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POOR ORIGINAL

The conclusion then is that there was not much of an effect of water temperature (subcooling) on the threshold water flow rate in the baseline configuration, except perhaps very near to saturation temperature, and further that the characteristics of waterhammer such as overpressure were largely independent of water temperature in the range tested.

Pipe Material: Baseline Configuration

Most of the studies in the steam generator model were performed with the acrylic pipe, since the transparent plastic permitted flow visualization. In Section 4.1 it was shown that the pipe material influenced the results in the water cannon model. Specifically, overpressure magnitudes were reduced by a factor of three, and the duration of the overpressure pulses was increased by a factor of three in the acrylic tube. To run elevated pressure tests (the topic following this one) a metal pipe was required. Therefore, experiments were run in the baseline configuration with a metal pipe in addition to those reported above with the acrylic tube.

Overpressure measurements were made at several water flow rates (T_f = 65°F) and the results are shown in Figure 27 in comparison with the results from the acrylic tube. The low flow threshold was near 0.70 gpm with both materials. It is seen that overpressures were less than 100 psi at the threshold water flow rate (in the acrylic tube) of 0.70 gpm. Above this flow rate, overpressure ranging from 150-700 psi were measured. The overpressure measurements in the steel pipe were on the average, larger than in the acrylic tube by a factor of approximately two.

Figure 27 illustrates the gross data scatter in the overpressure measurements. Single sample experiments are clearly unreliable, even in a research facility.

Sample traces of the depressurization characteristics and the corresponding time-expanded traces of the overpressure pulses (obtained simulataneously) are shown in Figure 28. The traces are for various water flow rates. The depressurizations (left) reached 6 to 10 psi over a time period of about 150 msec, as for the acrylic tube. The overpressure pulses (right) had a magnitude of 400-700 psi. The duration of the pulses ranged from 0.5-1.0 msec, a factor of two to three shorter than with the acrylic tube, as expected. The shape of the pulses was

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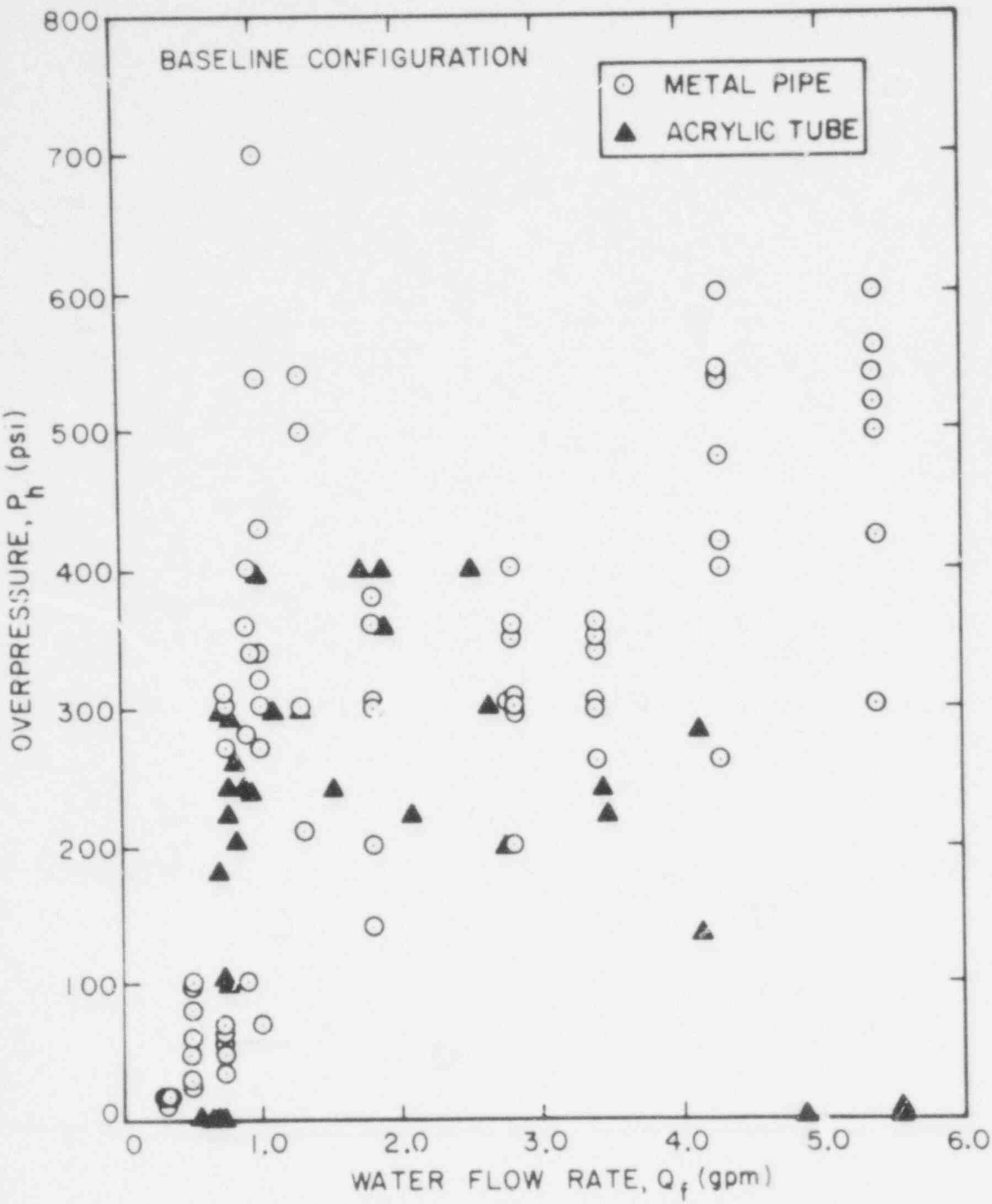

OVERPRESSURE FOR VARIOUS WATER FLOW RATES

FIGURE 27

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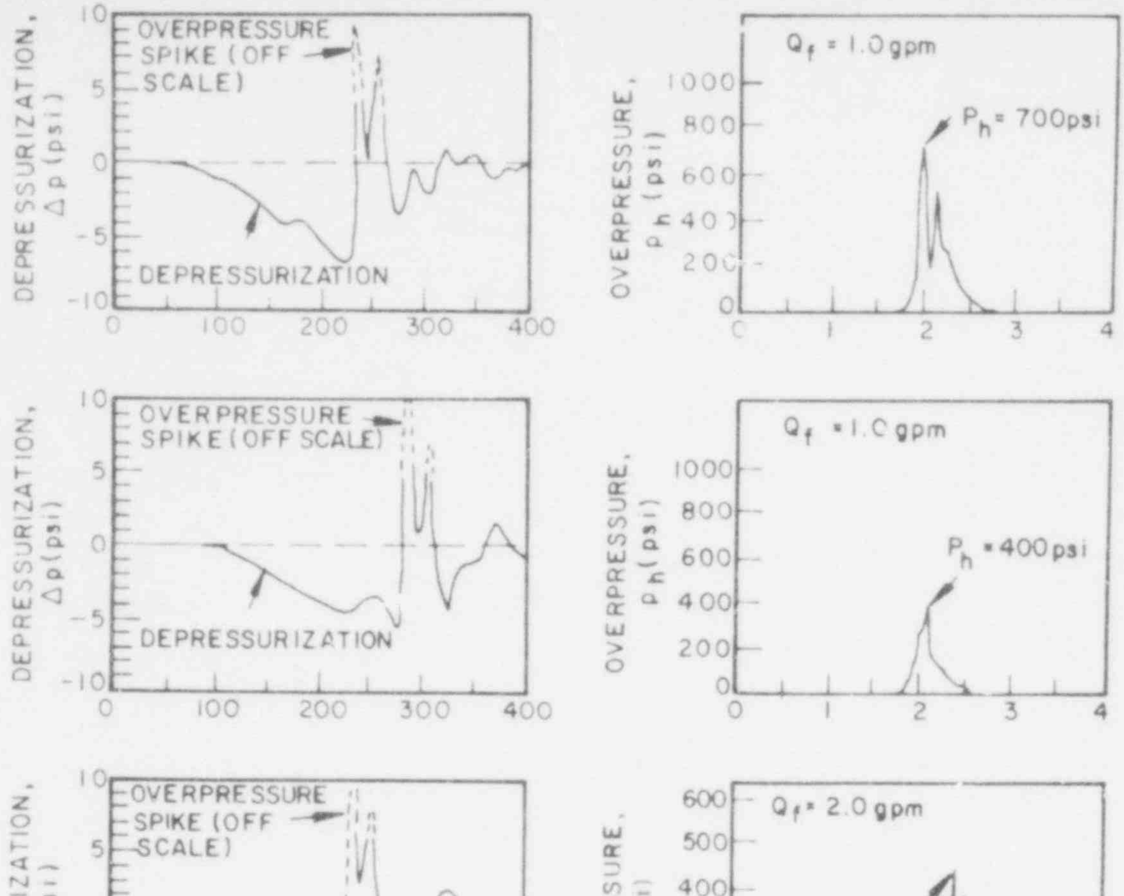

SIMULTANEOUS TRACES IN BASELINE CONFIGURATION (METAL PIPE)

FIGURE 28

POOR ORIGINAL

729 256

triangular and they had a very ragged appearance compared with the earlier traces. Again, the ragged appearance of the pulses is due to factors that were not isolated in these experiments, but in view of use of the same instrumentation on similar vertical piping, such factors as the slug interface and the collapse of bubbles trapped by the water lying on the bottom of the pipe are implicated.

These and additional depressurization and overpressure pulse traces are examined in Section 5.3, where they are compared with the analysis which has been developed.

Pressure: Baseline Configuration

Several experiments were performed at elevated pressure in the baseline configuration (with a metal feedpipe) and 65°F water injected. Overpressure measurements are compared with those made at ambient pressure in Figure 29. Nine experiments at 75 psia indicated generally higher overpressures than at ambient vessel pressures. These overpressures at elevated pressure are seen to be about a factor of two higher than at ambient pressure. To first order, the analyses of Section 5.3 suggest that overpressure should be proportional to the square root of system pressure and this model is consistent with the data. The large amount of scatter in the data makes quantifying such conclusions nebulous. Two tests at 35 psia also showed higher overpressures than in the tests at ambient pressure. The shapes of the overpressure pulses as recorded were very similar to the shapes of the pulses at ambient pressure shown in Figure 28--only the magnitudes of the overpressures were larger.

Effects of Noncondensibles: Baseline Configuration

The effect of noncondensibles was studied in the baseline configuration (acrylic feedpipe) by the addition of air mixed with the input steam flow to the model. Except for the air addition, the test procedures were the same as described above. Whenever the air flow was changed, it was first turned off and the system purged with steam, then the new air flow rate was set and the test was run after waiting several minutes. The steam flow into the model in these tests was 0.016 lb/sec and the air addition ranged from 0.05-2.0% by mass. The air would be expected to collect near points where condensation occurred and the local air concentration might be much higher than the input concentration. Direct measurements of local air content were not made and only the qualitative effect of adding air to the inlet steam was studied. Typical results are described below.

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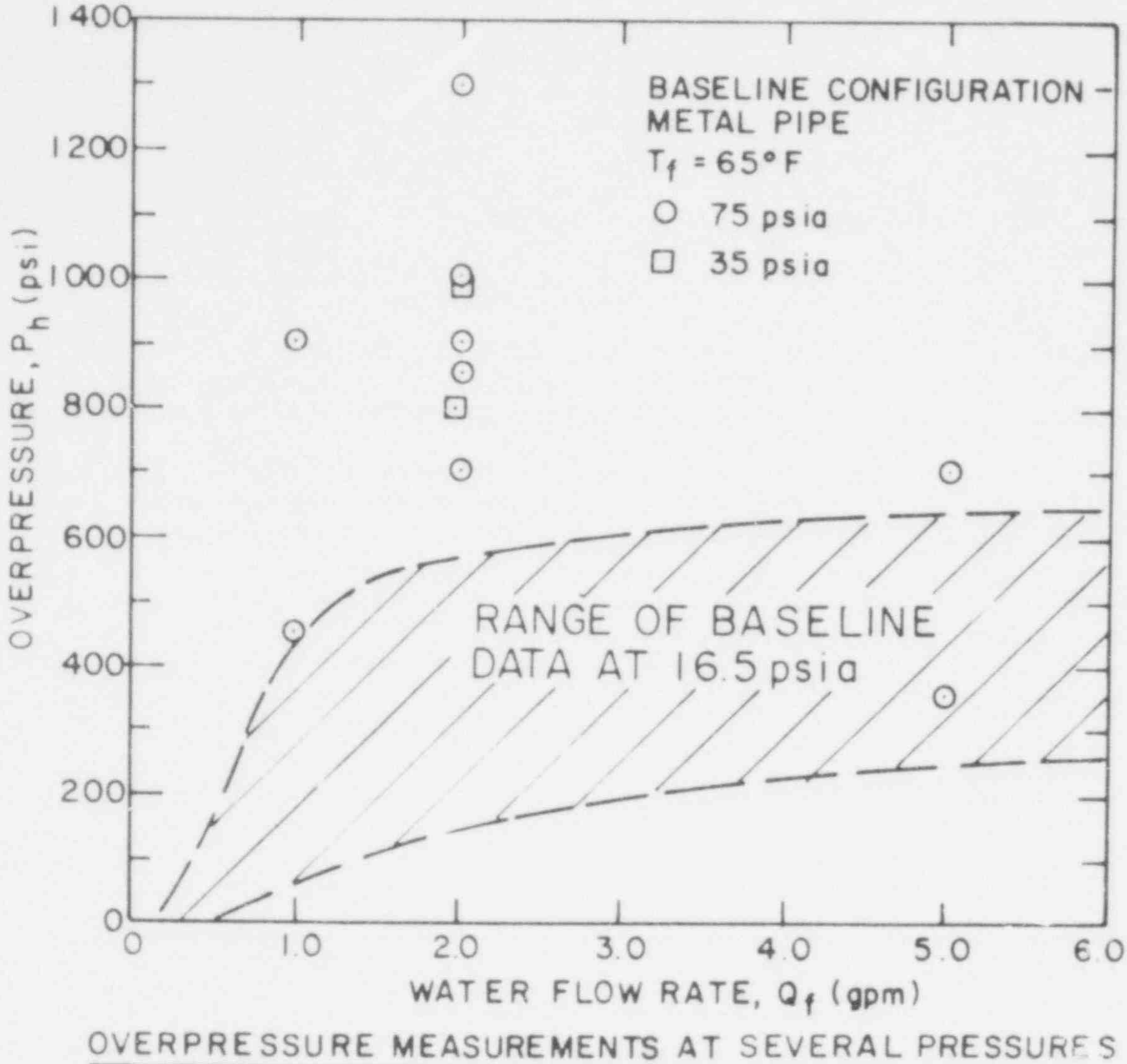


OVERPRESSURE MEASUREMENTS AT SEVERAL PRESSURES

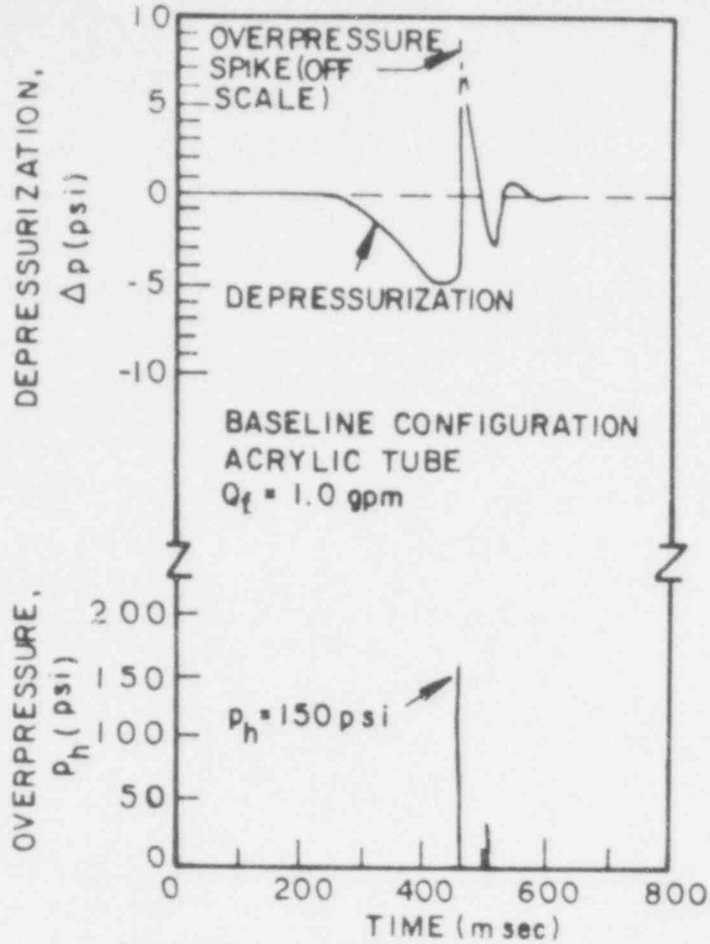
FIGURE 29

POOR ORIGINAL

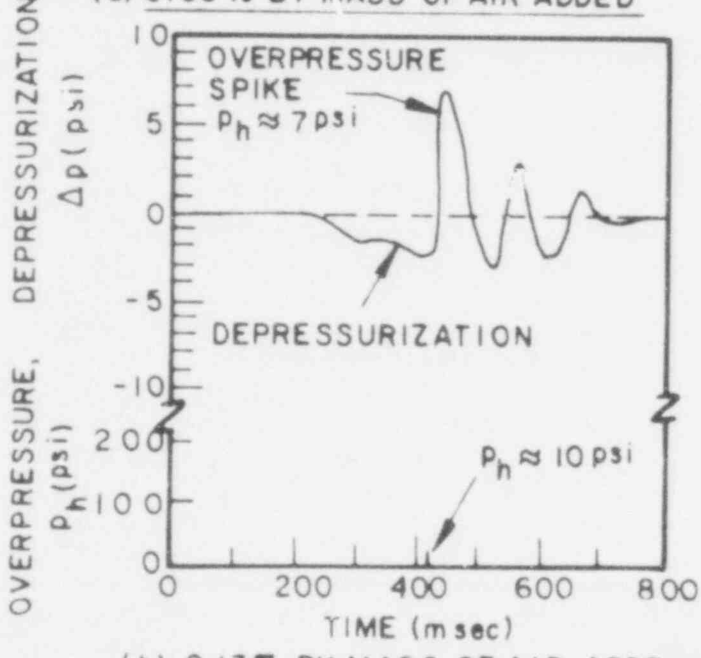
At 0.05% air addition, the results were somewhat lower than with no air addition (compare Figure 30a with Figure 21a). The depressurization was 5 psi over 100 msec and the overpressure spike was about 150 psi, or perhaps a factor of two less than with no air addition, but within the scatter of the latter data. Increasing the mass flow ratio to 0.13% produced the traces shown in Figure 30b. The depressurization was small--only 2 psi over 100 msec--and waves and a slug were observed to be generated. However, the overpressure was measured at only 10 psi which is a factor of 20 less than typical results with no air addition. With even greater amounts of air added (0.32%, 0.52% and 2.0%) neither a depressurization nor a waterhammer event was measured, and there were no slugs formed.

Thus, these scoping experiments strongly suggest that even small quantities of non-condensible gases can powerfully suppress slug formation and impact. It is believed that this effect is directly related to retardation of condensation rate by accumulation of non-condensibles at the condensation sites. Certainly, the possible effect of noncondensed gases on sonic velocity in the water or on "cushioning" the shock at slug impact were of no importance in those tests where no slug formed, although these additional effects may also play a role if a slug forms. It should be recognized that these experiments were very limited and performed at a single water flow rate (1.0 gpm). Due to the considerable scatter in the baseline data, any quantitative conclusions are likely to be unreliable.

Since air had a powerful effect and was not measured directly in the feedpipe during the experiments, it is worthwhile to question how much air might be introduced with the water or steam during these experiments. The water was pumped from the bottom of a quiescent but open trough. This would tend to suppress bulk entrainment of air, but there might be appreciable quantities of dissolved gases in the water. The maximum solubility of air in water is 2.36×10^{-5} lb_m air per lb_m water at 62°F (close to the usual storage and experimental temperature). At a typical 2 gpm flow rate this corresponds to approximately 6.29×10^{-6} lbm/sec of maximum possible air introduction as a dissolved gas with the water which is 0.04% by mass of the inlet steam flow, an amount that was shown to be negligble when introduced directly with the steam. Moreover, it is unlikely that maximum solubility was achieved and means to remove any air from solution in the water tend to be very slow relative to the flow processes under study, so that it is unlikely that even this low concentration was achieved in practice.



(a) 0.05% BY MASS OF AIR ADDED



(b) 0.13% BY MASS OF AIR ADDED

PRESSURE DATA WITH AIR ADDED TO STEAM

FIGURE 30

POOR ORIGINAL

729 300

Comparable arguments may be applied to the steam. Assuming maximum dissolved gas in the boiler feedwater, a 60°F feedwater temperature, and neglecting all means of noncondensible gas removal, there could be as much as 2.36×10^{-5} lb_m air per lb_m steam, i.e., 0.0024% by mass of air in the steam, a negligible amount. In fact, even this amount is reduced dramatically by chemical means and by residence time in a vented preheater train.

In view of the above, it is concluded that a negligible quantity of non-condensible gas was introduced with the water and steam during the present experiments.

Steam Generator Vessel Water Level

Another effect explored with the baseline configuration was the effect of the steam generator vessel water level. Cold (65°F) water was injected independently into the vessel alone (without feedpipe injection). In other tests simultaneous injection in both locations was employed, with a feedpipe injection flow rate just below that at which a waterhammer event occurred without vessel injection (i.e., less than 0.70 gpm). In neither of these cases did a waterhammer event occur.

With vessel injection alone, eight rates of water level rise in the vessel were studied ranging from two to 96 in/min. Observations of the flow indicated that as the water level in the vessel reached the level of the drain holes, water was sucked into the sparger and feedpipe, but the process was rather quiescent, with few waves generated in the tube. The tube filled quietly with water even though the water level in the vessel was well above the top of the tube, and the tube was not yet full. (Some steam may have been coming in the holes; it was not possible to observe bubbling through the water.) Temperature measurements indicated that the temperature at the thermocouple location dropped from the steam temperature to a lower value in the range 100-195°F as the tube filled (the water was colder at higher level rise rates), showing that subcooled water was present, especially at the more rapid level rise rates.

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The same comments applied when the water level in the vessel was raised at similar rates to the above but with simultaneous feedpipe injection of 0.43 gpm (i.e., slightly below the threshold flow with feedpipe flow alone). Naturally, as the feedpipe injection rate was increased above 0.70 gpm, waterhammer occurred as it occurred previously, even with the vessel water level well below the feedring.

The fact that slug formation and impact was not enhanced by rising vessel water level in these experiments is in conflict with the regular evidence from PWRs where waterhammer events were sometimes recorded just as the feedring was being covered. It is quite likely that some relevant phenomena have been suppressed by the very small scale of the present model facility.

Summary of Parameter Effects With Baseline Configuration

A threshold flow rate has been identified below which waterhammer events did not occur because a slug did not form. At high flow rates the pipe runs full and slugs also do not form. The low flow rate threshold was insensitive to water subcooling or pressure even at temperatures quite close to saturation temperature.

When waterhammer events occurred, the quantitative characteristics such as depressurization history or slug impact overpressure were insensitive to water flow rate and subcooling. A factor of five increase in vessel pressure led to a factor of two increase in overpressure, which agrees qualitatively and quantitatively with the expected trend.

Small amounts of non-condensible ,as introduced with the steam (order 0.1% by mass) powerfully suppressed slug impact or eliminated slug formation altogether in limited exploratory experiments.

Slug formation was insensitive to variation of vessel water level covering the simulated feedring at various rates during this model study.

4.5 Sparger Geometry Effects

The effect of the sparger geometry on the water slug behavior was investigated with several different hole sizes and number of holes in place of the ten 0.375 inch diameter holes on 1.0 inch centers used in the baseline configuration. These

729 302

consisted of twenty 0.375 inch diameter drain holes on 0.5 inch centers, ten 0.750 inch diameter holes on 1.0 inch centers, and no drain holes, but with an open-ended tube. The main purpose of these tests was to determine the water flow rates at which slugs formed in these geometries, since the number of holes draining was shown to be significant in the baseline configuration.

Twenty 0.375 inch Holes

The first geometry tested merely increased the number of holes, doubling the area ratio of holes to feedpipe (to 125%). The water level in the tube and the number of holes draining as a function of water flow rate in an air environment are shown in Figures 31 and 32. The maximum number of holes observed to be draining without a slug forming (in a steam environment) was eight or nine for 61°F water. The threshold flow rate for waterhammer events at this water temperature was in the range 1.74-2.0 gpm. Thus, the threshold water flow rate was nearly three times higher than in the baseline (10 hole) configuration and the number of holes draining water at the threshold flow increased accordingly so that in both cases just below half the holes were draining at the threshold flow. This and also the effect of water temperature on the threshold water flow rate are shown in Figure 33. At water temperatures up to 180°F, the threshold water flow rate was unchanged. The effect of water subcooling was minimal in this and the baseline configuration as shown in the Figure 33, although it is expected that slug formation would be suppressed entirely by use of saturated water.

The typical depressurization and flow visualization results were virtually the same as in the baseline configuration at all temperatures and are, therefore, not displayed. Typical overpressure values were 50-100 psi less than in the baseline configuration, but otherwise the overpressure pulses were similar.

Ten 0.750 Inch Holes

The flow behavior for this geometry is summarized in Figure 34. The threshold water flow rate was in the range of 4.4 - 5.0 gpm for this geometry, corresponding to 5-6 holes draining without a waterhammer event occurring. At these relatively high flow rates, the rate of turning on the water affected whether or not a waterhammer event occurred. The velocity of the water at these relatively high water flow

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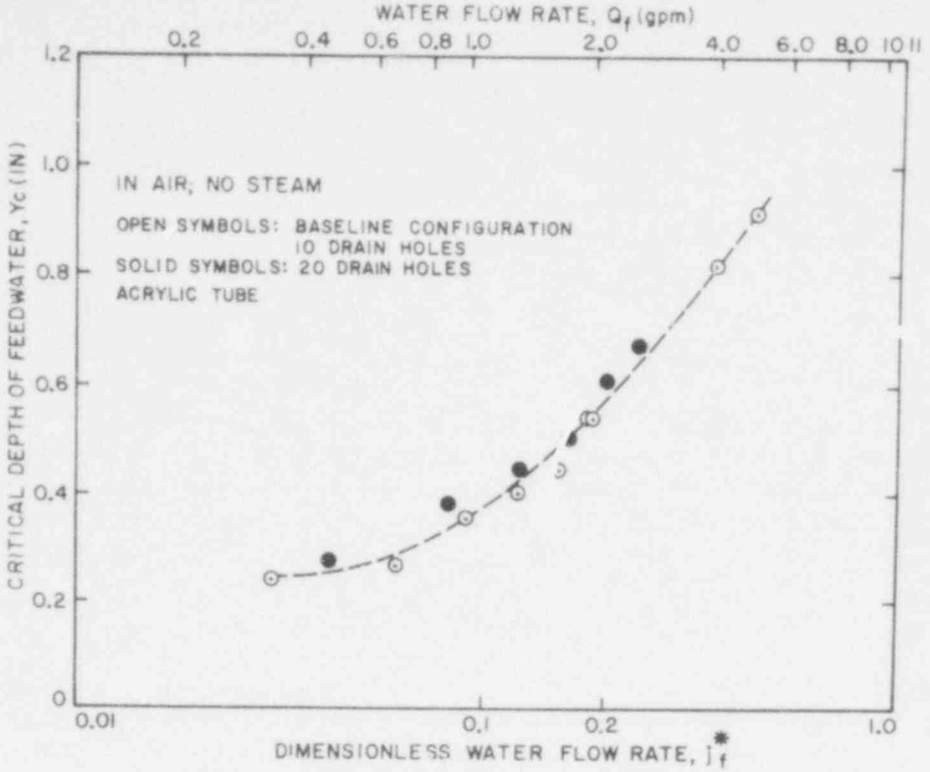
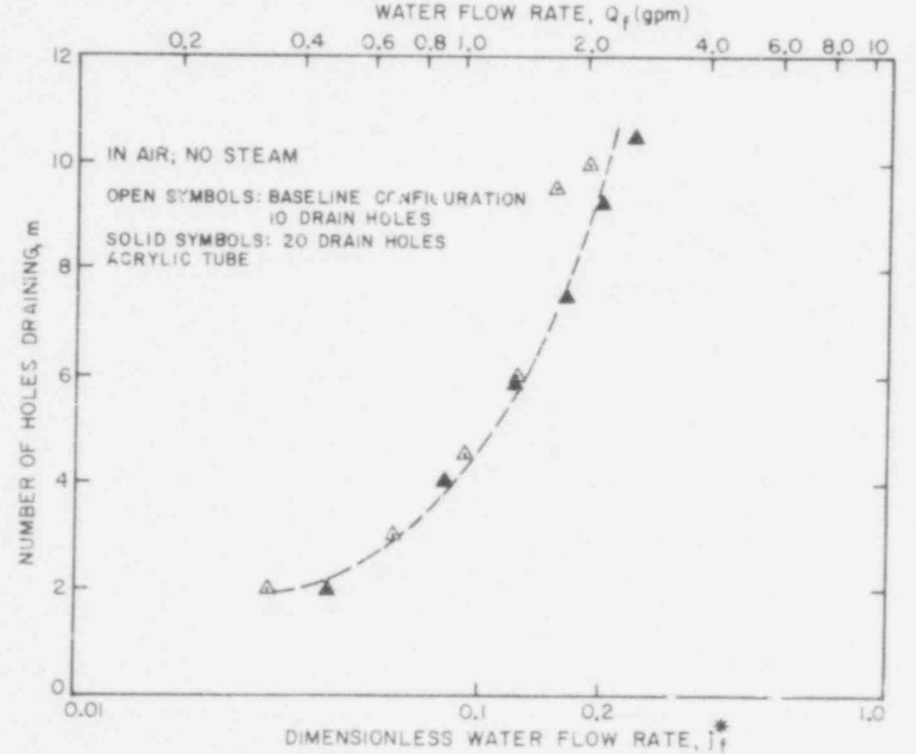


CRITICAL DEPTH OF FEEDWATER VS. WATER FLOW RATE

FIGURE 31



NUMBER OF HOLES DRAINING VS. WATER FLOW RATE

FIGURE 32

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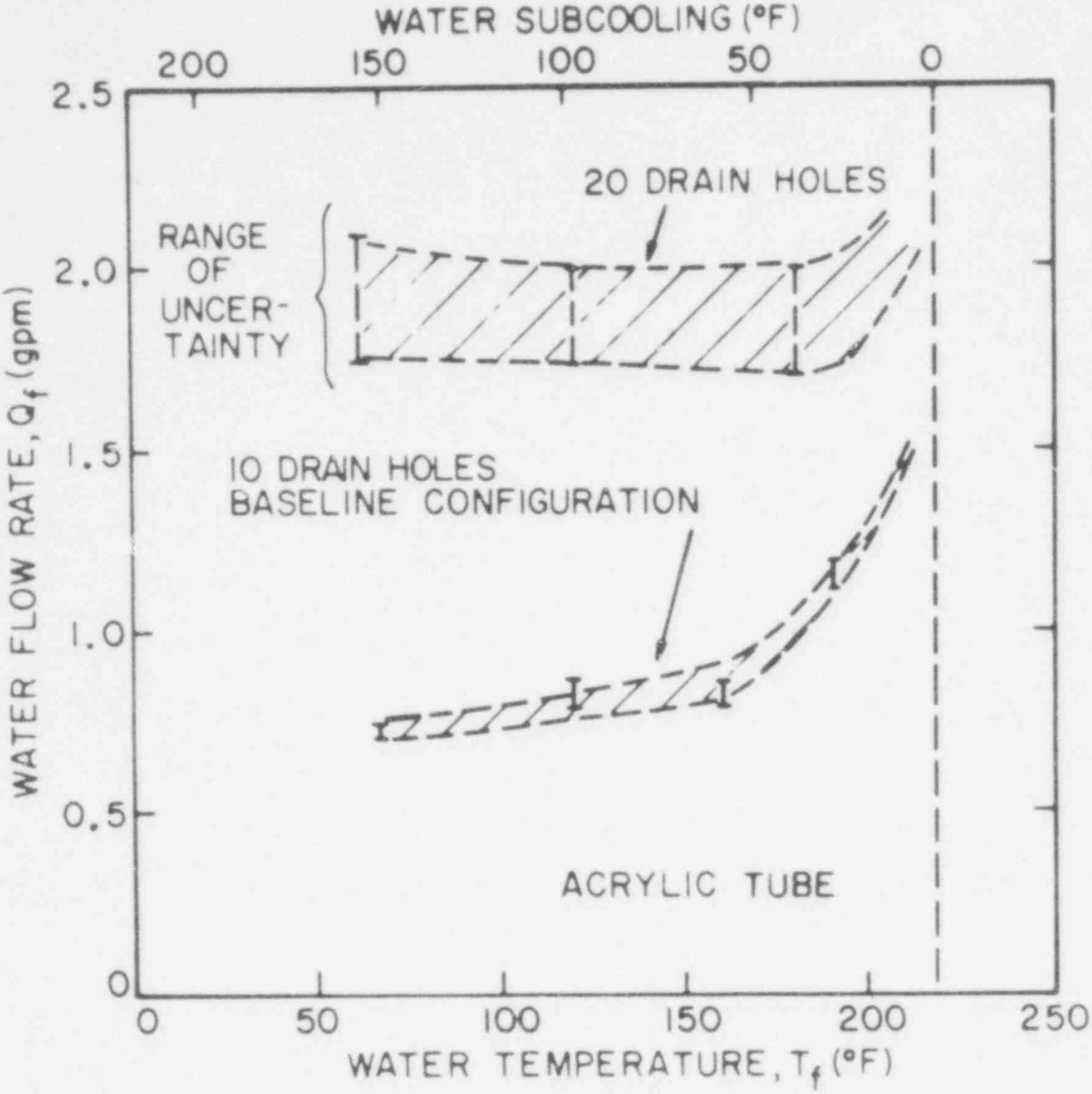

THRESHOLD WATER FLOW RATE VS. WATER TEMPERATURE IN BASELINE (10-) AND 20-HOLE MODELS

FIGURE 33

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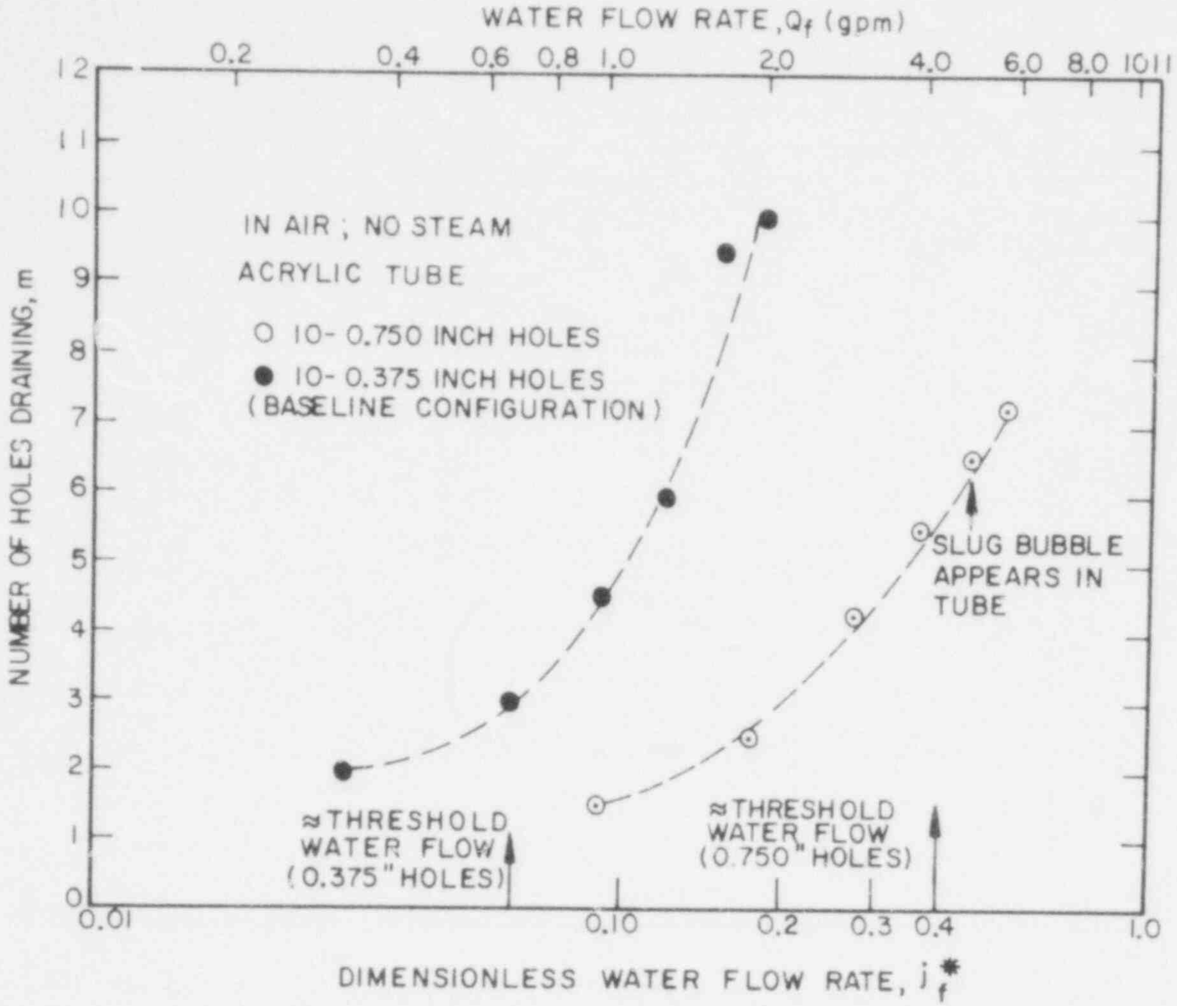

NUMBER OF HOLES DRAINING WITH 0.75 INCH HOLES

FIGURE 34

POOR ORIGINAL

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rates could be important. If the water was turned on quickly, its momentum could carry it over several holes without draining and, therefore, waterhammer might occur at lower flow rates than if the water was turned on slowly. Such transient behavior contributed to the data scatter and is probably reflected in the relatively large range where the threshold flow rate is uncertain (4.4 to 5.0 gpm).

Open-Ended Tube

With an open-ended pipe instead of drain holes, there was no flow rate at which a waterhammer event occurred when the water was turned on slowly. Flow rates from 1.7 to 7.8 gpm were tried. Above 3 gpm, turning the water on quickly did result in waterhammer events with overpressres of around 150-200 psi. As with the ten 0.750 inch hole configuration, at higher water flow rates the rate of turning on the water flow into the feedpipe was important

Summary of Sparger Geometry Effects

Experiments with various hole patterns in the straight sparger demonstrated that the threshold flow rate and the hole characteristics were linked in this study. This supports the direct qualitative observation of a hydraulic instability leading to slug formation in the sparger (i.e., not in the upstream feedpipe) during the baseline experiments. Quantitative hydraulic data were obtained for comparison with analyses. Transient flow effects were assessed in configurations that had high threshold flow rates.

4.6 Modeling Vendor-Recommended Modifications

Several modifications to the baseline sparger and feedpipe geometry, intended to model configurations recommended by the vendors for use in PWRs were also tested. For example, top discharge of the ten 0.375 inch diameter drain holes (rather than bottom discharge) simulated the functional effect of top discharge devices (e.g., J-tubes) in PWR steam generators.* Another geometric variation was to retain the downward discharge orientation of the drain holes but to shorten the length of the feedpipe outside of the steam generator vessel. Finally, both of these modifications were combined in tests with top discharge and a shortened feedpipe. The results obtained with these modifications are discussed below. The primary conclusions of this section will deal with the effects of the geometric modifications on the threshold flow rate (for slug formation) and slug impact measurements in each geometry compared with the baseline configuration.

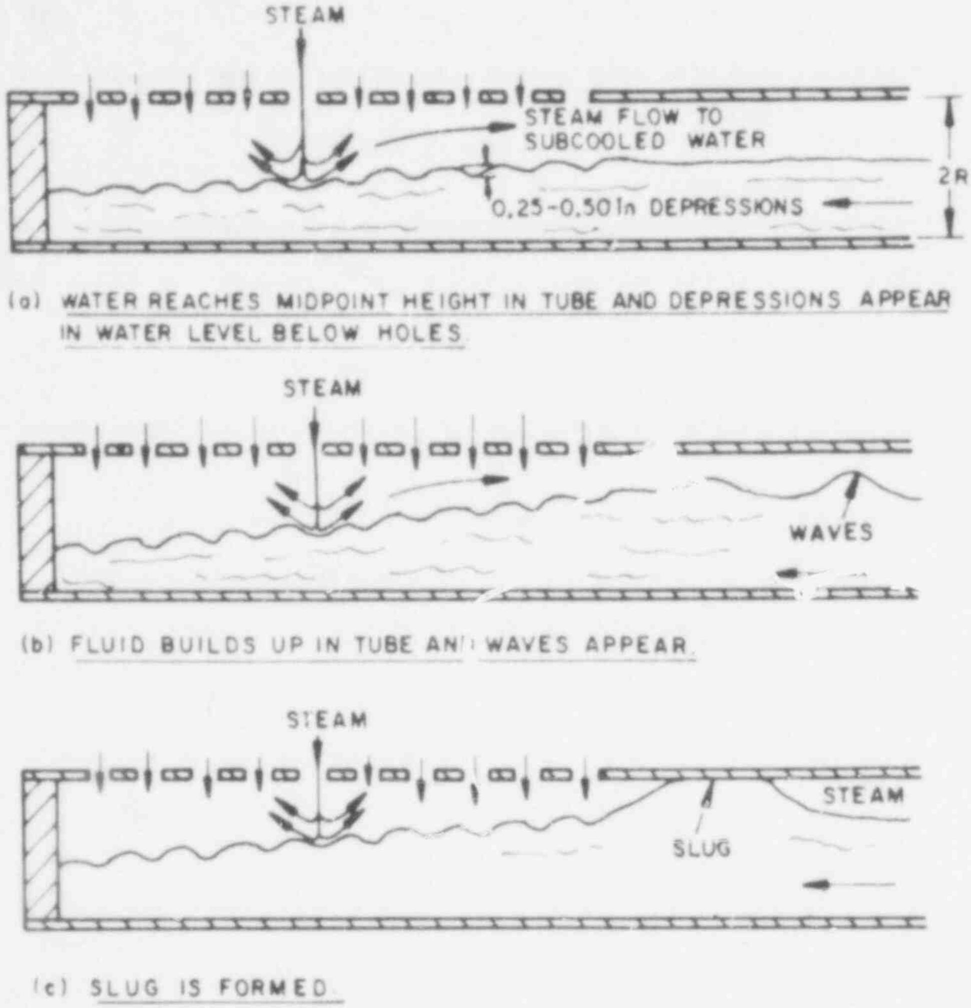
*Since the sparger section and the feedpipe had the same diameter, it was unnecessary to install model J-tubes in these tests.

Top Discharge Alone

In the top discharge geometry, the feedpipe and sparger of the baseline configuration were merely rotated 180°, from a bottom draining orientation to a top discharge orientation. This simulated the function of top discharge modifications such as J-tubes in PWR steam generators.

The primary intent of top discharge in PWRs is to reduce the rate of feedring drainage. Preliminary experiments demonstrated the clearly expected result that there was no impact if the pipe was undrained initially. Subsequent experiments were conducted with the pipe fully drained initially for the purpose of exploring other phenomena such as the possible "venting" effect of the top discharge holes and the expectation that some of the multiphase hole effects that were implicated in slug formation with bottom discharge would be absent with top discharge. Effects of partial initial drainage were not explored.

With top discharge, flow visualization studies revealed that after rapid establishment of feedwater flow at a controlled rate, the feedpipe and sparger sections filled to a level of about one-half full (visual estimate) and then reached a critical condition that led to slug formation. As the water level reached the midpoint height in the tube, depressions were observed in the water level underneath each of the holes. These depressions were about 0.25 to 0.5 inches in the 1.5 inch ID tube. (A steam velocity of 15-30 ft/sec through each hole is estimated based on the observed depressions.) Figure 35 is an illustration of the water flow patterns observed as the tube became half full. Figures 35b and 35d show the build-up and formation of a slug filling the pipe over the sloped water-steam interface in the tube. Some waves were also observed traveling up and down the feedpipe outside of the steam generator vessel. The general appearance of the behavior was similar to that reported in the counterflow experiments of Wallis and Dobson [20]. The slug formed upstream of the section of the tube with the holes, in contrast to the bottom discharge configuration where the slug formed above the drain holes.



SKETCH ILLUSTRATING SLUG FORMATION WITH TOP DISCHARGE

FIGURE 35

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If the flow rate was less than 1.20 gpm (± 20%), when the water level reached the halfway mark in the tube, small depressions in the water level below the holes were observed, but no waves were observed to be traveling up and down the feedpipe. The water level seemed to pause momentarily at this point, and then continue until the pipe was near full. Occasionally, a mild bubble collapse and consequent impact was observed as the pipe filled the last 10% of the tube, but otherwise, it filled without incident at the lower flow rates.

Above 1.20 gpm, slug formation and impact occurred up to a flow rate of 4.0 gpm. Figure 36 shows the overpressure measurements as a function of water flow rate in this top discharge geometry compared with those in the baseline configuration. The results are similar as far as the magnitude of the overpressure measurements although they are statistically lower with top discharge, by perhaps 20%. Similar data for the metal pipe are displayed in Figure 37 and lead to the same conclusion. Namely, the prime effect of top discharge in these experiments with the feedring drained initially is that the threshold water flow rate (for slug formation and impact) is about 50% higher in the top discharge configuration than in the bottom discharge configuration; the reduction in overpressure is statistically only 20%.

Figure 38a shows a typical trace of the pressure measurements in this configuration. The depressurization preceding the slug formation (upper trace, Figure 38a) was 7 psi in 100 msec, about the same as seen with bottom discharge. The overpressure, p_h, in this example was 220 psi, followed 40 msec later by a rebound overpressure of 80 psi, also about the same as observed in the baseline configuration (Figure 21). Figure 38b shows a time-expanded overpressure pulse. This differs from data in the bottom discharge configuration (Figure 21) in that the pulse appears to be even more crowned (ragged) at the top. The overpressure was 300 psi and the duration of the pulse was 5 msec (± 50%). The duration of the pulse was thus over twice as long as in the baseline configuration, implying that the slug length was over twice as long in the top discharge configuration. This is consistent with the observation that the feedpipe was more full at the time of the waterhammer events in this configuration than in the baseline configuration.

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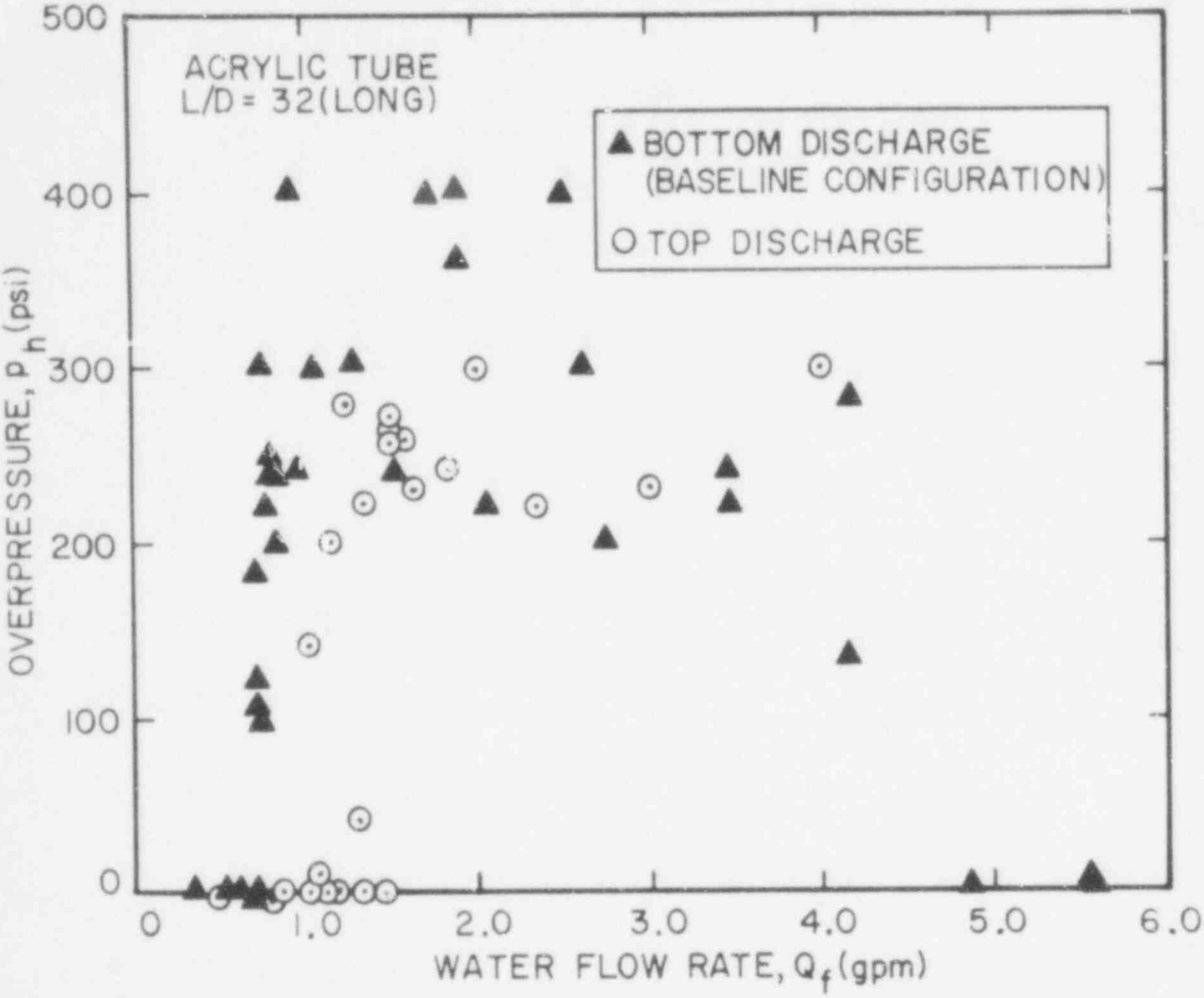

OVERPRESSURE AND THRESHOLD WATER FLOW FOR TOP AND BOTTOM DISCHARGE

FIGURE 36

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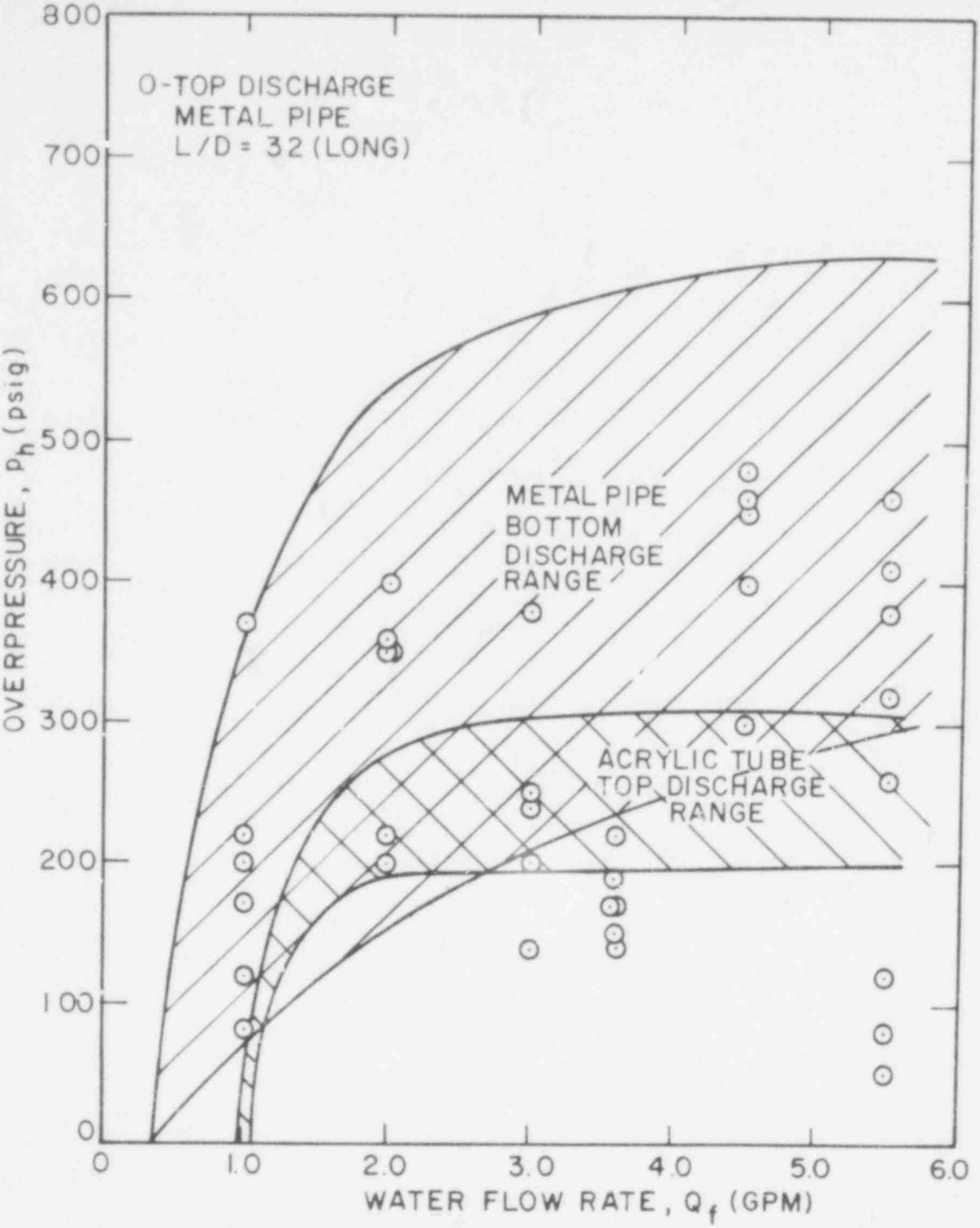

OVERPRESSURE WITH TOP DISCHARGE IN METAL PIPE

FIGURE 37

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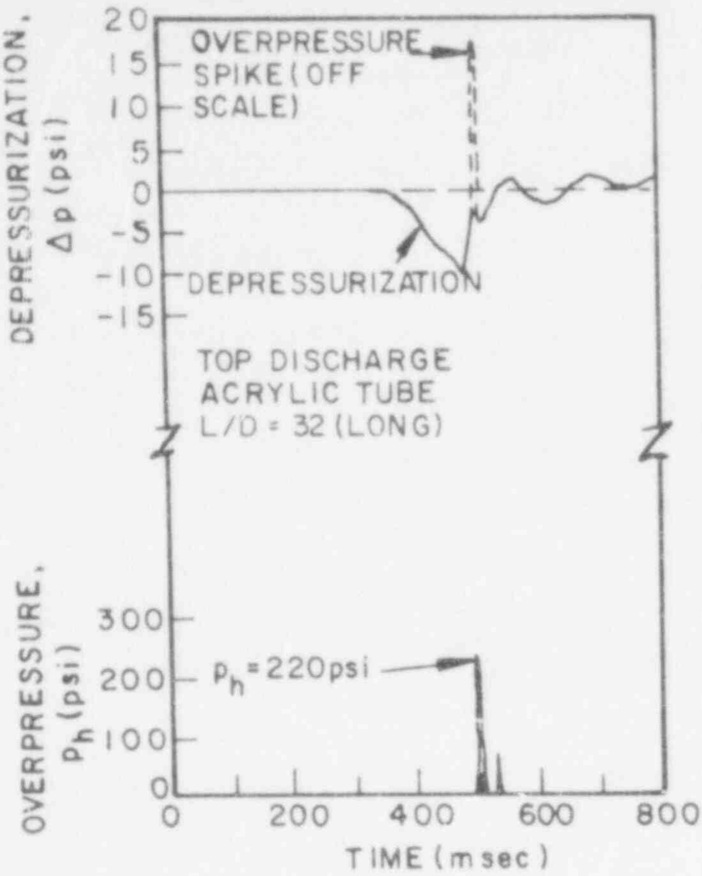
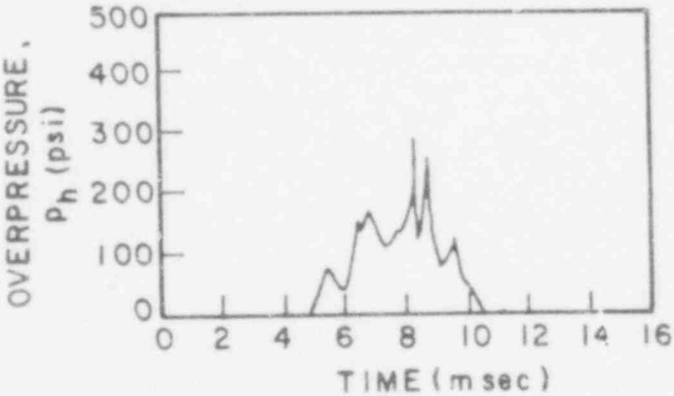


(a) SAMPLE PRESSURE TRACES WITH TOP DISCHARGE



(b) SAMPLE OVERPRESSURE PULSE WITH TOP DISCHARGE

FIGURE 38

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The conclusions reached with the top discharge configuration are therefore, a 50% higher threshold water flow rate was seen, the magnitude of the overpressures was reduced by 20% and the duration of the overpressure pulse was twice as long compared with the baseline configuration. Thus, the impulse at impact at the top discharge configuration was actually twice that with the bottom drain holes in our experiments.

Shortened Feedpipe Length Alone

The baseline steam generator model used an overall four foot length of acrylic tubing or metal pipe (overall L/D = 32). The effect of feedpipe length was explored by halving the overall feedpipe length (to overall L/D = 16) with an acrylic tube and also by reducing the feedpipe length to an overall L/D = 10.7 with a metal pipe. In all three configurations the overall length includes a one foot length (L/D = 8) of pipe inside the vessel (with the baseline ten 0.375 inch, bottom discharge holes) and 36 inch, 12 inch and four inch lengths (or L/D = 24, 8, 2.7) of feedpipe outside the vessel respectively.

In PWRs, the length of feedpipe with volume equivalent to the feedring volume is 15 to 30 feet. Linearly scaled this is 1.5 to 3.0 feet at 1/10 scale. Thus, the one foot straight sparger length is "short" by a factor of 1.5 to three relative to a linearly scaled feedring. The length to diameter ratio of PWR feedpipe horizontal runs adjacent to the steam generator ranges from 2 to 24. Thus, the baseline configuration of the present experiments is a linearly scaled model of the longest pipe presently in existence (at Palisades). The other configuration chosen for these experiments represent "very short" and "typical" values of scale horizontal pipe runs.

Figure 39 shows the resultant overpressure measurements as a function of water flow rate for the acrylic tube (L/D = 16) compared with the baseline configuration. The threshold water flow rate for slug formation and impact is 0.6 gpm (± 10%) in this configuration, compared with 0.70 gpm in the baseline configuration. Observations of the number of holes draining indicated that up to three holes and part of a fourth could drain without the process leading to slug formation, but if four holes became covered, a slug was formed and a waterhammer event occurred. This observation is consistent with the baseline configuration.

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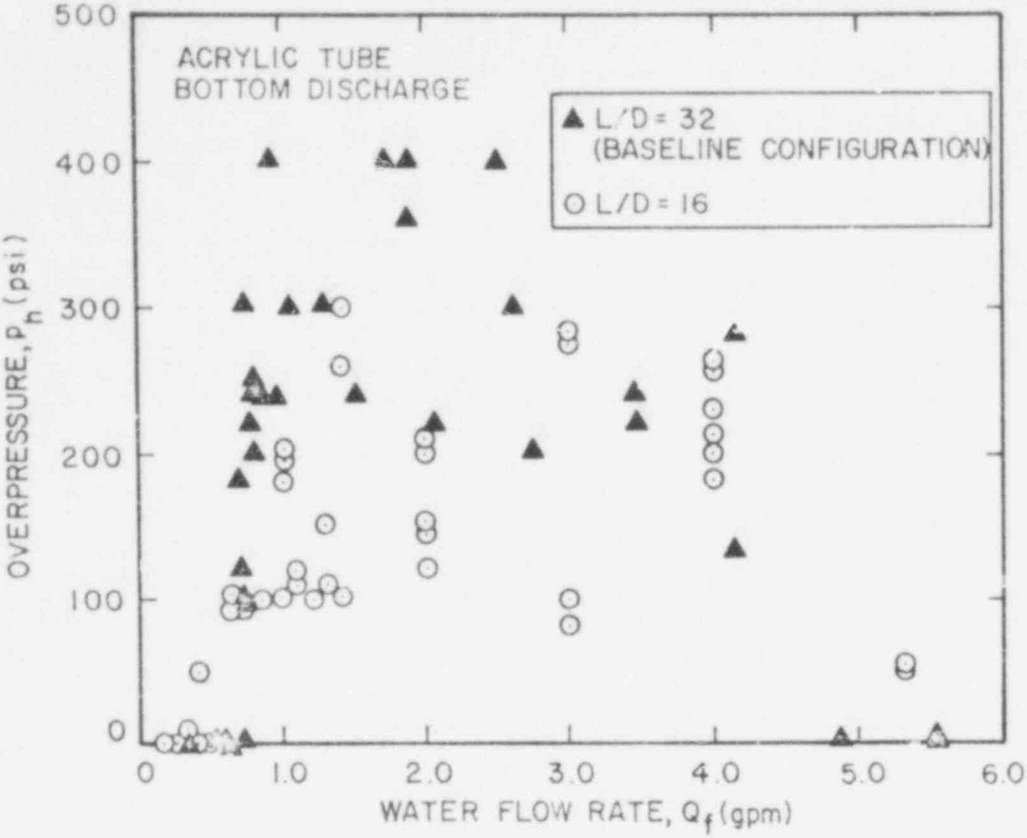

OVERPRESSURE AND THRESHOLD WATER FLOW FOR TWO FEEDPIPE LENGTHS

FIGURE 39

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Qualitatively there was no difference in the flow behavior. Quantitatively, overpressure measurements were statistically about 100 psi less than in the baseline configuration.

The depressurization measurements gave the same indications as in the baseline configuration for the magnitude and duration of the depressurization. The magnitudes of the overpressures were shown in Figure 39. The overpressure pulse shapes were triangular, as in the baseline configuration (Figure 21) and the durations were 1.0 msec (± 20%) or slightly smaller than those seen in the baseline configuration, implying that the slug length was slightly less. The reduction was smaller than the factor of two reduction in overall pipe length, however. Thus, a disproportionate amount of the slug must originate in the sparger section.

Figure 40 shows the overpressure measurements as a function of water flow rate for the metal pipe (L/D = 10.7) compared with the baseline configuration. As with the acrylic tube, the overpressures are comparable with or only slightly less than in the baseline configuration. The measured depressurizations were similar to the cases seen previously. The overpressure pulse shapes were triangular as in the baseline configuration (Figure 28) but the duration of the overpressure pulses was typically 0.4 msec (± 20%), again only slightly less than the 0.5 msec duration seen in the baseline configuration (Figure 28).

The conclusions of these experiments with a very short length (L/D = 2.7) of feedpipe <u>outside</u> the model vessel (with bottom discharge) are that the threshold water flow rate was unchanged, overpressures were unchanged and overpressure pulse durations decreased only 20% compared with the baseline configuration which had a very long feedpipe (L/D = 24) outside the model vessel.

<u>Top Discharge and Shortened Feedpipe Together</u>

Experiments combining the previous two modifications were conducted. The shortened feedpipe length (overall) was L/D = 10.7, the same as the shortened metal feedpipe above. Top discharge was obtained again by rotating the feedpipe so that the drain holes were on top of the sparger section.

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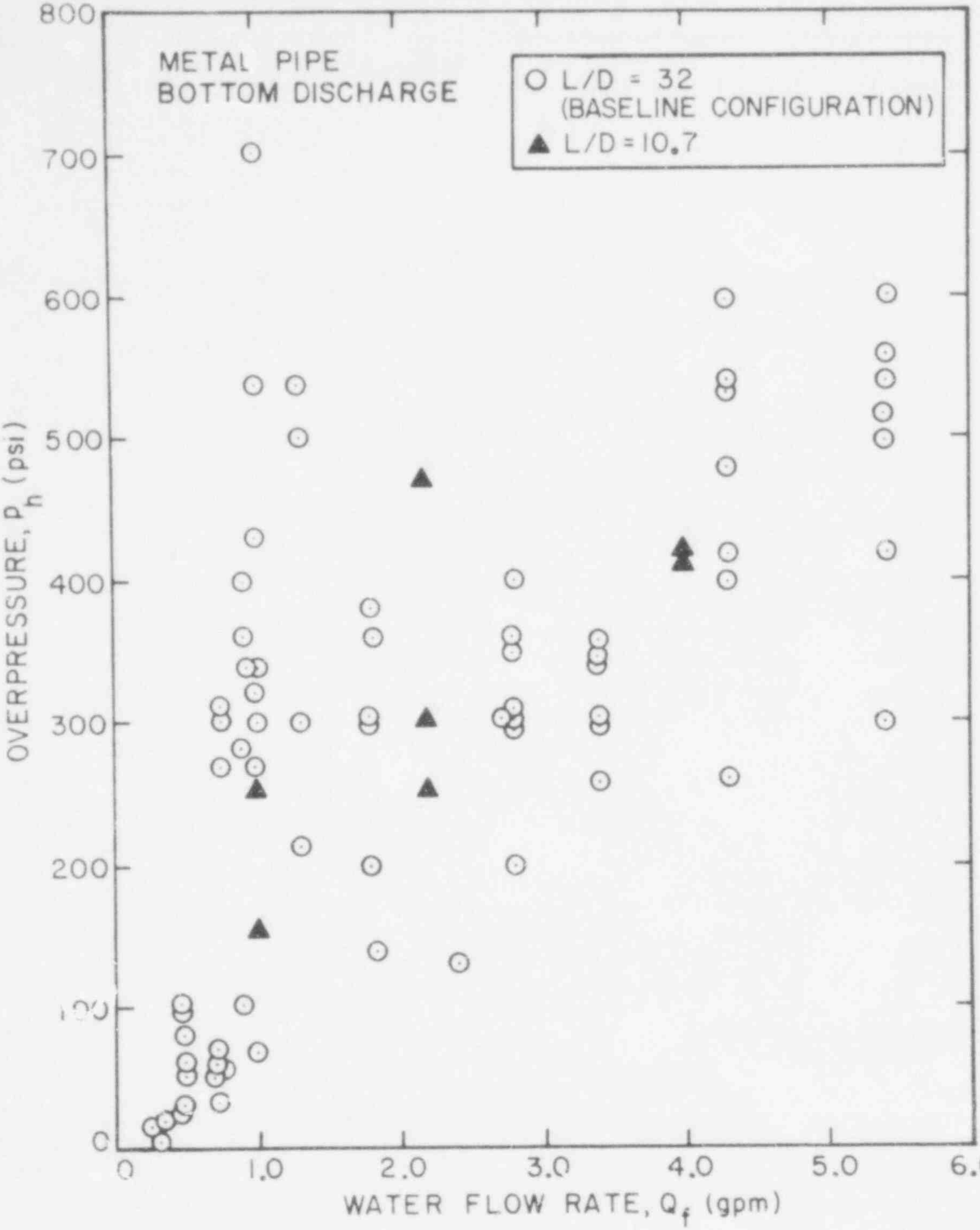

OVERPRESSURE FOR TWO FEEDPIPE LENGTHS

FIGURE 40

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The overpressure measurement results as a function of water flow rate are shown in Figure 41. Depressurizations similar in magnitude and duration to those of the previous tests were recorded. The overpressure measurements however, were consistently lower than those in any of the other models. In general, the overpressure measurements at all flow rates were very low (in the range 50 psi ± 100%) compared with the baseline result, and in fact no slug impacts were indicated in many of these tests. Slightly higher overpressures, approximately 100 psi (± 50%), were generated by on/off transient control of the water flow.

The main conclusion for this configuration is that overpressure measurements were a factor of five to ten less at all flow rates tested, compared to measurements with the other configurations.

These results can be understood in view of a very idealized model of the behavior. Postulate that top discharge effectively "shortens" the overall length of the pipe by an amount equal to the length of the sparger section due to the local venting action of the top discharge holes. An "effective" pipe length may be defined equal to that of the remaining pipe. With bottom discharge the effective length is assumed to be equal to the actual overall pipe length. The overpressure data are plotted as a function of effective pipe length in Figure 42. This plot shows that in our experiments it was necessary to have a very short effective pipe length in order to reduce the overpressure appreciably. Such a reduction in overpressure can accrue both from the reduced size (volume and length) of the void that can be trapped, and from suppression of slug formation altogether in short pipes. Analyses in Section 5 suggest that the behavior may be relatively insensitive to pipe length, however, unless the pipe is very short. This is confirmed in Figure 42.

Based on the present data, top discharge in conjunction with short pipes may act together to suppress slug formation and substantially reduce slug impact intensity even though top discharge alone had only a moderate effect on slug formation (it increased the threshold flow rate by 50% and short pipes were relatively inconsequential).

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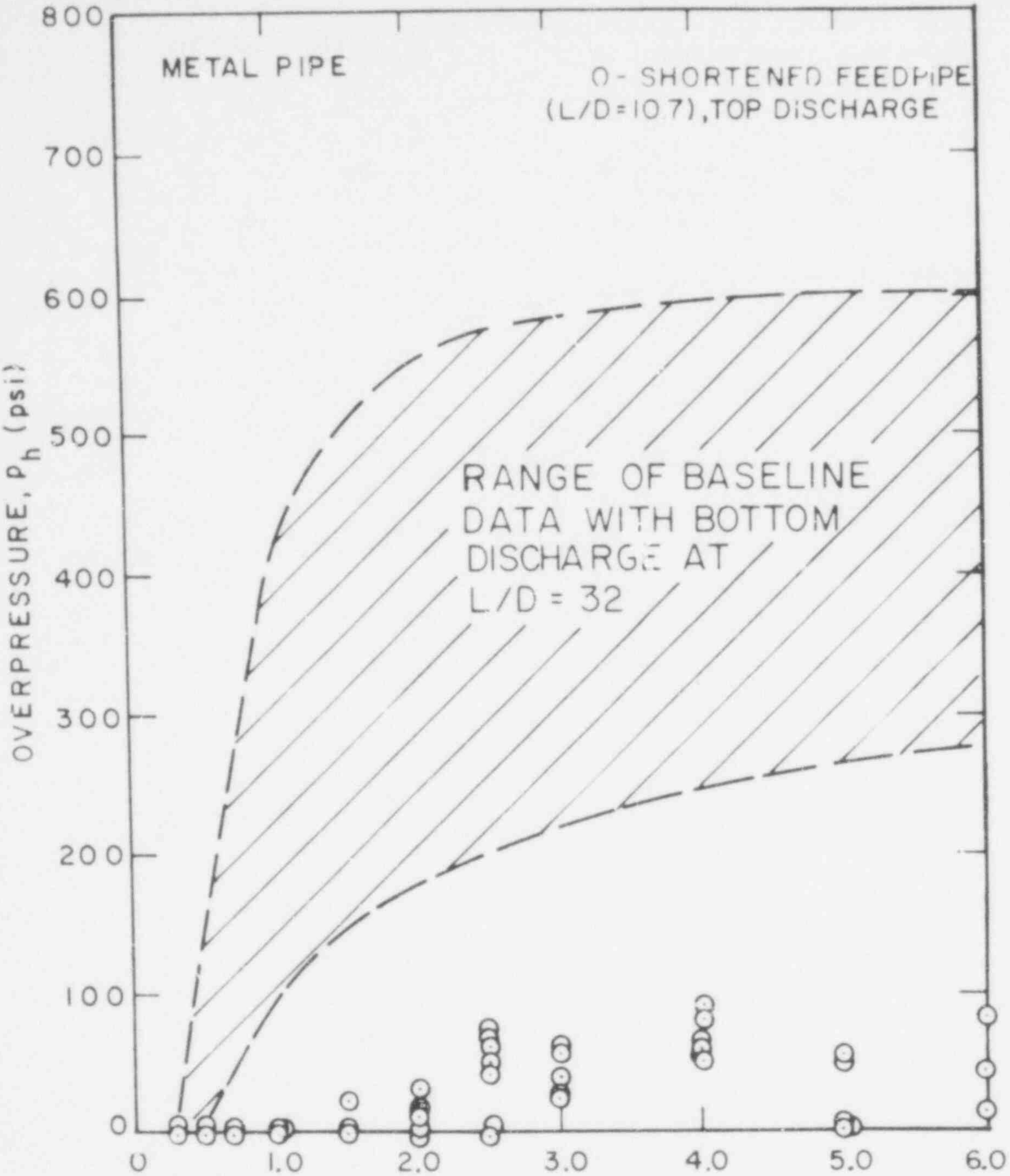

OVERPRESSURE MEASUREMENTS WITH COMBINATION OF SHORTENED FEEDPIPE AND TOP DISCHARGE

FIGURE 41

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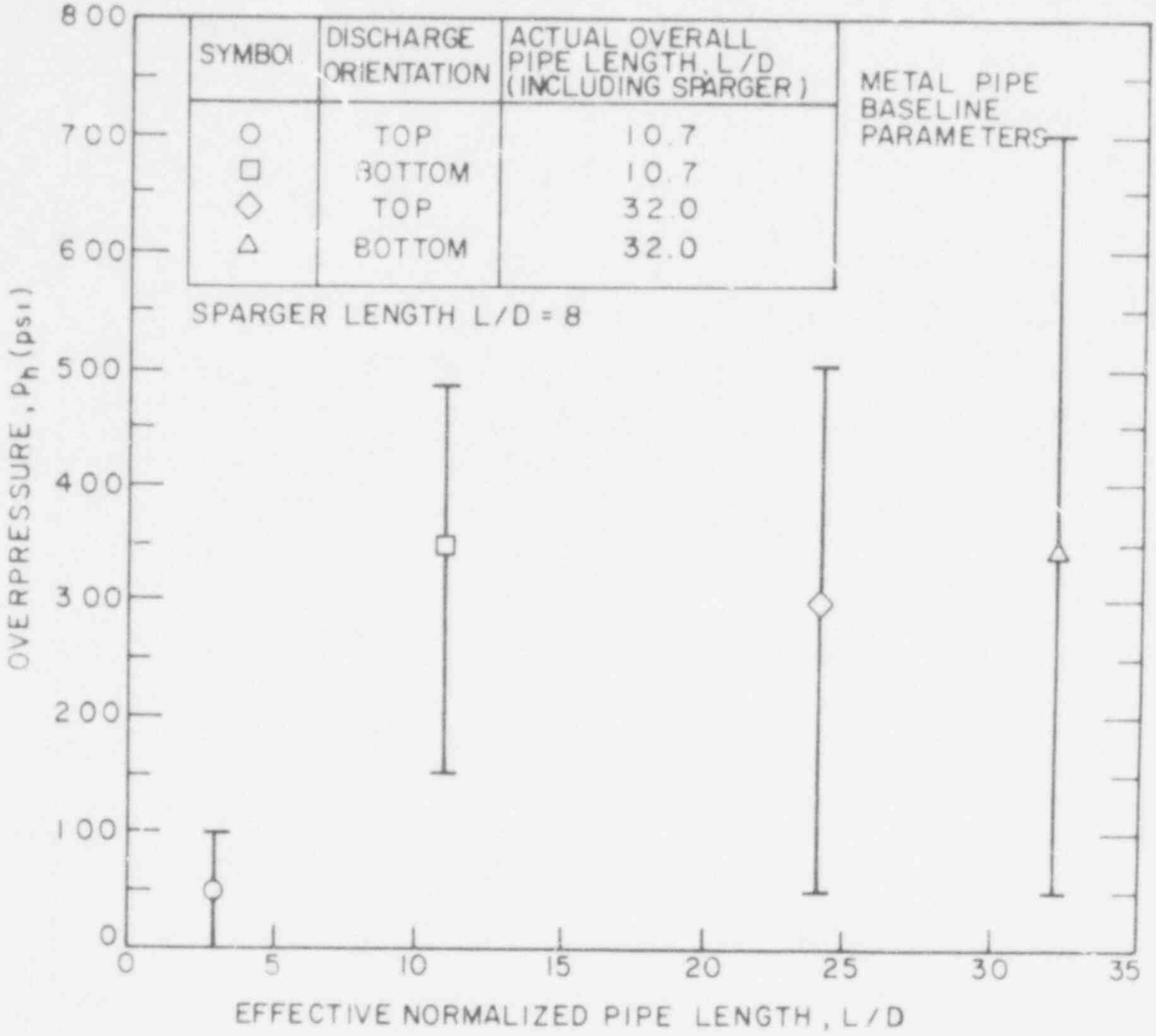

IDEALIZED COMPARISON OF DATA WITH SHORTENED PIPING AND TOP DISCHARGE ALONE AND IN COMBINATION

FIGURE 42

POOR ORIGINAL

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4.7 Summary of 1/10-Scale Model Study

The primary general conclusions from the model study are that condensation can proceed rapidly so that relatively high overpressures at impact of order 500 to 1000 psi, can occur even at 1/10 scale with vessel pressures near atmospheric pressure. It is shown in Section 5 that these pressures are of order 20 to 50% of that calculated with an analysis that includes some apparently extreme and conservative assumptions. The phenomena are very whimsical as evidenced by the gross scatter in most of the data; single or limited sample experiments are unreliable. It was possible to reduce the overpressures by a factor of five to ten during our study by employing top discharge and short feedwater piping outside the vessel together. Separately, these vendor recommendations were ineffective during our experiments with pipes that were drained initially. An extensive body of generally self consistent data has been obtained, as listed below.

Summary tables for the experimental results in the various geometries investigated are included in Tables 7, 8, and 9. The parameters summarized are threshold water flow rates, overpressure measurements, and overpressure pulse duration recorded during this 1/10 scale PWR steam generator model study.

Quantitative Characteristics: Water Cannon Model

Typical depressurizations were in the range 7 to 10 psia over a time period of 100 to 150 msec.

The magnitude of the overpressure spikes was 1000 psi (± 30%) in the metal pipe and 300 psi (± 30%) in the acrylic tube.

Overpressure pulses were of very uniform duration in the model, approximately 1.0 msec (± 10%) for the metal pipe and 3.5 msec (± 20%) for the acrylic tube.

Flow visualization motion pictures showed that the typical velocity of the water column was 20 ft/sec in these experiments.

Quantitative Characteristics: SG Model Baseline Configuration

Typical depressurizations were in the range 7-10 psia over a time period of 100 to 150 msec as for the water cannon.

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TABLE 7

SUMMARY OF THRESHOLD WATER FLOW RATES (GPM) IN STEAM GENERATOR MODEL

Overall Pipe Length*	ACRYLIC TUBE				METAL PIPE	
	Bottom Discharge	Top Discharge	20,0.375 in. Sparger Holes	10,0.75 in. Sparger Holes	Bottom Discharge	Top Discharge
L/D = 32 (Baseline)	0.70 (± 10%)	1.2 ± 20%	1.8 (± 10%)	4.75 (± 10%)	0.60 (± 10%)	N.T.
L/D = 16	0.60 (± 10%)	N.T.**	N.T.	N.T.	N.T.	N.T.
L/D = 10.7	N.T.	N.T.	N.T.	N.T.	-	1.5 (± 10%)

TABLE 8

SUMMARY OF OVERPRESSURE MEASUREMENT (PSI) IN STEAM GENERATOR MODEL

Feedpipe Length*	ACRYLIC TUBE				METAL PIPE	
	Bottom Discharge	Top Discharge	20,0.375 in. Sparger Holes	10,0.76 in. Sparger Holes	Bottom Discharge	Top Discharge
L/D = 32 (Baseline)	300(± 50%)	250(± 20%)	250(± 50%)	300(± 50%)	500 (± 50%)	N.T.
L/D = 16	200(± 50%)	N.T.**	N.T.	N.T.	N.T.	N.T.
L/D = 10.7	N.T.	N.T.	N.T.	N.T.	300 (± 50%)	50 (± 100%)

TABLE 9

SUMMARY OF OVERPRESSURE PULSE DURATION MEASUREMENT (MSEC) IN STEAM GENERATOR MODEL

Feedpipe Length*	ACRYLIC TUBE				METAL PIPE	
	Bottom Discharge	Top Discharge	20,0.375 in. Sparger Holes	10,0.75 in. Sparger Holes	Bottom Discharge	Top Discharge
L/D = 32 (Baseline)	1.5(± 50%)	5 msec (± 50%)	1.5(± 30%)	-	0.75 (± 50%)	N.T.
L/D = 16	1.0(± 20%)	N.T.**	N.T.	N.T.	N.T.	N.T.
L/D = 10.7	N.T.	N.T.	N.T.	N.T.	0.4 (± 20%)	-

*Length includes a 12 inch (L/D=8) length of sparger.
**Not Tested

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The magnitude of the overpressure spikes was 500 psia (± 50%) in the metal pipe and 300 psi (± 50%) in the acrylic tube. These values are less than those in the water cannon and the data displayed more scatter.

Overpressure pulses were of 0.75 msec (± 30%) duration in the metal pipe and 1.5 msec (± 30%) in the acrylic tube. These durations are roughly half that measured in the water cannon indicating that the slug length in the steam generator model was approximately half that of the water column in the water cannon model.

The slug formation process was observed to be the same qualitatively under a variety of conditions.

Parametric Dependence Baseline Configuration

A flow rate of 0.70 gpm (± 10%) was determined to be a low flow rate threshold for slug formation and impact. A flow rate of 4.8 gpm (± 10%) was determined to be a high flow rate limit to slug formation and impact. The low-flow threshold was linked to multiphase flow processes and slug formation in the sparger, the high flow threshold was due to "running the pipe full".

The threshold water flow rate was nearly insensitive to water temperature up to T_f = 190°F.

At elevated pressures (75 psia), a factor of five higher than the baseline pressure, overpressure spikes were larger by a factor of two.

When waterhammer events occurred, the quantitative characteristics such as depressurization history and slug impact overpressure were insensitive to water flow rate and subcooling.

The effect of adding greater than roughly 0.1% by mass of noncondensibles to the input steam flow was to eliminate slug formation and impact altogether.

Independently raising the water level in the steam generator vessel did not cause slug formation and impact in this small scale model.

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Sparger Geometry

Changing the number and size of the drain holes on the sparger changed the threshold water flow rate at which slug formation and impact took place.

Modeling of Vendor-Recommended Modifications

Shortening the feedpipe as much as possible in the model reduced overpressures statistically by approximately 100 psi (≈ 20%) or less.

Top discharge changed the hydraulics of slug formation but did not reduce the magnitude of the overpressure appreciably. Furthermore, the overpressure duration and impulse were doubled.

Combining top discharge and a shortened feedpipe either prevented slug formation altogether or reduced overpressures by roughly a factor of five to ten compared with other configurations.

Caveat on Interpreting These Results

All of these results were obtained in small 1/10 scale model of a PWR steam generator feedwater system. Applying these results directly to PWRs or attempting to scale the results to full scale and typical PWR operating conditions is unreliable because the phenomena are complex and because appropriate dimensionless parameters have not been identifed or confirmed sufficiently.

The following section 5 of the report will present analyses of key phenomena anticipated from early work or identified during the model study. The description of these analyses will clarify the role of these model experiments in providing a limited verification of quantitative analyses and qualitative modeling ideas and descriptions of phenomena. Quantitative evaluation of the data is also deferred to Section 5.

5 ANALYSIS OF STEAM GENERATOR WATERHAMMER PHENOMENA

Previous sections of this report have reviewed the evidence from PWRs, described the analytical state-of-the-art (including previous analytical and experimental work on this problem by other groups), and presented original experimental data from 1/10-scale model studies. This section of the report presents original analyses derived to describe each of the phenomena cited in the Introduction. In so doing, the work presented and reviewed previously is tied together. Additional experiments conducted to support specific analytical assumptions or to provide critical confirmation of the theories are also presented in this section in the context of the analyses as they are derived.

The four main subsections successively treat initiating mechanisms, void collapse, water slug dynamics, and piping damage modes.

5.1 Initiating Mechanisms

Introduction

It is pointed out earlier that two events are necessary in order to initiate the process leading up to waterhammer. The first of these is the presence of both steam and water in the feedring and associated piping. The second is the formation of a water plug, trapping a steam void that can then collapse.

Since the feedring is normally filled with water, steam can only penetrate if water can drain out at a rate greater than the rate of supply from the feed (or auxiliary feed) pumps. The first topic that we will consider is therefore the mechanics of feedring drainage.

Slug formation is a more complex process involving the transient hydraulics of water flow in the pipe, the ring and the drain holes, and interaction with steam flowing into the ring to condense on the incoming cold water. The steam condensation rate is influenced by thermodynamic, hydraulic, two-phase flow and heat transfer processes, and is also hard to measure directly. For this reason, Creare has performed some simple model studies using air and water in an attempt to obtain independent definition of the two-phase flow aspects of the situation isolated from the thermodynamic and heat transfer aspects. The following pages will consider these hydraulic, two-phase flow and heat transfer aspects in sequence, in order to build up theoretical understanding by a series of steps.

The Creare 1/4-scale hydraulic facility is described in Appendix D, along with a brief summary of the experiments. The results of this modeling work are presented below in the context of the analyses as they are developed.

Hydraulics

Draining of the Feedring: Top Discharge

In order to provide a simple closed form expression for the purpose of estimating feedring drainage rates, a highly idealized feedpipe geometry was modeled. It consists of a single straight pipe of length L_{tot} and square cross section of side D_p. The pipe has a hole of area A_h in the bottom surface. In contrast, real feedwater systems of the type illustrated in Appendix A include a straight length of feedwater pipe, a reducing tee, and a ring of pipe having somewhat smaller diameter than the main feedwater pipe. The pipes are circular, not square. Bottom drain holes are indeed in the bottom of the ring, but the main utility of this analysis is the prediction of drainage rate with top discharge. In this case, the "hole" is a clearance gap at the thermal sleeve which may be uniformly distributed, at the bottom, at the top, or plugged up. The uncertainties introduced by the various idealizations are negligible compared with the uncertainties in specifying the leakage gap, and the idealizations simplify the analysis considerably.

The volume of water V_w in the pipe as a function of water level in the pipe y is:

$$V_w = yD_pL_{tot} \qquad (18)$$

It is assumed that the hole is sharp-edged and has contraction coefficient unity so that the leakage flow rate out of the hole is:

$$Q_w = A_h(2gy)^{1/2} \qquad (19)$$

The actual flow resistance of a thermal sleeve clearance passage may be much higher than indicated by Equation (19) (even if the gap were at the bottom of the pipe). Finally, the volume of water remaining in the pipe at any time is related to the leakage rate by:

$$\frac{dV_w}{dt} = -Q_w(y) \qquad (20)$$

Equation (20) can be expressed by the chain rule and inverted to give:

$$\frac{dy}{dt} = -\frac{Q_w(y)}{dV_w/dy} \qquad (21)$$

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Differentiating Equation (18) and substituting that expression and Equation (19) into Equation (21) gives:

$$\frac{dy}{dt} = \frac{A_h (2gy)^{1/2}}{D_p L_{tot}} \qquad (22)$$

With the inital condition $y = D_p$ at $t = 0$, integrating to an arbitrary time t gives the inverse function t(y):

$$t = \frac{2 D_p L_{tot}}{A_h (2g)^{1/2}} [D_p^{1/2} - y^{1/2}] \qquad (23)$$

This expression may be normalized to:

$$\frac{t}{\tau} = 1 - \left(\frac{y}{D_p}\right)^{1/2} \qquad (24)$$

where the normalizing time τ is equal to the time at which the pipe is fully drained and is given by:

$$\tau = \frac{2 L_{tot} D_p^{3/2}}{A_h (2g)^{1/2}} \qquad (25)$$

Equation (24) may be rewritten to give the normalized drainage transient:

$$\frac{y}{D_p} = \left(1 - \frac{t}{\tau}\right)^2 \qquad (26)$$

Figures 43 and 44 show plots of Equations (25) and (26), respectively. The lengths on the plot of τ were chosen to be representative of realistic system volumes (including a correction to square cross section). $L_{tot} = 15$ ft corresponds to to a Westinghouse feedring (approximately 40 ft of 10 inch pipe) with a short (5 foot) horizontal run of 16 inch feedring ($D_p = 14.3$ inches); $L_{tot} = 45$ ft corresponds to a CE feedring (approximately 60 ft of 12 inch pipe) with a long (28 ft) horizontal run; $L_{tot} = 30$ ft represents intermediate volumes of a Westinghouse feedring and a long pipe run or a CE feedring and a short pipe run.

It is useful to estimate the quantitative effects of the various idealizations. It should be straightforward to refine the analysis to treat pipes of circular cross section, if desired, although numerical calculations may be required. It is likely that only a small correction factor on τ will result.

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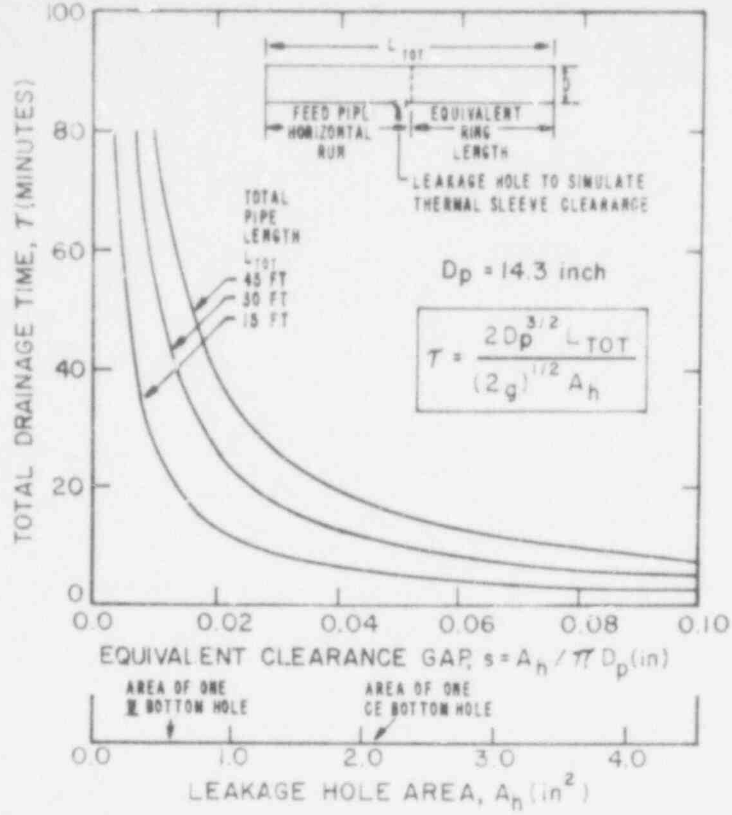
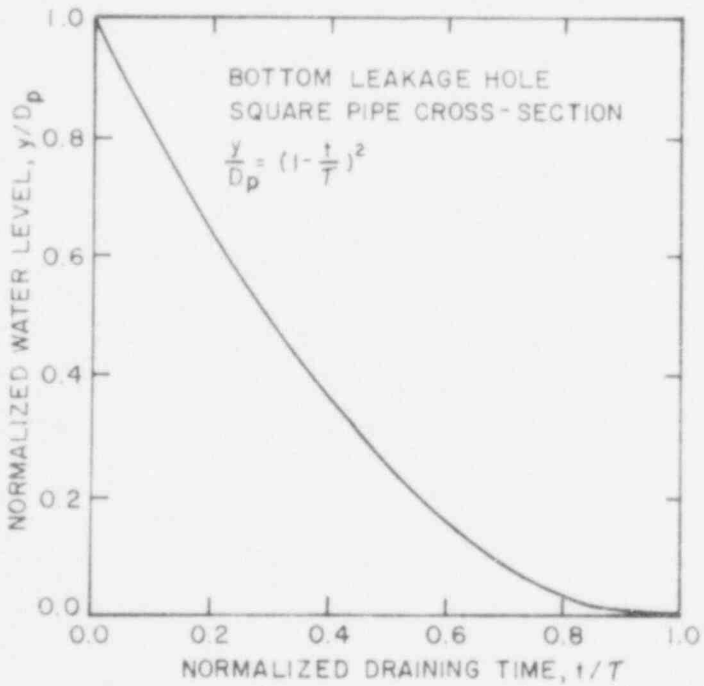


ESTIMATED DRAINAGE TIME WITH TOP DISCHARGE DEVICES

FIGURE 43



TRANSIENT DRAINING CHARACTERISTIC

FIGURE 44

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Similarly the effect of multiple pipe sizes in a feedring assembly is slight since all pipes are near to the same size, the greatest disparity coming between 10 inch pipe (10 inch I.D.) and 16 inch pipe (14.3 inch I.D.). A more significant question concerns the distribution of the leakage gap. If the gap were uniformly distributed about the periphery (with a square pipe), the total drainage time τ would be doubled. Of course, if the gap were only at the top then the water could only drain to that level and the total drainage time would be infinite. The present idealized gap distribution (and "clean" hole assumption) offers the advantage of giving a lower bound, but it should be appreciated that such an analysis will tend to predict more rapid drainage than is realistic.

The major uncertainty is the size of the leakage gap. Combustion Engineering personnel [3] indicated that CE thermal sleeves are a machined part with a cold gap of 0.030 ± 0.030 in. This is shown on typical drawings in the public domain such as those supplied to the NRC by the Louisiana Power and Light Company for the Waterford #3 plant [65]. From informal discussions with Westinghouse personnel following the meeting [2], it was determined that the "cold" clearance is a 0.020 ± 0.015 inch tolerance stack up. Bounding analyses conducted by Westinghouse predict up to 0.030 inch thermal expansion and 0.008 inch pressure expansion. Thus the gap can range from 0.005 to 0.073 inch and moreover is not a simple annular clearance. Publication of these data and analyses is needed.

In CE plants, simple thermal expansion may increase the internal gap, but in Westinghouse plants the thermal sleeve clearance is also subject to expansion by vessel pressure. Finally, all of these gaps may or may not exist at all and in practice could vary widely in size due to variations in alignment, potential buildup of corrosion products and potential enlargement by erosion.

With a nominal gap of 0.020 inches, complete drainage in approximately 10 to 40 minutes is indicated, depending on pipe length.

Drainage tests at Indian Point #2 (data reported in Appendix A) with the system "cold" gave approximately 30% drainage in 15 minutes. Based on Equations (25) and (26), a gap size of 0.010 inches or less is indicated. During the feedwater flow instability tests at Trojan (hot) described in Appendix A, thermocouple records indicated that drainage was less than 50% after 30 minutes, but complete after two hours. Either of these numbers indicates small or heavily blocked clearance gaps equivalent to 0.010 inches or less. Thus, the limited available full-scale data indicate a smaller than nominal (0.020 inch) clearance gap in the context of a conservative analysis that would tend to overpredict drainage rate.

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Drainage tests were conducted using the 1/4-scale feedring model in our hydraulic facility with most of the holes sealed to leave only one or a few holes open. The data are compared with the theory in Figure 45 and demonstrate excellent agreement. The dashed line is Equation (26) and the solid line represents the equations below which result from a refined theory that accounts for the change 2δ in cross-sectional diameter at the junction of the feedring and the feedpipe.

$$\tau = \left(\frac{2}{g}\right)^{1/2}\left(1 - \frac{\delta}{D_p}\right)^{1/2} \frac{D_p^{3/2} L_{tot}}{A_h} \tag{27}$$

$$\frac{y}{D_p} = \frac{\delta}{D_p} + \left(1 - \frac{\delta}{D_p}\right)\left(1 - \frac{t}{\tau}\right)^2 \tag{28}$$

In this formulation L_{tot} is the equivalent total length of feedpipe having the same volume as the actual system, and $\delta = 0.125D_p$ in these experiments.

The evidence suggests that the actual clearance gaps in PWRs have a somewhat higher flow resistance than is indicated by calculations with the conservative analysis above using the nominal 0.020 inch gap specified by the vendors. The theory itself is probably adequate if the leakage characteristic or the size, geometry, and distribution of clearance is specified accurately. Improved information on thermal sleeve clearances and additional drainage data are needed to provide a quantitative basis for establishing technical specifications or automatic controls to be used in conjunction with top discharge devices.

Pipe Running Full: Bottom Drainage

In bottom discharge systems, drainage is rapid if the feedwater flow is stopped. Application of Equation (25) suggests that nearly complete drainage of a typical system can occur in seconds. The significant problem is therefore to find the water level characteristic as a function of feedwater flow. This is done later for the low flow rates characteristic of auxiliary feedwater systems. Below a preliminary estimate is made of the limiting flow rate for the pipe to run full of water.

First ignore the smaller feedring and consider the feedpipe as if it were open-ended. Previous work such as that of Wallis, et al [66] has demonstrated that an open-ended pipe will run full with:

$$j_f^* = \frac{\rho_f^{1/2} j_f}{[gD_p(\rho_f - \rho_g)]^{1/2}} = 0.5 \tag{29}$$

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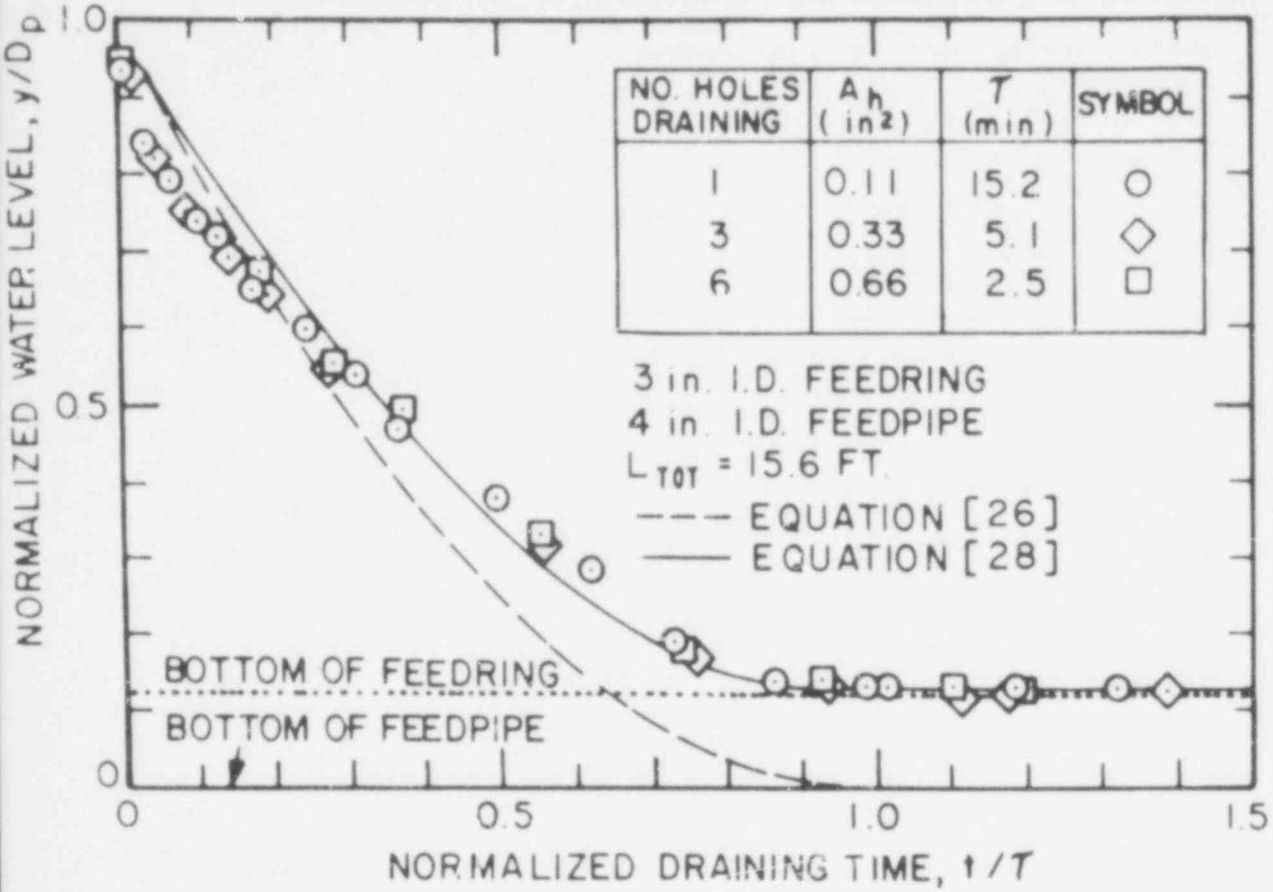

COMPARISON OF DRAINAGE ANALYSIS WITH 1/4-SCALE FEEDRING DATA

FIGURE 45

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where $j_f = Q_f/(\pi D_p^2/4)$ is the superficial water velocity. With a pipe diameter of $D_p = 14.3$ inches (16 inch Schedule 80 pipe) this gives a limiting flow rate $Q_f \approx 1500$ gpm.

The feedring may be considered separately. When the feedring is full, the bottom discharge holes should behave, crudely, as sharp-edged orifices with a contraction coefficient of approximately $C_c = 0.6$. Assuming that the driving head is purely hydrostatic, the discharge velocity can be estimated by $V = \sqrt{2gh}$ where h is the sum of the feedring diameter D_r and the ledge height ($[D_p - D_r]/2$). A discharge velocity of approximately $V = 8$ ft/sec results. The flow rate is then $Q_f = C_c A_h V$ where A_h is the total hole area. In typical Westinghouse systems with 251 holes of 3/4 inch diameter a flow rate of approximately 1500 gpm results; in typical CE systems with 72 holes of 1.6 inch diameter a flow rate of approximately 2200 gpm results. There is thus some indication that the feedring may contain a void even with the feedpipe running full, particularly in CE systems, but the estimates for the two components are quite close.

Flow rate available in the 1/4-scale hydraulic facility was only 30 gpm, which was insufficient to run the system full. This is consistent with the analysis above which predicts 60 gpm to run the 4 inch feedpipe full. At the highest flow rates (near 30 gpm), the level in the scaled feedring was appreciably lower than the level in the scaled feedpipe, which was over half full.

Measurements at Doel #2 using conductivity probes in a "cold" system indicated that the feedpipe ran full at 1750 ± 250 gpm with increasing flow and 1200 ± 300 gpm with decreasing flow. This is quite consistent with the predictions above. Such hysteresis is common in tests of this type, as described for example by Wallis, et al [66].

A bounding first-order estimate has been made of the flow rate required to maintain bottom-discharge feedwater systems full of water. These estimates derive support from the data available from tests in air-water systems. However, there may be additional effects in steam-water systems due to condensation and local "flooding" phenomena in and near the feedring holes (similar to the effects causing ECC bypass in subscale models of PWR downcomers). The potential for such an effect with highly subcooled feedwater can be demonstrated by approximate calculations. Moreover, 1/10-scale model experiments with steam and water demonstrated that the feedpipe and sparger could be maintained full at very low rates (2 gpm) without waterhammer even though there were waterhammer events when the same flow rate was introduced into an initially drained feedpipe. Further analyses and experiments exploring these potential additional effects may be useful to help explain some observations, such as the fact that waterhammer has never been reported in CE systems following an automatic ramp down to 5% flow (approximately 500 gpm).

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The present estimate of 1500 gpm required to run the system full is well above the few hundred gpm (per steam generator) supplied by typical auxiliary feedwater systems. This low flow rate is sized to avoid overcooling the primary coolant, and it is unlikely that a 1500 gpm flow rate could be sustained for long times following an unusual operating transient, e.g., a reactor trip, even if the pumping capacity were available.

Hydraulic Transients

Rapid variations in flow rate cause hydraulic waves to travel back and forth in the piping. Such waves disrupt the water surface, may enhance condensation locally, and may cause water slugs to be formed. Although the remaining work in this section of the report describes analyses and experiments of quiescent flow up to slug formation, it is important to bear in mind that such behavior can be augmented by hydraulic transients that are difficult to describe.

During our air-water tests in the 1/4-scale facility, water slugs could be produced readily in the feedpipe by turning the water flow on rapidly with some air flowing. It was not possible to produce such slugging if the water flow rate was increased gradually because the maximum water flow rate possible with our facility did not fill the pipe sufficiently (as with typical auxiliary feedwater systems).

In what follows, relations are developed to describe the upstream water depth and number of holes flowing water as a function of water flow rate. This hydraulic information is subsequently employed in a prediction of a hydraulic instability arising from multiphase flow at the discharge holes and leading to slug formation in the feedring.

Water Depth in the Pipe

After the initial hydraulic transient a steady open-channel flow is set up in the feedpipe and bottom-discharge ring (unless either slug formation or the pipe "running full" occurs first). At low flow rates only the first few holes in the ring carry any water flow. Figure 46 shows this situation for an idealized feedring represented by a straight pipe; a similar condition will probably occur in the two branches of a PWR feedring.

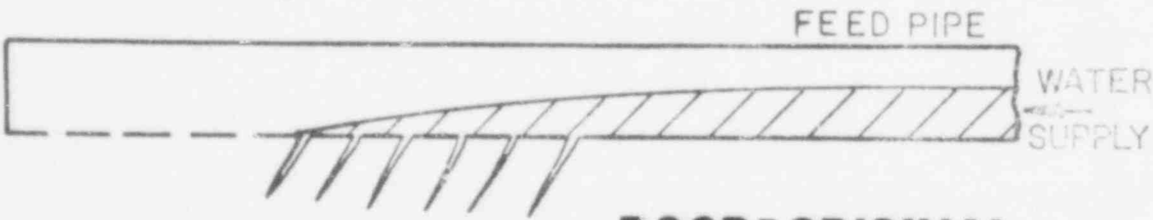

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FIGURE 46 IDEALIZED REPRESENTATION OF A FEEDRING BY MEANS OF A STRAIGHT PIPE.

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An early attempt was made to model the flow in the ring. It was assumed that the holes could be characterized by a constant discharge coefficient and that the flow through them was proportional to the square root of the local depth. The analysis predicted that 570 gpm would be needed to cause water to flow from all of the holes in the ring of a typical PWR (without countercurrent steam flow), while 220 gpm would be sufficient to cover half of the holes. The water depth needed at the start of the ring, near the tee, was only 2.5 inches in a 4.0 inch diameter pipe in order to cover all of the holes. Rather surprisingly, it was found that all of the open channel flow in the ring was in the "shooting flow" regime with average velocities bigger than the local velocity of a surface wave at all locations. This discovery implied that a better analysis would be needed at low flow rates since some of the original assumptions were based on "tranquil flow".

In order to achieve shooting flow in an open channel with a low velocity upstream (at the bend) there must be a transition somewhere through the "critical" condition. In a PWR steam generator this is most likely to occur at the feedring tee; this is in fact where a transition to supercritical flow was observed in the Creare feedring model.

Water depth measurements three pipe diameters upstream of the first hole are shown in Figure 47. The critical depth for a given water flow in a circular channel can be computed by the method described by Chow [14]. This prediction is plotted in Figure 47 and is seen to fall close to all the data up to the point close to the onset of slug formation. The depth change over the last few feet of horizontal run before the ring was negligible until about 2–4 inches before the first hole.

When the same theory is applied to the "water only" data from the Creare steam generator model, the agreement is equally good and is sufficiently close to confirm the conclusion that the critical depth was achieved shortly before the water reached the first discharge hole (Figure 48).

Now that means to determine water depth are established, relations are developed to determine the number of holes discharging water as a function of water flow rate.

Discharge Through the Bottom Holes

Since the water in the ring is in the shooting flow regime, its forward kinetic energy is comparable with or greater than its potential energy relative to the base of the pipe. Therefore, the assumption of a constant discharge coefficient for the holes, based on the local depth, is questionable.

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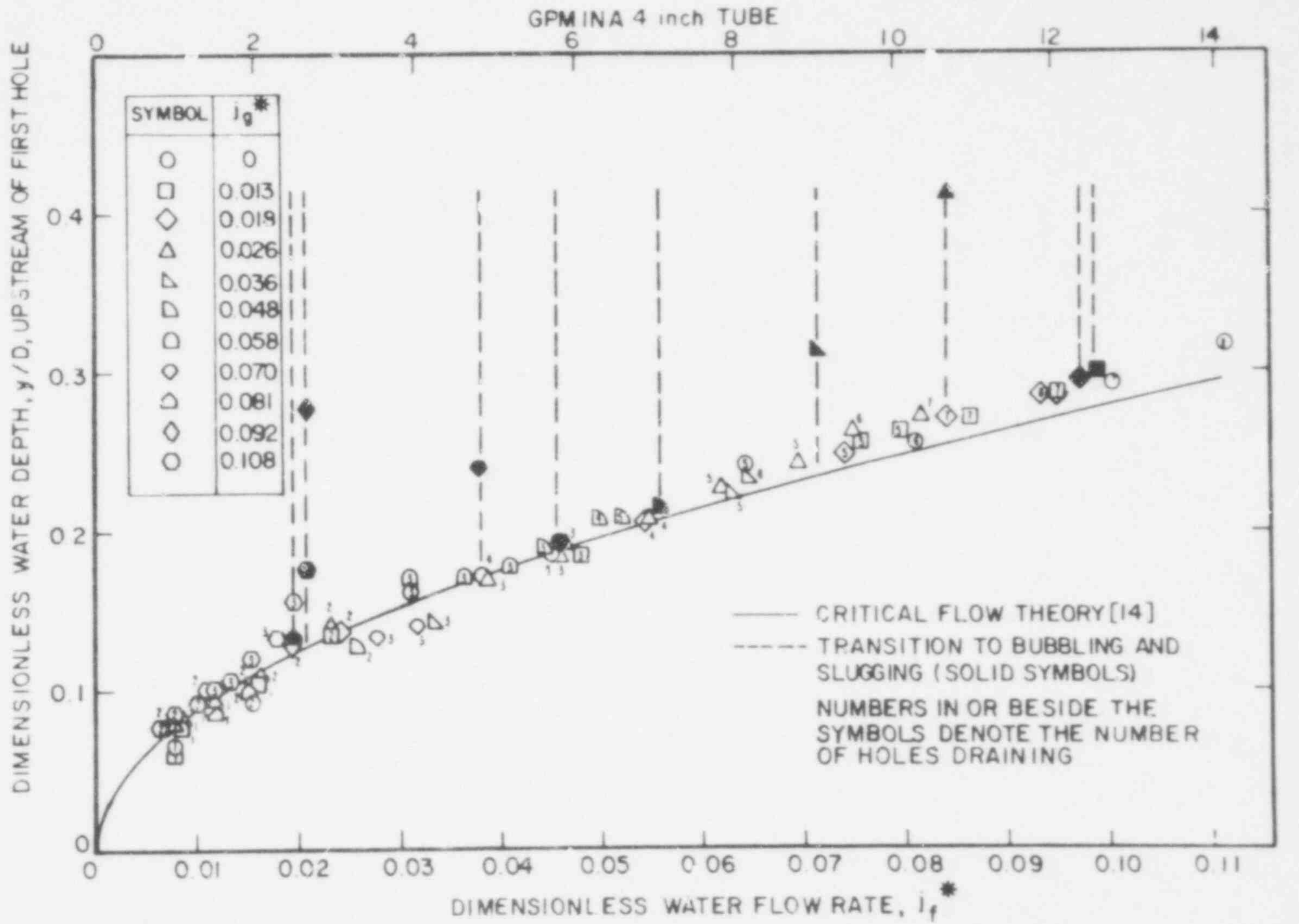
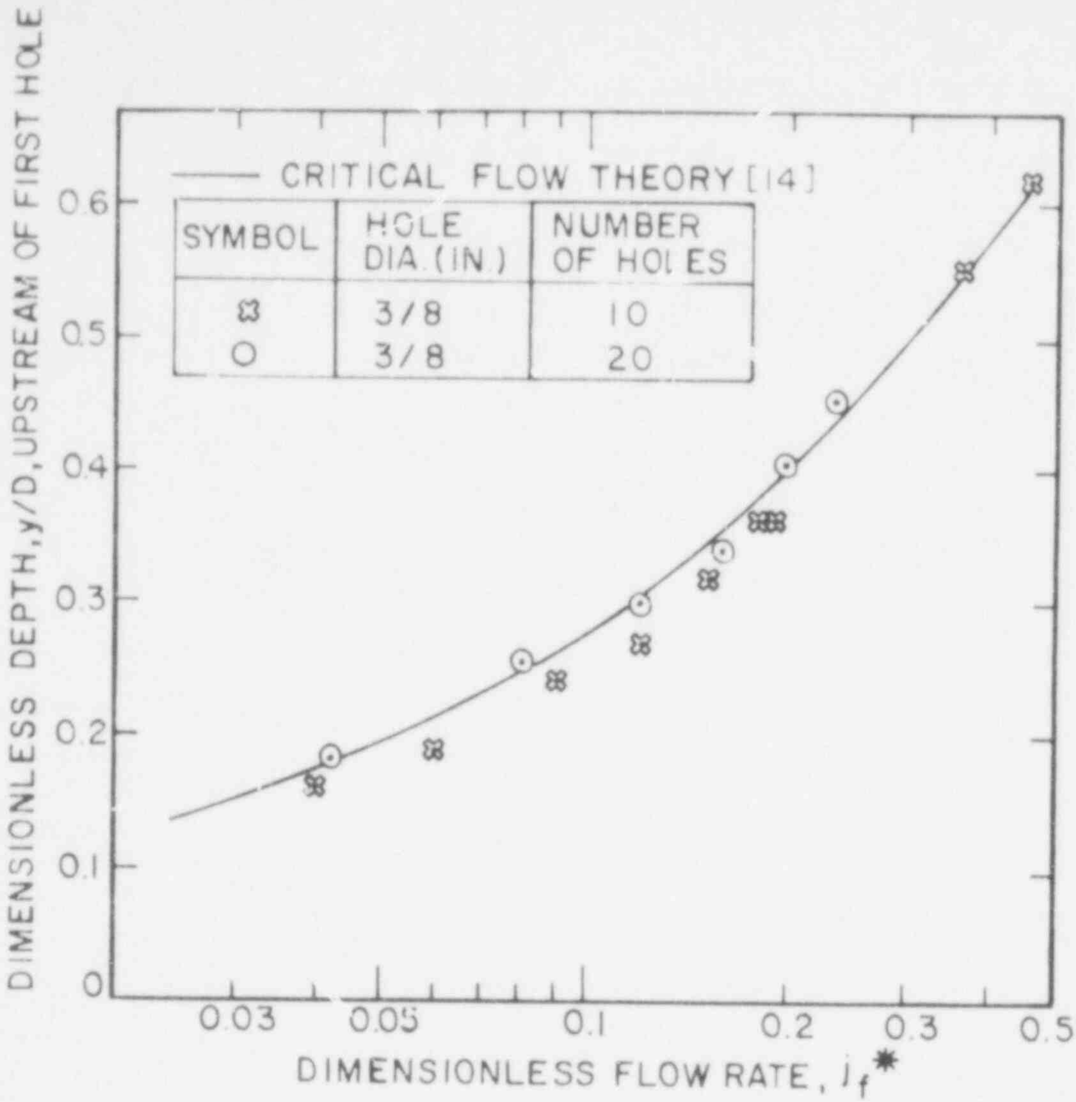
FIGURE 47



OBSERVED WATER DEPTH BEFORE FIRST HOLE AS A FUNCTION OF AIR AND WATER FLOW RATES

D = 4 INS.; d = 1 INS.; 10 HOLES



WATER DEPTH DATA FROM THE 1½ INCH DIAMETER MODEL FEEDPIPE COMPARED WITH CRITICAL DEPTH THEORY.

FIGURE 48

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Figure 49 shows the situation. The water approaches the hole with a mean velocity V and depth y. There are variations in depth across the tube, as indicated. The water flows out of the hole at an angle, instead of straight downwards.

The details of the flow near the hole are quite complicated. However, we may make approximate conclusions.

Applying Bernoulli's equation to the water jet leaving the lip of the hole, we have

$$\frac{1}{2} V_o{}^2 = \frac{V^2}{2} + yg + \frac{(p_t - p_g)}{\rho_f} \qquad (30)$$

In the case where all of the holes are not covered and there is no steam (or air) flow, $p_g = p_t$ and we may write

$$V_o{}^2 = V^2 + 2gy = 2gH_o \qquad (31)$$

where H_o is the total head of the water flow, equal to the upstream total head if friction can be ignored.

If (y/H_o) is close to 1, the flow is almost stagnant in the pipe and the jet is directed straight downwards. The discharge rate from the hole is

$$Q_h = V_o \frac{\pi}{4} d^2 C_{Df} \qquad (32)$$

Where C_{Df} is a "discharge coefficient" equal to 0.61 for a potential flow (from a quiescent water pool) springing neatly clear at a sharp-edged circular orifice in a plane wall. If the jet wets the wall and hangs on to it until reaching the bottom edge, a higher coefficient, up to 1.0, is possible.

On the other hand, when (y/H_o) is small, V is close to V_o and the jet is "squashed up" at the front end of the hole allowing a much lower discharge area perpendicular to V_o and hence, a lower discharge rate. This could be accounted for by allowing C_{Df} to depend on (y/H_o) or (V/V_o) or some other suitable parameter. Since we shall later be concerned with situations in which $p_t \neq p_g$, the velocity ratio is a better choice.

The data shown in Figure 50 clearly show this decrease in discharge rate as y decreases. It was additionally observed that the depth y decreased approximately linearly from the first to the last hole flowing water with a curved surface before the first hole. This situation is idealized in Figure 51 in which the upstream (critical) depth, y_c, is assumed to occur at a "o^{th}" hole which is upstream of the first

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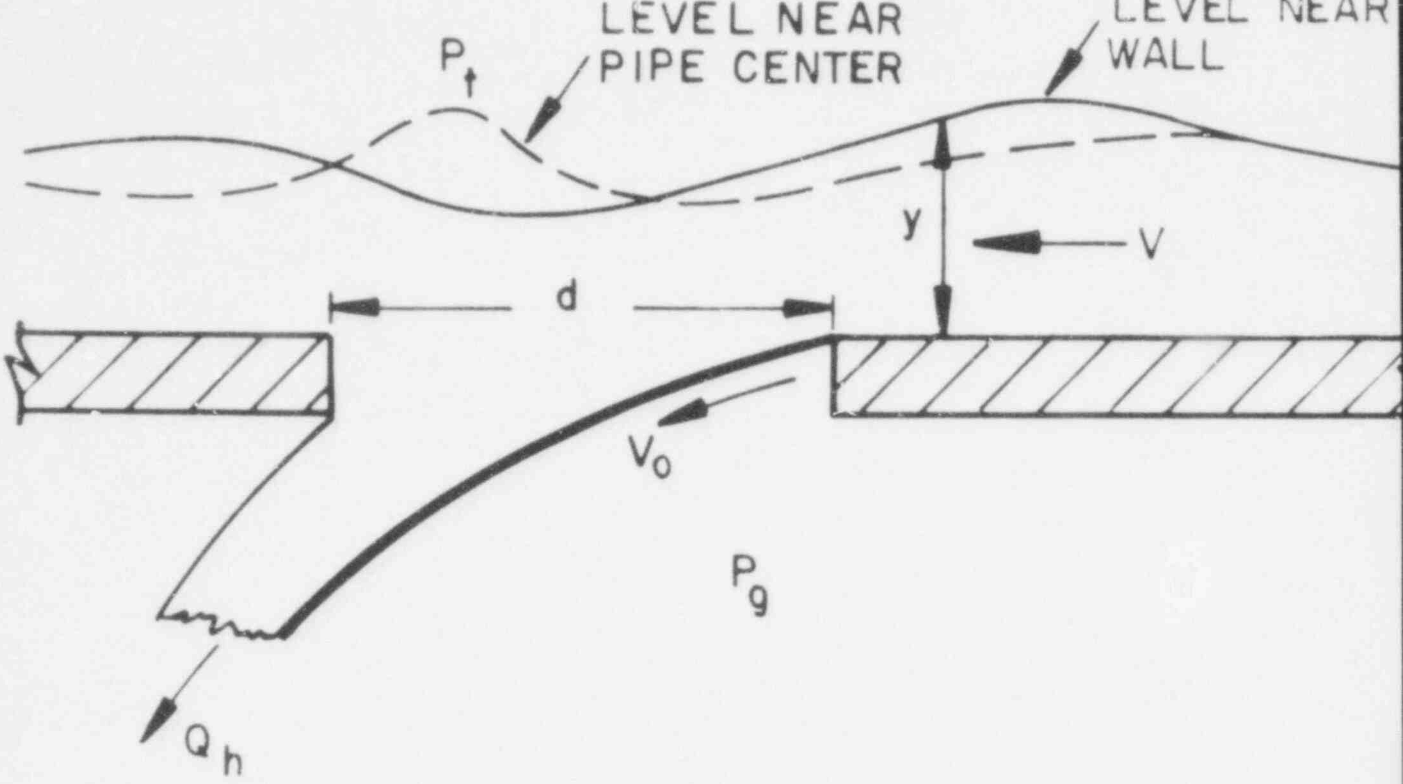
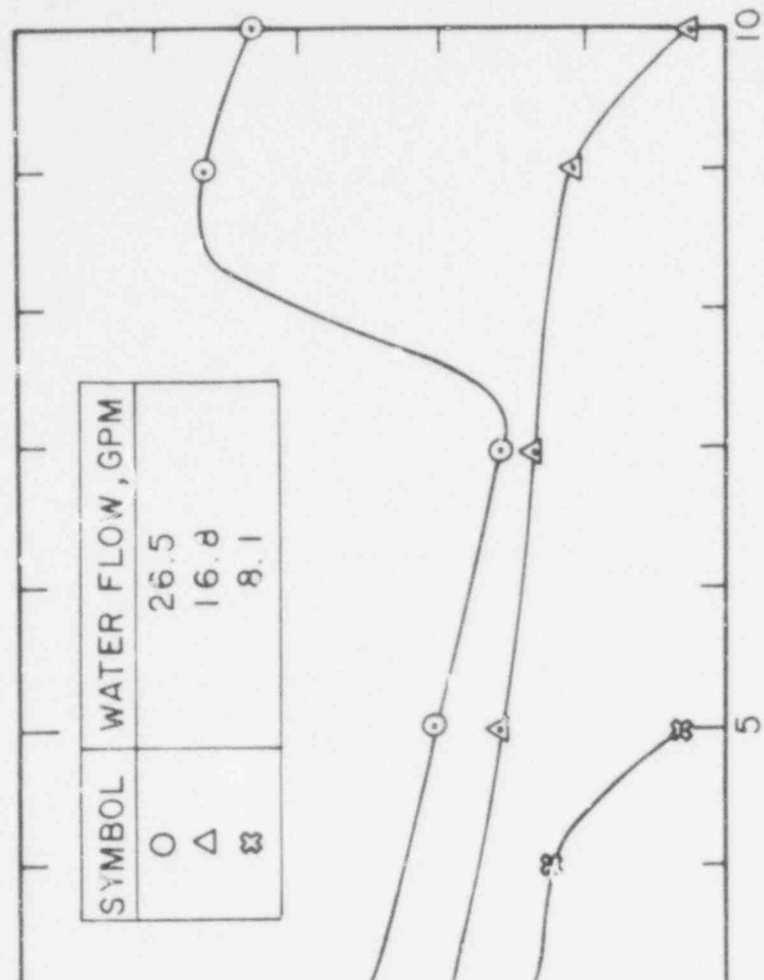


DISCHARGE OF WATER FROM A SINGLE
FEEDRING HOLE

FIGURE 49



FLOW RATE PER HOLE AT VARIOUS OVERALL WATER FLOWS. "WATER ONLY" TESTS IN 4 INCH DIAMETER PIPE.

FIGURE 50

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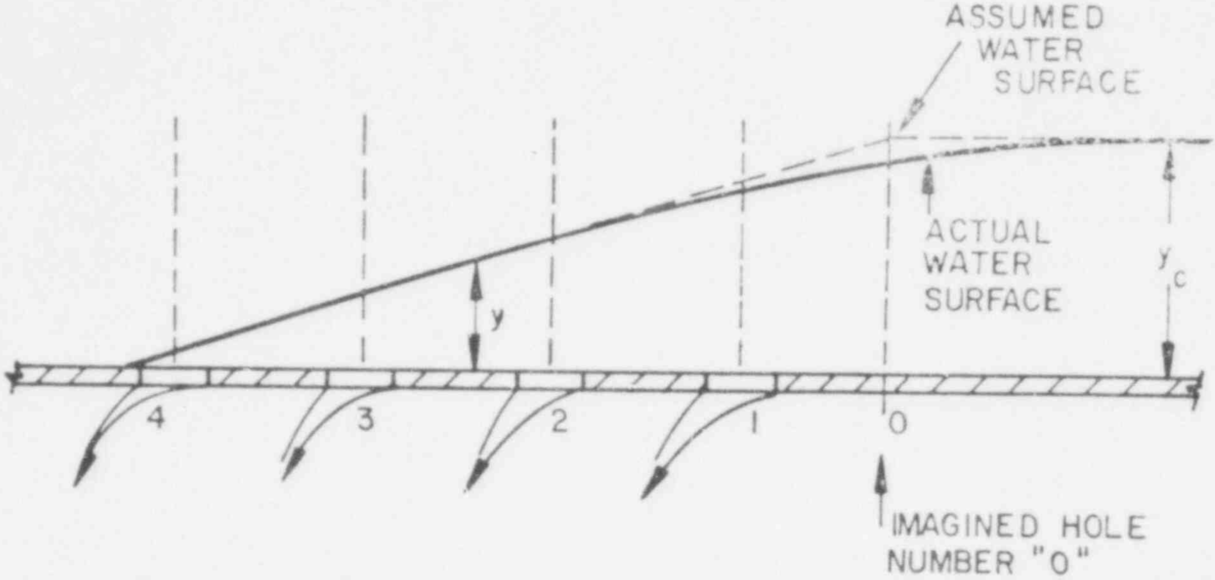

IDEALIZED WATER SURFACE PROFILE

FIGURE 51

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hole by the normal hole spacing. Since the upstream depth is known, the depth above each hole can now be computed. The data which should be least sensitive to these assumptions are for the 16.8 gpm flow rate.

For the range of depths of interest the ratio (y_c/H_o) is close to 3/4. Since y_c is known from critical flow theory, H_o (and V_o) can be calculated. Knowing y at each location, V follows from (31) and V/V_o can be computed. Since the discharge from each hole was measured, C_{Df} can be calculated for the present experiments using Equation (32).

Figure 52 shows C_{Df} plotted versus V/V_o for this case. The line shown is chosen to start at $C_{Df} = C_{Do} = 0.61$ when $V = 0$. It can then be fit with the equation

$$C_{Df} = C_{Do}(1 - 0.8\frac{V}{V_o}) \qquad (33)$$

going to zero at the extrapolated value $V/V_o = 1.25$).

For practical purposes we do not wish to have to calculate on the basis of individual holes. It is more useful to employ an average value of C_{Df}, for a group of m holes, based on the common velocity V_o and a characteristic axial velocity which we could choose as V_c, the velocity at the critical depth. Since to a good approximation for a circular pipe over the range of interest $y_c/H_o \approx 3/4$, $V_c \approx V_o/2$ for $p_t = p_g$. Since $V \approx V_o$ at the last hole $(y = o)$ an average value of V is $\frac{V_o/2 + V_o}{2} = \frac{3}{4}V_o = \frac{3}{2}V_c$. Therefore, it seems reasonable to rewrite (33) as

$$C_{Df} = C_{Do}\left[1 - \left(\frac{3}{2}\right)(0.8)\frac{V_c}{V_o}\right]$$

$$= C_{Do}\left(1 - 1.2\,\frac{V_c}{V_o}\right) \qquad (34)$$

Since "averaging" a non-linear phenomenon like this is subject to several errors, we shall choose to use the expression

$$C_{Df} = C_{Do}\left(1 - B\,\frac{V_c}{V_o}\right) \qquad (35)$$

with B expected to have a value close to 1.2.

The flow through m similar holes is then, from (32) and (30)

$$Q_f = m\frac{\pi}{4}d^2\,C_{Do}\,V_o\left(1 - B\frac{V_c}{V_o}\right) \qquad (36)$$

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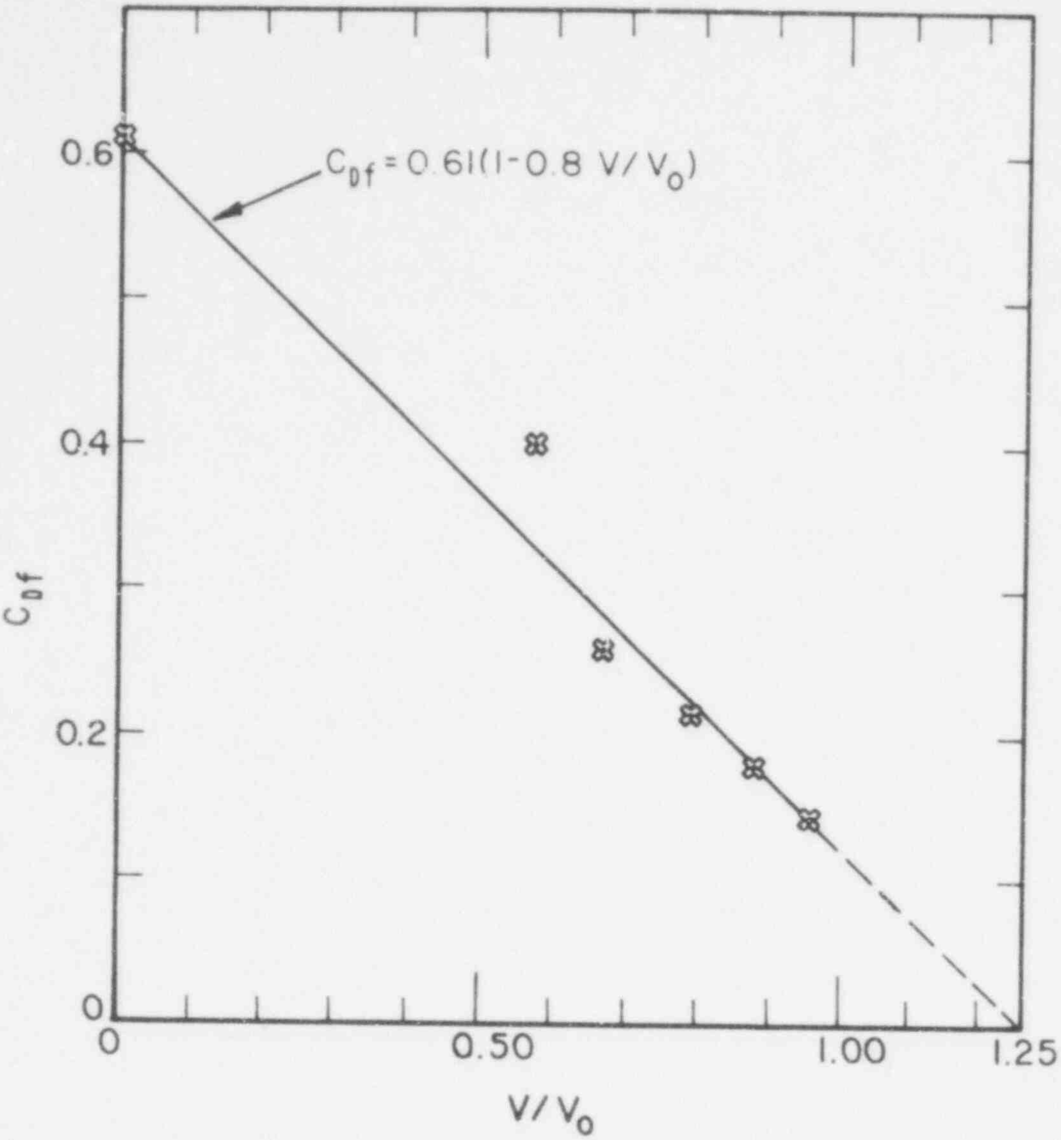

C_{Df}, THE HOLE DISCHARGE COEFFICIENT, VERSUS THE RATIO V/V_0 FOR A FLOW OF 16.8 GPM IN THE 4 INCH DIAMETER PIPE

FIGURE 52

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or

$$j_f = \frac{Q_f}{\frac{\pi}{4}D^2} = m\frac{d^2}{D^2}C_{Do}(V_o - BV_c) \tag{37}$$

A check on the validity of Equation (37) can be performed by using it to solve for the number of holes carrying flow in a "water only" experiment. In this situation $p_t = p_g$. Knowing j_f (i.e., Q_f) we can calculate V_c from the critical depth theory, deduce H_o and V_o from (31) and predict m by Equation (37).

In order to compare data from different pipe sizes, it is more convenient to work in terms of the dimensionless parameters.

$$j_f^* = j_f\left(\frac{\rho_f}{gD\Delta\rho}\right)^{1/2} \approx \frac{j_f}{(gD)^{1/2}} \tag{38}$$

$$V_c^* = \frac{V_c}{(gD)^{1/2}} \approx V_c\left(\frac{\rho_f}{gD\Delta\rho}\right)^{1/2} \tag{39}$$

$$H_o^* = \frac{H_o}{D} \tag{40}$$

Then, Equation (37) can be written as

$$j_f^* = m\frac{d^2}{D^2}C_{Do}[(2H_o^*)^{1/2} - BV_c^*] \tag{41}$$

or

$$m^* = \frac{md^2}{D^2}C_{Do} = \frac{j_f^*}{(2H_o^*)^{1/2} - BV_c^*} \tag{42}$$

Since j_f^*, V_c^* and H_o^* can all be interrelated by critical flow theory, Equation (42) predicts that m^* should be a unique function of j_f^* for all pipe diameters and hole sizes. Since the theory is not valid for non-integral numbers of holes, errors of the order of one hole are to be expected.

Figure 53 shows a comparison between this theory and the results obtained by Creare in the models with a straight pipe sparger. In the case of the 3/8 inch holes, it was noticed that surface tension acted to attach the issuing water jet to the lower run of the holes; therefore, a value of $C_{Do} = 1$ has been taken for these cases. The agreement between theory and experiment is quite good for a value of $B = 1.2$ while $B = 1.1$ may give a better estimate of a "lower limit" to the number of active holes.

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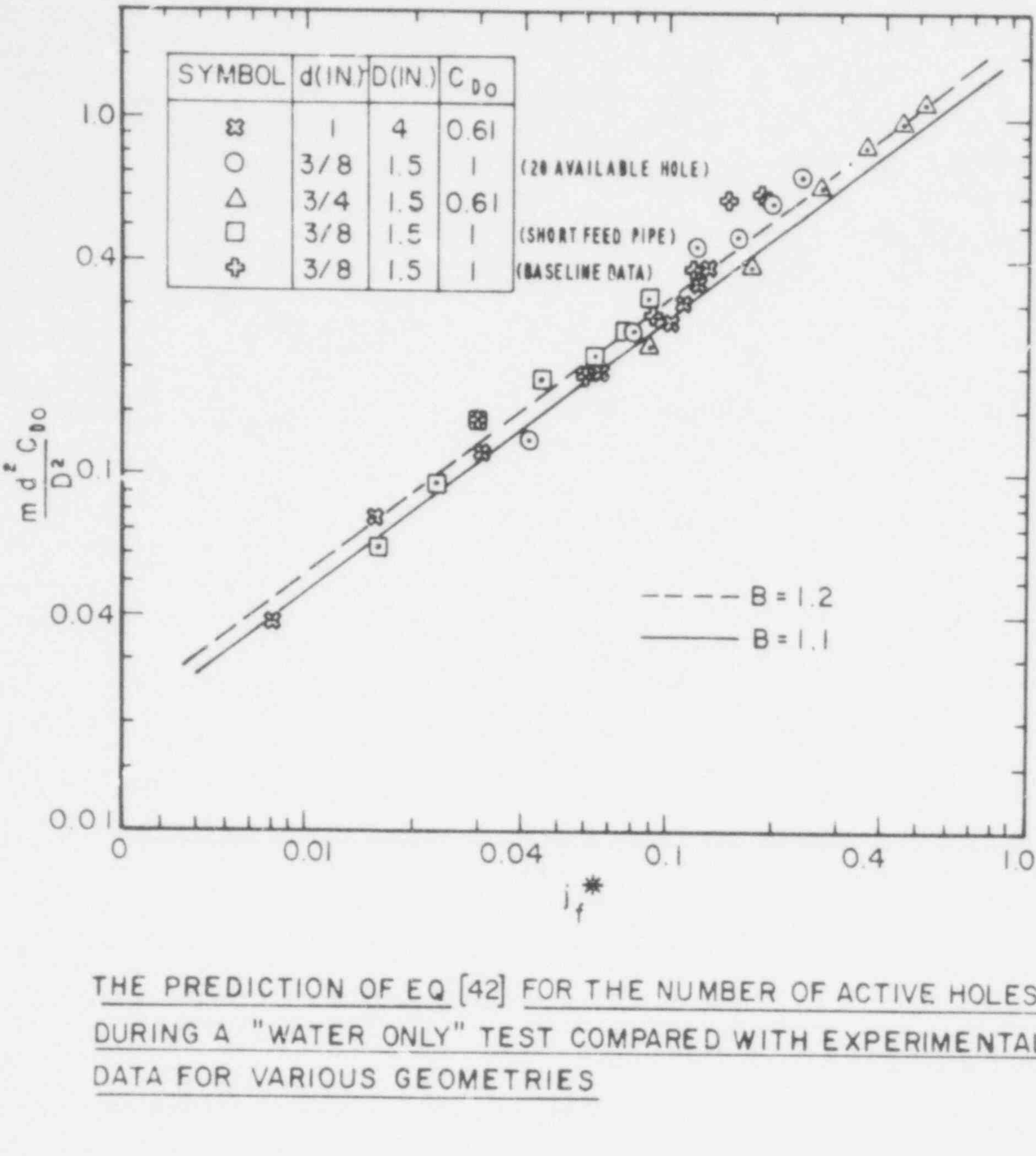

THE PREDICTION OF EQ [42] FOR THE NUMBER OF ACTIVE HOLES DURING A "WATER ONLY" TEST COMPARED WITH EXPERIMENTAL DATA FOR VARIOUS GEOMETRIES

FIGURE 53

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The parameter $\frac{md^2}{D^2} C_{Do}$ is a measure of the ratio of the effective flow area of the holes that are actually carrying water flow to the cross-sectional area of the feedpipe.

The straight pipes used to simulate the feedring in these hydraulic experiments were quite short. In a longer feedring (i.e., greater L/D) the effects of friction will be more important and this theory will be less accurate. In an actual feedring, both friction and inertia forces are likely to be important; therefore, a more elaborate theory, or more comprehensive experiments, may be needed if an accurate calculation procedure is desired.

Summary of Feedring Hydraulics

An analysis has been developed which describes the steady state characteristics of the idealized straight pipe feedring model and may be regarded as an approximation to the behavior of a PWR feedring. It is emphasized that the hydraulic behavior observed in the 1/4-scale circular feedring model (Appendix D) was qualitatively identical to the behavior observed in the straight sparger model, although quantitative data were obtained only with the straight sparger in place.

In the next section we shall combine this theory with an analysis of countercurrent gas flow to derive a model for slug initiation.

Slug Initiation With Air Flow

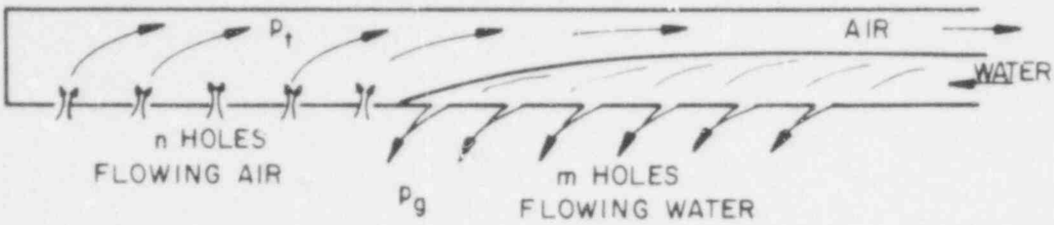
Hydraulic Instability With Bottom Holes

During tests in the Creare 1/4-scale Hydraulic Facility, a transition was observed between a regime of steady orderly water flow through several holes (air flowing through the other holes) and a regime of splashing and bubbling with all holes covered by water. The purpose of this section is to explain the dynamics of this transition and derive a theoretical explanation of it.

Figure 54 illustrates the "orderly" flow regime. It differs from the "water only" situation in that the air flow through the uncovered "n" holes gives rise to a pressure drop. Assuming uniform flow through the holes, we may write

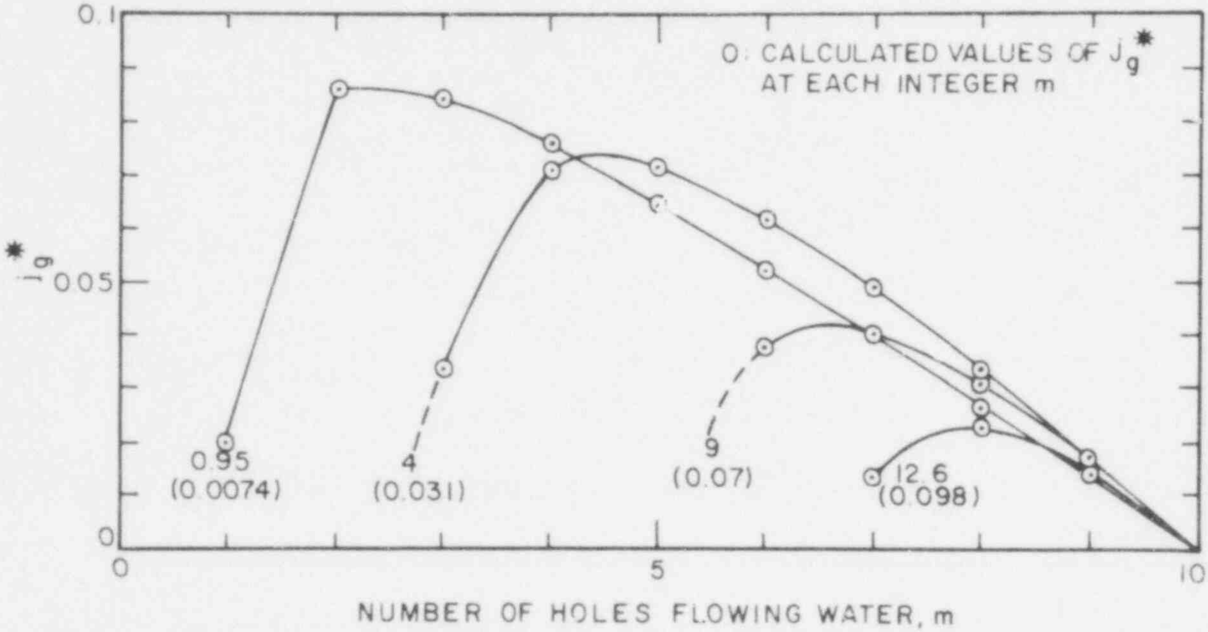
$$p_g - p_t = \frac{1}{2} \rho_g \left(\frac{Q_g}{nC_{Dg}\frac{\pi}{4} d^2} \right)^2 \qquad (43)$$

where C_{Dg} is the discharge coefficient for each hole and Q_g is the total gas flow rate.



THE MODEL FEEDPIPE OPERATING WITH "ORDERLY" AIR – WATER COUNTERCURRENT FLOW.

FIGURE 54



PREDICTION OF THE NUMBER OF HOLES CARRYING WATER FLOW AS A FUNCTION OF DIMENSIONLESS GAS FLOW RATE, j_g^*, FOR VARIOUS VALUES OF WATER FLOW RATE, GPM, (VALUES OF j_f^* IN PARENTHESIS) IN A 4 IN. DIAMETER TUBE WITH 10 - 1 IN. DIAMETER HOLES. B = 1.1

FIGURE 55

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Substituting (43) into (30), making use of the definition $j_g = Q_g/(\frac{\pi}{4} D^2)$ and introducing the total head H_o, we have

$$\frac{\rho_f V_o^{\,2}}{2} = gH_o\Delta\rho - \frac{\rho_g}{2}\left(\frac{j_g D^2}{nd^2 C_{Dg}}\right)^2 \qquad (44)$$

Now, V_o can also obtained by using (37), which is independent of whether there is countercurrent air flow or not, as

$$V_o = BV_c + \frac{j_f D^2}{md^2 C_{Do}} \qquad (45)$$

Combining (45) and (44) and using the parameters defined previously as well as the dimensionless parameter

$$j_g^* = j_g \rho_g^{\,1/2} (gD\Delta\rho)^{-1/2}$$

we have

$$j_g^* = nC_{Dg}\frac{d^2}{D^2}\left[2H_o^* - \left(\frac{j_f^* D^2}{md^2 C_{Do}} + BV_c^*\right)^2\right]^{1/2} \qquad (46)$$

The open-channel hydraulics are essentially independent of the air flow at the velocities of interest and therefore, H_o^*, V_c^*, and j_f^* are all functions of each other as described previously. For a given geometry and a given water flow rate, H_o^*, V_c^*, and j_f^* are known constants. The only variable on the right hand side of Equation (46) is the number of holes occupied by each phase. Since the total number of holes is fixed, this only represents one variable, which we will choose to be m.

Figure 55 shows some computations, based on Equation (46), for the four inch diameter tube with ten 1-inch holes facing downwards, using $C_{Dg} = C_{Do} = 0.61$ and $B = 1.1$. The calculations have been carried out for four values of water flow rate.

When m is large (approaching the total number of holes), n is small and j_g^* must tend to zero; this is the right hand side of Figure 55. As m decreases, it is possible for the square root term of Equation (46) to approach zero. Thus, j_g^* can be zero for two values of m, the total number of holes N and a lesser critical value m_c. Since j_g^* must be greater than zero according to Equation (46), j_g^* must have a maximum at some m within $m_c < m < N$, as seen on Figure 55. (j_g is imaginary for $m < m_c$, representing a physically impossible situation.) This value is the maximum possible gas flow rate that is tolerable (i.e., consistent with the above equations for orderly flow) at the specified water flow rate.

We may also deduce from Figure 55 and physical reasoning what will happen if an experiment is performed in which the air flow rate is increased at constant water rate. For example, consider a flow rate of 4 gpm. With zero air flow about 2-1/2

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holes are covered (in practice this will probably imply some discharge from the third hole). At a value of $j_g^* = 0.034$ the third hole is fully utilized. At about $j_g^* = 0.07$ the fourth hole is running full and water begins to penetrate to the fifth hole. This increased pressure difference reduces V_o and hence, the water discharge per hole. Since a constant water rate is flowing into the pipe, the excess water tends to progress to a further hole. This reinforces the whole process. Because, according to Figure 55, a progression of the water beyond the hole corresponding to the maximum value of j_g^* decreases the equilibrium value of j_g^* which is allowable, there is no way to stop the progression of the water, which advances to the end of the pipe covering all the holes. The air must now either bubble up through the water or blow the water in bulk away from several holes to form a large wave or slug.

Figure 56 shows this predicted transition locus for several values of B and the same pipe geometry as Figure 55. The small numbers beside each curve denote the number of holes corresponding to the maximum value of j_g^* . If this maximum is "flat", transition might actually be observed at a lower value of the number of holes draining water from the pipe.

Figure 57 compares this transition with the results observed in the 4-inch pipe. The agreement is excellent, without the need for introducing any new empirical parameters. The transition is somewhat sensitive to the parameter B, however, indicating the need to characterize the discharge coefficient of realistic hole configurations accurately if these methods are to be employed for calculations on full-scale systems.

It should be pointed out that all the above work applies to a hydraulic transition observed in straight pipe sections. Precisely the same type of hydraulic transition was observed qualitatively in a 1/4-scale model of a feedring (dimensions given in Appendix D). All of these hydraulic transitions occurred at low flow rates such that only a fraction of the feedwater pipe was filled. Much higher flow rates would be required to produce slugging in the tee section and feedpipe such as observed and reported by Roidt [5]. The hydraulic transition which occurs first, i.e., at low flow rates, may be expected to govern the slug formation behavior.

In the air-water experiments at flow rates above the transition, water covered all of the holes and splashed about violently in the feedring as air bubbled up through the holes and water flowed out of the holes alternately. Literal slugging was not observed in the air-water experiments, however, because the void collapse was limited by the blower pressure rise. As described in Section 4, the steam-water experiments in straight sparger sections displayed the same type of hydraulic transition just before slug formation and impact. Slugs formed in the sparger section and not in the upstream pipe. Similar behavior may be expected to occur in simulated feedrings during steam-water experiments, although this configuration was studied only with air and water in the present work.

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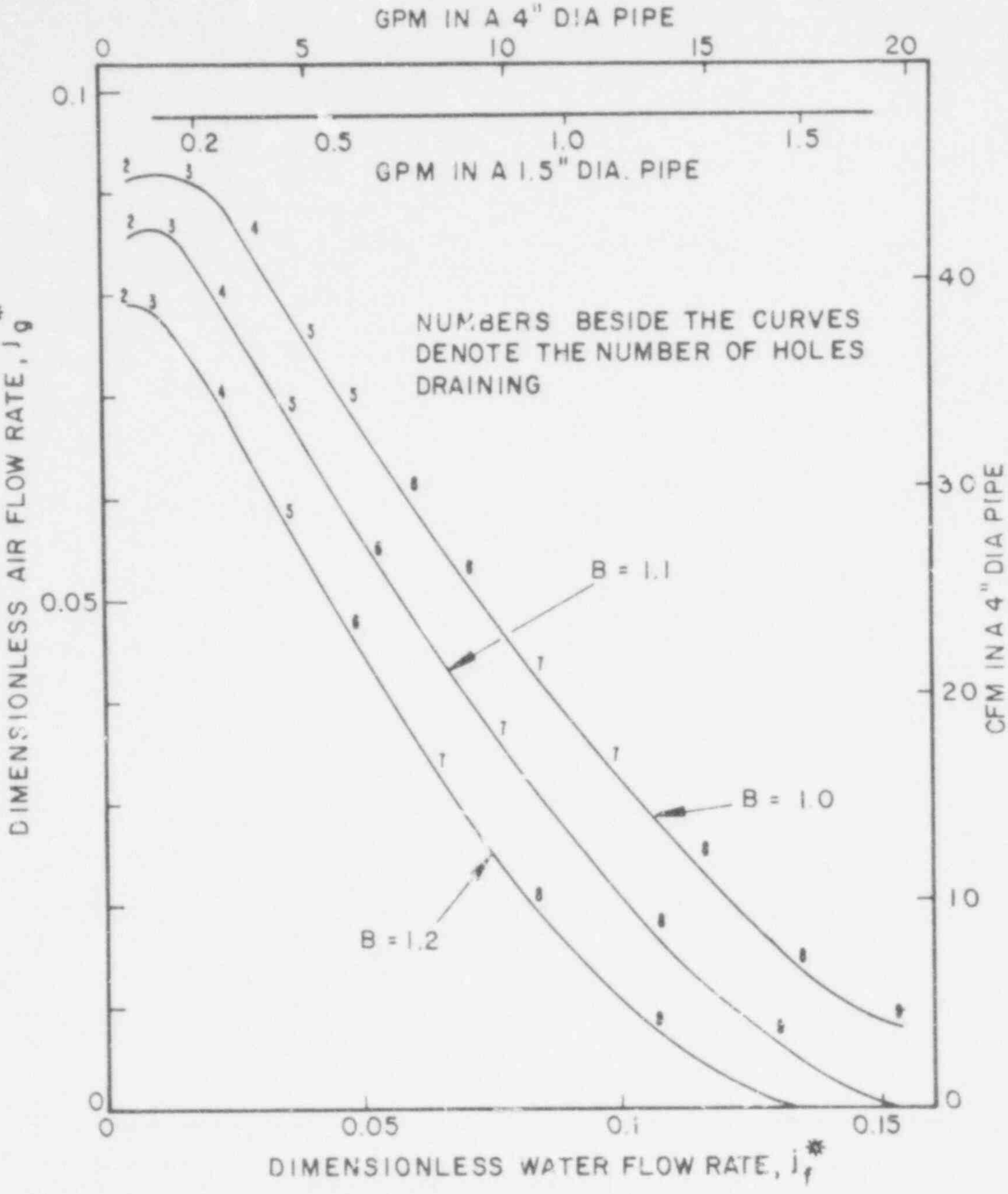

PREDICTED INSTABILITY LOCUS FOR 10 HOLES, $d/D = 1/4$, $C_{Do} = C_{Dg} = 0.61$

FIGURE 56

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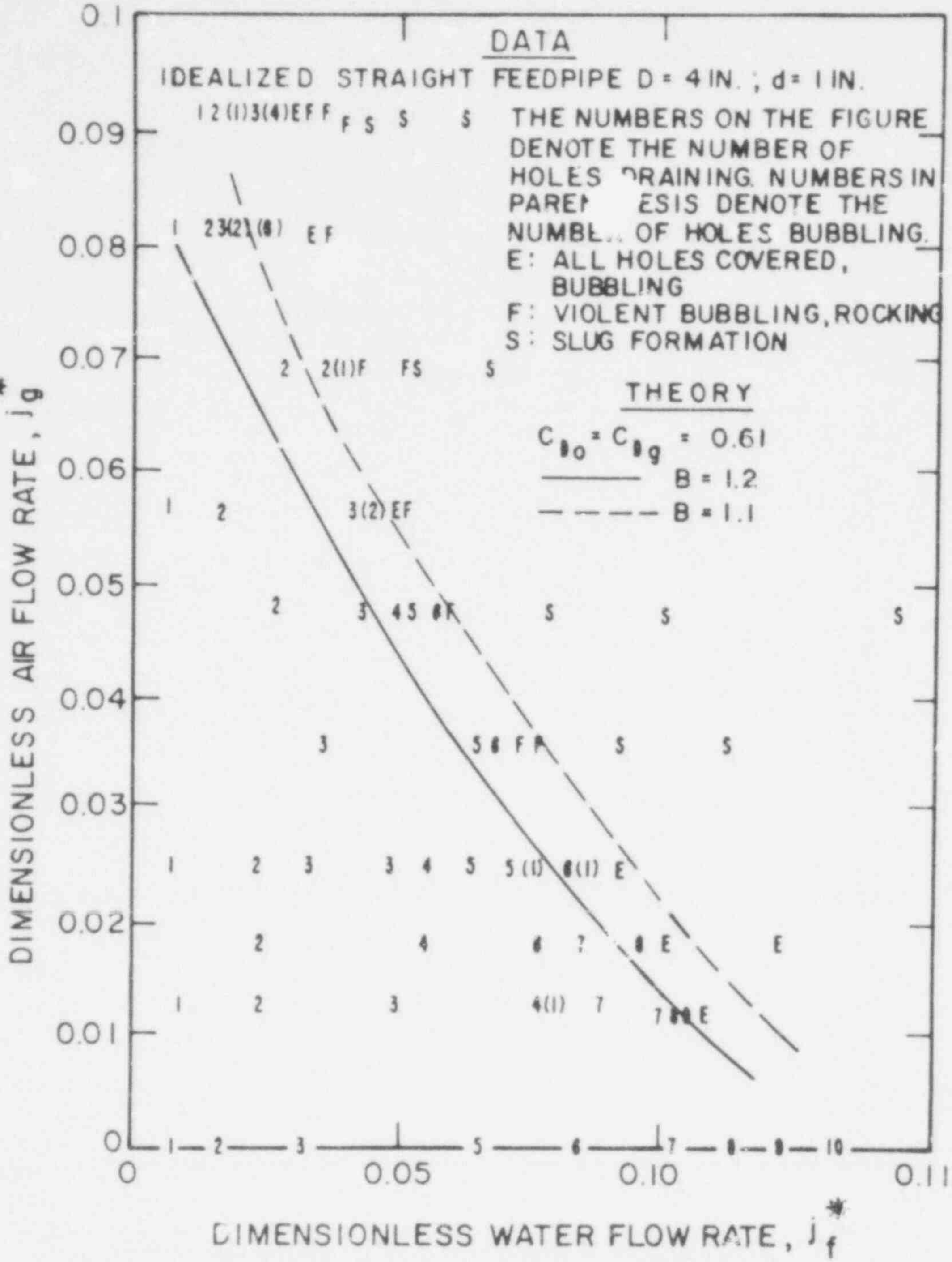

PREDICTED INSTABILITY LOCUS COMPARED WITH AIR-WATER DATA IN 1/4-SCALE HYDRAULIC FACILITY.

FIGURE 57

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The hydraulic type of slug initiation instability described in the foregoing applies to feedrings with bottom discharge flowing unimpeded into a vessel. If the flow is impeded, as may be the case with a rising water level in a PWR steam generator vessel, then this instability mechanism could be enhanced. (Alternatively, a rising water level might trap a void even without water flowing in the feedring.) Since an effect of rising vessel water level was not observed in our experiments, we have not attempted to derive an analysis describing the behavior. It should be appreciated that the hydraulic instability is already complex so that the addition of a flow impediment might prevent the development of even a first-order analysis and therefore should be handled on a rudimentary level.

Before extending the present analysis to treat slug initiation by the countercurrent flow of steam (rather than air), a different class of flow instability that might occur, particularly in the vendor recommended top discharge configuration, is considered.

Countercurrent Flow Instability in the Feedpipe

With discharge from the top of the feedring there is no equilibrium situation. At all water flow rates the pipe steadily fills, restricting the space above the water surface more and more. At a constant air flow rate the air velocity over the water surface increases as the level rises, approaching an infinite value as the pipe fills. There will, therefore, always come a point at which slugs can be formed by the Wallis-Dobson mechanism [20]. In a steam water test the resulting waterhammer will be milder the lower the steam flow rate and hence, the higher the water level before slug formation.

To illustrate the sort of numbers that can be deduced from such an analysis, consider a feedpipe half full of water. Then $\alpha = 0.5$ and Equation (1) predicts that slugs will form if $j_g^* > 0.177$.

At 1000 psi, with 40°F feedwater, assuming that condensation takes on its maximum possible steady state value, corresponding to raising all of the injected water to saturation temperature, this value corresponds to a water flow rate in a 16-inch pipe of

$$Q_f = j_g^* \frac{h_{fg}}{C_p \Delta T} (\rho_g g \Delta\rho D)^{1/2} \frac{\pi}{4} D^2$$

$$= (0.177)\frac{650}{535}\left(32.2 \times \frac{16}{12} \times \frac{1}{0.0216} \times \frac{1}{0.4456}\right)^{1/2} \frac{\pi}{4}\left(\frac{16}{12}\right)^2 \frac{7.48 \times 60}{62.4}$$

$$= 144 \text{ gpm}$$

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This is the minimum water flow rate calculated to cause slug formation at $\alpha = 0.5$, since steady-state condensation rates will generally be lower than predicted by thermodynamic equilibrium assumptions. In practice, subcooled water lying in the feedpipe may be exposed suddenly (e.g., by a hydraulic transient) and lead to instantaneous condensation rates in excess of those that can be sustained on a steady-state basis.

This counterflow mechanism might also be expected to play a role in systems with bottom discharge. However, the test evidence displayed in Section 4 consistently indicated that the hydraulic instability described previously governed the slug-formation behavior during our experiments with bottom discharge. During careful experiments with our 1/4-scale model feedring, the water consistently trapped a void (air) due to the hydraulic instability in the feedring at water flow rates well below those necessary to form a water slug by countercurrent flow instability in the feedpipe. Indeed, it was not possible to induce a slug by the latter mechanism in our experiments and slugs did not form at the tee as speculated in earlier work by Roidt [5].

The countercurrent instability mechanism was not pursued further theoretically because it was clear that no purely hydrodynamic regime transition could be established which would define a set of circumstances in which no slug formation would occur, short of reducing the water or air flow to zero. It may be possible to extend the theory by original analytical and experimental work to consider the steam-water behavior of top discharge systems. The main utility of the present analysis is to permit calculation of the size of the void at the time of possible slug formation.

Based on our experimental findings, top discharge extends the flow range of operation without slug formation by suppressing the hydraulic instability that is prevalent with bottom discharge leaving only a milder countercurrent flow instability in the feedpipe. Moreover, apparently thermal effects suppress slug formation by the countercurrent flow instability at water flow rates below a "threshold" value. Top discharge systems are also not subject to the complexities of treating the effects of rising water level in the vessel. Thus, the hydraulics in top discharge systems are easier to analyze and generally more stable than the hydraulic behavior in bottom discharge systems.

Summary of Hydrodynamic Analysis

The contribution of this section has been to show how a hydrodynamic analysis is able to predict a criterion for slug formation by a two-phase flow mechanism in an idealized feedpipe geometry. This is a significant advance over the previous situation in which no known criterion had been postulated, let alone confirmed by experiment. However, because certain

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details, such as the ring geometry and friction effects, render the situation in a PWR feedring more complex, this analysis needs further development and confirmation before it can be applied with confidence to full-scale systems.

At least two mechanisms for slug formation have been identified. The first involves a hydraulic instability arising from the flow of water through some holes and gas flow through the other holes in a bottom discharge configuration. The second mechanism involves the more basic wave instability to countercurrent flow in pipes described in previous work by Wallis and Dobson. With bottom discharge either mechanism may govern although the former prevailed in our small-scale experiments; with top discharge only the countercurrent flow instability can occur. In addition, a rising water level may enhance the hydraulic instability or provide an independent mechanism for trapping a void in a bottom discharge system.

The foregoing has dealt with air-water noncondensing flow behavior in order to isolate the hydraulic and multiphase flow aspects of the problem from the thermal behavior. The same models are extended below to consider the effects of condensation in a steam environment in the place of independently controlled air flow.

Slug Initiation with Steam Flow

In both a PWR feedring and the Creare steam generator model, countercurrent two-phase flow occurs because of steam condensation on the incoming cold feedwater. This steam flow can promote the flow regime transitions described in the previous section. Moreover, once either bubbling, splashing, wave or slug formation occurs, the surface area and turbulent mixing in the pipe will increase, enhancing condensation and promoting both slug development and steam void collapse. This probably explains the very sudden initiation of waterhammer, from a previously quiescent open channel flow into a few feedpipe holes, in the Creare model steam generator experiments.

Assuming that the hydrodynamic mechanism described in the previous section describes the instability that precedes waterhammer, we should be able to replace the air flow by an equivalent steam flow and make a direct comparison.

Rather than trying to predict the detailed thermal transient in the pipe we take as a "limiting case" the situation in which the incoming cold water condenses as much steam as is thermodynamically possible; in other words, it is heated up to equilibrium at the saturation temperature. An energy balance then gives

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$$W_g h_{fg} = W_f C_{pf}(T_{sat} - T_f) \qquad (47)$$

where W_f and W_g are the steam and water flow rates, h_{fg} the latent heat, C_{pf} the specific heat of the water, T_{sat} and T_f the saturation temperature and the temperature of the entering cold water, respectively.

Departure from equilibrium can be modeled crudely by introducing a "condensation efficiency", f, which has been used for similar purposes in previous Creare analysis of LOCA problems [19,67]. Adding the factor f and converting to our chosen dimensionless groups, Equation (47) becomes:

$$j_g^* = f \frac{C_{pf}(T_{sat} - T_f)}{h_{fg}} \left(\frac{\rho_f}{\rho_g}\right)^{1/2} j_f^* \qquad (48)$$

If we now have a predicted flow transition boundary, of the type shown in Figure 57, we use Equation (48) to supply a further relationship between j_g^* and j_f^*, and solve for the limiting condition. Figure 58 shows, for example, the predicted stability limit corresponding to the "baseline" conditions in the Creare steam generator model. The straight lines drawn in this figure represent Equation (48) evaluated at atmospheric pressure for several values of f and T_f.

This type of theory may be compared with the transition observed during steam-water experiments in our 1/10-scale facility. Figure 59 shows the measured water flow threshold as a function of water subcooling for the baseline configuration. The calculated transition curves for $f = 0.25$ and $f = 0.5$ are also shown and agree closely with the data over the range tested.

An alternative means to compare the theory and data is to take the observed threshold water flow rate for the initiation of waterhammer and use this to deduce what the value of f must have been to give agreement with the present theory. The results of this calculation are shown in Table 10. The values of f are quite variable, ranging from 0.17 to close to unity. They are within the range observed by Creare in another direct contact condensation situation (ECC bypass) [67]. In each case the critical number of holes at the stable flow limit agreed with the experimental observation.

In the experiment with ten 3/4-inch holes, the threshold water flow rate was about $j_f^* = 0.4$ with 5-6 holes draining. The theoretical prediction is for the critical number of holes to be 6 with $f = 0.11$ for 60°F water and 0.52 for 180°F water.

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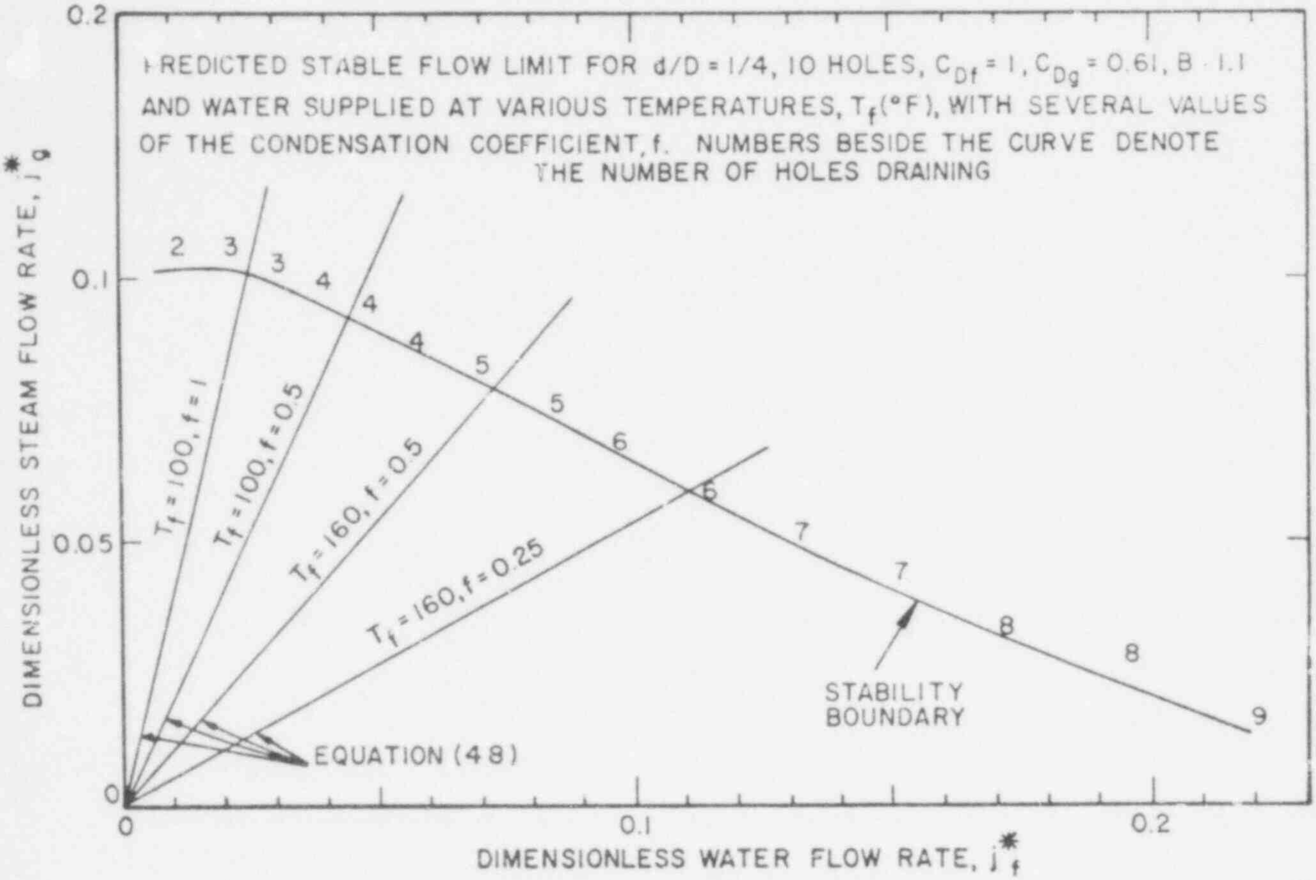

FIGURE 58

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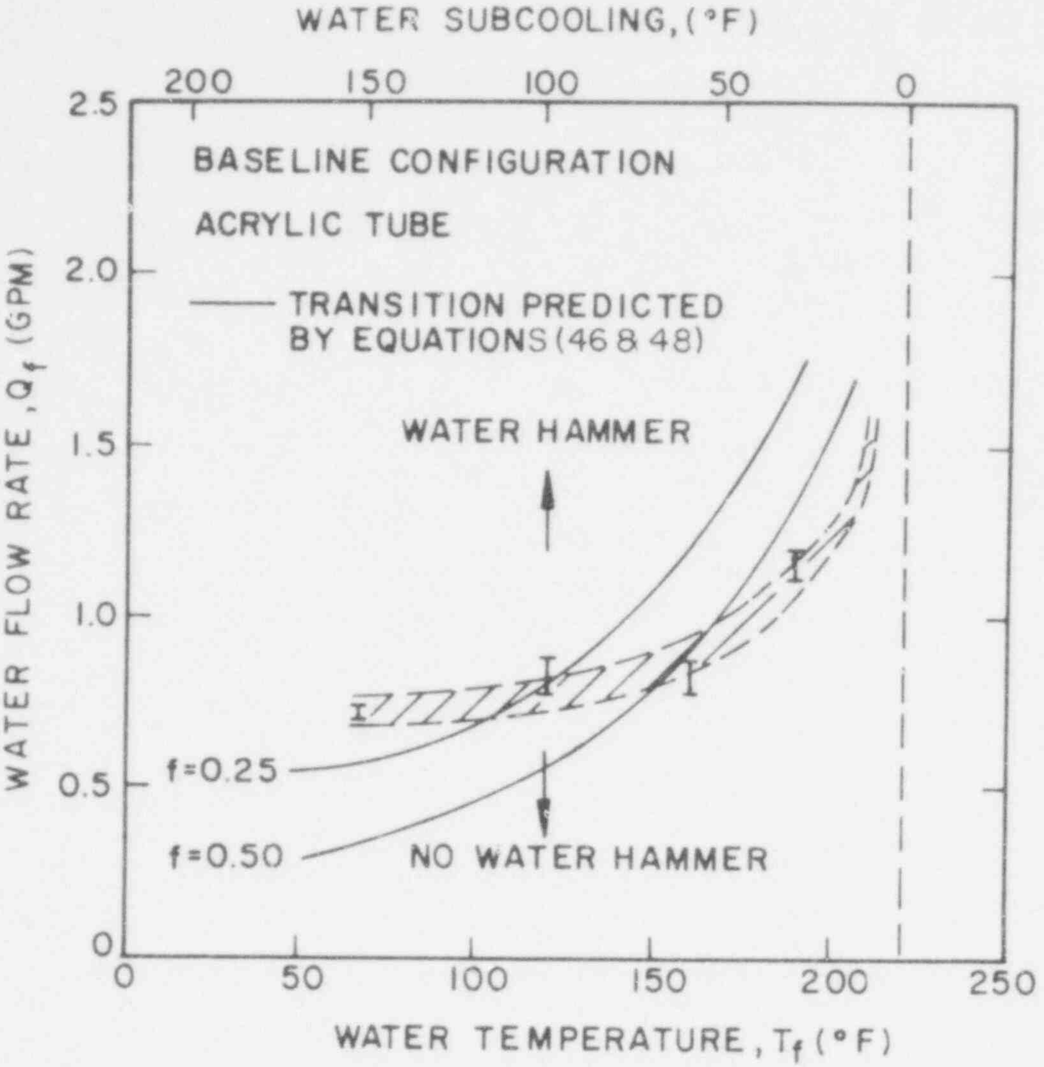

COMPARISON OF FLOW THRESHOLD THEORY AND $\frac{1}{10}$-SCALE DATA.

FIGURE 59

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TABLE 10

VALUES OF f CORRESPONDING TO THE DATA SHOWN IN FIGURE 25

Number of Holes	T_f (°F)	f
10	66	0.19 - 0.20
10	120	0.25 - 0.29
10	160	0.44 - 0.52
10	192	0.67 - 0.75
20	60	0.17 - 0.22
20	120	0.29 - 0.34
20	180	0.83 - 1.05

Possible Heat Transfer Effects

The variation of f as a function of water temperature is much wider than the range of threshold water flow rates. This suggests that some other effect may exert a predominant influence. There are various possible candidate explanations; what we shall do here is to consider two of them in order to show what sorts of phenomena may need to be considered in a more complete analysis.

The main reason that f falls below unity is that fluid mixing and perhaps condensation heat transfer are inadequate to allow complete thermodynamic equilibrium to be reached in the feedpipe. This is clearly demonstrated by the thermocouple traces in Figure 22 that show a cold layer of water on the bottom of the pipe as it enters the simulated steam generator.

As mentioned in Section 3, condensation heat transfer is likely to be limited by the turbulent mixing processes on the water side of the interface. This phenomenon has not yet been sufficiently researched for quantitative predictions to be made. However, it is likely that the form of some conventional forced convection heat transfer correlations may apply. These correlations normally relate the Nusselt number to the Prandtl number and the Reynolds number.

At temperatures between 60°F and 212°F, the kinematic viscosity of water is highly dependent on temperature, ranging from 1.2×10^{-5} ft/sec^2 at 60°F to 0.3×10^{-5} ft/sec^2 at 212°F. The Reynolds number corresponding to 1 gpm in a 1.5 inch diameter pipe at 212°F is Re = 7600, whereas at 60°F the value is 1900. It, therefore, seems likely that the data for the

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10 hole model shown in Figure 59 are in the transition region of Reynolds number and the reason for an apparent "threshold" value of 0.75 gpm could reflect a transition to turbulence with a sudden increase in heat transfer (and hence j_g^*) at this point.

Another explanation is possible for the relatively constant threshold water flow rate with the 20 hole model. This is likely to be a turbulent flow situation in which the Nusselt number varies as $Re^{0.8}$. In other words, the condensation rate (all else being equal) will depend on the product $k\Delta T\, Re^{0.8}$ and hence, on $K\Delta T\nu^{-0.8}$ at constant velocity. This product turns out, surprisingly, to be almost constant over a range of mean water temperatures between 60 and 150°F (Table 11).

TABLE 11

DEPENDENCE OF RATE HEAT TRANSFER OR WATER TEMPERATURE IN TURBULENT CONVECTION

T_F °F	60	90	120	150	180
ν ($ft^2/sec \times 10^6$)	12.2	8.25	6	4.77	4
k Btu/hr ft°F x 10	3.4	3.59	3.7	3.84	3.9
ΔT °F	152	122	92	62	32
$k\Delta T\nu^{-0.8} \times 10^5$	44	51	51	43	26

The conclusion is that variations in water properties (which influence "f") act to almost cancel out the direct effect of varying water temperature and could act to make the steam condensation rate on the water surface almost independent of water temperature. This would explain the relative constancy of the threshold water velocity.

It should be recognized that the above discussion is highly speculative. The purpose of these calculations has been to show what sort of additional effects would need to be considered in a more complete analysis.

It may be also be necessary to consider other relatively complex phenomena not discussed above such as the potential for sudden exposure of and rapid condensation on all of the water in the layer running along the bottom of the feedpipe if the interface should "shatter" locally. A previous calculation (page 155) showed that if f=1 at 1000 psi and 40°F in a PWR feedpipe, then 144 gpm would lead to $j_g^* = 0.177$. At 144 gpm, $j_f^* = 0.035$. Figure 56 shows that this point is well beyond the transition from orderly flow. Thus, there is ample potential for slug formation if only equilibrium processes are assumed and it is unnecessary to invoke the presence of the heat sink of cold water in the feedpipe. We believe, however, that the state-of-the-art precludes confident prediction of all of the interacting effects speculated on above.

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Summary of Analysis of Initiating Mechanisms

This section has served to show several things.

A hydrodynamic mechanism for slug initiation can be derived by combining original research and analysis. It could not have been deduced from the existing state-of-the-art. Therefore, research of this sort is needed if a clear quantitative understanding of hydrodynamic initiating mechanisms is to be obtained.

Adequate theory exists to describe the countercurrent flow instability in feedpipes that has been described in earlier work such as that of Roidt [5]. However, the available evidence indicates that the hydraulic instability described for the first time in this report governs the slug formation behavior because it is more prone to occur in common bottom discharge systems than is the countercurrent flow instability.

In a steam-water system the steam flow results from condensation. Because condensation cannot be accurately predicted from the state-of-the-art, and appears to be influenced by several effects, the initiating event can only be described approximately. Even if the hydrodynamics are well understood, representation of condensation can only be done at present by introducing empiricism (in the form of a factor such as "f") which is insufficiently mature to provide confident predictions.

Many interacting phenomena contribute to the initiating mechanism. While Creare has provided the first known quantitative analytical models for several of these, further work is needed before applying any of these results to full-scale systems. Systematic tests at higher pressure and larger scale are needed to derive suitable empirical coefficients for analytical models of the type developed in this section of the report.

One means to increase confidence in means to prevent slug formation is to decrease the number of configurations and procedures in use, ideally to the single configuration that is easiest to model and least prone to slug formation. In this respect, the identified complexities of flow in the bottom discharge configuration indicate that the top discharge configuration is a superior choice.

5.2 Void Collapse

When a water slug is formed it traps a steam "void" in the feedpipe. If the water is subcooled, steam will condense, the pressure in the void will drop and it will tend to collapse, drawing the water slug towards it.

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This process involves three component phenomena, steam condensation, depressurization and slug motion, governed in turn by heat transfer, thermodynamics and mechanics. This section will be concerned with the first two of these topics; the mechanics of slug motion will be discussed in Section 5.3 where the entire process is treated.

Condensation at the steam-water interface is generally a complex process, depending on mixing phenomena in both phases and on the interface shape; for example, disruption of the interface into droplets will increase the rate of condensation by orders of magnitude. Since comprehensive proven methods for predicting these phenomena do not exist, the most that we can do at present is to compare the available data with some "limiting" analyses based on simple, but extreme, assumptions.

The lowest rates of condensation will occur if the interface is flat, with the minimum possible area, and if there is no turbulent mixing. The latent heat released by condensation is then removed by transient conduction, as for a solid.

The maximum rates of condensation occur when mixing is so rapid at the interface that the rate at which steam flows towards it is governed by the flow processes occurring in the steam. This limit is reached when the Mach number of the steam reaches unity at the "governing cross-sectional area", as suggested by Maa [68]. Appendix E discusses this process for an ideal case. This type of model has been used successfully in a previous Creare analysis of water slug behavior with rapid condensation [19].

In between these two limits, many phenomena interact and it is unlikely that any simple theory or correlation will represent all of the data. The major reason for this is the myriad number of possible flow regimes and interface geometries.

The following paragraphs will compare our results and published data with the predictions of several theories and attempt to reach conclusions when possible.

Water Cannon Results

The rates of condensation observed in the water cannon experiments can be inferred approximately from the recorded pressure histories.

Even though the phase geometry in these tests is the simplest that we could devise, it is clear from the experimental results that we are not dealing with an accurately repeatable phenomenon. The pressure traces differ qualitatively, as well as quantitatively, from test to test, sometimes

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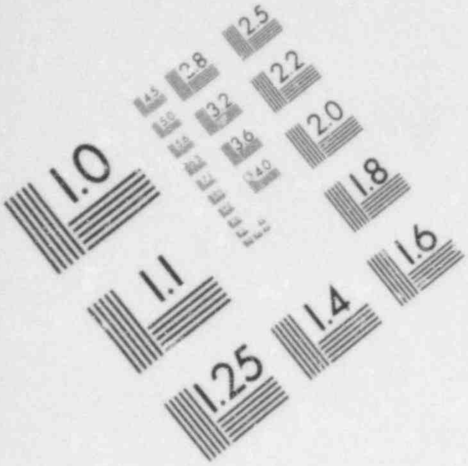


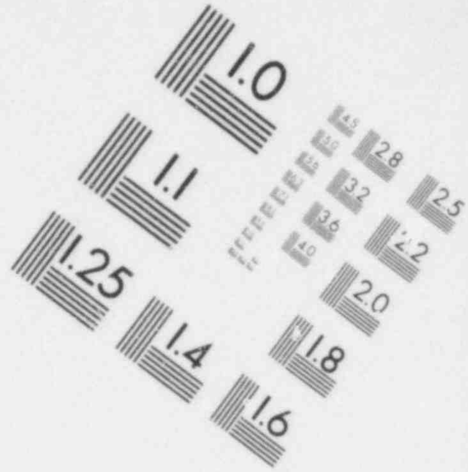
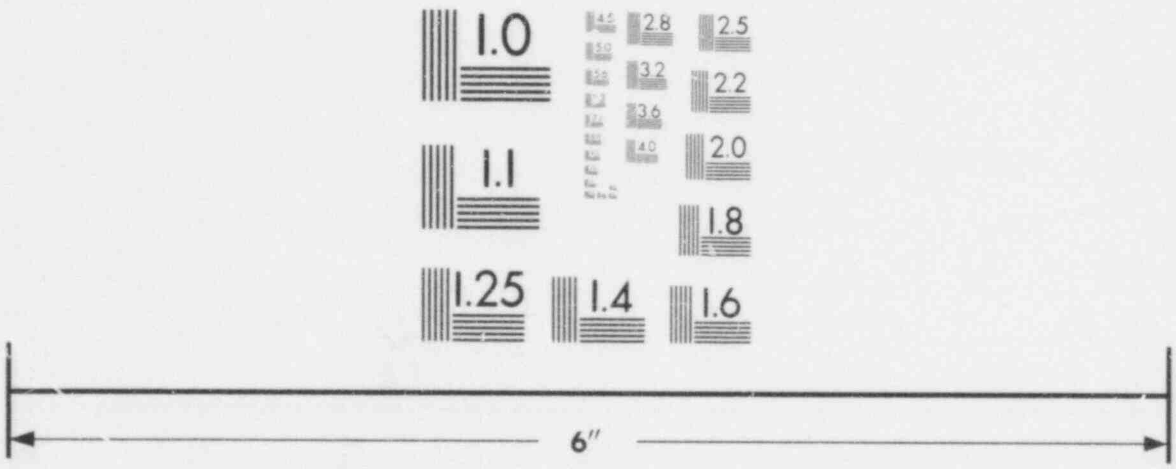
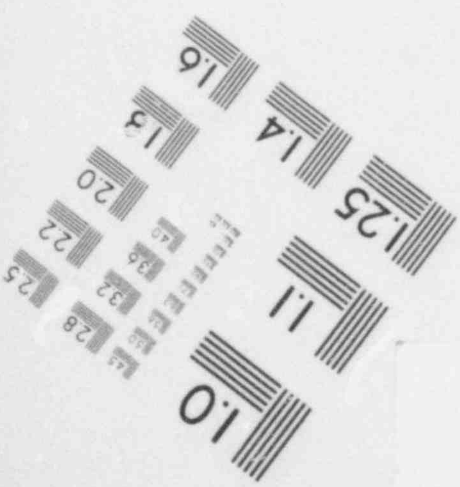
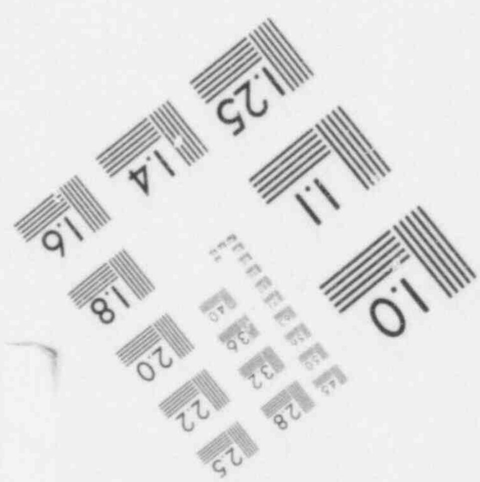
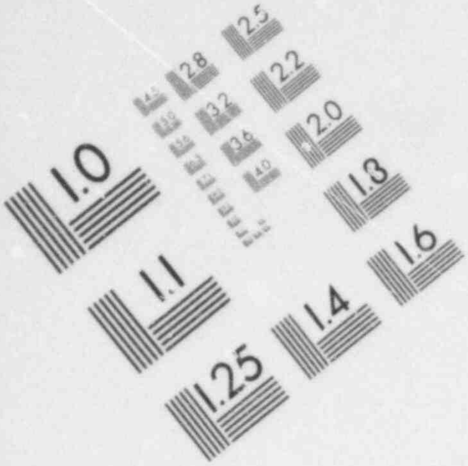


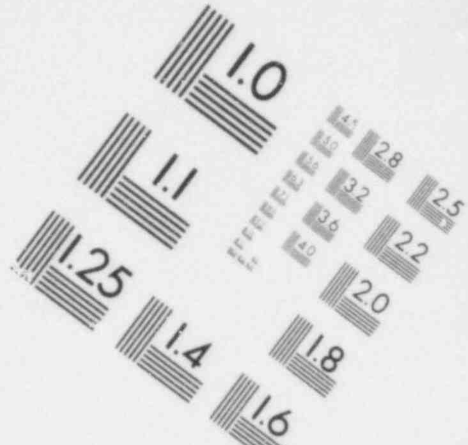
IMAGE EVALUATION TEST TARGET (MT-3)











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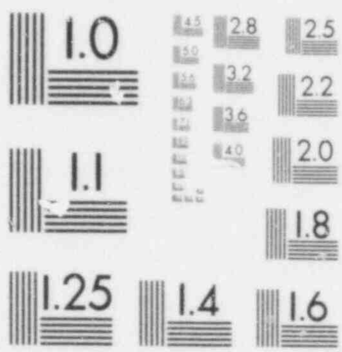
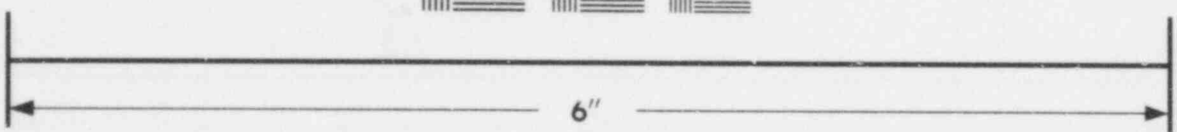
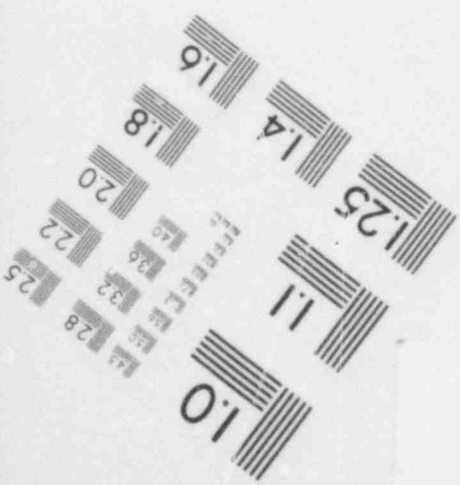
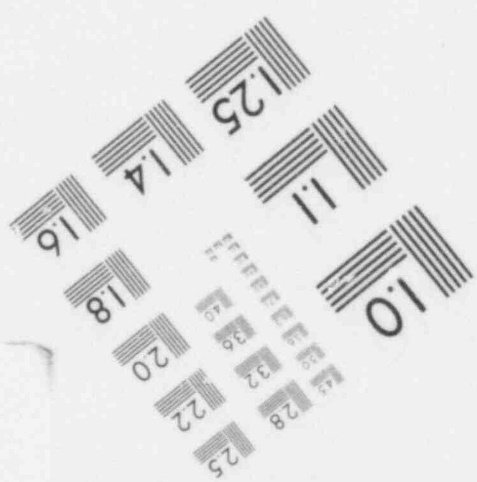
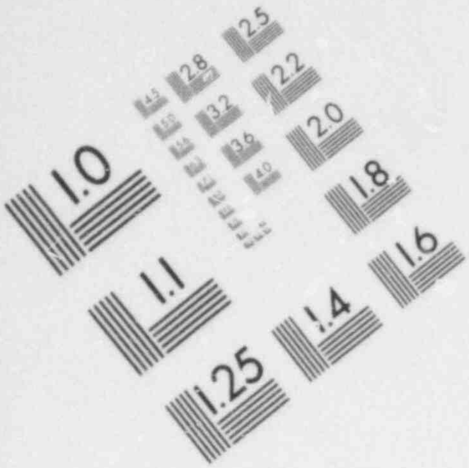
TEST TARGET (MT-3)











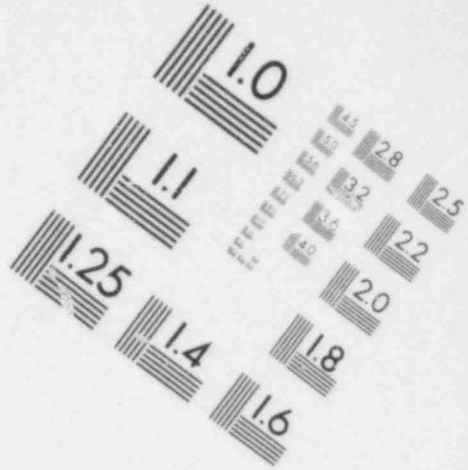
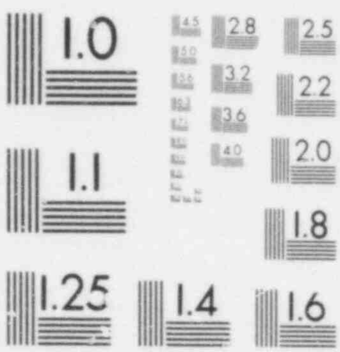


IMAGE EVALUATION TEST TARGET (MT-3)



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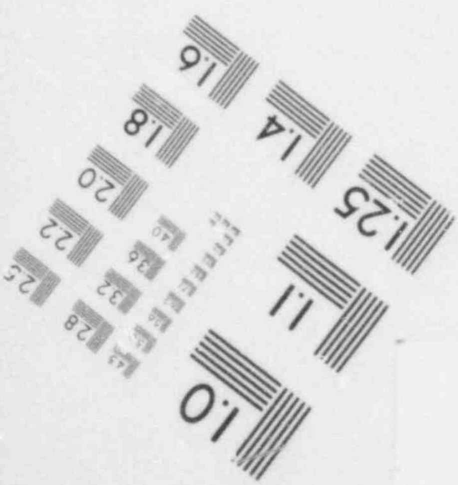

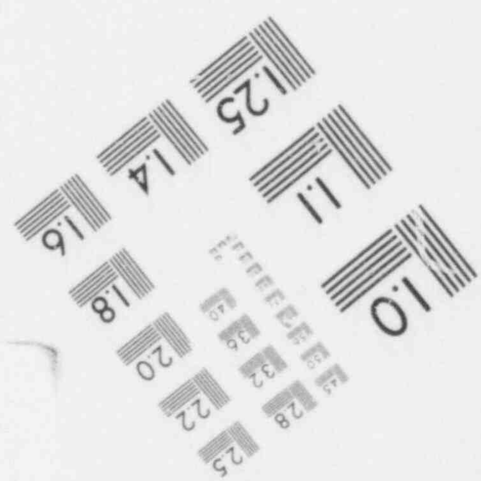

showing a "steady" depressurization and at others an erratic intermittent depressurization with periods of compression while the void is collapsing but mass removal by condensation is temporarily inhibited.

The minimum condensation rate can be estimated by transient conduction theory. In order to condense a column of steam of length L on a plane interface, of the same cross section as the steam, with initial subcooling ΔT, a sufficient "penetration depth" must be warmed up. An energy balance then gives:

$$L\rho_g h_{fg} \approx 2\rho_f c_f \Delta T \left(\frac{\alpha_f t}{\pi}\right)^{1/2} \qquad (49)$$

where α_f and c_f are the thermal diffusivity and heat capacity of the water, and t is the time.

Putting $L = 2$ ft, $\Delta T = 150°F$ and using the properties of water at atmospheric pressure we have

$$t = \frac{\pi}{4\alpha_f}\left(\frac{L\rho_g h_{fg}}{\rho_f c_f \Delta T}\right)^2 = 40 \text{ sec} \qquad (50)$$

which is the time needed to condense all of the steam. Since our experiments show a complete depressurization in 100 to 200 ms, this estimate is clearly way off the mark.

It is our experience with many other problems of applied interest involving condensation of steam on cold water that transient conduction limited heat transfer through an undisturbed interface rarely leads to a reasonable result. The single possible exception is condensation on a subcooled spray of fine droplets.

The upper limit to condensation rate, representing choking of the steam flow near the interface, predicts almost complete depressurization in a time

$$t = \frac{5L}{a_g} \approx \frac{5 \times 2.2 \text{ ft}}{1550 \text{ ft/sec}} \approx 7 \text{ msec} \qquad (51)$$

which is much too rapid

There is no question that sufficient cold fluid is available to condense all of the steam. A fluid layer of thickness only 0.1 inches, if raised from 60°F to saturation temperature at ambient pressure (212°F), is sufficient to condense all of the steam in the 26-inch tall water cannon. (An equal volume of steam was injected during a typical measured depressurization.) Thus, the limiting factor must be mixing of cold fluid at and near the interface.

A theory which assumes good mixing near the interface and merely borrows an order of magnitude estimate of an interface heat transfer coefficient from the literature (10^4 Btu/hr ft^2°F in Reference [69], 1.7×10^4 Btu/hr ft^2°F in Reference [70]) gives a more reasonable prediction

$$t = \frac{L\rho_g h_{fg}}{h\Delta T} = 112 \text{ to } 191 \text{ msec} \qquad (52)$$

However, some of the traces show much more rapid initial rates of depressurization. Since the steam mass is approximately proportional to pressure at constant volume and the initial depressurization occurs before the slug has moved much (if it has moved, even more condensation must have occurred), we may interpret the observed initial rate of change of pressure of 6 psi in 15 msec (shown for example in Figure 21a) as equivalent to 15 psi (all condensed) in [(15)(15)/6]=37.5 msec corresponding to a condensation rate five times as fast as the average.

The conclusions from these comparisons are that observed condensation rates in one apparatus near atmospheric pressure lay between the two theoretical limits (each of which was orders of magnitude from the data) and were variable, with average values of representative heat transfer coefficients in the range 10^4 to 2×10^4 Btu/hr ft^2°F and peak values around 5×10^4 Btu/hr ft^2°F. There is ~~no~~ reason to expect such values to apply universally since they are purely empirical and lack a flow physics basis.

Steam Generator Model

Depressurization traces for the steam generator model resembled those from the water cannon but showed more variability. Based on condensation only over an area equal to the pipe cross section, the condensation coefficients would then be about the same for the two setups. Since condensation before slug formation is less rapid, it appears that turbulence ahead of the slug enhances condensation. The same qualitative conclusions can be drawn in this case as in the previous paragraph. Neither limiting theory is close to the observations, although they do bound the observation.

It was also found that an addition of much less than one percent by mass of air to the steam reduced the rate of condensation by so much that no measurable depressurization occurred. It is this effect that makes the Westinghouse model results [5], for which 10% air was reported, so unrepresentative of the real situation that we have not been able to use them for comparison with our results.

At higher pressure (75 psia) we observed depressurizations larger in amplitude by a factor of three, and occurring correspondingly more rapidly than the data at 15 psi system pressure. Since the temperature difference between the steam and the supplied cold water did not change appreciably with pressure in the range tested, this implies that the condensation coefficient increases in some way, perhaps as a result of more disruption of the interface by the momentum of the higher density steam. There are also fundamental questions on physical scale that have not been addressed in any of the available experimental work. Droplets and interfacial waves may be relatively smaller at larger scales, effectively enhancing the apparent condensation rate in full size systems. The implication is that one cannot apply heat transfer coefficients measured at low pressure and at small scale to predict results at higher pressures, such as those found in PWRs, without better understanding of suitable scaling laws.

Tihange Data

The only source of void collapse data at full scale and operating pressure is a single trace from an experiment at Tihange, shown here as Figure 60 and described in Appendix A.

It has already been pointed out by Vreeland [34] that the Tihange data can only be explained by condensation rates orders of magnitude larger than would be estimated from the sorts of heat transfer coefficients observed in condensers. For example, the condensing rate of 500 lbm/ft^2 mentioned by Vreeland corresponds to a heat transfer coefficient of

$$h = \frac{(500)\,\text{lbm/ft}^2\text{sec}\ (650)\,\text{Btu/lbm}\ (3600)\,\text{sec/hr}}{450°}$$
$$= 2.6 \times 10^6\ \text{Btu/hr ft}^2\,°\text{F} \qquad (53)$$

or over one hundred times the average value in our tests at roughly 1/10-scale and atmospheric pressure.

Using the simple theory of depressurization limited only by gas dynamics (Appendix E) and assuming an initial void length of eight feet with condensation occurring at Mach 1 on one end of the void (where the advancing slug breaks up the interface) the time for "almost complete" depressurization is

$$t = \frac{5 \times 8\ \text{ft}}{2200\ \text{ft/sec}} = 18\ \text{msec} \qquad (54)$$

which is close to the observed value of approximately 20 msec in the Tihange trace of Figure 60. Almost any void size from zero to the full volume of the feedring and horizontal run of feedpipe could be assumed. Realistic estimates are unlikely to be very different from the eight foot length of the feedpipe. Therefore, the simple theory in Appendix E not only predicts the order of magnitude correctly, but also predicts the condensation rate as well as possible from the available information.

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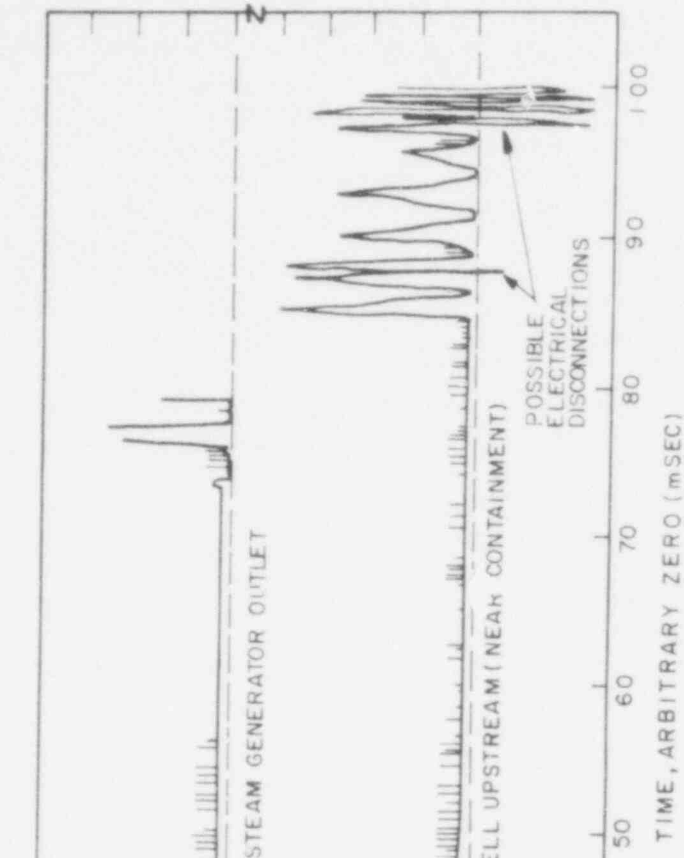

PRESSURE HISTORIES IN FEEDWATER PIPE AT TIHANGE (FROM REFERENCE [64])

FIGURE 60

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This single piece of evidence is the most significant of all the observations made so far on this topic. It is direct evidence that in a PWR a steam void can depressurize to nearly zero absolute pressure in a time that is short compared with the total void collapse period. It indicates further that condensation can be so rapid in a full-scale situation that it is no longer a limiting process for void collapse. Even more rapid depressurization is conceivably possible if the water lying below the steam void is broken up and presents a larger interfacial area. One is led to the inescapable conclusion that the only "conservative" approach to steam void collapse at full scale and typical operating pressure is to assume an instantaneous and complete depressurization of the void once the slug forms. It should be appreciated that this conservative assumption represents only a 20% increase over the measured integral of void depressurization as a function of time recorded at Tihange.

5.3 Slug Dynamics and Impact

The purpose of this section is the ... vation of analyses for predicting the motion of a water slug, as a trapped steam void collapses, and the resulting waterhammer pressures as functions of time. These analyses are compared with data available both from the Creare models and from a few full-scale tests, such as those at Tihange and Doel.

The level of analytical sophistication has been chosen to be compatible with our level of physical understanding. The reader should be aware of the many simplifying assumptions needed in order to develop these mathematical models and is cautioned against extrapolating them to conditions which may differ from those considered here.

Examples of Water Slug Motion for Various Starting and Boundary Conditions

This section will present some idealized analyses of water slug motion based on one-dimensional conservation laws. The cases considered include different initial slug lengths, amounts of water lying on the bottom of the pipe, conditions behind and in front of the slug, and rates of condensation. The results are useful for comparison with experimental data and approximate predictions of performance. It is shown that the maximum anticipated slug velocity at impact is not very sensitive to the assumed parameters under several limiting circumstances.

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Constant Length Slug, Constant Applied Pressure Difference

This is probably the simplest example, physically and mathematically, and serves to illustrate some of the features that are found in other, more complex examples.

Figure 61 shows the starting conditions and the situation at a time t later. A pipe of length L contains a water slug of length L_s and a void of length $(L-L_s)$ between the slug and the closed pipe end. This situation is initiated instantly at $t = 0$. A pressure p_o acts on the open end of the slug and is assumed to be constant. We assume one-dimensional frictionless motion and a constant pressure p_1 in the void. The pressure difference $(p_o - p_1)$ is denoted by Δp.

The equation of motion for the slug is

$$\ddot{x} = \frac{\Delta p}{\rho_f L_s} \qquad (55)$$

At impact, $x = (L-L_s)$ and the liquid velocity and elapsed time are

$$V_1 = \left[\frac{2\Delta p}{\rho_f}\left(\frac{L}{L_s} - 1\right)\right]^{1/2} \qquad (56)$$

$$t_1 = \left[\frac{2\rho_f}{\Delta p}(L-L_s)L_s\right]^{1/2} \qquad (57)$$

The net impulse given to the water slug before impact is equal to its impact momentum, i.e.,

$$\int_o^{t_1} \Delta p\,dt = [2\rho_f \Delta p\, L_s\,(L-L_s)]^{1/2} \qquad (58)$$

The left-hand side of this equation can be used to calculate the momentum of any slug before impact, even if p_o and p_1 vary with time or if other boundary conditions apply (such as water lying in the bottom of the pipe). With p_o and p_1 constant, the maximum value of the impact momentum occurs when $(L_s/L) = 1/2$. On the other hand, V_1 is predicted to be indefinitely large as L_s tends to zero.

Equation (55) gives the motion of the center of gravity of the slug. If the depressurization of the void is rapid, this will generate acoustic waves traveling back and forth in the slug, with the speed "a" described in Section 3, superimposed on this average motion. The condition for these acoustic waves to have completed several cycles (and so have undergone considerable damping) by the time that slug impact occurs is:

$$^{3} << t_1 \qquad (59)$$

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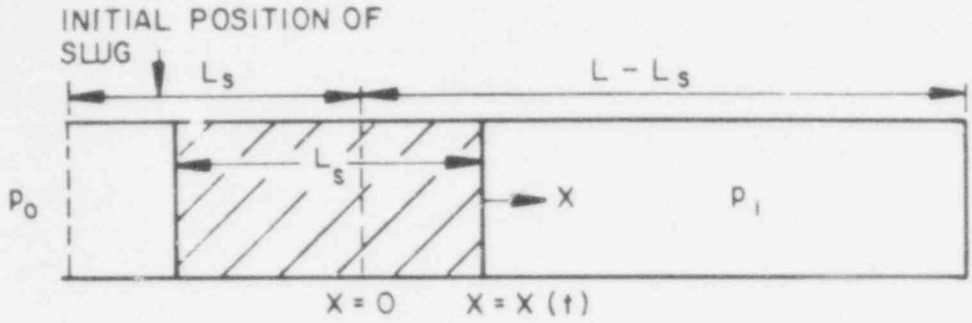

FIGURE 61 ONE-DIMENSIONAL MOTION OF A SLUG OF CONSTANT LENGTH IN A STRAIGHT PIPE

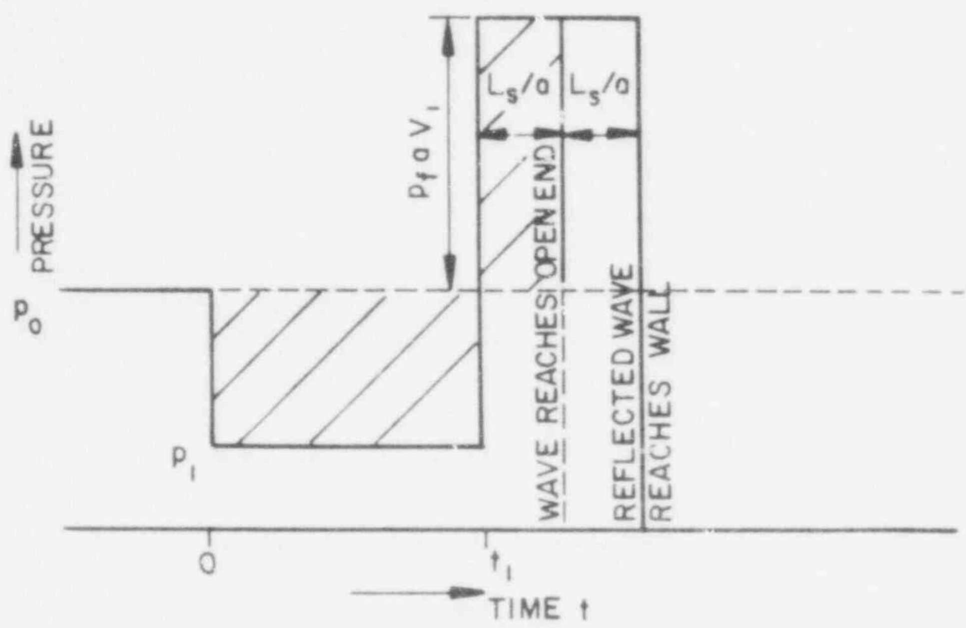

FIGURE 62 PRESSURE AT THE CLOSED END OF A PIPE PLOTTED VERSUS TIME FOR THE CONDITIONS OF FIGURE 61
(p_1 = CONSTANT; p_0 = CONSTANT; BOTH p_0 AND p_1 ARE MUCH LESS THAN $\rho_f a^2$). THE TWO SHADED AREAS ARE APPROXIMATELY EQUAL.

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Using (57) in (59) this condition can be rewritten as:

$$\frac{\Delta p}{\rho_f a^2} << 2 \left(\frac{L}{L_s} - 1\right) \qquad (60)$$

For water in a steel pipe, $\rho_f a^2$ is about 300,000 psi and Equation (60) will be satisfied under most circumstances of interest.

The subsequent history depends on the boundary condition at the point of impact. For this example, we will consider two cases, a rigid wall and a liquid column of indefinite length.

A. Rigid Wall

When the slug impacts the rigid wall, its front layers are immediately brought to rest. This causes an almost instantaneous rise in pressure at the wall to the value $(p_1 + \rho_f V_1 a)$ where a is the velocity of propagation of a compressive wave in the liquid (see Section 3). The pressure now rises linearly with time for a further period (L_s/a) to a value $(p_o + \rho_f V_1 a)$. It then holds constant at this value for another (L_s/a) seconds until the reflected wave, which has by now run down the liquid slug and back again, arrives at the wall, dropping the pressure value and leads to void formation and a new "column separation".

Considerable simplification is possible if the waterhammer overpressure $p_h = \rho_f V_1 a$ is much bigger than either p_o or p_1. The wave can then be approximated as a square pulse, as in Figure 62. This will be so if:

$$p_o << \rho_f V_1 a$$

i.e., using (56), if

$$\frac{p_o}{\rho_f a^2} << 2\left(1 - \frac{p_1}{p_o}\right)\left(\frac{L}{L_s} - 1\right) \qquad (61)$$

This condition resembles (60) and should be valid under comparable circumstances.

To get an idea of the orders of magnitude predicted by this theory, let $p_o = 1000$ psia, $p_1/p_o = 1/2$, $L_s/L = 1/2$, $\rho_f = 62\ \text{lbm/ft}^3$, $a = 4500$ ft/sec, $L = 6$ ft. Then

$$V_1 = 273 \text{ ft/sec}, \; t_1 = 31 \text{ msec}, \qquad (62)$$
$$p_h = \rho_f V_1 a = 16,400 \text{ psi}$$

The high pressure on the end of the pipe lasts until the compression wave travels to the free end of the slug and reflects as a decompression wave, back to the closed end. This takes a time $2L_s/a$.

At a time L_s/a the water in the slug is all compressed and stationary (as long as (61) is valid). The total impulse given to the end of the pipe by the waterhammer, and vice versa, in this time is

$$(\rho_f V_1 a)\left(\frac{L_s}{a}\right) = [2(\Delta p)\rho_f(L-L_s)L_s]^{1/2} \quad (63)$$

which is exactly equal to (58)—a result which must follow on the basis of momentum conservation if p_o is constant. Put another way, when the slug has been brought to rest the area under the overpressure curve (plotted versus time) is equal to the area representing the depressurization impulse (Figure 62). This will be approximately true even when the depressurization and overpressure waves are not "square". Because the slug rebounds, the total area under the overpressure curve can be as much as twice this value, but may be lower as a result of compliance effects in a non-rigid pipe.

Figure 63 shows how the pressure traces vary as the ratio (L_s/L) is changed and everything else is kept constant. The smallest slugs are predicted to give the highest overpressures but they last for the shortest time

B. Liquid-Liquid Impact

In the other case which we will consider, the closed end is replaced by a long water column—a situation probably more representative of a steam generator pipe.

Up to the time of impact, the motion of the slug is the same as before. While the slug is moving, a depressurization wave moves down the water column at a speed a. The water behind this wave moves at speed $(p_o - p_1)/(\rho_f a)$ towards the void. (For the numbers used in our previous example this speed is 8.3 ft/sec.) The condition for this velocity in the water column to be negligible compared with the slug velocity is given by (60) and will usually be satisfied.

After impact, compression fronts move away in both directions from the point of impact and have an overpressure amplitude of $\rho_f V_1 a/2$ (see Section 3.4). When the wave in the original slug reaches the free end it reflects as a negative wave. Thereafter a square overpressure wave of length $2L_s$ and amplitude $\rho_f V_1 a/2$ moves at constant speed a into the water column. The pressure signal recorded at some point in the water column will show a depressurization to around p_o, lasting a time t_1, followed by an overpressure by an amount of $\rho_f V_1 a/2$

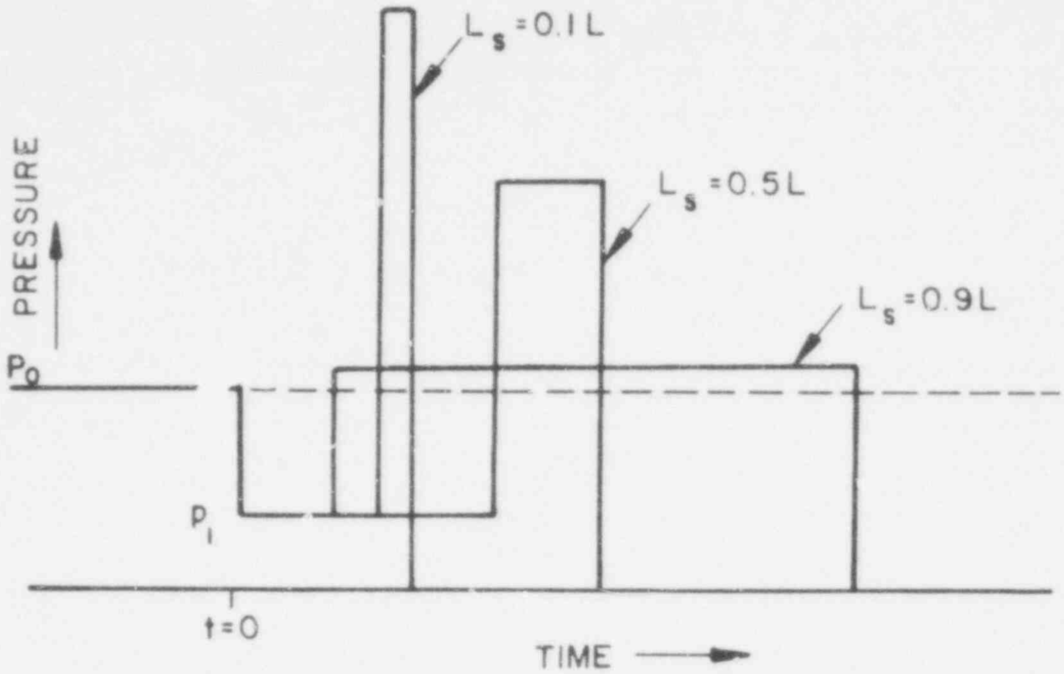

FIGURE 63 EFFECT OF SLUG LENGTH ON THE PRESSURE VARIATION AT THE END OF A CLOSED PIPE

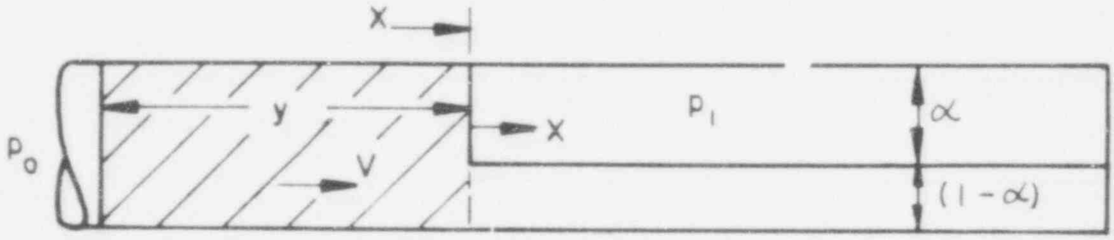

FIGURE 64 A "GENERAL SLUG" IN A STRAIGHT PIPE

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lasting a time $2L_s/a$. After this time the pressure is p_o again until a much larger time when reflected waves may return from other points in the piping system and maximum pressures may be as large as $\rho_f V_1 a$.

Because of variations in the velocity of parts of the slug as waves set up by the depressurization travel back and forth in it, and because of the neglect of other effects based on using criteria (60) or (61), there will be uncertainties in the overpressure of the order of magnitude of p_o.

In general, the arguments given in these two cases A and B will be approximately valid no matter how the slug is formed and no matter what its motion prior to impact. All that is needed in order to predict the shape of the pressure pulse is to know V_1 and L_s at the time of impact. Different depressurization histories and slug development phenomena may give rise to the same waterhammer pulse.

The following sections will consider a number of examples of slug motion subject to different conditions before impact.

General Equations for Slug Momentum

Figure 64 shows a general representation of a water slug in a pipe. The pressure behind the slug is p_o. The pressure in front of it is p_1. The pressure difference $(p_o - p_1)$ is denoted by Δp. The water in the slug is all moving to the right with velocity V. The slug has length y and its front end has reached the point x (measured from the open end of the pipe) at time t. The fraction of the pipe area ahead of the slug occupied by vapor is α. One-dimensional motion is assumed and the water is initially at rest.

Since all of the water composing the slug starts with zero velocity, the momentum it possesses must all come from the intergrated impulse given to it by the acting pressure difference, therefore:

$$\Delta p dt = \rho_f y V \qquad (64)$$

or, in differential form,

$$\Delta p = \frac{d}{dt} (\rho_f y V) \qquad (65)$$

(gravitational forces and friction will generally be small compared with the pressure difference).

If the pressure difference is constant, we may define a "characteristic velocity" V_o as

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$$V_o = \left[\frac{\Delta p}{\rho_f}\right]^{1/2} \tag{66}$$

and write (64) as

$$V_o^{\,2} t = yV \tag{67}$$

Different initial and boundary conditions will alter the relationships between y, V, x, and $\dot{x}$. However, in all cases in which the driving pressures are constant it will be found that the slug velocities before impact are of the order of magnitude of V_o.

<u>Zero Initial Slug Length, Slug Fed by Reservoir, $\alpha = 1$</u>

Figure 65a represents this situation. Clearly,

$V = \dot{x}$, $y = x$ and Equation (67) becomes

$$V_o^{\,2} t = x\dot{x} \tag{68}$$

which can be integrated, with the condition $x = 0$ at $t = 0$, to give

$$V_o^{\,2} t^2 = x^2 \text{ or } V = V_o \tag{69}$$

The velocity of the slug is a constant equal to V_o at all times before impact.

If we had assumed a pressure drop at the inlet from the reservoir equal to $C\dot{x}^2$, the result would have been

$$V = \frac{V_o}{\sqrt{1+C}} \tag{70}$$

and the slug velocity is reduced by a constant factor.

Had the pipe been fed by a "well-rounded" inlet, it might have been more appropriate to use Bernoulli's equation (which is easy since the velocity is again constant). This gives

$$\frac{\rho_f V^2}{2} = \Delta p \tag{71}$$

Therefore $V = V_o\sqrt{2}$.

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Initial Slug Length L_o, Fed by Reservoir, a = 1

Equation (68) again applies. However, (69) becomes

$$x^2 = L_o^{\,2} + V_o^{\,2} t^2 \tag{72}$$

Moreover, from (68)

$$V = \frac{V_o^{\,2} t}{x} \tag{73}$$

Equations (16) and (17) allow various relationships to be expressed between position, speed and time. The slug speed as a function of position, for instance, is

$$V = V_o \left[1 - \left(\frac{L_o}{x} \right)^2 \right]^{1/2} \tag{74}$$

and tends asympototically to V_o as x increases.

Slug Length Initially Zero, Fed by Reservoir, $\alpha \neq 1$

From Figure 65b, by using principles of continuity we find that

$$y = x,\ V = \alpha \dot{x}$$

The only change in the result from Equation (14) is that now

$$x = V_o t/\alpha^{1/2},\ V = \alpha^{1/2} V_o \tag{75}$$

Slug Length Initially L_o, Fed by Reservoir, $\alpha \neq 1$

Again we have $y=x$, $V=\alpha\dot{x}$ to use in (56) and integrate with $x=L_o$, $\dot{x}=o$ at $t=o$. The results are

$$V_o t = \left[\alpha \, (x^2 - L_o^{\,2}) \right]^{1/2} \tag{76}$$

$$V = V_o \left[\alpha \left(1 - \left(\frac{L_o}{x} \right)^2 \right) \right]^{1/2} \tag{77}$$

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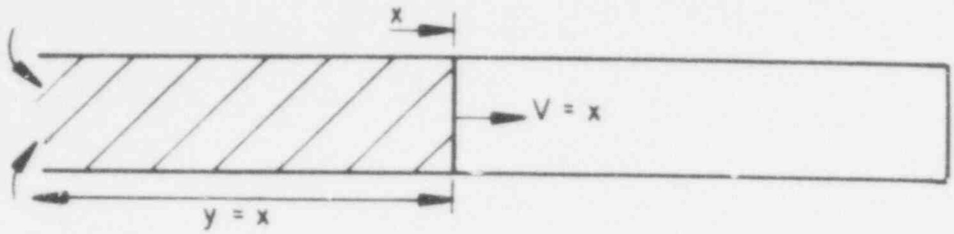
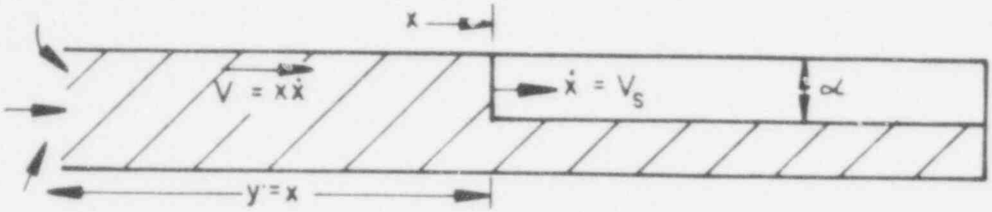
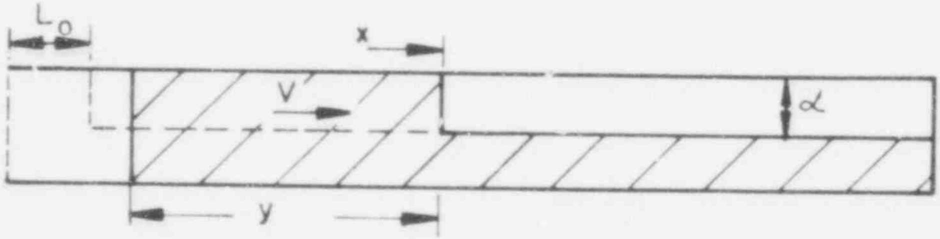


(a) SLUG FED BY A RESERVOIR. PIPE INITIALLY EMPTY.



(b) SLUG FED BY A RESERVOIR. PIPE PARTLY FULL INITIALLY.



(c) NO RESERVOIR. INITIAL LENGTH L_o. PIPE PARTLY FULL INITIALLY.

SEVERAL IDEALIZED ONE-DIMENSIONAL MODELS OF SLUG MOTION

FIGURE 65

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Slug Length Initially L_o, Reservoir, $\alpha \neq 1$

From Figure 65c by continuity

$$y = L_o + (x - L_o)(1-\alpha)$$

$$= \alpha L_o + (1-\alpha)x \qquad (78)$$

and $V = \alpha\dot{x} = \alpha\dot{y}/(1-\alpha)$ (79)

It is easiest to solve for y in (67),

$$V_o^2 t = \frac{\alpha}{(1-\alpha)} y\dot{y} \qquad (80)$$

Integrating this with $y=L_o$ at $t=o$ gives

$$y^2 = L_o^2 + t^2 V_o^2 \frac{1-\alpha}{\alpha} \qquad (81)$$

Using (67) and (81) we can solve for V,

$$\frac{V}{V_o} = \left(\left[\frac{\alpha}{1-\alpha}\right]\left[1 - \left(\frac{L_o}{y}\right)^2\right]\right)^{1/2} \qquad (82)$$

or, in terms of the distance $(x-L_o)$ traveled by the slug front (the initial void length),

$$\left(\frac{V}{V_o}\right)^2 \frac{1-\alpha}{\alpha} = 1 - \frac{1}{\left[\frac{(1-\alpha)\ (x-L_o)}{L_o} + 1\right]^2} \qquad (83)$$

The velocity, V_1, of the water in the slug upon impact may be computed for all of these cases. The results are summarized in Table 12.

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TABLE 12 SLUG IMPACT VELOCITIES FOR VARIOUS INITIAL CONDITIONS

Reservoir	Initial Slug Length	Void Fraction	V_1/V_o
Yes	o	o	1
Yes, Inlet Loss	o	o	$(1 + C)^{-1/2}$
Yes, Rounded Inlet	o	o	$\sqrt{2}$
Yes, Rounded Inlet	L_o	o	$\sqrt{1 - (\frac{L_o}{L})^2}$
Yes	o	α	$\sqrt{\alpha}$
Yes	L_o	α	$\sqrt{\alpha}\sqrt{1 - (\frac{L_o}{L})^2}$
No	L_o	α	$\sqrt{\frac{\alpha}{1-\alpha}}\sqrt{1 - \left[\frac{L_o}{\alpha L_o + (1-\alpha) L}\right]^2}$

In the scenario sketched out in Section 1, it was pointed our that slug motion was coupled with the depressurization of the trapped bubble. In this case the pressures p_o and p_1 are variables which have to be solved for by using the equation of motion of the slug, a model for condensation, and a gas dynamic model of the feedring holes.

In this example, we study the case in which a straight tube initially contains a fraction $(1-\alpha)$ of liquid which is swept up into a slug without any further liquid addition from the open end. A constant mass rate of removal of gas in the trapped bubble is assumed, to provide an idealized representation of condensation. A constant pressure P_o is assumed on the other side of the slug. The earlier examples may be thought of as the limiting idealized case with condensation proceeding much more rapidly than slug inertial behavior.

Equation (65) applies with $V=\alpha\dot{x}\ (1-\alpha)$, therefore,

$$\frac{d}{dt}(x\dot{x}) = \frac{\Delta p}{\rho_f \alpha(1-\alpha)} \tag{84}$$

(mass addition to the slug by condensation is neglected).

The continuity equation for the gas, with a constant mass rate of condensation "m_c" is

$$\frac{d}{dt}\left[\rho_g\ \alpha\ A(L-x)\right] = -m_c \tag{85}$$

If the gas is "perfect" and follows an approximately isothermal path everywhere in the void

$$\rho_g = \frac{p_1}{p_o}\ \rho_{go} \tag{86}$$

where ρ_{go} is the original gas density at pressure p_o.

Futhermore, we define V_{go} as the equivalent velocity of gas over a cross-section of αA which would be equivalent to m_c at a density ρ_{go}, i.e.,

$$m_c = A\alpha\rho_{go}\ V_{go} \tag{87}$$

Using (87), and (86) and (85) we get

$$\frac{d}{dt}\left[p_1(L-x)\right] = -\ p_o\ V_{go} \tag{88}$$

which can be integrated, with $x=o$, $p=p_o$ at $t=o$ to give

$$p_1(L-x) = p_o(L - V_{go}t) \tag{89}$$

as long as p_1 is not negative. Equation (89) can be substituted into (84) to give a differential equation for x,

$$\frac{d}{dt}(x\dot{x}) = \frac{p_o}{\rho_f \alpha(1-\alpha)}\ \left(\frac{V_{go}t - x}{L - x}\right) \tag{90}$$

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This equation is only valid if p_1 is positive. It is assumed that negative pressures are impossible in the void. Therefore, after a time $t > L/V_{go}$ we have to set $p_1=o$ and replace (90) by (84) with $\Delta p=p_o$, i.e., if $t > \frac{L}{V_{go}}$,

$$\frac{d}{dt}(x\dot{x}) = \frac{p_o}{\rho_f \alpha(1-\alpha)} \tag{91}$$

A more general way to view Equation (90) is to put it into dimensionless form. We define

$$x' = \frac{x}{L} \tag{92}$$

$$t' = \frac{t\ V_a}{L} \tag{93}$$

and

$$C^* = \frac{V_{go}}{V_a} \tag{94}$$

with

$$V_a = \left[\frac{p_o}{\rho_f \alpha(1-\alpha)}\right]^{1/2} \tag{95}$$

being the asymptotic velocity of the slug front when the condensation rate is large. Equations (90) and (91) can be rewritten as

$$\frac{d}{dt}\ (x'\ \frac{dx'}{dt'}) = \frac{C^*t' - x'}{1 - x'} \quad (C^*\ t' < 1)$$

$$\frac{d}{dt'}\ (x'\ \frac{dx'}{dt'}) = 1 \quad (C^*\ t' > 1) \tag{96}$$

which gives a universal solution as a function of C^* starting with $x'=o$, $dx'/dt'=o$ at $t'=o$. It is now a simple matter to solve Equations (95) and (96) numerically for various values of the dimensionless condensation rate C^*.

The velocity of the slug front is

$$V_s = \frac{dx}{dt} \tag{97}$$

or, in dimensionless form,

$$V_s' = \frac{V_s}{V_a} = \frac{dx'}{dt'} \tag{98}$$

The water velocity in the slug, however, is equal to

$$V = \alpha\, V_s \tag{99}$$

Therefore, a dimensionless water speed in defined, for convenience, so that it equals V_s',

$$V' = \frac{V}{\alpha\, V_a} = V_s' = \frac{dx'}{dt'} \tag{100}$$

The impact velocity when the slug reaches the end of the pipe follows by evaluating (100) at x'=1.

$$V_1' = \frac{V_1}{\alpha\, V_a} = \left(\frac{dx'}{dt'}\right)\bigg|_{x'=1} \tag{101}$$

The results of this computation are shown in Figures 66, 67, and 68.

In Figure 67 the end of each trace corresponds to the impact of the slug on the end of the pipe. At high values of C* the pressure is predicted to go to zero over a period of time (in practice this will be limited by the vapor pressure of the cold water). In order to distinguish between these parts of the traces they have been drawn at pressures close to, rather than exactly at, zero over the range for which they overlap. There are two limiting regions of operation with a transition in between.

With C* < 0.2 the void pressure approaches an asymptotic value corresponding to constant slug front speed close to V_{go}. This pressure difference is just enough to accelerate the water, scooped up in front of the slug, to the slug speed. The impact velocity is governed entirely by the rate of condensation.

At the other extreme, with C* > 2, the pressure in the void drops close to zero early in the process and the slug accelerates to the asymptotic speed V_a.

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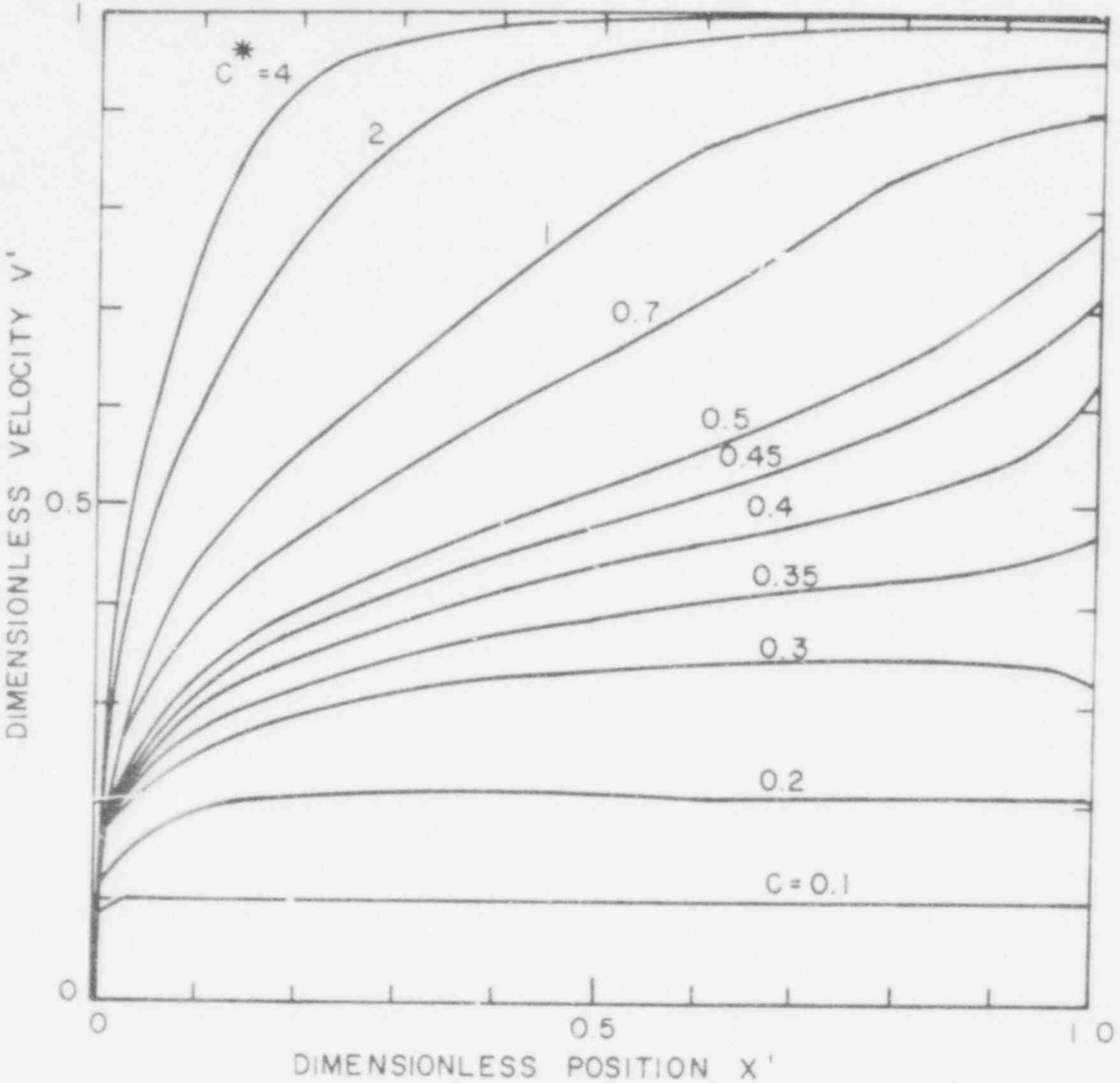

DIMENSIONLESS PLOT OF SLUG VELOCITY VS POSITION FOR VARIOUS CONDENSATION RATES

FIGURE 66

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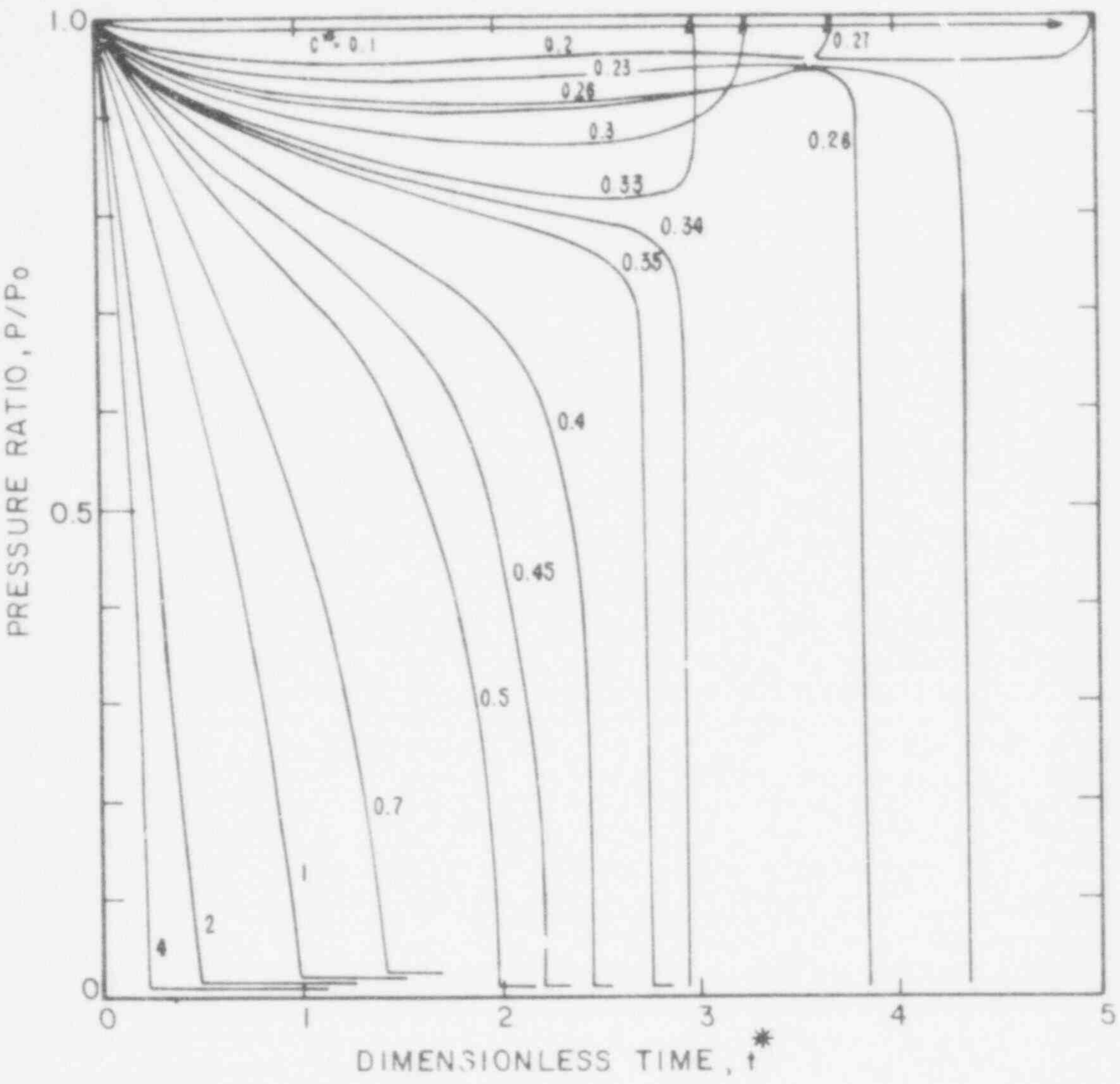

DIMENSIONLESS PRESSURE HISTORY IN THE VOID FOR VARIOUS CONDENSATION RATES, C*

FIGURE 67

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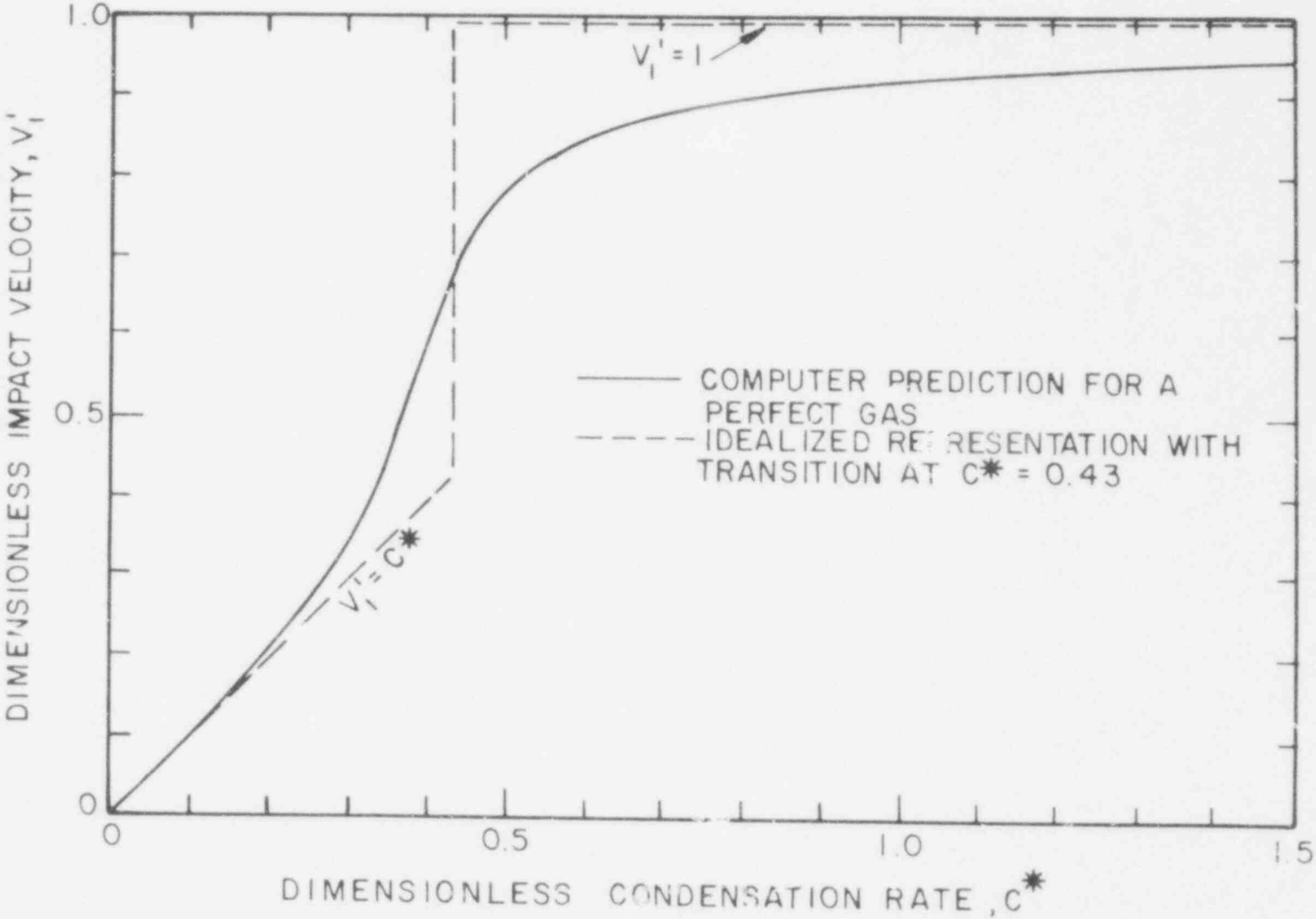

FIGURE 68
IMPACT VELOCITY VERSUS CONDENSATION RATE ON DIMENSIONLESS COORDINATES

730 022

If the two asymptotic theories are assumed to apply at "low" and "high" condensation rates, with a step change at a "critical" value of $C^* = 0.43$ (Figure 68) the error in predicting V_1 is less than 40% at all values of C^*. In view of the inevitable uncertainties in predicting C^* this computational idealization is probably acceptable as a reasonable approximation to practical behavior.

Figure 67 is particularly interesting because it shows a set of characteristic pressure signatures that can be matched with the output of a pressure transducer. Certain simple parameters, such as the fraction of time for which the pressure is close to zero at large values of C^*, can be used to give an immediate indication of what value of C^* might be chosen to represent the data approximately. There appears to be significant change in the character of the results between $C^* = 0.33$ and $C^* = 0.34$. In the range $0.28 < C^* < 0.33$ the gas is compressed over the latter part of the void collapse and the idealized analysis predicts a very rapid compression just before impact. For $C^* > 0.34$ or $C^* < 0.26$ the gas is removed faster than it is being compressed and the pressure drops sharply before impact. Figure 66 shows corresponding oscillations in the slug velocity. One way to viewing this is to think of the slug and the void as a mass-spring system with the end of the spring being drawn along at a roughly constant velocity (at low C^*) while the slug oscillates slightly about this velocity. Whether the final small element of gas is compressed or expanded just before the impact depends upon the phase angle of the oscillatory component of velocity.

If condensation is assumed to proceed at a steady, equilibrium rate dictated by the water injection rate according to Equation (47), then rapid void collapse canno- occur at PWR operating conditions. At $P_o = 1000$ psi and $Q_f = 200$ gpm, $V_{go} \approx 10$ ft/sec and $C^* \approx 0.05$. It is plain from Figures 67 and 68 that this condensation rate is too low to cause rapid void collapse and waterhammer. Therefore, it is necessary to assume that condensation has been strongly enhanced by sudden exposure of some of the pool of cold water stored in the feedpipe.

The following paragraphs discuss a few of the phenomena that have not been treated in these idealized analyses.

Pressure in Feedring

The above analyses have all been expressed in terms of the pressure difference $\Delta p = p_o - p_1$. Emphasis has been placed on determining the pressure p_1 in the void, particulary since these are the only direct pressure data that exist at any scale. It has generally been speculated that the pressure in the feedring and on the back face of the slug is approximately the steam generator pressure, as a conservative assumption. The report by Roidt [5] includes a calculation of pres-

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sure drop through the feedring holes due solely to the slug motion, but this introduces only a negligible correction term. However, in the likely event of rapid condensation on the trailing face of the slug as well as on the front face that advances into the void, rapid depressurization in the feedring to very low pressure may also occur.

Crudely, by the same arguments invoked in Section 5.2 to consider depressurization of the void, it might be possible to sustain a flow of steam condensing on the trailing face of the slug at a rate corresponding to as much as unity Mach number in the feedpipe cross-section. Since the total cross-sectional area of the feedring holes is only 60 to 90% of that of the feedpipe, this flow of steam could not be maintained through the holes even if they all run choked. Thus, in these circumstances p_o might also decrease rapidly so that the relevant impulse imparted to the slug by $\int(\Delta p)dt$ might be much less than $\int p_1 dt$ over the same time period.

Unfortunately, in the absence of quantitative measurements of p_o, it is only possible to speculate qualitatively on what might occur. The only truly conservative assumption is that p_o remains at the steam generator pressure. However, the possibility that p_o might be less than the steam generator pressure should be considered in any interpretation of quantitative data.

Three-Dimensional Effects

The analysis presented above ignores three-dimensional effects. It is to be expected that if the water velocity in the slug varies across the cross-section of the pipe (as it must do considerably when the slug is moving into a layer of stationary water) and if the front of the slug is not flat, the shape of the waterhammer pulse will not be square, perhaps showing a more gradual rise or a "spikey" top depending on the exact details of the flow. However, the simplified analysis should give a reasonable estimate of certain overall, average or limiting characteristics of the phenomenon.

Slug Stability

It is possible that the pressure difference Δp may simply be large enough to drive a hole through the slug rather than accelerate the entire slug. This situation is most prone to occur if the initial slug is short or has far to travel. It may provide a lower limit on slug length to preclude extreme overpressure which tends to infinity as slug length tends to zero. This possibility has not been treated previously and is mentioned here only for completeness.

Importance of the Entire Pressure Transient

There is a natural tendency to emphasize the brief, high-pressure spike at impact. However, it is shown in Section 5.4 that piping damage can occur by bending processes that depend to first-order on the pressure impulse, i.e., the integrated area under the pressure-time curve. Whereas analysis of the pressure impulse at impact is subject to significant uncertainties, the pressure impulse during the depressurization period prior to impact is easier to analyze, is unlikely to be reduced by the uncertain phenomena discussed above, and has already been measured unequivocally during the tests at Tihange. The significant gains already achieved in predicting the depressurization (e.g., Figure 67) should not be understated.

Summary of Limiting Slug Dynamic Analyses

Idealized analyses of water slug motion have been developed for several limiting situations based on one-dimensional conservation laws. The calculated slug velocity at impact is of the order of magnitude $V_o = [\Delta p/\rho_f]^{1/2}$. Closed form approximate expressions are derived for the effects of initial slug length, amounts of water lying on the bottom of the pipe initially, and conditions in front of and behind the slug.

Sensitivity calculations are performed to illustrate the two principle regimes of void collapse as condensation rate is varied. At low condensation rates the slug follows the slowly condensing void and the slug motion and impact is governed primarily by the condensation rate. At high condensation rates the void depressurizes rapidly and the slug motion and impact are insensitive to condensation rate and are governed primarily by inertia.

It has been demonstrated that condensation rates calculated on a steady state basis assuming that the injected water is brought instantaneously to saturation temperature are insufficient to lead to rapid void collapse and waterhammer. Condensation on the heat sink available in the pool of water in the feedpipe is necessary for rapid void collapse and waterhammer, as pointed out by Vreeland [34].

The possible effects of depressurization in the feedring (behind the slug), three-dimensional interface shape, and the stability of slugs subjected to large differential pressure have been identified, but not examined closely because quantitative data on these effects do not exist.

Analysis of Creare Results

The data described in Section 4 were examined relative to the idealized analyses presented above. The water cannon model may be viewed as a more ideal situation than the steam generator model that is more likely to yield results in agreement with first-order analyses. By first identifying deviations from ideal in the water cannon model, a more realistic perspective is established for examining the data obtained in the steam generator model.

Simultaneous recordings of depressurization traces (giving p_1 as a function of time) and the corresponding overpressure spike (giving p_h as a function of time, on an expanded time scale) were obtained in the water cannon model and steam generator model at several pressures in both metal pipes and plastic tubes. In addition, fluid velocities were determined from high speed motion pictures of the water cannon model. These data are suitable for making several comparisons with the analysis presented earlier in this section.

Overall Comments-Approximate Analysis

Almost all of the data (except those in which air was added and condensation inhibited) show depressurizations that, apart from various wiggles, are within the range C* = 0.3 to 0.4 on Figure 67. The predicted maximum depressurizations in this range are very sensitive to C*. The predicted dimensionless impact velocities are in the range 0.35 to 0.6.

The depressurizations are roughly the same for the acrylic and steel tubes. This is to be expected since the influence of the tube material during a slow transient is likely to be confined to wall friction and heat transfer effects. On the other hand, the waterhammer overpressures last roughly three times as long in the acrylic tubes. The explanation for this lies in the different value of "a" predicted by Equation (7).

$$a = \left(\frac{K}{\rho_f}\right)^{1/2} \left[1 + \frac{DK}{\varepsilon E}\right]^{1/2} \qquad (7)$$

For water at $\sim 70°F$, K = 320,000 psi, ρ_f = 62.4 lbm/ft^3 and $(K/\rho_f)^{1/2} \approx 4900$ ft/sec. For our tubes D=1.5 in. ε = 0.125 inches for Lexan and 0.145 inches for steel, E for steel is $3x10^7$ psi, while for Lexan it is about 400,000 psi. Evaluating Equation (7) for these values we get

$$a_{steel} \approx 4500 \text{ ft/sec}$$
$$a_{Lexan} \approx 1500 \text{ ft/sec}$$

Therefore, the speed of a waterhammer wave is one third as great in the Lexan tube.

Table 13 shows the approximate duration, t_h, of the waterhammer pulses and their amplitude, p_h, for the experiments that were duplicated in the steel and acrylic tubes.

730 026

Table 13

APPROXIMATE RESULTS OF COMPARABLE WATERHAMMER TESTS IN TUBES OF DIFFERENT MATERIAL

Apparatus	Tube Length L, ft	Material	Pulse Duration t_h, msec	Overpressure p_h, psi
Water Cannon	2.3	Steel	1.0	800-1200
Water Cannon	2.3	Acrylic	3.0	200-400
SG Model	4.0	Steel	0.5	200-600
SG Model	4.0	Acrylic	1.5	200-400

As we discussed earlier in the theoretical part of this section, we should expect the following approximate results immediately following impact by a one-dimensional slug in these circumstances, for the water cannon,

$$p_h \approx \rho_f V_1 a, \quad t_h \approx \frac{2L_s}{a} \tag{102}$$

for the steam generator model,

$$p_h \approx \frac{\rho_f V_1 a}{2}, \qquad t_h = \frac{2L_s}{a} \tag{103}$$

In the water cannon, if the tube is filled with water, $L_s = 2.3$ ft, and the predicted values of t_h are

$$t_h = \frac{2 \times 2.3}{4500} \times 1000 = 1.0 \text{ msec for steel}$$

$$t_h = \frac{2 \times 2.3}{1500} \times 1000 = 3.0 \text{ msec for Lexan,}$$

in agreement with observation.

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The impact velocity in the water cannon would be given by Equation (66) if the pressure p_h had dropped close to zero (large C*). However, our observed depressurizations are consistent with 0.3 < C* < 0.4 and therefore, from Figure 68,

$$p_h = (0.35 \text{ to } 0.6)\rho_f a \left[\frac{p_o}{\rho_f}\right]^{1/2} \qquad (104)$$

Substituting the appropriate values of a, with $p_o = 14.7$ psia, we obtain the following predictions:

p_h = 700 to 1200 psi for a steel pipe

p_h = 230 to 400 psi for a Lexan pipe

in general agreement with the measurements.

The pulse durations in the model steam generator feedpipe are found, in a similar way, to be consistent with a slug length of 1.2 feet, representing most of the water that would be scooped up into a slug in a pipe four feet long and about one-third full. For the acrylic pipe the overpressures are the same range as in the water cannon. According to (103) this implies twice the velocity of impact in the steam generator model (a water-water impact) which is consistent with roughly the same depressurization acting on a slug half as long. The overpressures measured in the steam generator model with a steel pipe, however, are a factor of two lower than suggested by this hypothesis. Mechanisms for significant deviation from idealized behavior are discussed in detail below

The preliminary and approximate calculations demonstrate the usefulness of some of the idealized models for slug motion, coupled with one empirical parameter, C*, that accounts in an overall way for the relative importance of the condensation effects. They also illustrate how the data from the idealized water cannon configuration are helpful to remove some uncertainties from interpretation of the data from the steam generator model. A more detailed examination of the data is described below.

Analysis of Simultaneous Pressure Traces

Figure 69 sketches the typical shapes of the simultaneous depressurization and overpressure traces obtained in the water cannon model (Figure 69a) and the steam generator model (Figure 69b). These sketches are not scaled, but merely illustrate the terminology used in the analysis. The parameters I_s and I_h are the areas under the depressurization and overpressure spikes respectively. The parameters p_h and t_h are the peak overpressure and the duration of the overpressure pulse respectively, and V_s and V_h are the calculated slug velocities based on the depressurization and overpressure spikes. The equations used for calculating V_s, V_h and L_s for each model are listed in Table 14, based on the analysis presented earlier. The slug length at impact is derived from the duration of the pulse. The impact velocity is derived in two ways; V_h is calculated from the overpressure amplitude while V_s is evaluated from the overall impulse represented by the area of the measured depressurization.

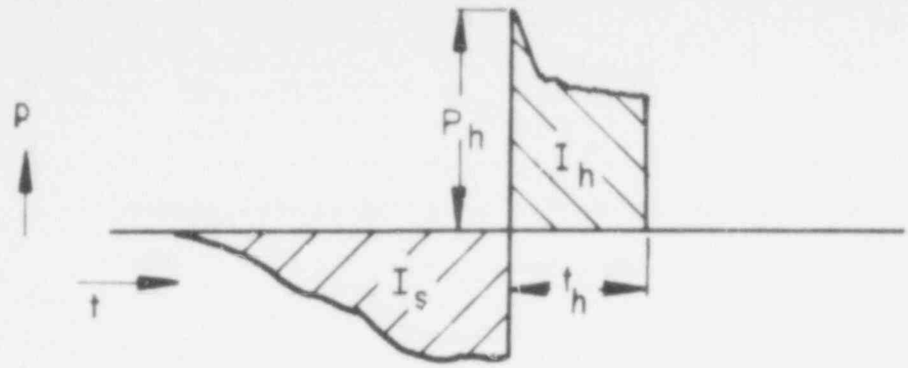
TABLE 14

EQUATIONS USED FOR COMPUTING SLUG LENGTH AND IMPACT VELOCITY

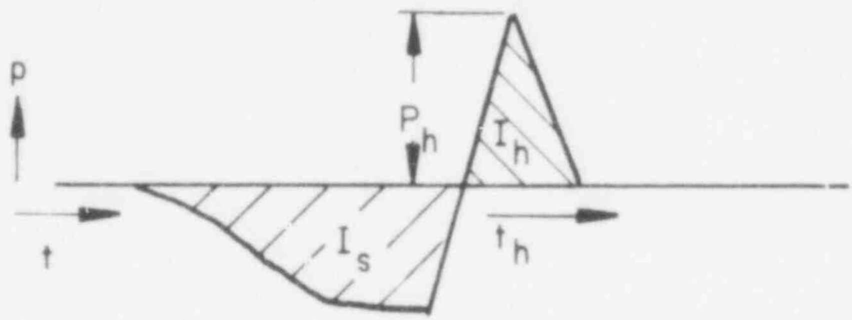
Water Cannon	Steam Generator
$L_s = \frac{a\ t_h}{2}$	$L_s = \frac{at_h}{2}$
$V_h = \frac{p_h}{\rho_f a}$	$V_h = \frac{2p_h}{\rho_f a}$
$V_s = \frac{I_s}{\rho_f L_s}$	$V_s = \frac{I_s}{\rho_f L_s}$

The expected area ratio I_h/I_s ranges from one to two for the water cannon model depending on whether the slug impact is inelastic ($I_h/I_s = 1$) or perfectly elastic such that the slug rebounds with the same velocity in the opposite direction ($I_h/I_s = 2$). The expected area ratio is one for the steam generator model. The ideal velocity ratio V_h/V_s is unity in both cases.

730 029



(a) WATER CANNON



(b) STEAM GENERATOR

SKETCHES OF PRESSURE BEHAVIOR VERSUS TIME IN WATER CANNON AND STEAM GENERATOR MODELS

FIGURE 69

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One additional calculation is included for the steam generator model. This is the ratio of the volume of the slug ($L_s A$, where A is the pipe cross-section area) to the total volume of water, N in the pipe before slug formation estimated from the flow rate by calculating the critical depth, deriving the actual water flow cross-section for this depth and multiplying by the tube length.

Water Cannon Model

The results from the water cannon agree well with the analysis as shown in Table 15. (Examples of the traces were presented and discussed in Section 4.1.) The duration of the overpressure was uniformly 1.0 msec in all traces, implying a slug length of 2.3 ft (uncertainty about 10%) in a pipe 2.3 ft in length. The area ratio I_h/I_s (expected to be as much as two) is in the range of 1.5 to 2.2 and these numbers are consistent with losses in the system, i.e., I_h/I_s tending to be somewhat less than two), and with the data scatter and uncertainty. The velocity ratio V_h/V_s is close to unity, as expected and the velocities of 15-20 ft/sec also agree with the motion picture data discussed in Section 5.1. Therefore, the analysis and the data are consistent for the water cannon model, giving us some confidence that slug dynamic calculations can be carried out successfully at least for situations where the size of the slug and the void and the driving pressure difference are well specified.

Steam Generator Model

The results from the steam generator model showed some trends at variance with the idealized analysis by a factor of two or more. Examples of these traces were presented in Section 4.2. The shape of the overpressure trace was usually triangular (rather than rectangular as in the water cannon model) and the duration, t_h, ranged from 0.5 to 1.0. The results are compared with the analysis in Table 16.

The calculated slug lengths range from 1.2 to 2.1 feet in this pipe which was four feet long total (10 inches of perforated length, 38 inches of regular piping). So the calculated slug length was 1/4 to 1/2 the total pipe length compared to the water cannon model where the calculated slug length equalled the pipe length. The calculated slug length in this model tended to increase with increased flow rate. The ratio of the calculated volume of the slug to the volume of water in the pipe ranges from 0.[illegible] to 1.96 with the ratio decreasing with increasing flow rate. At low water flow rates, the numbers greater than unity may indicate that there is some water storage (during the "percolation" before the waterhammer occurs).

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TABLE 15

ANALYSIS OF SIMULTANEOUS PRESSURE TRACES FOR WATER CANNON MODEL WITH STEEL PIPE

#	I_s (psi-msec)	I_h (psi-msec)	R_h (psig)	V_s (ft/sec)	V_h/V_s	I_h/I_s	L_s (ft)
1	598	1000	1200	20.0	1	1.67	2.3
2	345	450	500	11.5	0.71	1.30	2.3
3	522	900	1000	17.4	0.93	1.72	2.3
4	428	950	1100	14.3	1.25	2.22	2.3
5	546	1200	1300	18.2	1.15	2.20	2.3
6	586	800	1000	19.6	0.83	1.37	2.3
7	400	800	1000	13.3	1.22	2.00	2.3
8	482	800	1000	16.0	1.02	1.66	2.3
9	506	750	1000	16.9	0.96	1.48	2.3
10	473	900	1100	15.8	1.14	1.90	2.3
12	549	1000	1200	18.3	1.08	1.82	2.3

TABLE 16

ANALYSIS OF SIMULTANEOUS PRESSURE TRACES FOR STEAM GENERATOR MODEL IN BASELINE CONFIGURATION WITH FOUR FOOT STEEL PIPE

#	I_s (psi-msec)	I_h (psi-msec)	P_h (psig)	V_s (ft/sec)	V_h/V_s	$\frac{I_h}{I_s}$	L_s (ft)	Q (gpm)
1	525	175	700	34.1	0.67	0.33	1.18	1
2	479	94	375	31.2	0.39	0.20	1.18	1
3	411	125	500	26.7	0.61	0.30	1.18	1
4	494	88	275	22.9	0.39	0.18	1.65	1
5	478	178	475	20.7	0.75	0.37	1.77	2
6	632	188	500	27.4	0.60	0.30	1.77	2
7	517	193	550	24.1	0.75	0.37	1.65	2
8	540	227	500	22.9	0.71	0.42	2.12	4
9	447	252	475	16.2	0.95	0.56	2.12	4
10	428	210	600	19.5	0.98	0.49	1.65	5.3

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The calculated area ratio I_h/I_s ranges from about 0.2 to 0.5, tending to increase as the flow rate increases. The expected ratio was 0.5 to 1.0, so the results are less than predicted by the idealized analysis by a factor of two to five. The calculated velocity ratio V_h/V_s ranges from 0.5 to 1, somewhat less than the ideal value of unity. Both of these ratios have the same trend, a result which is expected from the manner in which the calculations were made. No motion pictures were made, so actual slug velocities are not able to be compared. The indication is that slug velocities and overpressures are less than the idealized analytical model calculates. This is probably because of complicating factors such as reduction of pressure at the trailing face of the slug, the slug interface shape, water lying in the pipe, water injection near the impact point or other losses all acting in combination (perhaps affecting the overpressure measurement), most of these not being present in the simpler water cannon model. These few tests are not definitive by themselves in determining the effect each of these parameters might have.

In addition, the considerable scatter in Table 16 is evidence of a rather ill-defined situation in which theory can be used to predict an "upper limit" but considerable statistical fluctuation below this limit occurs in practice. A similar conclusion follows from the histograms of maximum overpressure presented in Figure 16 or the "spray" of data displayed in Figure 27. It can be concluded that it may be insufficient to perform a limited number of tests if a good perspective of the possible range of overpressures and impulses to piping is desired for a given system.

In summary, the idealized analysis using the measured depressurization predicted the overpressure data from the simple water cannon model well within the data scatter and analysis uncertainty, each of which represent a factor of two range. When the same analysis (and instrumentation) was employed on the steam generator model with a steel pipe section, the overpressure tended to be overpredicted by a factor of two and the impulse was overpredicted by a factor of two to five even though the actual measured depressurization of the void was used in the analysis. Effects present in the steam generator model but not in the water cannon, such as possible depressurization at the trailing face of the water slug, and potential effects of water lying in the bottom of the tube and slug interface irregularity at impact are together implicated in the measured reduction of impact intensity. These findings contribute to the overall uncertainty of predicting PWR behavior which are dominated by effects discussed in previous sections such as the difficulties of predicting the void depressurization and specifying the size of the void and slug as the slug is for[illegible].

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Analysis of Full Scale Data

Tihange

Two pressure traces (Figure 60) are available from one experiment at the Tihange plant, as described in Appendix A. They were obtained from two transducers separated by a considerable length of piping (the actual length is not known, but since the traces were displaced by approximately 8 msec a separation distance of 36 feet can be deduced if a = 4500 ft/sec). Each trace reflects the same depressurization history, a reduction from 70 bar (1015 psi) to almost zero in about 20 msec, followed by a dwell time at low pressure for a further 30 to 40 msec before a sudden overpressure. The overpressure was not accurately recorded since both transducers failed; however, pressures at least as high as 400 bar (5800 psi) apparently occurred.

A depressurization trace with a dwell time at low pressure about 1.5 times as long as the duration of the decreasing pressure ramp corresponds to a value of C* of 2 in Figure 67, by far the largest value for any experiment of which we are aware. The corresponding impact velocities would lie in the "rapid condensation" range and should be close to αV_a from (99). If Equation (95) is used with $p_o \approx 1000$ psia, $\rho_f = 62.4$ lbm/ft^3 (cold water) and α is chosen to be 1/2, the predicted value of V_1 is 273 ft/sec, and the immediate overpressure for liquid-liquid impact is

$$p_h = \frac{\rho_f V_1 a}{2} \approx \frac{62.4 \times 273 \times 4500}{2 \times 32.2 \times 144} \approx 8300 \text{ psi} \qquad (105)$$

At a later time, pressure wave reflection at a closed end may be double this value. Larger values of p_h can be predicted by choosing other equations derived previously for alternative idealized assumptions. On the other hand, our experience with our small scale steam generator model indicates that the idealized model represented by Equation (95), tends to give an overestimate.

The Tihange data are reasonably self-consistent up to the time when the transducers failed. Should they have not failed, it is possible that either p_h or t_h would have to be larger than indicated on Figure 60 in order to make I_h comparable with I_s, so that it is also possible that p_h was as high as 8300 psi calculated above. It is also possible that I_h could be much less than I_s so that the actual recorded p2 trace would be reasonable if the two sets of spikes identified as possible electrical disconnections were removed. Since the transducer failed and could not be calibrated after the

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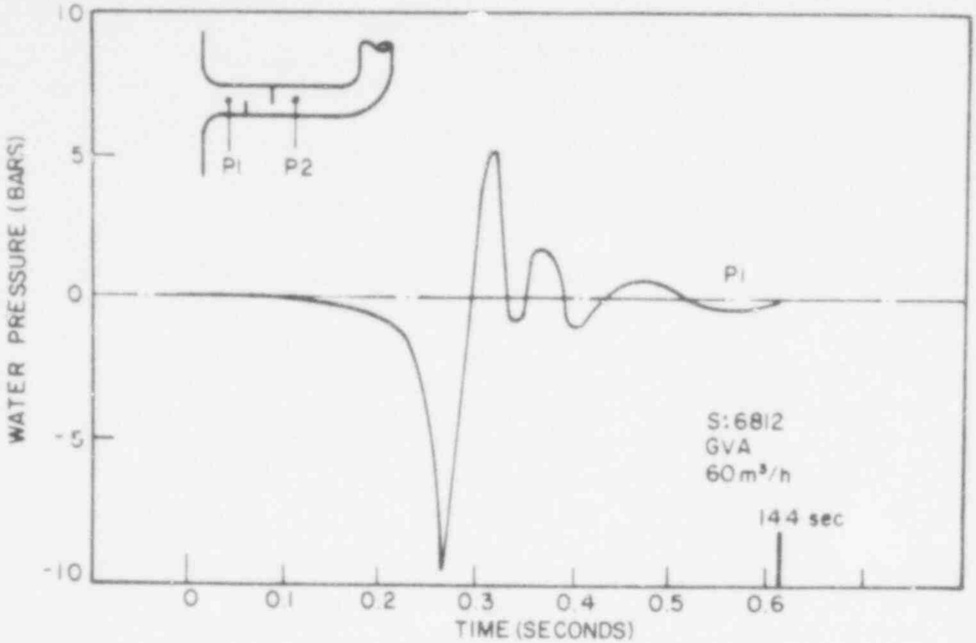
event, it is difficult to know what the true overpressure pulse was. If the latter assumption is made, then one must explain the repeated spikes [illegible]ly 2 msec apart. This is far too rapid to be a slug rebound, and subsequent collapse of a vapor cavity, or repeated slug formation such as observed in our experiments. It is comparable with the recorded pulse duration of approximately 2 msec which is the time required for a wave to propagate up and back in a slug of length 4.5 feet with a = 4500 ft/sec. Similar rapid ringing was not recorded in any of our experiments with a straight length of piping, however, and it is difficult to imagine a mechanism for reflecting such a wave more than once in the feedring. The 2 msec period is a factor of six too slow relative to the pipe natural frequency in the hoop direction, but is very rapid relative to the 2 to 10 Hz natural frequency of typical feedwater piping in the bending mode, or in propagation of waves down the lengthy piping. So we are forced to conclude that either the pressure record is spurious or that we have been unable to treat a fluid/piping interaction of some kind. This same conclusion has been derived by several groups with access to a complete description of the Tihange piping system.

This last point bears amplification. Only trace P2 gives any direct information on the impact pressure. However, transducer P2 is tens of feet from the steam generator nozzle. Without performing a detailed calculation of the response of the actual piping, it is difficult to explain the frequency and form of the measured P2 trace, (as described above). Westinghouse [2] and Bechtel apparently have done these calculations and still have not explained the P2 trace, whereas, Vreeland [34] claims to have been successful. Creare does not have a description of the Tihange piping system and has not attempted a detailed waterhammer calculation. It should also be noted that the compliance of the piping between points P1 and P2 may have attenuated the waterhammer wave appreciably. One might expect that the P1 transducer near the steam generator (which failed immediately) had been subjected to a stronger pressure pulse.

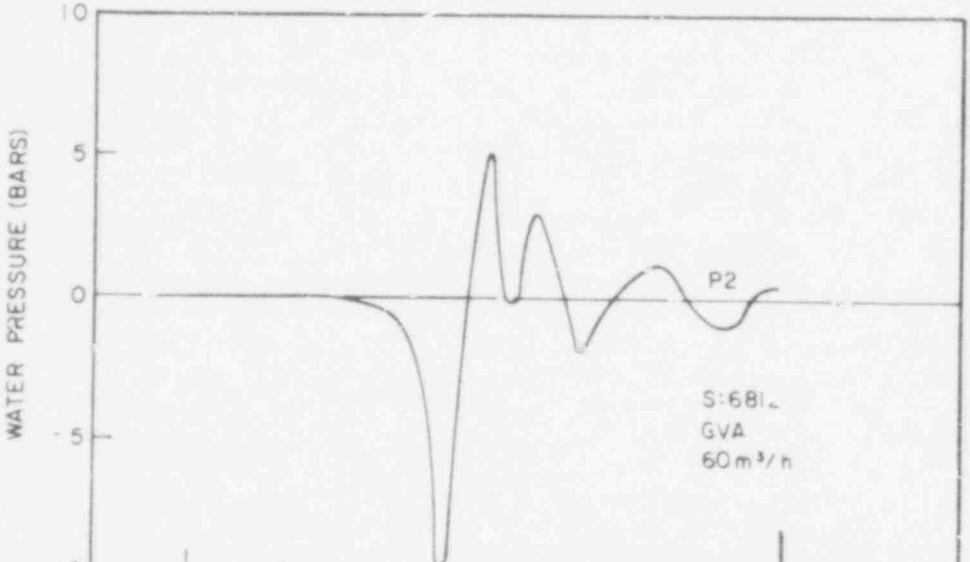
Doel

A dynamic pressure recording from the Doel tests (discussed in Appendix A) is provided as Figure 70. It shows a much more modest depressurization than the Tihange data, the maximum being about 10 bar in a system assumed to be at 70 bar. This minimum value of $p/p_o = 0.86$ corresponds to $C^* = 0.3$ in Figure 67, though the rapid fall off in pressure toward the end of the transient is more typical of a value such as $C^* = 0.35$. The pressure spikes are mild, with no real waterhammer, although a noise and pipe shaking were reported; the implication seems to be that there was either incomplete condensation or that the slug front was sufficiently three-dimensional. The Creare results, with almost the same value as C^*, gave a distinct waterhammer many times.

730 035



(a) TRACE FROM SENSOR P1



(b) TRACE FROM SENSOR P2

PRESSURE HISTORIES IN DOEL #2 FEEDWATER PIPE (FROM REFERENCE [64])

FIGURE 70

730 036

Indian Point #2

Pressure data taken at Indian Point #2 during reported waterhammer events also display mild pressure variations similar to those at Doel. The particular instrumentation employed at Indian Point #2 makes it difficult to assess these data in a similar fashion, however, and an analysis of these data is not warranted for present purposes.

Summary of Full Scale Data

The most striking conclusion from these extremely limited full-scale data is the difference by almost a factor of 10 in the values of C* that will model the two full-scale cases and the resulting change in magnitude of the pressure spikes. The only difference between the plants that is reflected in C* is the condensation rate--a parameter that we have already established to be highly variable, and unpredictable with any confidence at present. At Tihange, something happened to make condensation occur at a rate close to the maximum possible value allowed by gas dynamics; at Doel condensation was much milder. It is worth noting that the horizontal run of piping at Doel (two feet) is somewhat shorter than the eight foot run of piping at Tihange. While all sorts ofspeculations for the reasons behind these differences are possible (such as change of a fraction of a percent in the noncondensibles present) the evidence is too scanty for any definite conclusions to be drawn.

Summary of Slug Dynamic Analysis

An idealized mechanical analysis of water slug motion and impact has been derived and is confirmed by the Creare data, though errors increase as the water surface geometry becomes less well defined, and statistical spread in the data is quite large. The idealized analysis (with the measured void pressure history as input) tended to overpredict the measured overpressure magnitude by a factor of two and the impulse by a factor of two to five for our experiments with a 1/10 scale steam generator model at pressures near atmospheric pressure. These data are too restricted to provide a factor suitable for general use, but they do suggest that various phenomena related to slug dynamics may play a role in limiting that predicted by an idealized analysis even if the void depressurization is specified accurately.

Condensation rates can also exert a strong influence on the behavior, especially over a narrow "critical" range. They cannot be predicted accurately at all. Hence, it is difficult to improve on the crude bounding analysis that assumes complete and instantaneous depressurization in the void, although the actual depressurization has been much less in several cases. As a minimum, such an assumption together with the present analysis of slug dynamics can provide a

730 037

reasonable estimate of the pressure impulse during the period prior to slug impact. This impulse is likely to have central importance in the prediction of piping overstress, as described in Section 5.4.

The Tihange and Doel data are consistent with the analytical model if a suitable condensation rate is chosen. Wide variation in condensation rate is probably the major cause of differences in observed full-scale behavior and the present state-of-the-art does not allow its accurate prediction. Difficulties in specifying slug length and initial void size contribute to the uncertainty in assessing the limited data available from PWR tests.

5.4 Damage to Piping

There are at least two ways in which damage to piping can occur as a result of waterhammer. The first of these is a local stretching, or even bursting, of the pipe, probably near the region where the water slug impacts, as a result of exceeding the yield stress of the wall material in the hoop direction (similarly, piping components such as valves can be overpressured). The second mode of damage involves the "bending" response of the piping system to to the impulses it receives from the waterhammer wave; pipe hangers, restraints and joints are likely to be damaged by this mechanism.

The first failure mode can be anticipated whenever the calculated waterhammer peak pressure gives rise to hoop stresses above the yield stress of the pipe materials. There is evidence reported in Appendix A that this did indeed happen at Indian Point #2. We shall see that the Tihange data suggest that there may have been plastic deformation of the pipe in that case also.

The second failure mode is much more difficult to predict. One needs a computer code describing the piping system, the inertia of its various pieces and the force-deflection characteristics of all restraints, including the pipe itself. In addition, the coupling between the waterhammer pulses and a compliant piping system may need to be described--an undertaking that we believe to be beyond the present state-of-the-art. An analysis of this type is outside the scope of our study. However, we have been able to perform a highly idealized analysis that gives estimates approximately in line with the broken pipe evidence from Indian Point #2.

Local Bulging of the Feedpipe

The feedpipe at Indian Point #2 had a nominal radius of nine inches and a wall thickness of 0.7 inches. Assuming a yield stress of 70,000 psi, the pressure necessary to cause plastic deformation statically is,

$$p = \frac{\sigma\varepsilon}{r} = 70{,}000 \times \frac{0.7}{9.0} = 5400 \text{ psi} \qquad (106)$$

Even lower pressures would be sufficient if the overpressure pulse rise was sufficiently rapid. Since a figure of 8300 psi resulted from our order of magnitude assessment of the Tihange pressure data, there appears to be adequate potential for this mode of damage.

The feedpipe at Indian Point #2 did indeed bulge, by an amount equal to approximately 0.2 inches on the radius over a length of two feet. The minimum slug-impact energy needed to cause this bulging is the energy to yield:

$$E_{yield} = \int p_y d\upsilon = 1.23\text{x}10^5 \text{ ft-lb}_f \qquad (107)$$

where p_y is the pressure causing yielding (taken to be 5400 psi) and $d\upsilon$ is the change in pipe volume. A thorough analysis of this problem would involve a treatment of the dynamics of the water and pipe wall motion during this yielding, rather like the analysis of a tamped explosion, and is probably worth further work. For the moment we assume that the energy necessary to deform the pipe comes from a slug impact resulting from acceleration by a driving pressure difference of $\Delta p = p_o$, the steam generator pressure. The kinetic energy of the slug at impact E_{slug} is not completely absorbed by yielding of the pipe wall. The momentum equation requires that a waterhammer wave of magnitude $p_h = p_y$ travel away from the impact site, storing potential energy E_{wave}. The energy balance $E_{slug} = E_{wave} + E_{yield}$ can be evaluated for the estimated conditions at Indian Point #2 (in ft-lb_f):

$$E_{slug} = E_{wave} + E_{yield}$$

$$\frac{1}{2} \rho_f L_s A V_1^2 = \frac{1}{2} \frac{L_s A}{B_f} p_h^2 + 1.23\text{x}10^5$$

$$1.715\ L_s V_1^2 = 1.16\text{x}10^5\ L_s + 1.23\text{x}10^5 \qquad (108)$$

where

L_s = slug length (feet)
V_1 = impact velocity (ft/sec)
B_f = bulk modulus of water (psi)

Table 17 lists the slug velocities derived from this equation for various values of slug length.

The kinetic energy of the slug comes from the collapse of a steam void. The work done by the collapse is $p_o L_v A$ for a void of length L_v and cross-section A. If we assume that half the steam work is dissipated in hydraulic mixing during slug formation and acceleration, then the void length can be found:

$$\frac{1}{2} p_o L_v A = \frac{1}{2} \rho_f L_s A V_1^2$$

$$\frac{L_v}{L_s} = \left(\frac{V_1}{V_o} \right)^2 \qquad (109)$$

where V_o is given by Equation (66) with $\Delta p = p_o$. Evaluating Equation (109) with p_o=1000 psi gives the void lengths displayed in Table 17, which are quite reasonable. If L_s=2 feet is assumed (to correspond with the length of the measured bulge) then a void only 2.8 feet long would have had to collapse to bulge the pipe and also produce a waterhammer wave of approximately 10,000 psi amplitude and 0.4 msec duration. Shorter or longer lengths could have caused the measured bulge.

TABLE 17

IMPACT VELOCITY AS A FUNCTION OF SLUG LENGTH FOR INDIAN POINT AND TIHANGE

Parameter	Equation	Units						
L_s	-	ft	1	2	5	10	15	25
L_v	(109)	ft	1.9	2.8	5.5	10.1	14.6	23.7
V_1 - Indian Point #2	(108)	ft/sec	373	322	286	274	269	256
V_1 - Tihange	(110)	ft/sec	3000	1500	600	300	200	120

Considering now the Tihange pressure trace, we have a depressurization of about 1000 psi by 40 msec. Equating this to the impulse given to a water slug we have

$$V_1 L_s = \frac{1000 \times 40 \times 10^{-3} \times 144 \times 32.2}{62.4} = 3000 \text{ ft}^2/\text{sec} \qquad (110)$$

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This equation may now be used to give the second row of velocity predictions in Table 17. Since overpressure is proportional to slug velocity and exceeds the pipe yield criterion for $V_1 > 200$ fps, the slug length at Tihange must have been greater than about 15 feet if damage did not occur, and if the various crude assumptions made in the analysis are appropriate. Alternatively, there may have been a bulge that was overlooked, or some assumption may be incorrect (e.g., the driving pressure difference Δp may have been much less than the void depressurization $[p_o - p_1]$).

We may also consider the dynamics of void collapse at Tihange. Assuming constant velocity slug motion, 200 ft/sec times 40 msec gives a collapse length, L_v, of eight feet. The void can hardly have been longer than this, since eight feet is the entire length of feedpipe horizontal run outside the steam generator. Had L_v been larger, V_1 would have been greater and pipe bulging would have been likely according to this approximate analysis.

A slug length of 15 feet at Tihange is equivalent to dividing the entire volume of the feedring by the diameter of the feedpipe. To first order, therefore, the Tihange results are explainable if we assume that as the water level reached the feedring, water was sucked into the ring to form a large slug that then travelled into a void composed of most of the horizontal run of the feedpipe. This claim may be supportable by test evidence that is presently unavailable to Creare. The large surface areas available in the feedring and the turbulence associated with water squirting in through the feedring holes may account for the very rapid depressurization.

The conclusions from this analysis are:

1) The pipe bulge observed at Indian Point #2 can be explained by the collapse of a steam void 2.8 feet long acting on a water slug about two feet long at impact.

2) The absence of a pipe bulge at Tihange can be explained by assuming that a slug filled most of the feedring and that a steam void filled most of the horizontal run of the feedpipe before collapsing.

It must be noted that these calculations are primarily intended to illustrate the type of assumptions that must be made in order to derive quantitative information from the very limited data available from PWR tests. Various alternative assumptions are possible and may lead to other estimates and "explanations" of the measured behavior.

Bending of the Feedpipe

The potential for pipe damage from a waterhammer event is not limited to the vicinity of the event. The waterhammer pressure pulse, which arises at the point of slug impact or other abrupt flow stoppage, can travel long distances through a pipe network without significant attenuation in rigid systems. The subject of pulse transmission has been treated in the open literature although experimental data are scanty. The influence of some components such as pipe branches, area changes, and fluid property changes can be calculated, although sophisticated computer programs may be required to do the bookkeeping in multi-element systems. The traveling pressure wave changes the fluid momentum; unbalanced forces are exerted on the pipe whenever the wave passes through a bend. The resulting pipe motion must be kept small to prevent damage to the piping. The response of a pipe network to a specified collection of forces and torques can be computed by any one of several existing computer programs. Thus, the adequacy of proposed pipe hangers and snubbers can be ascertained. The following paragraphs present an extreme simplification of calculations of this phenomenon.

Cantilever Example

An example of a single pipe is treated to illustrate the physics and the analysis that can be performed for more complex systems. Consider a pipe cantilevered from a rigid anchor, with an elbow at the free end, as in Figure 71. Assume that a square pressure wave of the type sketched in Figure 63 propagates through the pipe bend with an amplitude p_h less than that required to deform the pipe plastically in the hoop direction. The precursor depressurization pulse is neglected and the fluid in the pipe is assumed to be stationary initially in this example. The force on a bend during the passage of a pressure pulse can be found from the one-dimensional momentum equation, applied to the control volume shown by the dashed line of Figure 72.

$$F_y = -p_1 A - \rho_f A V_1^2 + \frac{\partial}{\partial t}\int V \cos\theta \, \rho_f dv \qquad (111)$$

Let t=0 be the time at which a pressure pulse arrives at station 1. Prior to t=0 there is no velocity (or perhaps a negligible flow velocity), no pressure imbalance and consequently, no resultant force F. As the square pulse of magnitude p_h moves through the bend, the angle θ measures its progress:

$$R\theta = at \qquad (112)$$

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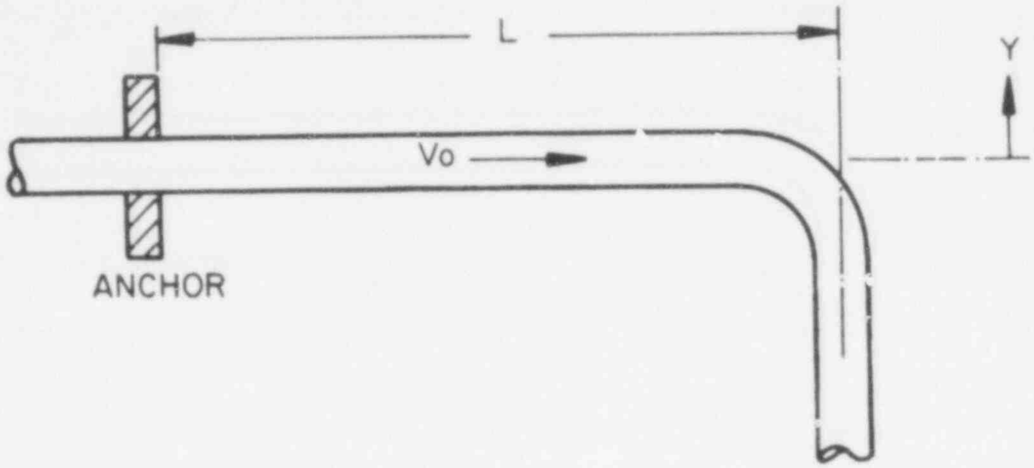
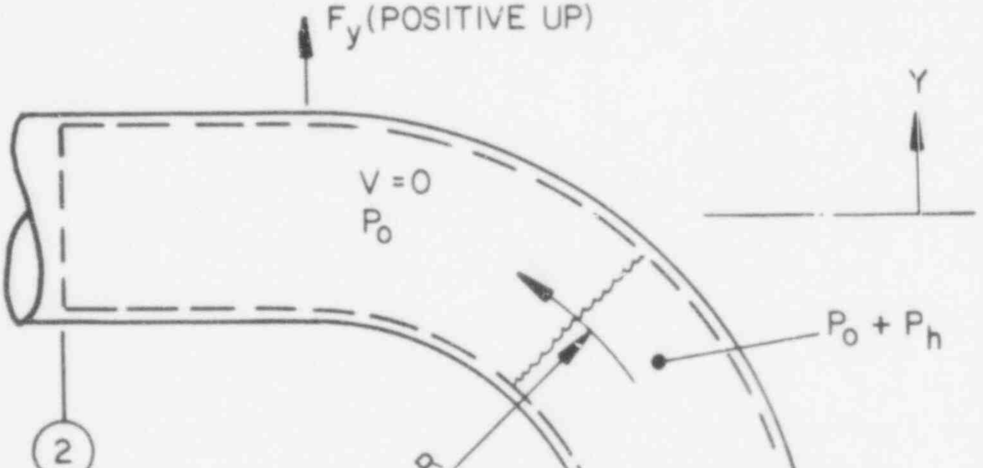


IDEALIZED PIPING SYSTEM

FIGURE 71



FREE BODY DIAGRAM

FIGURE 72

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The velocity change across the pressure wave is,

$$\Delta V = V = p_h/\rho_f a \quad (113)$$

The force equation (111) can be rewritten using (112) and (113) and the integration and differentiation performed.

$$F = -A[p_h + p_h^2/\rho a^2 - p_h \cos(at/R)] \quad (114)$$

The second term of the right hand side is generally neglected in waterhammer analysis, since it is of small magnitude of order V/a, the ratio of the initiating slug velocity to the speed of sound in water. Thus,

$$F \simeq - p_h A[1-\cos(at/R)] \quad (115)$$

Since this equaion applies only to the time during which the wave front traverses the bend ($0 < t < \pi R/2a$ for a 90° elbow), the maximum force magnitude is $p_h A$ and agrees with intuition. The force builds to this maximum with time, then remains constant until the end of the pulse enters the bend and drops the pressure to its original value.

If the bend radius is large (i.e., the pulse is brief), the force does not reach $p_h A$ before the end of the pulse enters the bend. For such a "short" pulse of duration $\tau < R/a$, the peak force on the bend is $-p_h A \sin(a\tau/R)$.

The pipe deflection y in response to the load can be found by integration of the one-dimensional dynamic spring equation:

$$d^2y/dt^2 = -\omega_o^2 y + F/M \quad (116)$$

where: ω_o = natural frequency of cantilever
M = effective mass of cantilever

The maximum deflection can be shown to be approximately:

$$y_{max} = \frac{F}{M\omega_o^2}[2-2\cos\omega_o\tau]^{1/2} \quad (117)$$

when $\omega_o\tau << \pi/2$ (which is the case for typical feedwater pipelines) the maximum deflection is:

$$y_{max} = 2F\tau/M\omega_o = 2p_h A\tau/M\omega_o \qquad (118)$$

Thus, the pipe deflection is proportional to the impluse $p_h A\tau$ in this idealized example of a pulse with period τ that is short relative to the period $(\pi/2\omega_o)$, even if $\tau < R/a$.

The stress in the pipe is greatest at the fixed end for the simple cantilever beam treated here. Beam theory for a thin-walled cylinder gives:

$$\sigma = 3Ery/L^2 \qquad (119)$$

where:

E = pipe bulk modulus
r = pipe radius
y = pipe free end deflection
L = pipe length

The natural frequency of the cantilever is:

$$\omega_o \quad 3.52 \quad [E\pi r^3 t/M'L^3]^{1/2} \qquad (120)$$

$$M' = \frac{8}{3} M \qquad (121)$$

where:

t = pipe wall thickness
M' = actual cantilever mass

The stress at the fixed end of the pipe can be directly related to the impulse given at the bend once the pipe parameters have been specified.

Indian Point #2 Data

The following numbers represent the feedwater pipe at Indian Point #2. The nominal values below, when used in the governing equations, should provide a feeling for the damage potential of pressure pulses in a pipe network. Further, the variation of peak stress with various pipe parameters should be qualitatively correct.

r = 9 inch radius
L = 50 feet length
t = 0.7 inch wall thickness
E = 3×10^7 psi modulus
M' = 12200 lbm actual mass (including water)
ω_o = 15.3 rad/sec = 2.4 Hz natural frequency

Inserting these numbers in the stress Equation (119) with the deflection y set equal to the value determined by Equation (118) gives:

$$\sigma = (2370 \text{ sec}^{-1})\ p_h \tau \qquad (122)$$

If a slug impact initializes the pressure pulse considered above, then further information can be derived from the stress equation. The period τ of the traveling wave is related to the speed of sound a and the slug length L_s by

$$\tau = 2L_s/a \qquad (123)$$

and the overpressure p_h depends on the slug velocity before impact, V_1, as

$$p_h = \rho_f V_1 a/2 \qquad (124)$$

The resulting impulse (divided by pipe area) can replace $p_h\tau$ in the stress equation by $p_h\tau = \rho V_1 L_s$.

Depending on the postulated initiation mechanism, different parametric studies of those basic equations become useful. If a bubble collapse energy is known, then $1/2\rho A L_s V_o^2$ is fixed, and only one variable is free. Stress as a function of slug length for the fixed energy of $1.23\text{x}10^5$ ft-lb_f is presented in Table 18. This energy is the minimum energy calculated previously that was necessary to produce the observed pipe bulge at Indian Point #2. Naturally, a

TABLE 18

CANTILEVER STRESS AT CONSTANT PRESSURE WAVE ENERGY

L_s (ft)	V_1 (ft/sec)	p_h (psi)	σ (psi) L=10 ft	σ (psi) L=20 ft	σ (psi) L=50 ft
0.5	379	12000	30200	15100	6050
1	268	8500	42800	21400	8550
2	190	6000	60500	30200	12100
5	120	3800	95600	47800	19100
10	85	2700	135000	67600	27000
15	69	2200	166000	82800	33000
25	54	1700	21400	106900	42750

single waterhammer event with this minimum energy could not both bulge the pipe and send a wave of equal energy down the pipe because the initial bulge would "absorb" the slug impact However, it provides a representative order of magnitude.

In Table 18, stresses exceeding 70,000 psi are underscored to emphasize that at these conditions, the yield stress of steel is exceeded. At short slug lengths, the high kinetic energy is manifested in high slug velocities, with consequently large waterhammer pressures and the possibility of local pipe deformation in the hoop direction. Corresponding pressures in Table 18 are underscored. Long slugs with more mass have lower velocity and lower overpressure magnitudes, but the longer pulse duration leads to higher impulse (at fixed wave energy) and higher cantilever stress. It is interesting to note that, within the limits of the short pulse approximation ($\omega_o \tau << \pi/2$), the stress decreases as the cantilever length L increases, in contrast to conventional wisdom expressed by some plant personnel that long pipes are more prone to fracture.

If the initiating mechanism is not one of known energy, but rather known velocities or slug accelerating pressure, similar tables can be readily constructed.

The above analysis and calculations are intended solely to indicate the main phenomena and to provide order of magnitude estimates. This work should not be regarded as predictions of possible PWR system behavior. However, it serves to illustrate that the main predictive uncertainties are more likely to be due to uncertainties in such parameters as slug size, slug velocity or void collapse energy than to modeling uncertainties in carrying out a competent waterhammer or pipe stress analysis.

The conclusions of this very simplified analysis of pipe damage due to "bending" can be summarized as follows:

1) The maximum force on a bend that is short compared with the waterhammer pulse length is the product of the waterhammer wave overpressure and the pipe cross-sectional area $p_h A$.

2) The maximum deflection of a simple cantilevered pipe subject to a waterhammer wave traveling through a bend at the unsupported end of the pipe is proportional to the wave impulse $p_h A\tau$ as long as the pulse period τ is short compared with the cantilever period. This wave impulse may in turn be expressed approximately as the total momentum of the impacting water slug or the integrated area of the void depressurization trace $\int A(p_o - p_1)dt$ unless there is reason to reduce the latter by hydraulic mixing, pipe yielding, or wave attenuation effects.

3) For a given slug kinetic energy before impact, the large slugs have greater momentum and hence, more potential for damage to piping systems by bending.

4) A first order analysis shows that the energy of the order of that required to cause bulging of the pipe at Indian Point #2, if it were converted to a waterhammer wave, would have potential for breaking unrestrained pipes anchored at one end with cantilevered lengths in the range that is found in a typical PWR plant.

5) A more realistic stress analysis will employ the same physics, but will utilize a more accurate hydraulic forcing function and will also treat pressure wave reflection, dynamic amplification, pulse attenuation, finite bend size, the distribution and interaction of forces, restraints and other pipe elements throughout the network, and the coupled interaction of the pressure pulse train with the compliant piping system.

In summary of piping damage analyses, first-order analyses have been derived to describe two potential modes of pipe system deformation corresponding to the damage observed at Indian Point #2, namely "bulging" of the pipe in the hoop direction due to internal pressure magnitude, and "bending" of the various pipes in the pipe network due to unbalanced dynamic forces that depend primarily on the pressure impulse as the waterhammer pressure waves propagate through bends in the pipe system. Limited calculations have been conducted to derive order of magnitude estimates, identify relevant parameters and their sensitivity, point out the types of assumptions that must be made, and provide an approximate assessment of evidence from Indian Point #2 and Tihange.

5.5 Summary of Analytical Efforts

First order bounding analyses have been developed in this report for each of the component phenomena described in the Introduction. Some major phenomena have been identified for the first time and an overall perspective has been achieved and presented in a single document. The present analytical models are not sufficiently mature for use in predicting PWR behavior, although they fairly represent the present state-of-the-art. Accordingly, Creare has not attempted to synthesize a "best-estimate" analysis combining all of the phenomena. Nor is a general "forcing function" proposed to represent the most severe credible slug impact event during anticipated abnormal operating transients in PWRs.

A detailed assessment of the present state of knowledge is deferred to the following Section 6 where recommendations for immediate action and further study are also made.

6 SUMMARY AND RECOMMENDATIONS

This section summarizes our findings and presents a general assessment of the state of knowledge on steam generator waterhammer. The approaches recommended by the PWR vendors to reduce the frequency or severity of steam generator waterhammer are evaluated alone and in combination. Our recommendations for action by the Nuclear Regulatory Commission are then made.

6.1 General Assessment of Knowledge of Steam Generator Waterhammer

The sequence of events termed "steam generator waterhammer" in this report is not yet amenable to routine analysis for the purpose of design and verification of PWR piping systems. A truly conservative analysis employing the most bounding assumptions described in Section 5 leads to peak overpressure and impulse histories that can overstress some secondary system piping based on present design practice. These bounding assumptions reflect substantial uncertainties surrounding the basic physics. Thus, considerable engineering judgment is required at present to strike the proper middle ground. The available evidence as well as recommendations based on it must be viewed from that perspective.

There exists little empirical evidence; model studies to date have been conducted at very small scale (approximately 1/10) and usable data from reactors is virtually nonexistent. The limited experiments conducted as part of the present work to support the analytical model development have in fact supplied the most comprehensive body of quantitative data presently available. Similar modeling experiments conducted by Roidt of Westinghouse [5] were marred by reported high air content in the steam and questions on the instrumentation that force us to disregard those data. (Typical impact pressures of 10 psi were reported by Roidt in contrast to the 500 to 1000 psi measured in a similar facility at Creare.) Other modeling experiments were carried out by the Framatome group at 1/10 scale and at pressures up to 750 psi. Unfortunately, the Framatome test report has been unavailable to Creare. It is our understanding, however, that this study, consisting of a very limited number of tests of various configurations such as bottom discharge, top discharge, and vents, was intended primarily to guide a decision on the hardware modification to be implemented in the French plants. The Framatome report may contain useful quantitative data, but its release is unlikely to dramatically advance the present understanding of phenomena. If the state-of-the-art is to be advanced, additional empirical evidence will be needed, particulary from facilities at larger scale and with more extensive instrumentation than employed in the past.

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Considerable gains in understanding have been made during the present work. A hydraulic instability responsible for slug formation in bottom discharge systems has been identified for the first time and confirmed by air-water experiments at 1/10 and 1/4 scale and steam-water experiments at 1/10 scale. A first-order analysis of these phenomena has been developed. This type of instability does not occur in top discharge systems, where instead a wave-like instability arises due to countercurrent flow in the feedpipe. The latter slug-formation mechanism is relatively well understood from previous work by Wallis and Dobson [20]. An appreciation of the governing phenomena and dimensionless parameters makes it possible to rationalize observed differences in the behavior of various systems. For example, the increase in threshold flow rate with top discharge in our experiments may be ascribed to the shift in the governing instability mechanism described above.

The main analytical limitation lies in deriving a realistic estimate of steam condensation rates and the resultant steam flow rates and void collapse rates for the infinite set of possible steam-water interface geometries. Some progress has been made. Bounding estimates are provided in Section 5.2 and supported by the very limited available data. However, the ability to calculate steam condensation rates realistically is unlikely to improve without extensive basic research well beyond the present workscope.

The basic physics of water slug motion and impact are relatively well understood and quantified by the available analyses, which are presented as explicit, closed-form expressions in Section 5.3. These analyses provide a framework for understanding the extensive quantitative data and experimental parametric studies obtained during our 1/10-scale model study. In addition, previously unidentified effects which tend to reduce the overpressure magnitude and impulse at slug impact (and which may do so powerfully) are also described. These include the reduction of the driving pressure difference due to rapid condensation at the back face of the water slug, instabilities which tend to break up the slug, and three-dimensional flow phenomena at impact. Empirical evidence needed to model these effects, so that more realistic calculations of maximum waterhammer forces on piping of systems can be made, does not exist at present.

The propagation of pressure waves through piping systems and the calculation of the resultant stresses are well developed engineering disciplines, although the complexity of typical piping systems usually demands that most calculations be performed numerically. Some phenomena not treated routinely in common procedures have been identified, such as the potential for coupled interactions between pressure wave propagation and the piping response in realistic compliant systems, but uncertainties introduced by these effects are somewhat smaller and more easily resolved than the uncertainties described above.

It is helpful to illustrate the gap in understanding. Typical feedwater piping may be expected to deform radially (i.e., bulge), as occurred at Indian Point #2, at local pressures of 3000 to 6000 psi. An estimate in Section 5 based on several conservative assumptions indicates that slug impact overpressures may be of the order of 16,000 psi. However, the available data at 1/10-scale and atmospheric pressure are a factor of three to six below the upper limit of 2200 psi derived for these conditions by the same means. Comparable uncertainties apply to analysis of the "bending" mode of deformation that is highly likely to have been responsible for the brittle pipe fracture at Indian Point #2 and the pipe-system damage recorded in most incidents to date. This mode of pipe deformation depends to first-order on the impulse of the pressure transient rather than simply on the overpressure magnitude. However, it is not possible at present to calculate these effects in an equivalent simple manner because additional factors such as pipe restraints and pipe lengths must be included in any analysis. Crude estimates provided in Section 5.4 indicate potential stress levels in excess of the yield stress of typical piping. Uncertainties in predicting the initial impulse tend to be somewhat less than those associated with predicting the overpressure magnitude. Several additional effects (such as those discussed in Section 5.4) play a role in the analysis of pulse propagation and its coupled interaction with the piping system. Thus, truly conservative analyses indicate the potential to overstress typical piping, but these analyses are not mature enough to be used to predict the behavior of PWR systems.

The history of PWR operating experience and tests documented in Section 2 and Appendix A of this report is a critical component of the available evidence. However, by their nature such observations of PWRs are useful primarily to provide a general assessment of the severity of recorded events and tend to teach little about the underlying phenomena. Tests of PWRs are costly and therefore are generally very limited in number and conditions tested. Quantitative data

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from PWR tests are extremely limited. Furthermore, the gross scatter in the data reported in Section 4, even for a research facility, demonstrates that single-sample tests of steam generator waterhammer phenomena are unreliable either as indicators of slug formation or to provide quantitative data. In some cases apparently the same PWR test has produced different results at different plants or different times. Similarly, the infrequence of triggering occurrences, the whimsical nature of the phenomena, and the general lack of trained observers or quantitative data make it difficult to interpret PWR operating experience beyond the most general assessment of the degree of pipe system damage recorded in prior incidents, or very general conclusions on the probable sequence of events, such as are summarized in Section 2.6.

The main conclusion of this general assessment of our knowledge is that means to reduce the frequency or severity of steam generator waterhammer at present should be simple and overpowering in their implementation and subject only to the most unsophisticated success criteria. The hardware and operating procedure recommendations made by the PWR vendors are ranked in Section 6.2 with this conclusion in mind. Section 6.3 recommends specific actions by the Nuclear Regulatory Commission.

6.2 Evaluation of Vendor Recommendations

Based on all the evidence, particularly the number of incidents of steam generator waterhammer during commercial operation and the three incidents involving substantial pipe deformation and pipe system damage in the past three years, improvements in hardware and procedures are needed. The PWR vendors maintain that the problem is now solved by their fixes and can point to over a year of operation with only three reported incidents at two plants. Thus, there is some indication that the situation is already improving. The recommendations made by the PWR vendors and the modifications made by the utilities in the past three years are examined in detail below.

The specific vendor recommendations cited in Section 2 are:

a. plug the bottom holes and discharge from pipes (e.g., J-tubes) at the top of the feedring*,

*As described in Section 2, the J-tubes (or a comparable straight pipe) are an important part of current "top discharge" systems. They prevent rapid drainage of the upper part of the feedpipe, as would occur with top holes alone (i.e., without J-tubes) if the feedring were uncovered. The only top discharge systems considered here are those with J-tubes or other pipes extending to an elevation above the top of the feedpipe.

b. make the horizontal runs of feedpipe extending from the steam generator nozzle as short as possible;

c. limit the maximum feedwater flow rate (while the feedring is uncovered) to a value below a previously determined threshold flow rate (e.g., 150 gpm).

To this list Creare supplies a fourth, namely:

d. ensure that feedwater flow is reestablished promptly subsequent to any event that uncovers the feedring.

Item a is a necessary complement to item d; therefore d is ranked only in combination with a. Top discharge devices greatly reduce the drainage rate while rapid reestablishment of feedwater flow reduces the drainage period. Only if these recommendations are followed together can these means act to limit the volume of water drained and the consequent size of the steam void. This combined effect is appreciated by the PWR vendors, but has not been included in the PWR vendor position statements and would benefit from futher development.

Each of these four recommendations has been described in detail in Section 2. They are examined and ranked individually, and in combination, below. In this ranking, means that tend to suppress the initiating event (i.e., drainage, steam void formation, and water slug formation) in addition to reducing the overpressure or impulse at slug impact are weighted favorably relative to means that address only the latter phenomena.

Table 19 presents Creare's subjective ranking of all combinations of these four recommendations based on our evaluation of their ability to reduce the probability of waterhammer events that would overstress the piping system. The combinations are ranked from best (1) to worst (13). A count is provided of the number of plants supplied by either Westinghouse or Combustion Engineering that have each combination. The tabulation is limited to these vendors because B&W steam generators already incorporate the best of these recommendations and implement them in a generally more positive manner than is possible with the other systems.

A description of the rationale for the rankings in Table 19 and of the relative merits of each vendor recommendation or combination is provided in Appendix F where critical quantitative evidence is also reviewed. The main points of Appendix F are summarized here:

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TABLE 19

CREARE'S RANKING OF WESTINGHOUSE AND COMBUSTION ENGINEERING RECOMMENDATIONS+

Ranking	Top Discharge	Flow on Soon (top discharge)	Short Pipe	Flow Limit	Number of Operating Plants
1 (best)	X	X	X	X	1
2	X	X	X		3**
3	X	X		X	2
4	X		X	X	-
5	X		X		-
6	X	X			4*
7	X			X	-
8			X	X	7
9				X	2
10	X				-
11			X		6
12	Flow Limit, Separate Sparger				2
13 (worst)	None of the Above				2

+Subjective ranking based mainly on the expected effectiveness of the approach as a means to reduce the probability of water slug impact or the magnitude of the waterhammer pressure wave.

*Top discharge is planned; all of these plants currently employ a variety of interim approaches.

**Top discharge is planned at two plants which are employing a flow rate limit in the interim.

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A) The problem is solved if no void forms. Top discharge together with rapid initiation of feedwater flow is the best presently available method to limit void size. Flow initiation before any drainage can occur, and at a rate in excess of drainage, is an absolute fix: only the reliability of means to achieve this needs to be questioned.

B) In systems that might be significantly drained initially, top discharge and short pipes are a major improvement over either device alone, based primarily on the present experiments at 1/10-scale and on qualitative descriptions of the behavior.

C) Top discharge alone has merits even if the feedring is initially drained, namely:

1) top discharge systems are not subject to hydraulic instabilities of the type described and analyzed in Section 5, although they are subject to counter-current flow instability,

2) top discharge eliminates the possibility of a void being trapped by rising vessel water level,

3) top discharge vents the feedring to the vessel,

4) top discharge shortens the refill period (i.e., only the feedring must be refilled, not the vessel) which may be favorable if the initial charge of water stored in the piping is hotter than the cold auxiliary supply.

Whether these features alone are adequate to prevent slug formation or reduce slug impact intensity sufficiently is questionable based on our experiments at 1/10 scale and due to the lack of other data.

D) Short pipes and flow limits are expected to be useful in support of other approaches, but are of questionable merit and reliability alone or in combination.

E) Any of these approaches improves on the earlier situation when the problem was largely unknown and unanticipated.

F) Although alternatives to the present vendor recommendations are readily conceived, there is virtually no evidence yet available to verify their effectiveness. Accordingly, any alternative approach or device will require extensive development and testing and cannot be recommended for use at this time. Several alternative approaches are listed in Appendix F.

G) Although the underlying evidence for our ranking is relatively limited, the above itemized findings are consistent with our present understanding of the phenomena, with the available data including the new data presented in Section 4, and with the analyses derived in Section 5. The subjective rationale employed in our evaluation is described in detail in Appendix F.

The first four items may be compared with the vendor position statements in Section 2. Item "A" is in general agreement with the vendor positions and in addition presents a limiting solution to the problem that has not been advanced by Westinghouse or Combustion Engineering although it is inherent in the B&W designs. Item "B" provides new information that is unavailable in the vendor positions. Item "C" elaborates on part of the vendor positions; it is in variance with the Westinghouse position, but not with the Combustion Engineering position. Item "D" provides a new evaluation that is in variance with both the Westinghouse and Combustion Engineering positions. More generally, the ranking of Table 19 advances two concepts not evident in the vendor positions, namely 1) that various combinations of approaches may be employed, and 2) that these combined approaches may be expected to display a spectrum of effectiveness which in this case ranges subjectively from "quite good" to "highly questionable".

A major conclusion that evolves from this ranking exercise is that although new plants coming on stream are generally employing the highest ranked approaches recommended by the vendors, most operating PWR plants still employ approaches with a relatively low ranking. No reasonable perturbation of the ranking is expected to alter this fact. However, it should be appreciated that the ranking in Table 19 is a subjective and relative assessment based on the technical merits of hardware and procedures now in use. This is consistent with the workscope defined by Creare's contract with the NRC. Any improved assessment must be based on additional information, including cost/benefit analyses and a safety evaluation, that fall outside the present workscope. It is our general impression that the highest ranked combinations of the vendor recommended devices and procedures are highly regarded by the vendors, and by those utilities that have chosen to employ these approaches. Confirmation by experiments at large scale and ultimately by PWR tests becomes increasingly desirable as approaches with successively lower rankings are employed.

6.3 Recommendations

Creare's recommendations to the Nuclear Regulatory Commission are made in this section. As with most issues of this type, the situation is not black and white. Our incomplete knowledge of the full economic and safety consequences of various actions makes it even more difficult to give firm recommendations. The Nuclear Regulatory Commission must recognize that our recommendations are based primarily on our technical assessment together with the assumption that continued, significant waterhammer incidents cannot be tolerated. The Nuclear Regulatory Commission must, of course, make their own evaluation of the need for further action and the nature of this action.

Creare's principal recommendations to the Nuclear Regulatory Commission are to:

A. encourage utilities to upgrade approaches,

B. continue to request tests of new PWRs,

C. consider tests of operating PWRs,

D. encourage the PWR vendors to develop a more complete technical information base,

E. encourage A/Es to analyze piping response to slug impact,

F. develop cost impact/benefit analyses,

G. plan an intermediate scale test program, and

H. continue technical studies of piping response.

These items are ordered in terms of the parties that will ultimately carry out the actions (utilities, vendors, A/Es, NRC). Most of these actions are interdependent and may be carried out to varying degrees. Although the Nuclear Regulatory Commission must synthesize an overall strategy comprising these and possibly other actions, each item is discussed individually below for ease of presentation.

A. Encourage Utilities to Upgrade Approaches

The vendor recommendations afford a spectrum of approaches having a broad range of anticipated effectiveness in suppressing slug formation and reducing slug impact intensity. Most present operating PWRs employ approaches that we rank low. Accordingly the most immediate means to reduce the possibility of future incidents and pipe system damage is to upgrade present operating plants within

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the present framework of the vendor recommendations. The advisability and timing of modifications at specific plants is a strategic decision that may be made by each utility based in part on the new technical findings provided in this report.

Specifically we recommend that the Nuclear Regulatory Commission:

1. request each utility to report what steps they are taking to avoid piping system damage from steam generator waterhammer, and their rationale for this approach relative to possible alternatives, in view of the present findings,

2. evaluate the information from item 1 above and formulate an appropriate action.

B. Continue to Request Tests of New PWRs

Until a better analysis of the probabilities and consequences of steam generator waterhammer is developed, the Nuclear Regulatory Commission is encouraged to continue its policy of requesting tests of new PWR plants during simulated conditions similar to those implicated in steam generator waterhammer. General recommendations for such tests are provided in Appendix C. The main points in Appendix C are that:

1. PWR test objectives should be narrowed to reflect the facts that a PWR is ineffective as a research facility and that historically PWR tests of steam generator waterhammer have been, of necessity, very limited in number and conditions tested. The main objective should be to improve confidence in predictions of PWR system behavior.

2. Test success criteria are needed and may have to incorporate considerable engineering judgment due to the vagaries of single sample experiments.

3. Baseline and exploratory test conditions should be clearly identified and isolated. All plants should be tested under common baseline test con-

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ditions, without precluding limited exploratory tests to advance the state-of-the-art or to test unusual features of specific plants. A baseline test series is suggested.

4. Plants with unusual or potentially inadequate systems should be tested most thoroughly.

5. A few well chosen, high response instruments are more likely to provide needed quantitative data than the extensive, but ill-chosen instruments employed in the past.

6. Some improvement in test documentation is desirable.

A detailed discussion of each of these points is provided in Appendix C.

C. Consider Tests of Operating PWRs

In view of the uncertainties surrounding criteria for slug formation and the characteristics of pressure waves at slug impact, the utilities should be requested to confirm the hardware and procedures at each operating plant. Tests are likely to be needed. Since the cost of such tests is significant, it should be weighed carefully by the Nuclear Regulatory Commission in determining the advisability and timing of requests for tests of operating PWRs. (Indeed, the cost of testing may be an impediment to performing modifications, since a test is likely to be requested for a modified system.) If tests are to be requested, initial tests should be conducted on those systems with the lowest ranking (by our scheme or any appropriate alternative) in order to maximize potential benefit relative to cost. In addition, this will provide a direct means to assess the benefits from such a test program.

It would be desirable for several reasons to delay testing, for example, to benefit from further experimental model studies recommended below, to permit the utilities to upgrade their approaches appropriately, and to minimize costs. In fact, if further research is planned and is to be performed in a timely fashion, it might be justified, in light of economic and alternate energy concerns, to delay all tests on operating and even new reactors until additional data are obtained.

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Specifically, we recommend that the Nuclear Regulatory Commission:

1. evaluate the available information, including the present technical findings and any cost/benefit analyses conducted by NRC staff or utilities in order to determine the advisability of tests of operating PWRs, and

2. after the evaluation above, and if tests of operating PWRs are to be requested, then establish a plan for the timing and execution of such tests.

D. Encourage the PWR Vendors to Develop A More Complete Technical Information Base

The phenomena treated in this report are generic effects that can occur in any PWR plant supplied with feedring steam generators. Analytical or empirical means (i.e., scaling laws) are not yet sufficiently mature to predict with confidence that piping system damage will not occur due to a typical steam generator waterhammer event in operating PWR plants. Moreover, conservative analyses predict that typical piping systems can be overstressed, as has been demonstrated by the extensive damage recorded at several plants. The vendors should be encouraged to develop a more complete technical description of the possible behavior during abnormal operating transients, and of the underlying phenomena, than has been reported to date. Specific efforts suggested include:

1. develop quantitative "best-estimate" predictions of criteria for waterhammer occurrence, of the characteristics of the resulting pressure waves, and of stresses in the piping in "standard" systems adhering to various combinations of the approaches presently recommended by the PWR vendors.

2. evaluate the costs and benefits (including the safety implications) of generic approaches recommended to mitigate steam generator waterhammer,

3. conduct additional experimental model studies at larger scale and with more extensive instrumentation than has been employed in previous work (the merits and objectives of such a program are discussed below), and

4. develop (and publish existing) quantitative information to describe the recommended systems more completely, such as calculations and data on thermal sleeve configurations and leakage rates in top discharge systems, or feedwater flow control and instrumentation characteristics specified by the vendors.

E. Encourage A/Es to Analyze Piping Response to Slug Impact

Some Architect-Engineers have tended to imply that the calculation of slug behavior and the resultant forcing function is routine and hence, pipe stress analysis can follow from existing codes. In conducting and presenting the results of such calculations, it should be appreciated and pointed out that the present understanding of slug impact phenomena is insufficient to provide a reliable "best estimate" forcing function for use in the design of PWR piping systems. However, A/Es are encouraged to conduct calculations of piping stress with various reasonable forcing functions of the slug impact type in order to identify easily corrected weak points in the piping systems and to reflect the potential of waterhammer pressure waves from various causes in addition to slug impact. For this purpose, the empirically motiviated forcing function recommended by Westinghouse [64, 8] is as good as any other and offers the advantage of providing a means to standardize calculations. However, there is little reason to apply the most conservative possible forcing function based on present understanding because this will surely overstress any reasonable piping system.

It is specifically recommended that the Nuclear Regulatory Commission:

1. apprise the utilities and Architect-Engineering firms on the present findings, of the continued technical efforts planned and now underway, and of inherent uncertainties and limitations of the understanding of the phenomena associated with water slug formation and impact, and

2. establish and publish guidelines for analyses of piping response (subject to forcing functions of the slug impact type) that will be acceptable in view of the state-of-the-art.

F. Develop Cost Impact/Benefit Analyses

Several of the strategic decisions and requests recommended above require cost and benefit information beyond the technical information supplied by the present report. The potential costs include the direct costs of modifications and confirming analyses, plant down time, model studies, and PWR tests; the potential benefits include a reduced probability of piping damage and extensive plant down time, and enhanced confidence in the safe and reliable operation of PWR plants.' It is recommended that the Nuclear Regulatory Commission:

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1. develop an independent first-order assessment of the potential costs and benefits of various strategies of action, and

2. consider carefully any cost/benefit analyses provided by the utilities and PWR vendors.

G. Plan an Intermediate Scale Test Program

A program of tests on a facility of intermediate scale (perhaps 1/3 scale) to study the thermodynamics and hydrodynamics of steam generator waterhammer and evaluate proposed fixes is needed. Modeling efforts to date can only be described as exploratory. Further analysis of slug formation, motion, or impact is not expected to be worthwhile until additional experimental data are available. An extensive intermediate scale test program is needed to provide direct verification of means to suppress slug formation and reduce slug impact intensity over a broad range of conditions. Tests on a flexible facility may eliminate the need for tests on PWRs or at least should dramatically reduce the number of PWR tests required for this purpose while at the same time enhancing our confidence level in methods of prediction. It is anticipated that an extensive program of intermediate scale testing can be mounted for much less than the cost of testing a single PWR, and at reduced risk.

The prime program objective should be to develop and verify means to preclude unacceptable waterhammer with high confidence. This would result in the evaluation of the relative technical merits of various approaches including the approaches presently recommended by the PWR vendors, alternative approaches recommended by other parties, and appropriate combinations of approaches. A second purpose of this program should be to establish an extensive and uniformly obtained body of quantitative evidence suitable for the development of empirical coefficients in a "best-estimate" analysis of the criteria for waterhammer occurrence and the characteristics of the resulting pressure waves. Such an analysis should be developed concurrent with the experimental program. Such work needs to be initiated soon in order to ensure that the results will be available on a timely basis to guide tests and modification of PWRs, if necessary.

It is recommended:

1. to plan an intermediate scale test program such as described briefly above, and

2. to adopt a strategy for implementing the intermediate-scale test program and ensuring that it is completed in a timely fashion.

H. Continue Technical Studies of Pipe Response

The Nuclear Regulatory Commission is encouraged to continue to fund studies that contribute usefully to the technical information base. The most prominent current work is the examination and application of pipe stress analyses now underway by Lawrence Livermore Laboratories. In general, this work will serve to identify and reduce the uncertainties of the waterhammer and stress analysis parts of the overall problem where a well developed engineering technology already exists. Work of this type is needed and may be expected to:

1. review and critique present computation tools and their underlying assumptions,

2. establish first-order guidelines for the design of piping systems to resist overstress by forcing functions of the slug impact type,

3. perform waterhammer and pipe stress calculations with several speculative forcing functions for a few typical systems (in essence, this is the inverse problem of defining the quantitative characteristics of forcing functions that are just sufficient to overstress typical piping systems). In particular, calculations of the response to the pressure impulse expected during the low pressure period just prior to slug impact are needed,

4. provide comparisons or calculated results with both the available quantitative evidence and with the results of previous extensive stress analyses conducted for key systems such as Tihange,

5. suggest standard methods to be followed by A/Es in analysis of piping response to forcing functions of the slug impact type.

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APPENDIX A

PWR EXPERIENCE WITH STEAM GENERATOR WATERHAMMER

This appendix supports the review in Section 2 of the available evidence from PWR operating and test experience. The main features of typical PWR systems are described. Common circumstances of the incidents to date are identified. Reported tests of PWRs are discussed in detail. Quantitative evidence useful for confirmation of analytical models is presented and discussed.

At the outset of the present study, Creare was supplied with all of the relevant evidence available to the Nuclear Regulatory Commission Division of Safety Systems. Foremost in this body of information was a complete set of responses to a questionnaire [6] sent on May 13, 1975 by the Nuclear Regulatory Commission to responsible personnel at all U. S. PWR plants operating at that time. Table A.1 lists the detailed questions. In brief, these questions address piping geometry, potential abnormal operation conditions, plant experience with waterhammer, means employed to avoid unacceptable waterhammer, and analytical and test verification of these means. In addition to the responses to this questionnaire, a nearly complete set of relevant U. S. incident reports, analysis reports, test procedures, and test reports for the period up to December 1, 1976 were supplied to Creare by the NRC. These documents are listed on a plant-by-plant basis as additional references for this appendix.

A.1 Relevant PWR Characteristics

There are three U. S. vendors of PWRs: Westinghouse, Combustion Engineering (CE), and Babcock and Wilcox (B&W). A recent listing of PWR plants including general characteristics can be found in the open literature (e.g., Nuclear News, August 1976).

Figure A.1 is a schematic of a typical PWR coolant system showing the reactor vessel, coolant pump, pressurizer, and steam generator. The role of the steam generator in this loop is as a heat exchanger that extracts energy from the primary (reactor) coolant and provides this energy in the form of high pressure saturated steam suitable to drive a power turbine for electrical power generation. To do this, secondary coolant water known as "main feedwater" is pumped into the steam generators and boiled.

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TABLE A.1

NRC QUESTIONNAIRE OF MAY 13, 1975
(Sent to All U.S. Operating PWR Plants)

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1) Describe all operating occurrences that could cause the level of the water/steam interface in the steam generator to drop below the feedwater sparger or inlet nozzles, and allow steam to enter the sparger and/or the feedwater piping.

2) Describe and show by isometric diagrams, the routing of the main and auxiliary feedwater piping from the steam generators outwards through containment up to the outer containment isolation valve and restraint. Note all valves and provide the elevations of the sparger and/or inlet nozzles and all piping runs needed to perform an independent analysis of drainage characteristics.

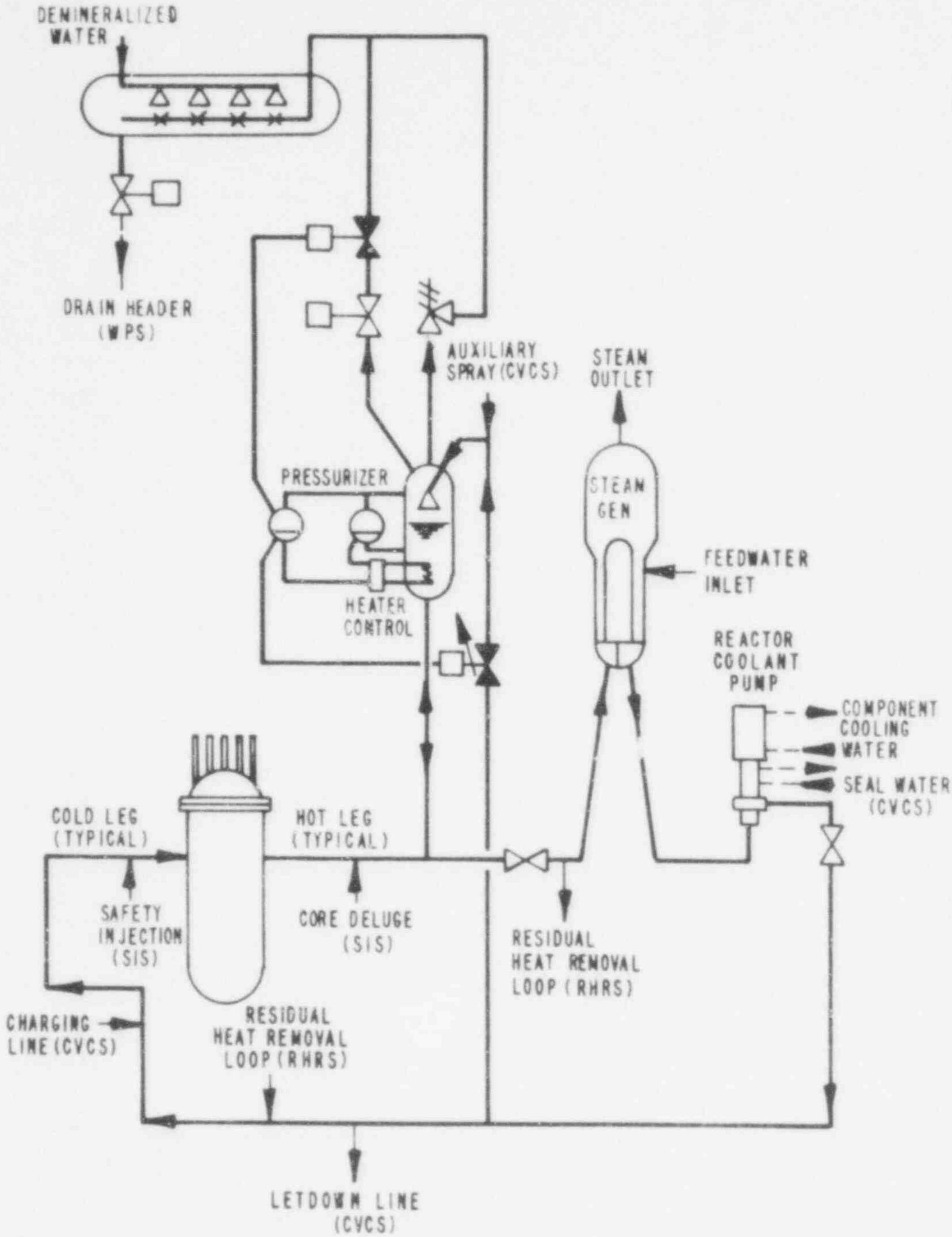
3) Describe any "waterhammer" experiences that have occurred in the feedwater system and the means by which the problem was permanently corrected.

4) Describe all analyses of the feedwater and auxiliary feedwater piping systems for which dynamic forcing functions were assumed. Also, provide the results of any test programs that were carried out to verify that either uncovering of the feedwater lines could not occur at your facility, or if it did occur, that "waterhammer" would not occur.

    a. If forcing functions were assumed in analyses, provide the technical bases that were used to assure that an appropriate choice was made and that adequate conservatisms were included in the analytical model.
    b. If a test program was followed, provide the basis for assuring that the program adequately tracked and predicted the flow instability event that occurred, and further, that the test results contained adequate conservatisms and an acceptable factor of safety, e.g., range of parameters covered all conceivable modes of operation.
    c. If neither a. or b. have been performed, present your basis for not requiring either and your plans to investigate this potential transient occurrence.

5) Discuss the possibility of a sparger or nozzle uncovering and the consequent pressure wave effects that would occur in the piping following a design basis loss-of-coolant accident, assuming concurrent turbine trip and loss of offsite power.

6) If plant system design changes have been or are planned to be made to preclude the occurrence of flow instabilities, describe these changes or modifications, and discuss the reasons that made this alternative superior to other alternatives that might have been applied. Discuss the quality assurance program that was or will be followed to assure that the planned system modifications will have been correctly accomplished at the facility. If changes are indicated to be necessary at your plant, consider and discuss the effects of reducing the magnitude of induced pressure waves, including positive means (e.g., interlocks) to assure sufficiently low flow rates and still meet the minimum requirements for the system safety function.



REACTOR COOLANT SYSTEM, FLOW DIAGRAM
FROM REFERENCE [A1]

FIGURE A1

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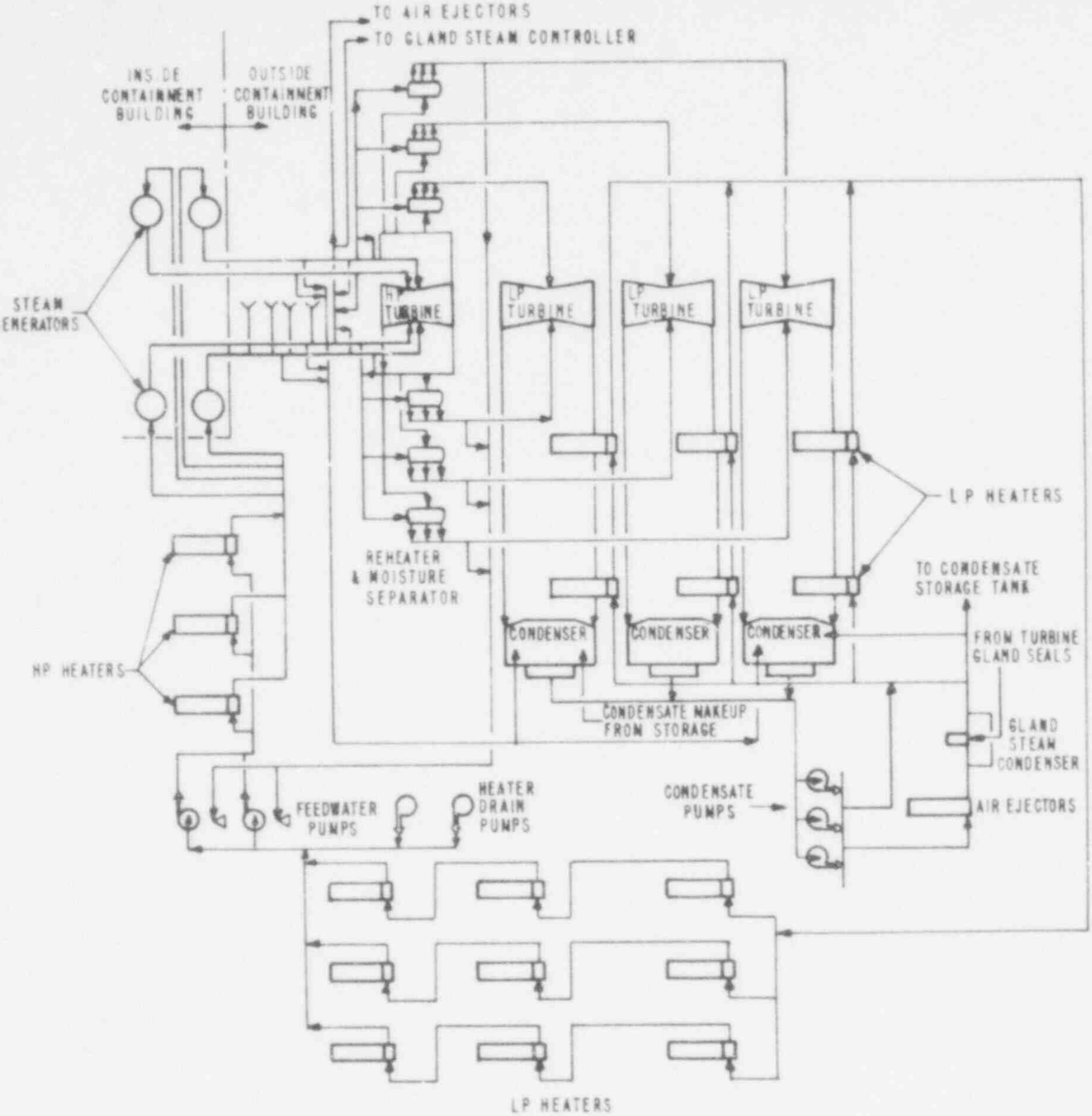
Figure A.2 shows a typical secondary coolant loop which includes the steam generators (four in this case), power turbines, condensers, condensate pumps, main feedwater pumps, and heater trains. Westinghouse plants typically have four steam generators and associated piping loops, while CE and B&W plants usually have only two steam generators. The steam generators are hydraulically interconnected on the secondary side by feedwater piping and by steam side piping at most plants.

Cutaway drawings of feedring steam generators typical of those in present operating PWR plants are given in Figures A.3, A.4, and A.5, respectively, for each vendor. Westinghouse and Combustion Engineering steam generators are of the U-tube type; the primary coolant flows through a bundle of U-shaped tubes surrounded by the secondary coolant. Babcock and Wilcox produces a single-pass heat exchanger, termed a "Once-Through Steam Generator (OTSG)", which has a straight vertical tube bundle that passes through a pool of secondary coolant.

The secondary coolant enters the steam generators through a main feedwater pipe and is distributed within the steam generator by a ring sparger called a "feedring". This system is visible in the cutaway drawings and is shown in greater detail in Figures A.6, A.7, and A.8. The dimensions given on these figures are typical of operating PWRs put into service during the last few years, but are not necessarily common to all operating PWRs.

All three PWR vendors are developing "preheat" or "economizer" steam generator designs which are planned for use in future PWRs now in the construction or proposal stages. These systems inject most of the feedwater at the bottom of the steam generator in order to preheat it and attain more efficient boiling heat transfer. A typical steam generator of this type is shown in Figure A.9.

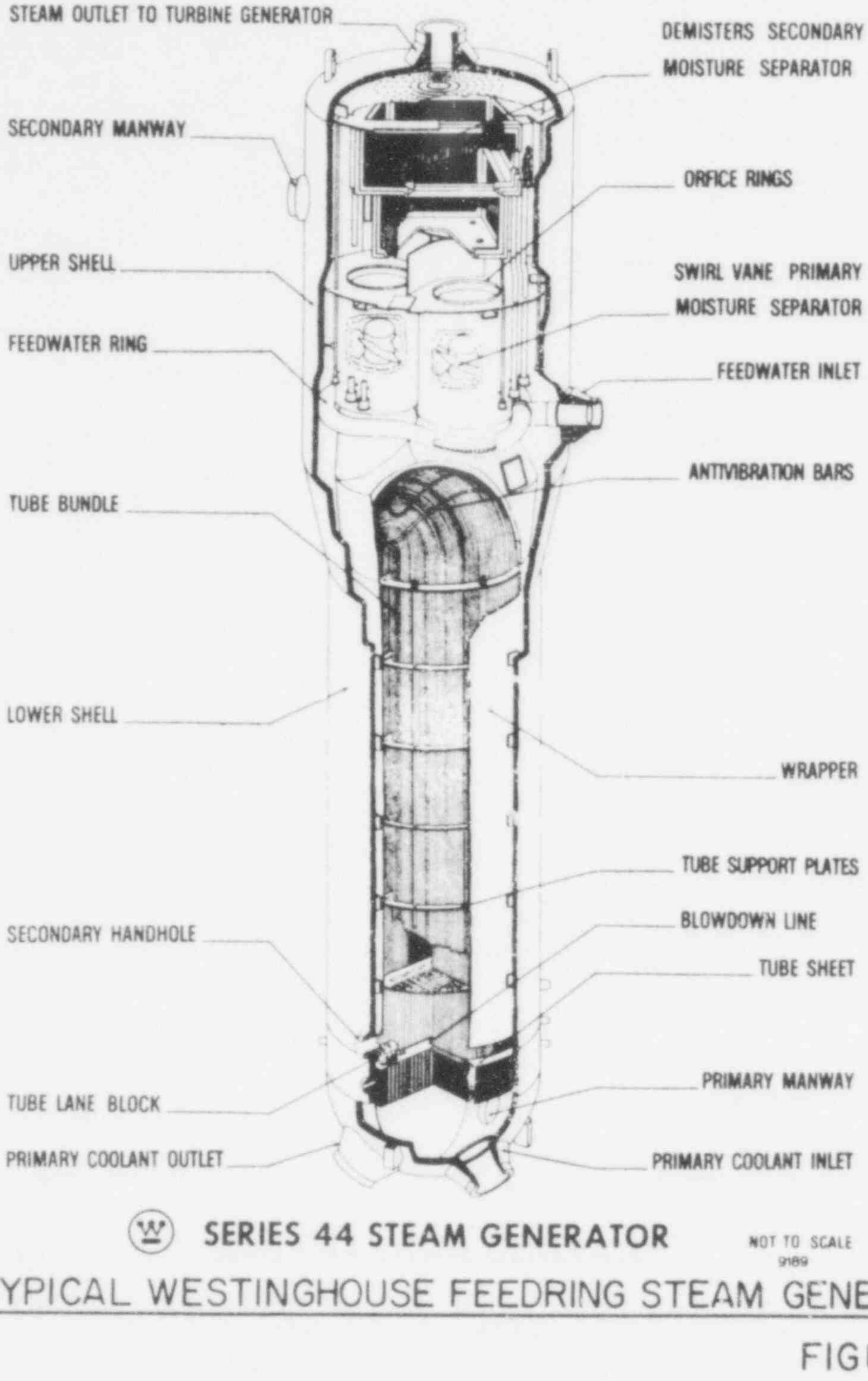


MAIN STEAM AND FEEDWATER SYSTEM, ONE-LINE DIAGRAM FROM REFERENCE [A.1]

FIGURE A2

POOR ORIGINAL

730 073

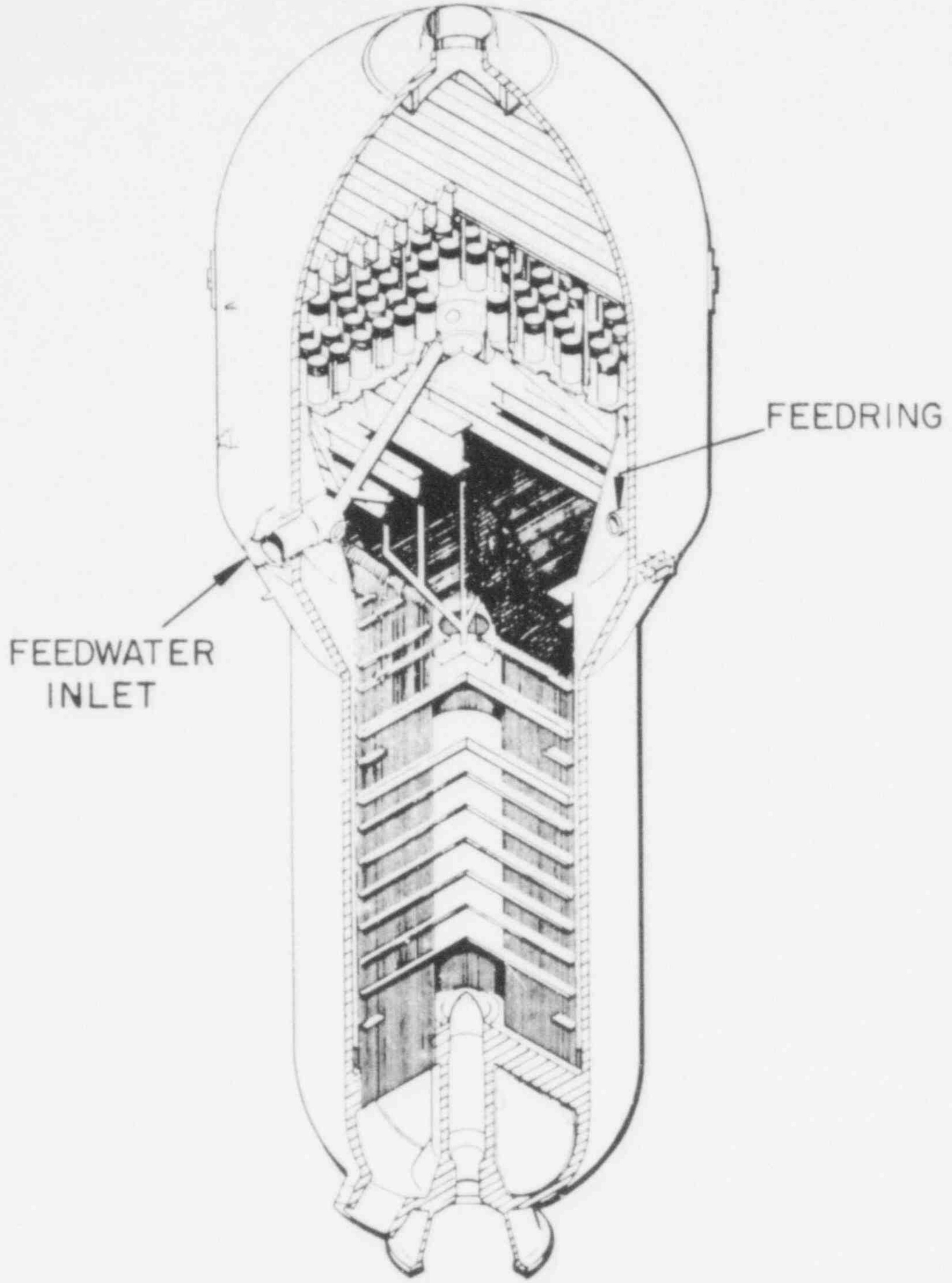


TYPICAL WESTINGHOUSE FEEDRING STEAM GENERATOR

FIGURE A3

POOR ORIGINAL

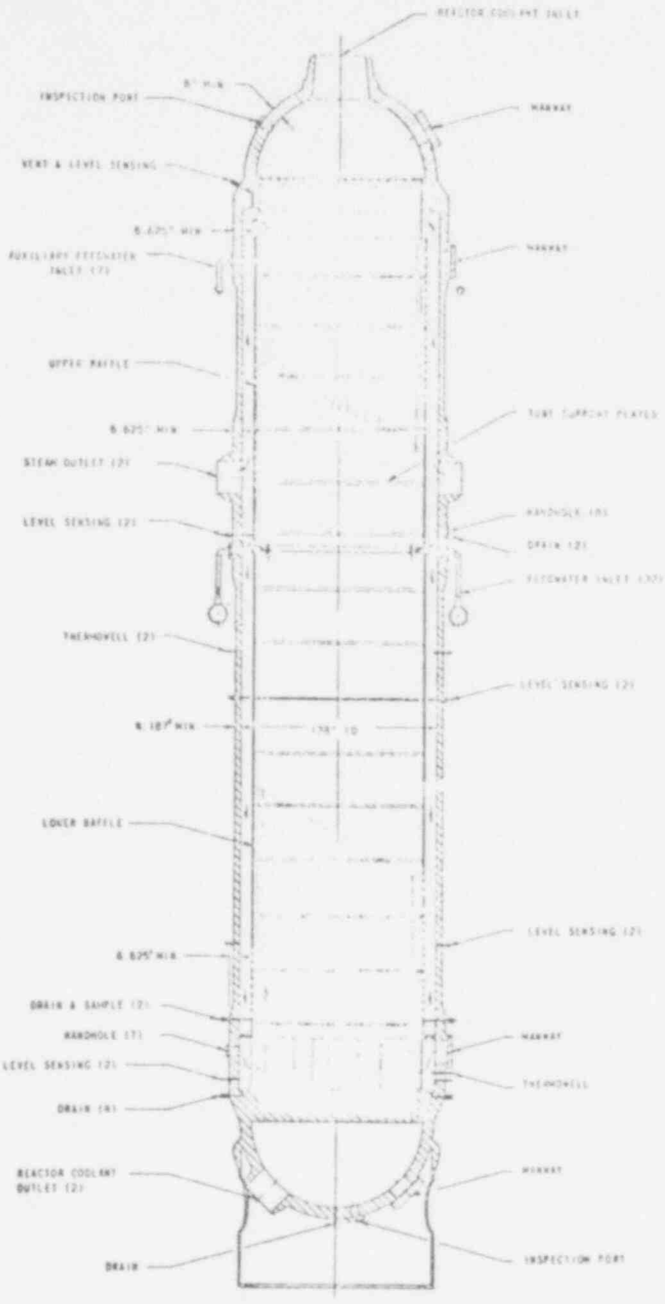
730 074



TYPICAL CE STEAM GENERATOR FEEDRING

FIGURE A4

POOR ORIGINAL



STEAM GENERATOR

STEAM GENERATOR OUTLINE

TYPICAL B&W FEEDRING STEAM GENERATOR

FIGURE A5

POOR ORIGINAL

730 076

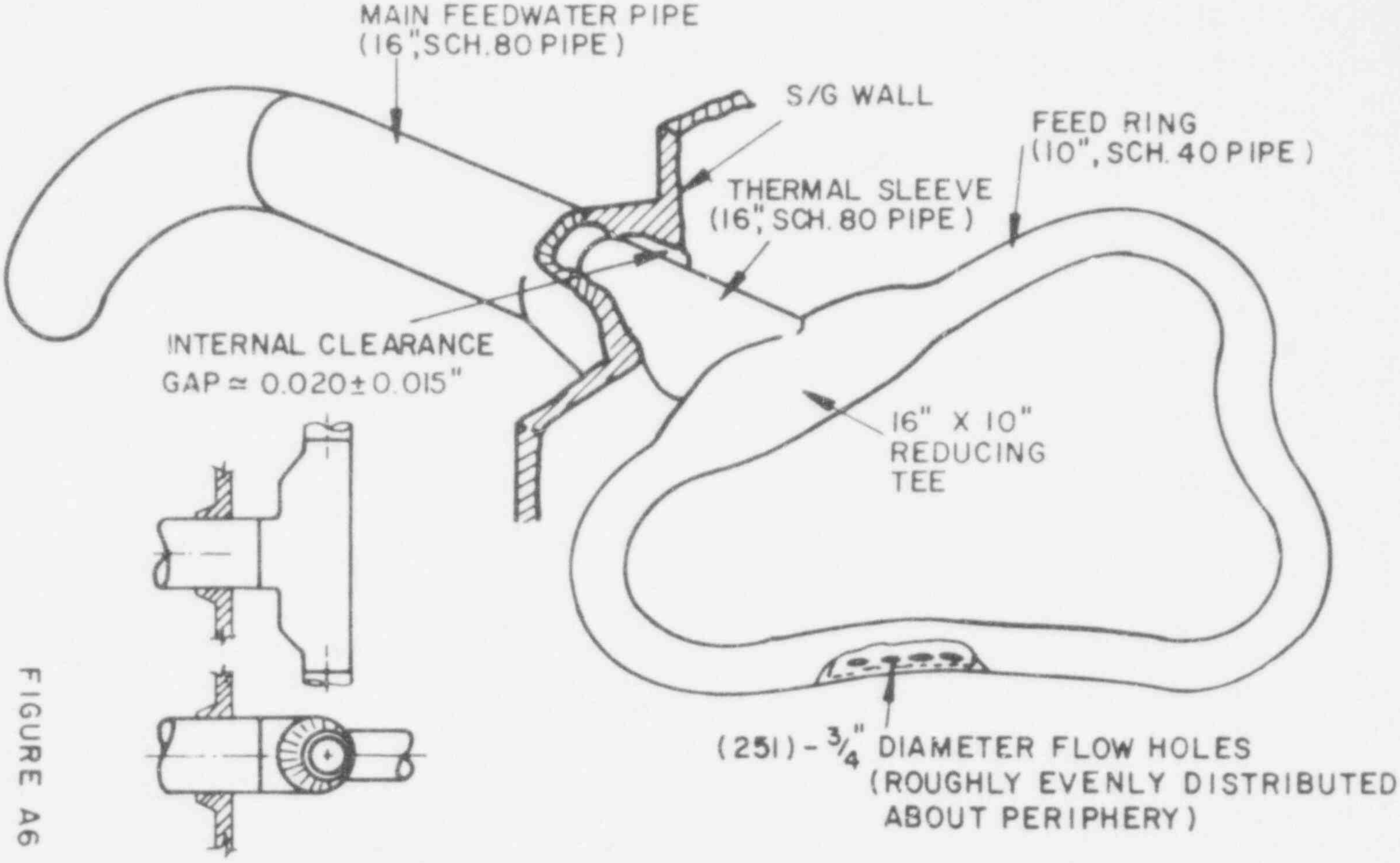
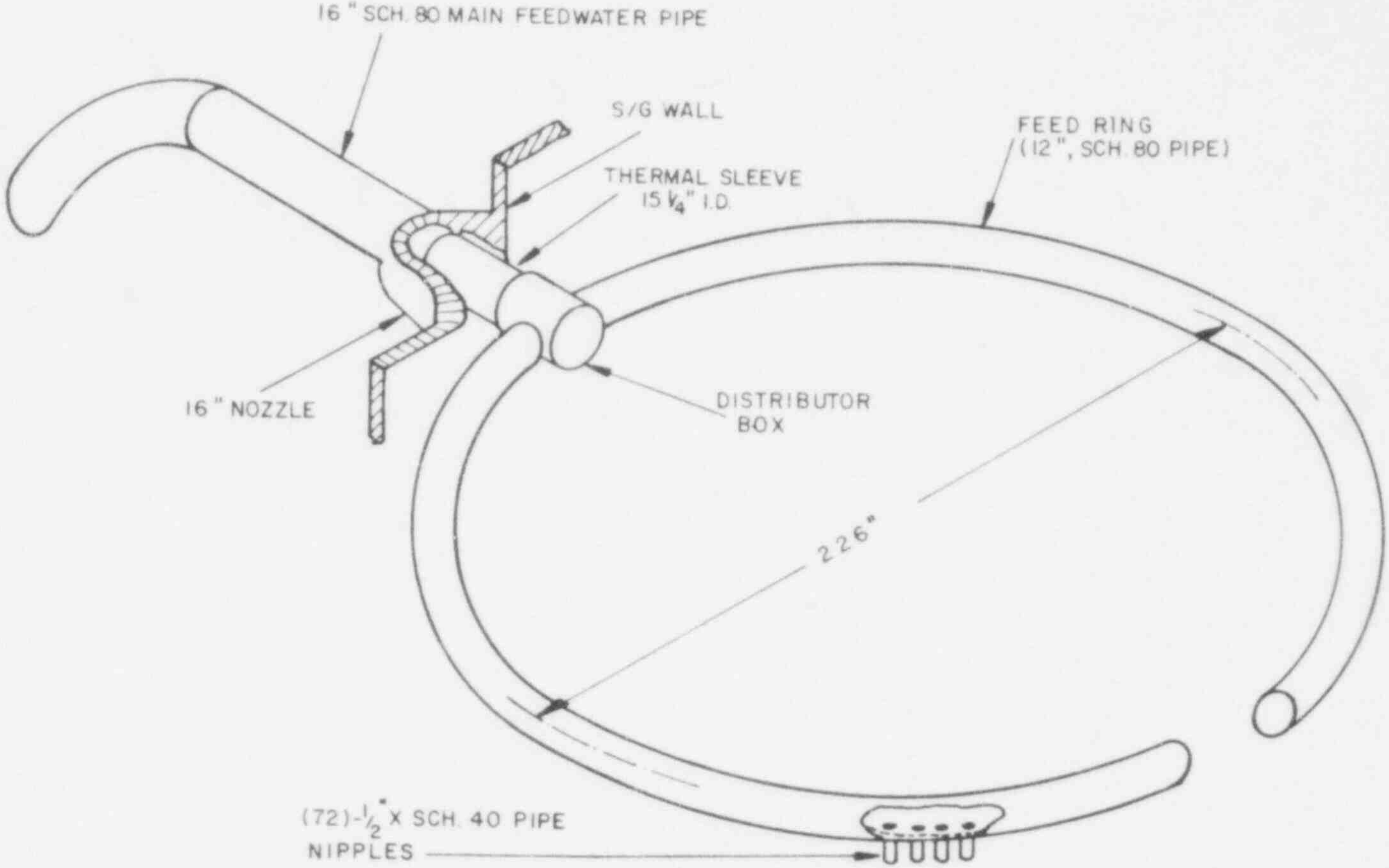


FIGURE A6

SKETCH OF WESTINGHOUSE FEEDRING

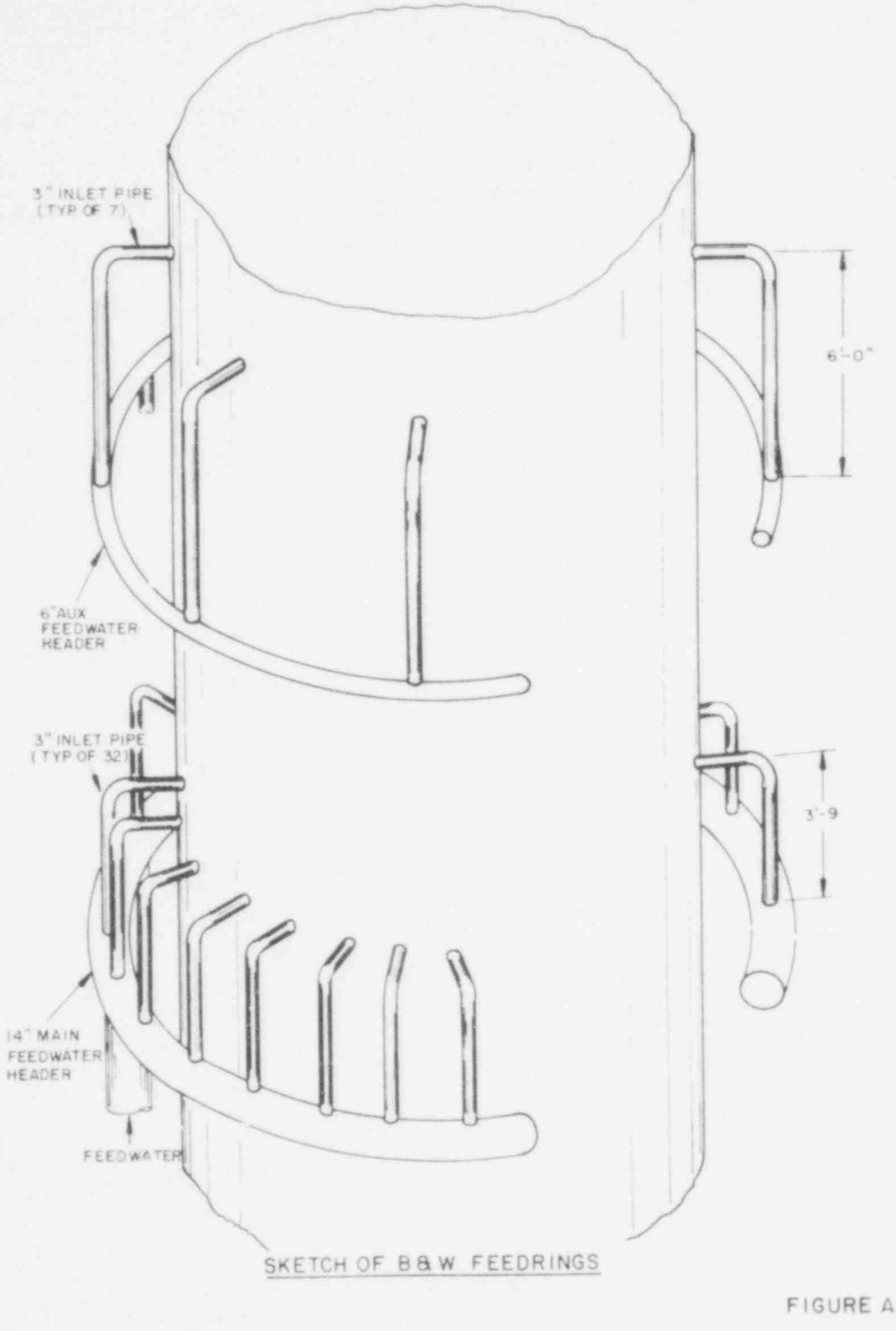
730 077



SKETCH OF COMBUSTION ENGINEERING FEEDRING ASSEMBLY

FIGURE A7

730 078



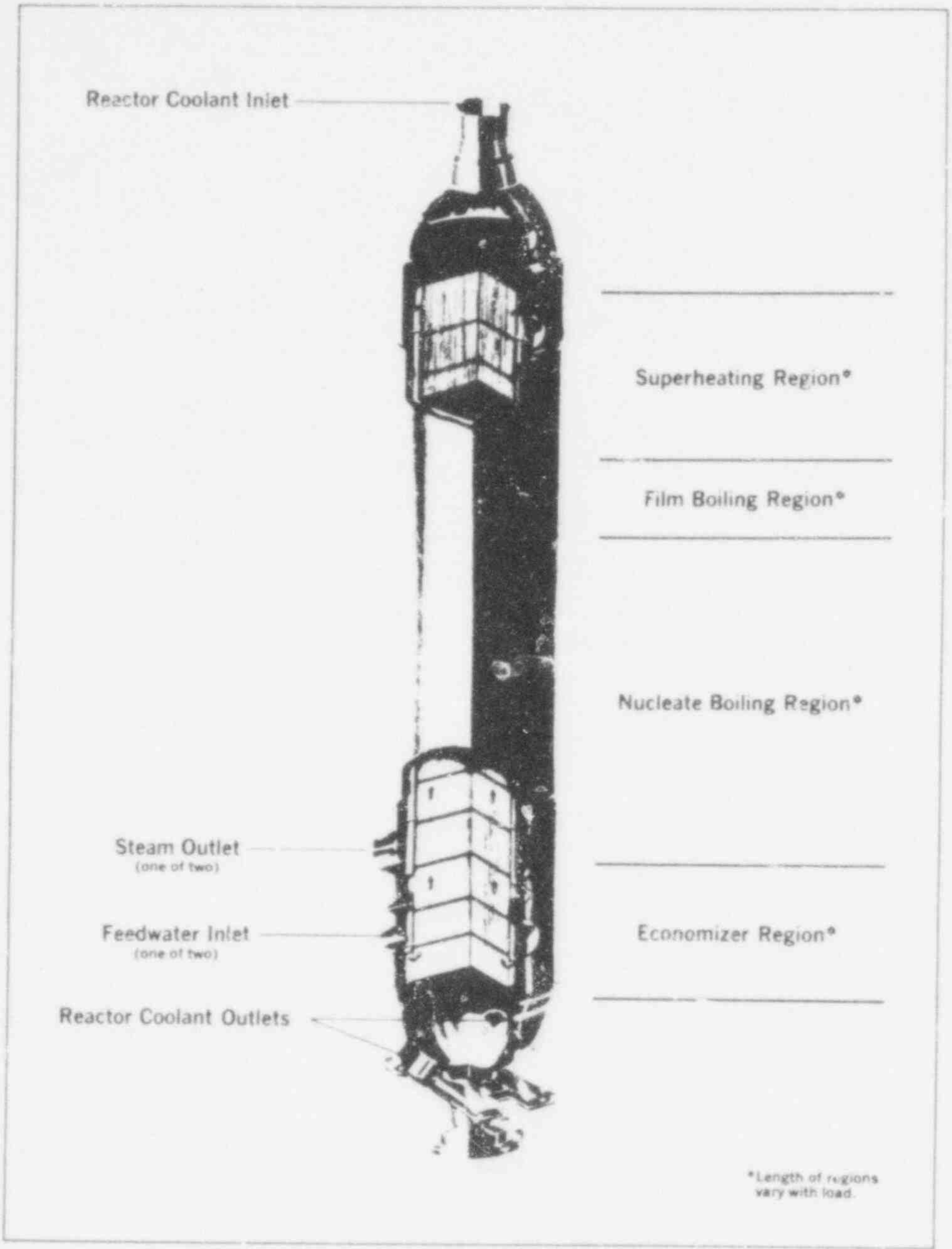
SKETCH OF B&W FEEDRINGS

FIGURE A8

POOR ORIGINAL

730 079

Once-Through Steam Generator



Babcock & Wilcox

TYPICAL "PREHEAT" STEAM GENERATOR

FIGURE A9

POOR ORIGINAL

730 080

Normal Operation

The range of the principal steam generator parameters during normal operation at 100% power are as follows: Gross output ranges from a few hundred megawatts (electrical) up to approximately 1000 megawatts. Steam generator vessel pressure ranges from 700 to 1100 psia. Main feedwater flow rate ranges from a few thousand to as much as 16,000 gpm per steam generator. Feedwater temperature is generally close to 450°F. In rough terms, the total feedwater flow rate is proportional to gross output (from thermal considerations) and the flow per steam generator is set by the total flow and by the number of coolant loops which ranges from 2 to 4.

The operating parameters described above can vary appreciably during normal plant operation. Gross output ranges from no load to rated 100% power and may encounter appreciable transients. Average feedwater flow rate varies in rough proportion to gross output and is itself a controlled variable subject to transients. During plant start-up, steam generator pressure may be as low as atmospheric pressure and undergoes a normal start-up transient up to operating pressure. As gross output decreases from 100% power to no load conditions, steam generator pressure increases gradually to a value approximately 100 psi above the value at 100% power. During steady operation, main feedwater temperature varies as a non-linear function of gross output and may be as low as condenser wet well temperature (approximately 100°F) at no load. Steam generator pressure and feedwater temperature are also subject to transients and dynamic lag during plant transients.

The secondary coolant water level in the steam generators is controlled closely during normal operation. In Westinghouse and Combustion Engineering systems it is desirable to keep the heat exchanger tubes covered, and the moisture separators must remain uncovered in order to function. The feedwater sparger is covered by water during normal operation of Westinghouse or Combustion Engineering plants. (B&W systems face comparable limits, however B&W feedwater spargers are not covered during normal operation.) In effect, the steam generator water level is controlled in a narrow range of several feet during normal operation. The system capacitance and control system response is designed to permit the water level system to sustain mild transients typical of normal operation readily, up to transients of roughly 10% of full power.

It is important to recognize that the secondary coolant is being boiled during normal operation so that it is a two-phase mixture with an ill-defined interface and subject to rapid level variations. Water level is only one of many interacting parameters that must be controlled during normal operation.

730 081

Abnormal Operation

Certain "abnormal" events are occasionally experienced in nuclear power reactors and all systems must be designed to prevent unacceptable consequences from such occurrences. For example, a reactor trip, or a main feed pump trip, or an operator error associated with necessary manual override of the automatic main feedwater flow control may occur a few times a year at each plant.

Uncovering the feedring sparger has been identified as the initial event in the sequence of events described in this report as steam generator waterhammer. According to Westinghouse and Combustion Engineering personnel [2,3], a reactor trip or a main feed pump trip will usually lead to the feed sparger becoming uncovered.* Typically, in these abnormal circumstances, there will be a rapid power transient which will drastically reduce the rate of heat exchange and thence the rate of boiling. As a result, the steam-bubble content of the two-phase secondary coolant will be reduced and the apparent water level will drop dramatically. The water level is said to "shrink".

It is obvious without recourse to any probability analysis that uncovering the feedwater sparger is far more likely than the highly unusual events postulated to occur prior to a design basis loss-of-coolant accident.

To enable the plant to be shut down safely and routinely following an occasional abnormal occurrence such as a main feedwater pump trip, each plant has an emergency coolant system driven by separate feedwater pumps supplied from a separate water tank that is normally isolated from the secondary coolant loop. This system often serves the additional purpose of supplying water as needed at various points in the plant and is commonly termed the "auxiliary" feedwater system.

In most operating plants, the auxiliary feedwater piping is connected to each of the main feedwater pipes at some arbitrary point downstream of the main feedwater isolation valve, but upstream of the steam generator feedwater nozzle. There may be a few feet or tens of feet of main feedwater pipe between this connection and the steam generator feedwater nozzle and the connection may be inside or outside containment. Some Combustion Engineering plants have a separate auxiliary feedwater nozzle and sparger so that there may be no interconnection between the main and auxiliary feedwater systems.

The following paragraphs review PWR operating experience with steam generator waterhammer and establish a common sequence of events.

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*Trips from very low power may not cause the sparger to become uncovered if the power transient is small, depending of course on the initial steam generator water level.

A.2 Sequence of Recorded Events

Table 1 of Section 2 lists the incidents of steam generator waterhammer reported during PWR commercial operation. This Appendix describes the evidence that has actually been recorded during these incidents. Evidence from tests of PWRs will be described in section A.3 of this Appendix.

The following events might be recorded during a well documented steam generator waterhammer incident:

- initial steam generator operating conditions:
  - power
  - pressure
  - water level in each steam generator
- precursor event that in some way uncovers the feedring,
- feedwater level and flow rate as a function of time in all steam generators in relation to the feedwater sparger(s), noting in particular:
  - time sparger uncovered,
  - timing of sparger recovery, if at all,
  - timing of precursor events,
  - timing of noises or other indications of waterhammer,
  - timing of automatic or manual actions to control feedwater flow rate, and
  - means and uncertainty of all timing and flow data.
- estimate of feedwater supply temperature,
- sharp noises or visible pipe motions,
- fluctuations of instruments such as feedwater pump pressure,
- damage, including permanent deformation or fracture of pipes or supports, and destruction of feedwater system components.

In order to clarify this sequence, the events of the most carefully documented incident are reviewed below.

Events at Indian Point #2 on November 13, 1973

According to Reference [1] the sequence of events at Indian Point #2 was as follows. Initially the reactor was critical and operating at 7% power. The steam pressure was 950 to 970 psig in all four steam generators. The steam

730 083

generator (SG) water level was approximately 40% of narrow range in SG#21, SG#22, and SG#24, but was over 70% of narrow range in SG#23.*

The precursor event occurred at 7:38 a.m. on November 13, 1973 when there was a turbine trip due to high water level in SG#23. As a result, the main feedwater pumps were automatically tripped.

Records of steam generator water level in all four loops are supplied in Reference [1] for the period from 7:41 a.m. to 7:52 a.m. and are provided here in Figure A.10. The records show that the water level in all four loops dropped rapidly following the precursor event at a rate of roughly 2 to 5% of narrow range per minute. At no time during the event was the recorded level in either SG#23 or SG#24 below the centerline of the feedwater pipe. At 7:44 a.m. the recorded water level in each of SG#21 and SG#22 was approximately at the centerline of the feedwater pipe. At 7:45.41 there was a reactor trip due to lo-lo level in SG#21. At this time, the recorded level in SG#21 was approximately at the bottom of the ten inch feedring and the level in SG#22 was still near the pipe centerline. Within twenty seconds after the reactor trip, the water level in all four steam generators dropped by about 10% of narrow range so that the levels in both SG#21 and SG#22 were below the level of the feedring by a few inches.**

Feedwater flow was reinitiated by two motor-driven auxiliary pumps. The precise timing of main feedwater flow shutdown and initiation of auxiliary feedwater flow is not recorded although it is indicated on Page 2-1 of Reference [1] that auxiliary feedwater flow was established prior to 7:45 a.m., i.e., just before the reactor trip.

Adequate instrumentation to measure feedwater flow rate directly was not in place at the time of the incident. (The feedwater flow rate data provided in Reference [1] are known to be grossly inaccurate and should be ignored; they were provided in Reference [1] solely to indicate the instant of a sharp instrument fluctuation.) At Indian Point #2, auxiliary feedwater flow is established automatically following a plant

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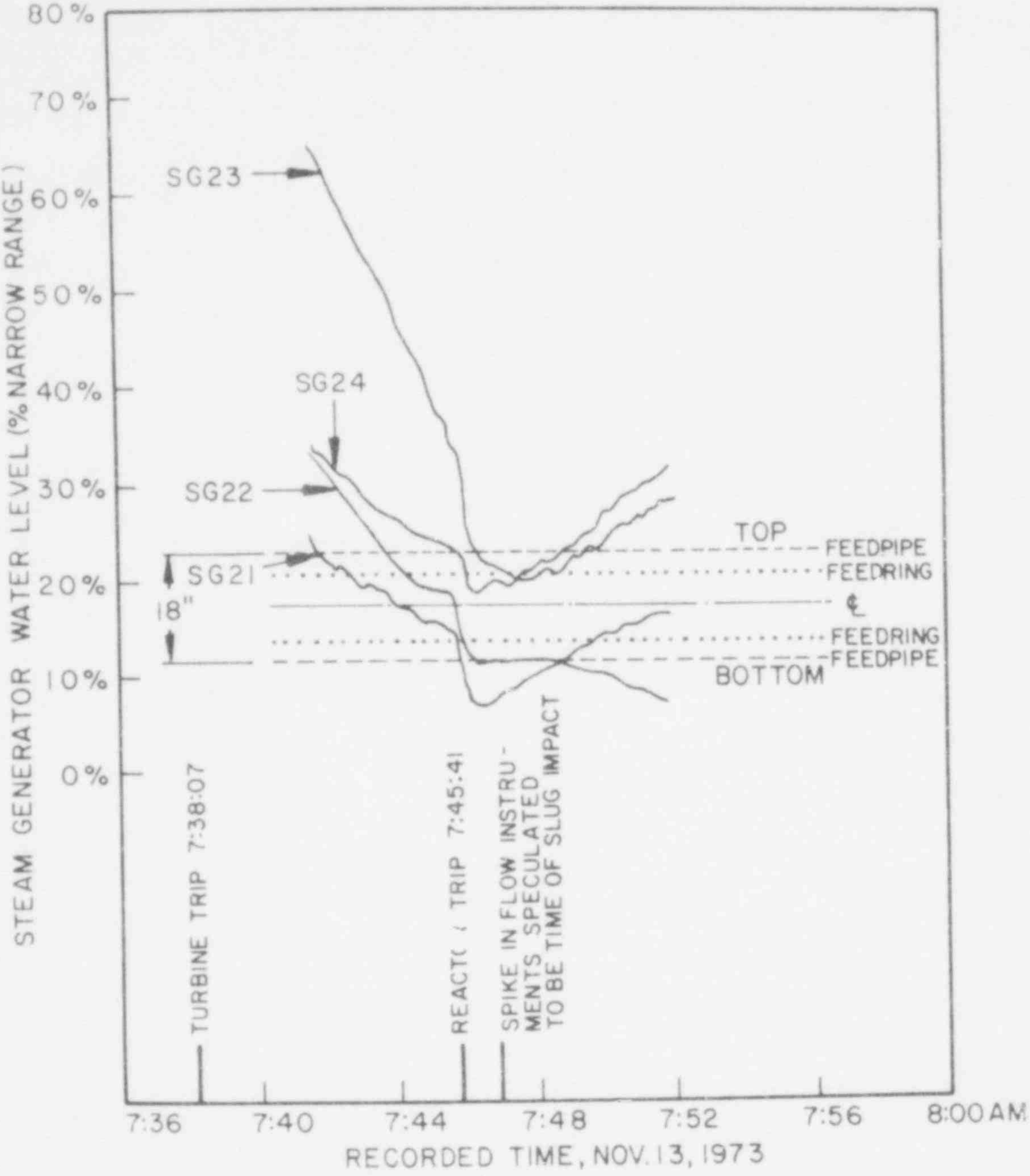
*Water level is deduced from a measurement of differential pressure on a scale of 0 to 100% between the locations of the pressure taps. Parallel measurements are made with coarse and fine scales known respectively as "wide range" and "narrow range". At Indian Point #2, 1% of narrow range is approximately 1.4 inches and the 18-inch feedwater pipe centerline is at approximately 18% of narrow range.

**The measurement precision is generally unknown and has been universally unreported. Conclusions from these records must be drawn cautiously.

730 084



RECORD OF INCIDENT AT INDIAN POINT # 2

FIGURE A10

730 085

trip and appropriate control valves open automatically to a preset operating point (at 50% of rated flow). The four loops are supplied by two motor-driven, auxiliary feedwater pumps with approximately 550 gpm capacity each. Accordingly, each loop receives roughly 140 gpm if the design flow splits uniformly. This flow rate and its distribution are imprecise and can be altered by manual control. Unfortunately, there is no known record of operator actions in this instance.

The indicated water levels in SG#21, SG#23, and SG#24 all increased after approximately 7:47 a.m., but the indicated water level in SG#22 continued to decrease slowly. Thus, the recorded water level in SG#22 did not recover the sparger during this incident. The data uncertainty is sufficient that there is no way to tell whether actual water level recovered the sparger.

Reference [1] indicates that at some time between 7:45 and 7:50 a.m. there was a loud noise and shaking of the piping to SG#22 was observed by a man (Tony M.) stationed outside containment. It is our understanding that this man was in a position to observe which loop was shaking and identified it as the piping to SG#22. The time of the water-hammer is collaborated and indicated more closely by the feed-water flow instrumentation which indicates a sharp spike in three of the loops at approximately 7:47 a.m. Much later, at 8:30 a.m., there was a second loud noise and shaking of the piping to SG#22 was again observed.* Efforts to feed SG#22 continued until roughly 9:40 a.m. when feedwater to SG#22 was secured. SG#22 was isolated completely at 11:05 a.m.

A thorough investigation of the damage was conducted and detailed records were made as reported in Reference [1]. In brief, the investigation included the following. A section of the fractured piping was removed and subjected to fractographic analysis. No material defects were indicated. The fracture was shown to be a brittle failure and an impact stress of 500 to 1000 kpsi was inferred. (Low carbon steel would normally fail in a ductile manner at a static stress of approximately 100 kpsi.) The orientation of the failure was recorded. The feedwater piping alignment was examined with surveying equipment. All snubbers and supports for the SG#22 piping were inspected and displacements, deformations, and broken bolts and structures were recorded. A section of the horizontal run of main feedwater pipe to the SG#22 nozzle was removed and measurements of a large bulge in the pipe a few feet from the steam generator feedwater pipe nozzle were recorded. The main bulge data is provided in Figure A.11.**

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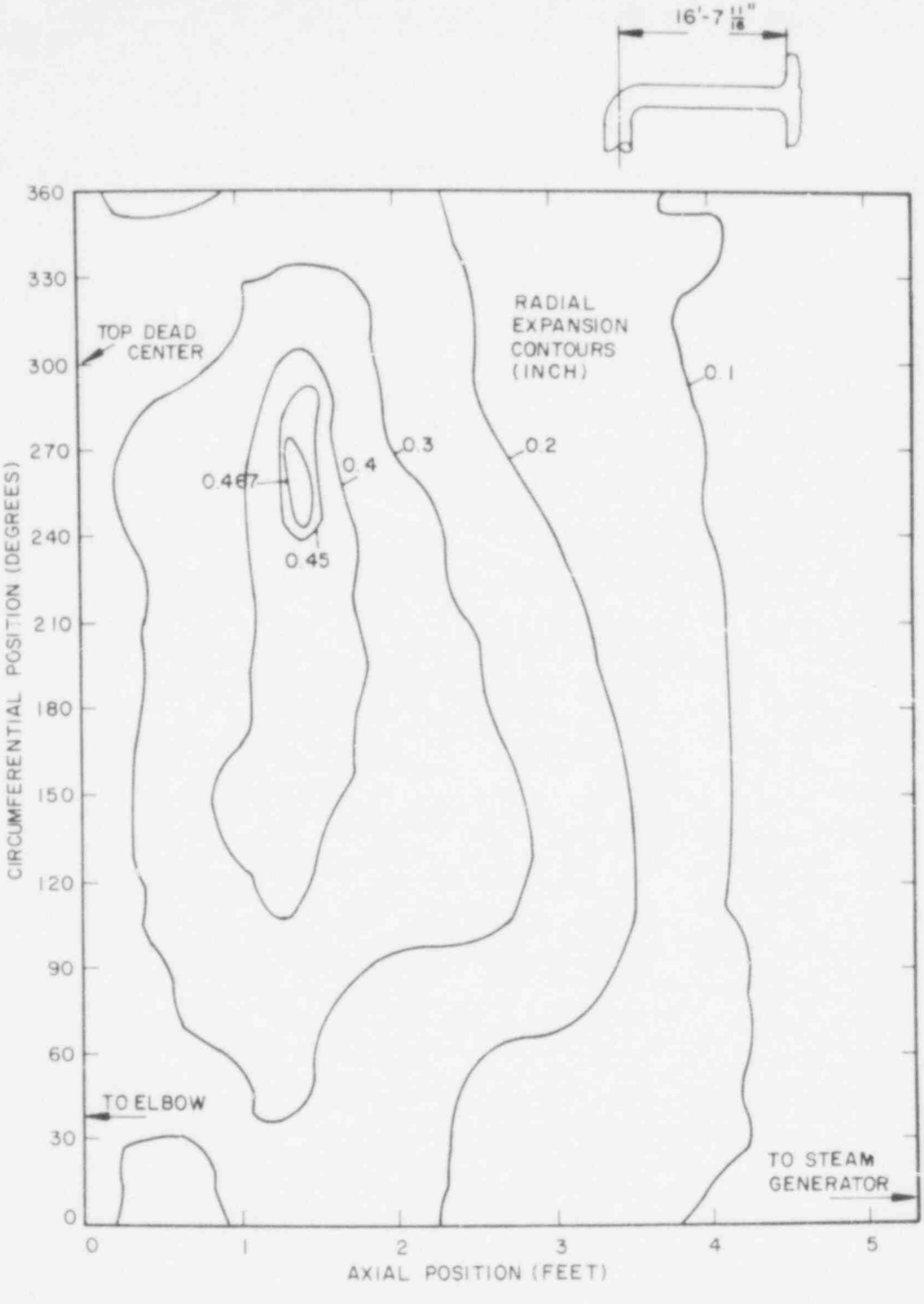
*Repeated noises, sometimes in rapid succession, have been reported in several other incidents as well.

**Data provided in personal communication to P. Rothe from J. D. O'Toole, Consoliated Edison Company, October 8, 1976.



RADIAL GROWTH OF INDIAN POINT # 2 FEEDWATER LINE TO SG # 22

FIGURE A11

730 087

In addition, SG#22 was inspected internally, valves and piping components were tested, and damage to the containment liner near the fracture was documented.

Recorded Events During Steam Generator Waterhammer Incidents

Creare has examined the documents transmitted to us by the NRC in an effort to establish circumstances common to the steam generator waterhammer incidents reported to date. Table A.2 presents a summary of the relevant evidence. Unfortunately, the reported evidence is too scanty to support any general claim. This is the main message of Table A.2. Scattered information on each of the events in the hypothetical sequence is reviewed in turn below.

Precursor Events. Incidents have occurred over a broad range of operating power. The four most severe incidents tended to be at low power levels, namely at Indian Point #2 (7% power), Calvert Cliffs #1 (unreported), Surry #1 (15% power during the start-up test program), and Turkey Point #4 (unknown, pipe and support deformation noted during outage). Initial pressures have tended to be at operating pressures of order 1000 psig, but there is no evidence to indicate that similar behavior cannot occur or cannot be more prone to occur at much lower pressure. Precursor events have included reactor trips, turbine trips, pump trips, and piping component failures such as leaking valves. Inadequate flow and water level control are often implicated in the precursor event.

Based on informal discussions with vendor and utility personnel and a few documented histories, such as the experience at Palisades during 1972 [A.2], we are able to conclude that such precursor events as those described above are many times more frequent (perhaps a factor of 10 to 100) than are the reported incidents of steam generator waterhammer. Thus, the phenomena and processes involved are likely to be whimsical.

Timing of Water Level and Feedwater Flow. It is unfortunate that there is a nearly complete lack of documented information in the reports supplied to the NRC, because steam generator water level records usually exist. (Some feedwater flow records may also exist.) The best that can be said based on the available documents is that there is no evidence that any steam generator waterhammer event occurred without first uncovering the sparger. Drainage times (the total time that the sparger was uncovered prior to waterhammer) of a few minutes and forty minutes have been reported. The timing of other events has not been considered since the evidence is lacking.

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TABLE A.2 - RECORDED EVENTS OF STEAM GENERATOR WATERHAMMER INCIDENTS DURING OPERATION OF PWRs
(Source: responses to NRC questionnaire of 5/11/75; incident reports.)

INCIDENT	PRECURSOR EVENTS	SG LEVEL[1]	FEEDWATER FLOW[1]	WAS FEEDRING UNCOVERED?	ESTIMATED DRAINAGE TIME[2]	ESTIMATED FEEDWATER FLOW RATE (per SG)	WAS FEEDRING RECOVERED?[3]	AUDIBLE OR VISIBLE WATERHAMMER EVIDENCE	DAMAGE RECORDS
Yankee Rowe (1966)	?	No	No	Yes	?	?	?	?	limited description
Haddam Neck (?)	?				?	?	?	?	limited description
	lo-lo SG level	No	No	?					
San Onofre #1 (1972)	High SG level	No	No	?	?	?	?	?	limited description
Surry #1 (1972)	Unit Trip leaking check valve	No	No	?	?	?	?	loud noise	limited description extensive damage
Robert Ginna (1973)	?	No	No	?	?	?	?	pipe shaking	inspection report
Oconee #1 (1973)	reactor startup established feed flow	No	No	Yes	?	?	No	none recorded	no damage reported
Indian Point #2 (1973)	Turbine Trip from 7% power, High SG level	Yes	Yes	Yes	1 to 5 minutes	140 gpm	Not In SG22?	loud noise pipes shaking SG22	elaborate records of extensive damage
Turkey Point #3 (1974)	?	No	No	?	?	?	?	none recorded	inspection report
San Onofre #1 (1974)	?	No	No	?	?	?	?	none recorded	inspection report
Turkey Point #3 (?)	?	No	No	?	?	?	?	none recorded	inspection report
Turkey Point #4 (?)	?	No	No	?	?	?	?	none recorded	inspection report
Fort Calhoun #1 (?)	?	No	No	?	?	?	?	piping vibration	limited description
Zion #2 (8/74)	Pump Trip Lo-Lo SG level	No	No	?	?	?	?	none recorded	limited description
Zion #2 (12/74)	failed valve regulator at 82% power	No	No	?	?	?	?	none recorded	no damage, safety injection
Maine Yankee (1975)	broken bypass line manual trip	No	No	?	?	?	?	noise and vibration	initially failed valve was only damage
Zion #2 (3/75)	turbine trip High SG level	No	No	?	?	?	?	pipe movement	limited description
Calvert Cliffs #1 (1975)	loss of feedwater reactor trip	No	No	Yes	40 minutes	500 gpm	nearly	loud noise instrument fluctn.	limited description of extensive damage
Praire Island #2 (1975)	Unit trip	Yes	No	Yes	?	100 gpm	?	noise, shaking in control room	limited description
Robert Ginna (1975)	plant trip 30% power	No	No	?	?	?	?	pipe shaking	inspection report
Donald Cook (1975)	loss of offsite power test	No	No		?	?	?	none recorded	no damage reported
Zion #2 (5/76)	Pump trip	No	No		?	80-100 gpm	?	noise heard	no damage, safety injection
Zion #2 (6/76)	planned turbine trip	No	No	?	?	?	?	none recorded	no damage, safety injection
Zion #1 (9/76)	no report received by December 1, 1976								
Beaver Valley (1976)	no report received by December 1, 1976								

[1] These columns indicate whether any detailed records were reported to the NRC.

[2] The drainage time indicated here is the estimated time between feedring uncovering and the evidence of waterhammer.

[3] The question is whether at the time of the evidence of waterhammer the feedring was clearly uncovered or in the process of being covered.

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Although little direct evidence is available, it is believed that following feedring uncovering, the feedring and associated piping has simply drained into the vessel in the expected manner during most of the reported incidents. However, in at least one instance, that at Surry #1 on October 1, 1972, backdraining (to the ambient environment) through a leaking check valve was implicated as a significant event.

Sparger Recovery. There is some question about sparger recovery. Early documents describing the sequence of events refer to recovering the sparger as a necessary event in the sequence in order to "trap" a steam void. This hypothesis has been discredited by recent evidence since waterhammer events have been recorded regularly in model studies and in at least two full-scale tests with the water level well below the sparger at all times. Sparger recovery may be an independent or contributing mechanism for trapping a steam void, however.

From U. S. operating experience, only the reports of incidents at Indian Point #2 and Calvert Cliffs #1 provide useful information on this point. At Indian Point #2, the operator was unable to recover the water level in SG#22 after uncovering of the feedring. Therefore, the recorded level remained below the feedring throughout the incident. However, the level in SG#22 only dropped a few inches below the feedring because the trip was only from 7% power. Data uncertainty prevents a definite conclusion. At Calvert Cliffs #1, there is no question that the incident was triggered approximately as the feedring was recovered thirty minutes after feedwater flow was reestablished and fully forty minutes after the inital main feedwater pump trip.*

Feedwater Flow Rate. Incidents are reported to have occurred over a broad range of feedwater flow rates from roughly 100 gpm to more than 500 gpm. There have been no incidents reported at high flow rates (roughly 1500 gpm or more) which we would expect would be sufficient to run a typical 16-inch pipe "full" based on the analysis of Section 5 of this report. (All flow rates in this report are for a single steam generator.)

*In this instance, the level was recovered using main feedwater through the main feedwater sparger, not auxiliary feedwater through the separate auxiliary sparger at Calvert Cliffs #1.

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The incident at Calvert Cliffs #1 is a useful example. The Incident Report [A.3] is terse and does not mention feedwater flow rate. An Inspection Report by the NRC [A.4] documents eyewitness estimates of 500 gpm auxiliary feedwater flow rate. Yet thirty minutes were reported to have elapsed as the level rose from a reported -100 inches to the -60 inch level of the bottom of the feedring. If the flow rate were steady, this level rise information corresponds to roughly 200 gpm feedwater flow based on the entire vessel cross section. Obstructions would lower this estimate.

<u>Feedwater Temperature</u>. The detailed distribution and timing of the temperature of the feedwater entering and within the sparger and piping is unknown in all cases. There is good reason, based on piping and instrument diagrams and information supplied by the vendors, to believe that during the recorded waterhammer incidents the feedwater supplied was cold (say 100°F) and thus highly subcooled (say 400 to 500°F) in most cases, but this has generally not been reported.

<u>Direct Evidence of Waterhammer</u>. For most incidents there have been descriptions of noises or pipes shaking. The timing and location of such observations have been generally unclear.

<u>Damage Records</u>. There has usually been an account of injured components. Quantitative information such as the location and degree of pipe or support deformations or motion, stress to produce the deformations, and the like have been scanty. Thus, there is little evidence that can be employed to confirm analyses.

The November 13, 1973 incident at Indian Point #2 is the only incident involving fracture of a major pipe. Several incidents have involved extensive pipe deformation and support damage. To cite two prominent examples, seven damaged hanger, three damaged slide supports, seven failed hydraulic shock suppressors, bolt elongation and a ruptured gasket on a feedwater check valve, and a possible 10-inch permanent deformation of the 14-inch main feedwater pipe was reported after the October 1, 1972 incident at Surry #1. In addition to hanger and support damage at Calvert Cliffs #1 on May 12, 1975, the main feedwater isolation valves on the two coolant loops and one of the main feedwater control valves were rendered inoperative when the overhung valve operators were severed from the valve during the incident. An immediate effect of this valve damage was a loss of feedwater flow cont [illegible], including the sudden flooding of SG#11 from -60 inches to + 52 inches in less than three minutes (after SG#11 had been filling carefully for forty minutes at 1 inch/minute).

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A.3 PWR Test Evidence

Tests conducted on PWRs in order to verify the geometries and procedures of the specific plants tested are listed in Table 5. The only broad and extensive test programs conducted in the United States prior to December 1, 1976 have been at Indian Point #2 and Trojan. The remaining tests in the United States have been generally limited to a single verification test at each plant. Extensive test programs have also been carried out at the three foreign utilities cited in Table 5. Each test or test program is reviewed in turn below.

Tests at Indian Point #2

Following the November 13, 1973 incident at Indian Point #2, a test program was initiated that ultimately required four months to complete.

Prior to the initial phase of this testing, the horizontal run of piping to SG#22 (which had been involved in the incident) was shortened from 17 feet to 4 feet by dropping a section of the pipe by approximately one pipe diameter. The other pipes were not modified and retained lengths of 7 feet, 12 feet, and 10 feet for SG#21, SG#23, SG#24, respectively. At the time, this was believed to be "acceptable according to the design criteria for Indian Point #2" [1].

Extensive high response pressure, acceleration and strain instrumentation was installed throughout the feedwater piping system by Atomics International [A.5]. FM tape recordings were made of all data obtained throughout the entire test program and these records were replayed at various speeds to display all relevant data in a comprehensive compilation of stripcharts. In addition, various scratch gauges were installed to record peak piping displacements and a few thermocouples were installed on the outside surface of the piping. Steam generator water level and feedwater flow rate were recorded during the testing, and plant personnel were stationed at various points to observe or hear evidence of waterhammer.

According to Reference [A.6], testing commenced and two tests were completed successfully (without any evidence of waterhammer) with the reactor subcritical and with the reactor critical and at 7% power. A final test was planned for a reactor trip from 100% power. However, on January 29, 1974 the reactor tripped inadventently from 35% power and there was extensive indication of a mild, non-damaging waterhammer event originating in SG#21. The planned test was aborted at this point and the test evidence was compiled as Phase I of the overall program.

It is significant that the January 29, 1974 waterhammer event occurred in one of the pipes that was short enough (seven feet) to satisfy the Westinghouse pipe layout guideline.

Phase II of the test program was a series of exploratory tests conducted to investigate the effect of auxiliary feedwater flow rate. The tests were conducted on February 2, 1974 and February 3, 1974. All four loops were tested independently by lowering the water level below the sparger and refilling the auxiliary feedwater in one steam generator at a time. The test sequence and results are indicated in Table A.3. Two non-damaging waterhammer events were recorded (Runs 6 and 13) at the highest flow rate tested (240 gpm). Waterhammer was not indicated in nine tests at flow rates between 75 and 200 gpm and in two other tests at 240 gpm.

TABLE A.3 - INDIAN POINT #2 PHASE II TEST SEQUENCE AND RESULTS [A.6]
(Numbers in Table are Run Number)

Auxiliary Feedwater Flow (gpm)	Steam Generators (Horizontal Pipe Runs, Feet)			
	21 (7 ft)	22 (4 ft)	23 (12 ft)	24 (10 ft)
75	1	-	3	2
150	4	-	7	-
200	5	12	8	10
240	6*	13*	9	11

*Waterhammer was indicated conclusively in this run. There was no indication of waterhammer in unmarked runs.

The waterhammer events recorded during the Phase II test program occurred in SG#21 and SG#22, the two loops involved previously. These loops had the shortest horizontal pipe runs and both were within the Westinghouse guideline.

Records of steam generator water level were examined for each of the three waterhammer events that occurred during the Phase I and II tests at Indian Point #2. Based on the data reported in Reference [A.6], each waterhammer event occurred when the water level was approximately at the center of the feedring. The precision of the water level indication is unknown, but it is unlikely to be so poor as to alter the prime conclusion that the feedring was covered or being covered at the indicated time of each of the waterhammer events.

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(The time of the event was determined by rapid verbal communication with personnel stationed within the containment.) In each case there is a nearly coincident decrease in water level within the vessel, as might occur if water within the vessel were rapidly drawn in to fill the feedwater piping. Crude calculations indicate that the water volume decrease within the vessel is consistent with refilling the feedring and associated piping. Thus, there is a framework of direct and indirect evidence that suggests that waterhammer events recorded during the tests at Indian Point #2 occurred as or some few minutes after the bottom discharge holes in the feedring were covered by the nominal water level.

At the conclusion of Phase II of the test program, Indian Point #2 personnel decided to install J-tubes on the feedring. The exploratory Phase II program must be considered a success in the sense that it generated useful experimental evidence. However, it did not lead to a procedure that was employed at Indian Point #2. Instead, a previously untried hardware modification was preferred.

The final Phase III of the test program [A.7] was conducted from March 16, 1974 to April 18, 1974 and was intended to verify that "the waterhammer effect which caused the November 13, 1973 feedwater line incident would not recur" [A.6]. Preliminary tests were conducted to measure the drainage rate under "cold" non-operating conditions. The data of Figure A.12 were obtained.*

A test was conducted individually on each of the four steam generators with the reactor subcritical and the plant in hot shutdown condition. There were no indications of waterhammer during these tests.

In each test, the water level was lowered to approximately eight inches below the bottom of the feedwater pipe, and then raised to recover the feedwater pipe. The system was tested cautiously, however. While the water level was being lowered (by blowdown), auxiliary feedwater flow to the generator was maintained and presumably the feedring remained full. With the water level below the feedwater pipe, auxiliary feedwater flow was stopped and restarted twice with intervening drainage intervals of five minutes the first time and ten minutes the second time. The actual water levels in the feedrings during any of the Phase III verification tests are unknown. (The drainage curve of Figure A.12 indicates that the top two inches of the feedring (top five inches of the feed pipe) would be expected to drain in ten minutes with the plant "<u>cold</u>".) Feedwater flow rates were not measured during the Phase III tests, but may be deduced from the valve position indication. The normal (50%) present operating point used during these tests is

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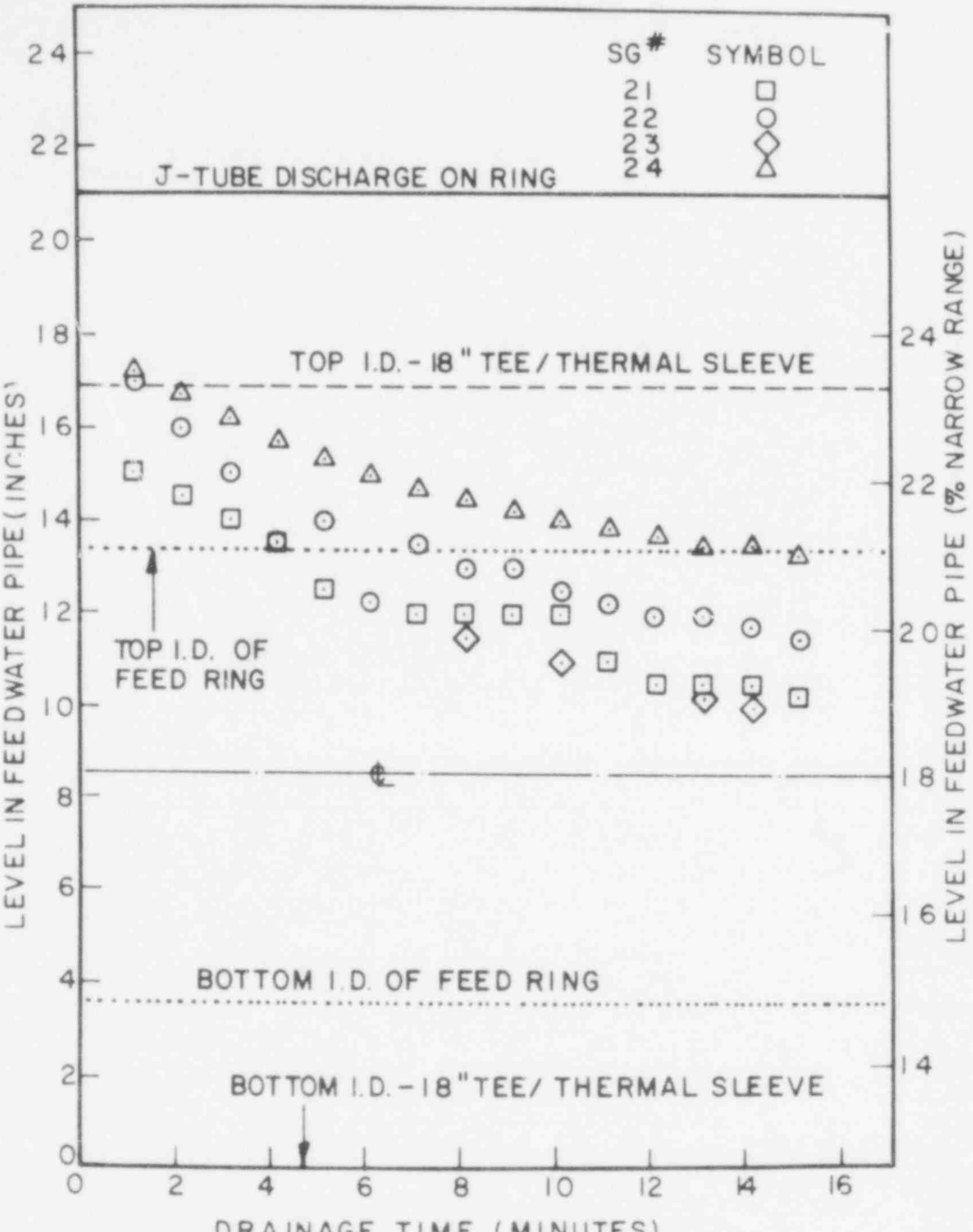
*Data provided by personal communication to P. Rothe from J. D. O'Toole, Consolidated Edison Company, September 15, 1976.



"COLD" DRAINAGE DATA FROM INDIAN POINT # 2

FIGURE A12

730 095

believed to correspond to a flow rate of approximately 140 gpm.* Thus, in the Phase III verification tests with the reactor subcritical, only limited drainage periods were employed and the feedwater flow was controlled at a rate where waterhammer had not occurred during the previous Phase II tests.

Three further tests were performed with the reactor at power levels of 7%, 35%, and 100% in order to simulate potential abnormal operating conditions realistically with all instrumentation still installed. There was no indication of waterhammer during these tests. Each test was initiated by lowering the level in one or more steam generators until a lo-lo level reactor trip occurred. Steam generator level records are included in Reference [A.7] only for SG#21 and SG#22. Although the time of the reactor trip is indicated, the timing of main feedwater pump shutoff and auxiliary feedwater flow rate is not indicated; presumably the preset 50% valve position was used. In the 7% power test, the J-tubes never uncovered in SG#21 and the level in SG#22 began to rise (suggesting that the feedring was full) only one minute after the reactor trip. The feedrings in SG#21 and SG#22 were uncovered for a total of twenty to thirty minutes after the trip from 35% power and the water level did not begin to rise for approximately fifteen to twenty minutes after the feedrings were first uncovered. The timing and rate of water delivery to the feedring is not reported, but the water level records again suggest that the feedring was refilled at approximately 140 gpm. Available thermocouple records suggest that there was steam in the upper part of the feedrings for roughly ten minutes during this test. For the trip from 100% power, the records only indicate that the SG#21 and SG#22 feedrings were uncovered. The recovery traces are not shown. One of the available thermocouple records suggests that cold feedwater was supplied to SG#21 within four minutes after the reactor trip from 100% power.

In summary, there was only a limited drainage period and quite possibly a low feedwater flow rate during the Phase III tests at Indian Point #2. Moreover, there were only three tests and these were limited to a single set of operating procedures intended to be typical of recovery from a reactor trip. Therefore, although the Phase III tests at Indian Point #2 constitute a meaningful limited verification of procedures at that plant, these tests taken alone are by no means sufficient to confirm that the J-tube modification will be effective over the range of credible operating circumstances at any plant.

Indian Point #2 personnel are commended for their careful and thorough pioneering study performed to investigate some of the generic phenomena associated with steam generator waterhammer.

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*After the water level was rising, the feedwater flow rate was increased to its maximum value (275 gpm estimated) but this is irrelevant since the feedring was presumably full of water at that time.

730 096

Tests at Trojan

The Trojan plant (supplied by Westinghouse) satisfied the vendor hardware recommendations by having J-tubes on the feedrings and 90° elbows on the feedwater nozzles (i.e., short horizontal runs of piping). There is no limit on the auxiliary feedwater flow rate and with the usual arrangement of two auxiliary feed pumps supplying four steam generators a flow rate of 440 gpm per generator is expected. It may be possible to deliver a higher flow rate to a single steam generator.

Eight tests were conducted with the reactor subcritical and one test was conducted with decay heat [A.8, A.9]. No waterhammer events were indicated during these tests.* During the eight subcritical tests, steam generator pressure, auxiliary feedwater flow rate, and drainage time were varied according to the matrix of Table A.4. Auxiliary feedwater temperature is not given in Reference [A.8], but was presumably 100 to 200°F.

TABLE A.4 - TROJAN SUBCRITICAL TEST MATRIX [A.8]

Steam Generator Pressure (psig)	Auxiliary Feedwater Flow (gpm/SG)	Feedring Drain Time	Steam Generator Tested
1100	120	30 min	A
1100	220	30 min	A
1100	440	30 min	A
880	220	60 sec	A
880	440	60 sec	A
880	440	2 hrs	A
400	220	30 min	A & D
400	440	60 sec	A

*According to the trip report by Bu'ut [A.10], there were 22 strain gages, 6 thermocouples, 7 accelerometers, and 4 feedwater pressure sensors installed on the main feedwater piping to SG#A in addition to normal instruments for steam generator pressure, auxiliary pump discharge pressure, auxiliary feedwater flow rate (for each steam generator) and steam generator water level. No audible, visible, or instrument indications of waterhammer were recorded.

730 097

In each test the auxiliary feedwater flow was maintained at approximately 100 gpm while the steam generator water level was brought to (or left at) a level below the feedring. The auxiliary feedwater flow was shut off during the drainage period and was turned on again to initiate the test. The transient associated with opening the valve lasted several seconds. Feedwater was supplied for a minute or two which should be expected to refill the ring but not fill the steam generator appreciably. Tests followed each other in rapid succession.

Creare has no reason to doubt that the tests were carried out as planned, but direct evidence to support the effectiveness of the test procedure is not available from the detailed data supplied with the test report [A.8]. Independent confirmation has been obtained from Pulling [A.11]. In brief, data are lacking to demonstrate conclusively that the water level was indeed below the feedring prior to each test and that the feedring was refilled during each test. The problem is that the water level data are too uncertain and the refill times too short for the water level data in Reference [A.8] alone to verify the procedures. Deductions of inital water level from records of thermocouples arrayed about the pipe periphery (on the outside surface) were reported in [A.8], but the raw records were not supplied. The adequacy of such data to determine water level transients is generally questionable since thermocouples cannot distinguish between saturated water and steam and because the piping introduces a thermal time lag (a minor effect usually). Pulling [A.11] has assured Creare that the detailed traces exhibit transient features that indicate without question that the feedring was uncovered and subsequently refilled. The quantitative deduction that the pipe was almost half drained after thirty minutes and fully drained after two hours is plausible, but highly uncertain.

If the data are interpreted as reported, then the tests at Trojan reported in Reference [A.3] are a critical hardware verification. No waterhammer events were indicated during a matrix of tests at pressures from 440 to 1100 psig, at typical auxiliary feedwater flow rates of 220 and 440 gpm and with an almost fully drained feedpipe as well as with partially drained feedpipes. Trojan has both J-tubes and a 90° elbow on each steam generator feedwater nozzle.

One follow up test was also conducted on the Trojan plant using decay heat. No waterhammer events were indicated during this test.

Tests at Calvert Cliffs #1 and #2

Following the major incident at Calvert Cliffs #1 on May 12, 1975, tests were conducted on each of the Calvert Cliffs plants (supplied by Combustion Engineering). It is

Creare's general impression that these tests were too limited and that plant personnel were overly constrained during their performance. Test documentation is scanty. Below we report information derived from several meetings and conversations held with utility and plant personnel.

The sequence of work conducted at Calvert Cliffs was described to Creare and to the NRC by Mr. A. R. Thornton of BG&E during a meeting with Bechtel on September 3, 1976. Shortly after the May 12, 1975 incident, a maximum 1.2 inch/min limit was imposed on the rate of steam generator water level rise during auxiliary feed. It is our understanding that this corresponds very roughly to a 168 gpm limit on auxiliary feedwater flow if the flow were steady, and if water were not being boiled off by decay heat.

At Calvert Cliffs #1 and #2, auxiliary feedwater is supplied through a one-foot horizontal run of 4-inch auxiliary feedwater pipe to a separate ring sparger located one foot below the main feedring; the pipe diameter of the auxiliary feedring is approximately three inches. There is some reason to believe that the small auxiliary feedwater sparger will run full at 168 gpm based on analyses and modeling studies described in Section 5.1. Therefore, the auxiliary feed piping may be less susceptible to slug formation and subsequent waterhammer although such behavior is certainly possible if suitable flow transients are incurred as auxiliary feedwater flow is being established or if the auxiliary feedwater flow rate is set too low. Presumably, the main rationale for the level rise limit is to ensure that the main feedring is not recovered too rapidly which might trap a steam void in the main feed pipe.

The procedures instituted at Calvert Cliffs #1 were tested on May 16, 1975 [A.12]. Only two tests were run, one on each loop, at conditions felt to be typical of operation subsequent to a main feedpump trip. No special instrumentation was installed to our knowledge. The steam generator pressure was 900 psia. The water level was dropped below the feedring and then recovered with auxiliary feedwater at a reported 175 gpm. There was no waterhammer indicated during the test of SG#12. A noise was heard during the test of SG#11 as the auxiliary feedwater pump was started. There was no damage reported. Since the auxiliary feedwater was supplied to a separate feedring and since the recorded water level was well below the main feedring, it is unlikely that the noise was generated by waterhammer in the main feedpipe. The initial water level in this test was near to the bottom of the auxiliary feedring. It is plausible that the noise resulted from waterhammer in the auxiliary feedwater piping during the initial transient filling of the pipe as flow was established. The utility has not attempted to explain the behavior in any document. Calvert Cliffs #1 was operating within a few days of the major incident at that plant.

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Following these tests, standpipes were installed in Calvert Cliffs #2 with the intent of employing this system in Unit #1 as well, after successful testing of Unit #2. This would permit removal of the auxiliary feedwater flow rate limit.

On May 12, 1976, one year after the incident on Unit #1, the standpipe fix was tested on Calvert Cliffs #2. A waterhammer apparently occurred according to informal reports. The test procedure and rough record of steam generator level submitted to Creare do not indicate or include direct evidence of a waterhammer event. According to eyewitness reports, a loud noise was heard as main feedwater was being supplied to the main feedwater pipe of SG#21 (with standpipes installed). The records and eyewitness reports both indicate that a few minutes after main feedwater was established, it was shut off and auxiliary feedwater flow was established (to the auxiliary sparger) and used to recover the steam generator water level. This discrepancy from intended procedures is convincing evidence that the tests were aborted. No damage was reported. The standpipes have been removed from Calvert Cliffs #2 and were found to be intact on removal [A.12]. Alternative fixes are being considered.

Since this event constitutes a failure of a device like the J-tubes, the limited operating records were examined closely and eyewitness descriptions were solicited. The steam generator pressure was very low, approximately 145 psig; this is the only test ever reported at such a low pressure. (This was the first test of six planned tests of the two steam generators at three pressures.) According to an eyewitness report, main feedwater flow rate was not measured, may not have been carefully controlled, possibly fluctuated appreciably as it was being established, and is suspected to have been quite high, as much as 1200 gpm to the steam generator tested. Intended operation with a measured 100 psid pressure drop across the preset (1/3 open) main feedwater bypass valve would be expected to supply flow at 800 gpm to one steam generator; the pressure may have been well above 100 psid. However, this is only speculation because instrumentation adequate to determine the main feedwater flow rate in this low flow range had not been installed at Calvert Cliffs #2. The steam generator water level was lowered from 0 to -90 inches over a period of three hours and was below the bottom of the feedring (at -60 inches) for roughly one hour. Main feed flow (approximately 100°F) was established and the water level had increased by approximately 15 inches in the roughly five minutes prior to the time that main feed was turned off.

730 100

(Taken literally, this data implies a flow rate of approximately 600 gpm.) It is our understanding that the noise was heard near this time. All feedwater was off for almost an hour before auxiliary feed was initiated and used to recover the water level slowly.

Certain features of these data appear to be inconsistent and may be unreliable. For example, if the noise was heard at the time main feedwater was secured, this would imply that the feedring was full at the time of the incident, an unlikely event in our judgement. Creare speculates that main feedwater flow was established by rapidly opening the valve to its preset position designed to give 5% flow (approximately 600 gpm at Calvert Cliffs #2). Within seconds a relatively steady flow near 600 gpm may have been established. The inadequate instrumentation probably exhibited gross fluctuations. The noise was probably heard a fraction of a minute later, but there may have been no action for a few minutes. Creare cannot be confident of these speculations.

An attempt was also made to measure feedring water level during this test using a crude gauge glass connected between the steam generator vessel (steam side) and a low point in the feedwater piping. These efforts failed due to basic uncertainties of the method and because the "zero" location was not properly established.

In summary, the paucity of the evidence makes it impossible to isolate phenomena or actions leading to waterhammer in this instance. It is very likely that a waterhammer event of the type under consideration occurred in the Calvert Cliffs #2 test with standpipes installed. It can be concluded that standpipes (and probably J-tubes) alone are not an absolute means to prevent slug formation and consequent waterhammer.

Tests at St. Lucie and Millstone

At this writing, limited records of the tests at St. Lucie are available to Creare, but we have no documentation of the tests at Millstone. The tests were conducted on May 14, 1976 at St. Lucie and in the Fall of 1975 at Millstone. It is our understanding from informal discussions that standpipes had been installed at the time of the tests at both plants. No waterhammer events have been reported. St. Lucie has a 90° elbow installed on the main feedwater nozzle, and Millstone has a seven foot horizontal run of main feedwater piping. A drainage period of over two hours was employed at St. Lucie, but the Millstone tests had only a 13 minute drainage period. Steam generator pressures were approximately 900 psi at both plants. The auxiliary feedwater flow rate (through the main feedwater pipe and feedring) was approximately 300 gpm at both plants.

730 101

At St. Lucie #1, only one test was conducted on SG#1B with the plant at hot standby conditions within two hours of operation at or above 30% power. (Additional tests are planned at this writing.) Steam generator water level and pressure, auxiliary feedwater flow and pressure were recorded. No special instrumentation was installed and feedring drainage measurements were not made. Conditions at Millstone are unknown.

It is concluded that the standpipes were tested successfully on two occasions at two different plants. This evidence is too limited and scattered to be of general value.

Foreign Plant Experience

The following paragraphs describe public available information derived from experience at the following plants:

- Tihange (Belgium)
- Doel (Belgium)
- Ringhals (Sweden)

No claim is made that this information is a comprehensive review of foreign experience. However, personnel at Westinghouse and Combustion Engineering were each asked if they were aware of any other waterhammer incidents or plant tests that would be of interest to the present inquiry [2,3]. No additional information was provided.

Tests at Tihange (Belgium)

The Foratom Congress paper by Stubbe, et al [A.13] briefly describes the experience at the Tihange plant (supplied by Westinghouse). The Tihange plant has bottom discharge holes and an approximately eight foot run of horizontal pipe from each of the three steam generator feedwater nozzles. A series of tests was conducted at hot standby conditions with steam generator pressures of approximately 1000 psia. A detailed report of the tests or procedures is not available to Creare. Apparently, several tests had already been conducted at auxiliary feedwater flow rates up to 140 gpm when a test at a reported 176 gpm flow rate was conducted on SG#1. A waterhammer event occurred. No damage was reported and it is stated that "no permanent deformation was discernible as measured at the plant site following the test". (Informal reports indicate that one hangar may have broken and that pipe insulation was shaken off.) No further tests were conducted, evidently to avoid risking damage to the plant.

730 102

The singular feature of this test is that it provides the only available high-response pressure data from a severe, though nonetheless non-damaging, waterhammer event. The pressure traces presented by Batchelor, et al [64] are provided in Figure 60 of this report. The approximate locations of the sensors P1 and P2 are shown in Figure 4 of Reference [64] which unfortunately lacks dimensions. (Also indicated in this figure is the Station D1 at which displacement histories were measured in three planes.) It is generally accepted that the pressure transducer or associated electronics failed during the event. (Pressures significantly less than absolute zero are indicated at several points.) It is also generally accepted that the precursor depressurization (up to approximately 78 msec on P1 and approximately 87 msec on P2) is real and relatively accurate. Part of the overpressure may also have been accurately recorded with the exception of a brief period near 91 msec when electrical connection was lost. (The records give the appearance of an intermittent electrical short circuit to ground.) The pressure range of the transducers, response characteristics, and estimated data uncertainty are not reported in Reference [64].

The strategy for continued operation of the Tihange plant is to restrict auxiliary feedwater flows to 140 gpm. Under some accident conditions such as a main steam line break, the feedwater flow rate may exceed this limit and in these circumstances Tihange personnel recognize the clear possibility of a waterhammer. Accordingly, a structural analysis [64] was conducted with the stated objective of demonstrating "the capability of the [Tihange] plant to withstand such a waterhammer in the unlikely event that it should recur". Since the plant had already demonstrated this fact admirably, the main utility of this effort would be to verify the stress calculation tool in a limited way in one instance. Back calculation to deduce the overpressure pulse based on the measured depressurization and displacement as described in Reference [64] is highly questionable.*

Tests at Doel (Belgium)

The Foratom Congress paper by Stubbe, et al [A.13] also describes the experience at the Doel plants (supplied by Westinghouse). No indications of waterhammer were observed during tests on Doel #1. Unfortunately, no other information on tests at Doel #1 is available to Creare. On February 23, 1975 during "hot test runs" a waterhammer event occurred in SG#2B of Doel #2 while cold auxiliary feedwater was being supplied at only 40 gpm. A second attempt was made to supply

*A proprietary document believed to contain a detailed version of this analysis is cited as Reference 2 of the paper given by Vreeland [34].

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a "very small" (but unfortunately unspecified) feedwater flow rate, but this caused severe vibrations of the feedpipe. The plant was returned to "cold" conditions.

This event occurred with the lowest flow rate (40 gpm) reported during our study. It is significant that the steam generator was only at approximately 45 psia at the time of the event. Unfortunately, other details of the test are unavailable to Creare at this writing. The Doel #2 plant has two steam generators with very long pipe runs. Figure 1 of Reference [A.13] shows for example that SG#B has an 18 foot horizontal run of main feedwater pipe from the steam generator nozzle to an upward facing elbow, a 23 foot elevation increase and a 33 foot additional horizontal run (with a slight downslope) to the first downward facing elbow. The geometry of piping to SG#A is not given in Reference [A.13].

Both loops were fitted with a novel feedwater trap (essentially a labyrinth seal) described in detail in Reference [A.13]. The feedwater traps were located in the horizontal run of piping approximately two feet from the steam generator vessel.

Preliminary tests were conducted to determine the effectiveness of the feedwater traps. According to the detailed test report [A.14], the traps had not drained after a static waiting period of 15 hours and also were not drained by sudden transients to no flow from normal flow rates of 4800 gpm per steam generator. Unfortunately, high static pressure losses at the traps were recorded during tests at normal main feedwater flow, which is a disadvantage of the device.

Special tests were run to measure the feedwater flow necessary to run the pipe full. The tests were run with the plant cold and the steam generator pressurized by air to 45 psia. Water level in the feedwater pipe was determined by conductivity probes. Special flow rate instrumentation (a venturi) was installed. The pipe ran full at 1750 $\pm$ 250 gpm with increasing flow and at 1200 $\pm$ 300 gpm with decreasing flow. These values are consistent with predictions presented in Section 5.1 of this report. (Such hysteresis is common in tests of this type.) These flow rates are much higher than can be achieved by typical auxiliary feedwater systems.

The "hot" tests at Doel (with heat from the primary coolant pumps) studied the effects of a 1 inch hole drilled in the top of the feedring diametrically opposite to the inlet tee intersection of the main feedwater pipe with the feedring. The analytical basis of design for this device is described briefly in the feasibility study by Stubbe and Gerwan [A.24].

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It is our understanding that there were no vent holes in the ring or main feedwater pipe initially so that these specially added holes were the only vent in the system.

Initially, the vent hole was added only to SG#B and tests were run in both SG#A and SG#B at various auxiliary feedwater flow rates. The two steam generators are identical in design and although the feedwater piping layouts are different, they would be expected to behave in a similar fashion with the feedwater trap installed. SG#B (with a vent hole) was tested up to 400 gpm with no indication of waterhammer; SG#A (without a vent hole) experienced waterhammer events in two separate tests at 260 gpm. (There were no indications of waterhammer at flow rates to SG#A up to 200 gpm.) A vent hole was drilled in the feedring of SG#A and the A loop was retested with no other known changes. There were no indications of waterhammer in eight further tests of SG#A at flow rates up to 530 gpm. The complete test matrix is given in Table A.5 and each run is described briefly in the test report [A.14].

These tests are compelling evidence that in some circumstances a venting device can act to prevent either slug formation or void collapse and thence prevent slug impact and waterhammer. Although the potential utility of the venting device has been demonstrated, the effectiveness of the device has not been verified over a broad range of circumstances. It is significant that the water level was well below the feedring in the two tests that had evidence of waterhammer.

The "hot" tests at Doel were performed with the steam generator pressure at approximately 940 psia. Auxiliary feedwater was supplied from tanks at 85°F. At the start of each test, steam generator water level was at approximately 5% and the feedring was drained up to the trap. The bottom of the feedring is at 13% and the top is at 21%. Auxiliary feedwater was supplied at the prescribed rate, but it is <u>not</u> stated that the feedring was covered in each instance. Records of steam generator water level are not supplied with the test report.

Extensive high-response instrumentation was installed on the Doel #2 feedwater piping prior to the testing. The instrumentation included two pressure transducers, nine accelerometers, fourteen strain gauges, forty-eight maximum displacement transducers, three pair of thermocouples, and three scratch gauges. The data presented in the test report [A.14] indicate that the waterhammer events recorded during the tests at Doel were mild. A typical pressure record is given in Figure 70 of this report.

730 105

TABLE A.5

CHRONOLOGICAL LIST OF TESTS AT DOEL
Chronology of the Experimental Hot Test Runs

Run	SG	Venting Hole	Auxiliary Feedwater flow (gpm)	Any Evidence of Waterhammer?
1	B	YES	90	NO
2	B	YES	150	NO
3	B	YES	220	NO
4	B	YES	220	NO
5	B	YES	310	NO
6	B	YES	400	NO
7	B	YES	20	NO
8	A	NO	65	NO
9	A	NO	140	NO
10	A	NO	200	NO
11	A	NO	260	YES (water level at 7%)
12	A	NO	260	YES (water level at 7%)
13	B	YES	260	NO
14	A	YES	260	NO
15	A	YES	530	NO
16	A	YES	350	NO
17	A	YES	440	NO
18	A	YES	15	NO
19	B	YES	530	NO
20	B	YES	45	NO

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Tests at Ringhals

Direct reports on the tests conducted at Ringhals are not available to Creare at this writing. It is our understanding [2] that vents similar to those installed on the Doel #2 plant were tested at Ringhals and judged to be unsatisfactory--in direct contrast to the findings of the Doel tests. Subsequently, the Ringhals plant was tested with J-tubes installed and this hardware modification was judged to be satisfactory.

Creare speculates that the main difference between the Ringhals and Doel systems in this apparent test of vents was the effective length of external feedwater pipe. It is our understanding that the horizontal pipe runs at Ringhals are approximately eight feet whereas at Doel the feedwater trap is only two feet from the nozzle. Thus, at Ringhals, a vent <u>alone</u> was tested and at Doel the combination of a vent and a "short" pipe run was tested.

730 107

A.4 Update of Utility Survey

An informal survey of operating reactor status was conducted in November of 1976 to update information available on operating procedures and experience.

Changes in hardware or procedures relative to those listed on Table 2 are cited below:

Beaver Valley.	Applying a 150 gpm flow limit in some (unreported) circumstances.
D. C. Cook #1.	Applying a 150 gpm flow limit.
Ginna #1.	Flow limited to 200 gpm (150 gpm reported previously). Flow may be as high as 230 gpm initially.
Haddam Neck.	No flow limit.
Indian Point #2.	Flow limit of 150 gpm if drainage period exceeds five minutes.
Indian Point #3.	Started Commerical Operation. Same hardware and procedures as Indian Point #2.
Kewaunee.	No flow limit. (200 gpm reported previously.)
Point Beach #1, #2.	No change.
Praire Island #1, #2.	Flow limit of 150 gpm.
Robinson #2.	J-tubes planned. Feed as slow as possible presently. (Based on personal communication.)
Salem #1.	J-tubes not installed yet. Limit flow to 1.2 in/min rise rate at present.
San Onofre #1.	No change.
Surry #1, #2.	No update report.
Trojan.	No change.

Turkey Point #3, #4. No change.

Yankee Rowe. No flow limits (procedure previously unknown).

Zion #1, #2. Flow limits 100 gpm (#1) and 50 gpm (#2).

Calvert Cliffs #1. Will not install standpipes.

Calvert Cliffs #2. Standpipes removed 1.2 in/min limit on level rise.

Fort Calhoun #1. No change.

Maine Yankee. No change.

Millstone #2. Conflicting reports. It is believed that standpipes are still installed, J-tubes planned. Has loop seal.

Palisades. No change.

St. Lucie #1. Conflicting reports. It is believed that the standpipes have been removed, that J-tubes are planned, and that a flow rate limit is being used in the interim.

Several waterhammer incidents were described informally in addition to those listed in Table 1. These additional incidents are merely listed below since formal reports are unavailable to Creare.

Beaver Valley. A waterhammer event on December 27, 1976 was reported informally to Creare after the November 1976 survey. As with the November 5, 1976 incident, it is claimed that this behavior was unrelated to steam water slugging and was instead due to a valve instability.

Haddam Neck. Incident on July 15, 1976.

Zion #1. "2 to 3 incidents".

Zion #2. "8 to 10 incidents".

Note: It is suspected that the multiple incidents at the Zion plants represent candid reporting of minor "bumping" events rather than several severe incidents.

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A.3 Denton, R. E.; ABNORMAL OCCURRENCE REPORT; Calvert Cliffs #1; Report 50-317/75/36, May 12, 1975.

A.4 NRC Inspection Report on Calvert Cliffs #1, Lic. DPR-53, Insp. No. 75-13, Docket No. 50-317, May 28, 1975.

A.5 Reiser, P. M.; INDIAN POINT UNIT #2 VIBRATION TEST, PARTS I AND II; Atomics International Division Report AI-75-27, Rockwell International, April 4, 1975.

A.6 Cahill, W. J.; INDIAN POINT UNIT NO. 2 REPORT ON RESULTS OF TEST PROGRAM FOLLOWING PLANT REVISIONS AFFECTING FEEDWATER PIPING; Consolidated Edison Company, Docket No. 50-247, March 12, 1974.

A.7 Newman, C. L.; INDIAN POINT UNIT NO. 2 RESULTS OF TEST PROGRAM FOLLOWING MODIFICATIONS TO STEAM GENERATOR INTERNALS; Consolidated Edison Company, Docket No. 50-247, August 30, 1974.

A.8 Christense, S. R.; REPORT ON TESTING OF AUXILIARY FEED-WATER ADDITION, FOLLOWING J-TUBE MODIFICATION TO THE STEAM GENERATORS OF THE TROJAN NUCLEAR POWER PLANT; Portland General Electic Company, Docket No. 50-344, October 1975.

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A.10 Bulut, N.; TRIP REPORT-FEEDWATER INSTABILITY TESTS FOR THE TROJAN NUCLEAR PLANT; U.S. NRC Internal Memorandum, November 3, 1975.

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(continued)

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APPENDIX B

PWR VENDOR MODELING EFFORTS

Meetings were held with each of the PWR vendors in order to determine the status of their analytical and experimental modeling efforts [2,3,4]. It is our understanding that, at the time of these meetings, none of the vendors had developed an analysis that they would propose for quantitative prediction of steam generator waterhammer behavior for the purpose of verifying the design of PWR piping systems. No one at any of the meetings felt that this situation would change during 1976. Of necessity, the PWR vendors have recommended hardware and procedural modifications based solely on the available experimental and PWR test evidence and on qualitative descriptions of some of the key phenomena. The status of vendor modeling efforts is reviewed below. Publically available documents are critiqued.

Babcock and Wilcox Modeling Efforts

Babcock and Wilcox personnel presented no quantitative analyses that addressed any of the phenomena described in section 1.3 of this report [4]. They also stated that they had conducted no subscale experimental modeling studies of phenomena related to steam generator waterhammer. Accordingly, no technical documents on these subjects have been written. B&W personnel strongly took the position that such work was unnecessary in view of the satisfactory operating experience of B&W steam generators to date.

Combustion Engineering Modeling Efforts

Combustion Engineering personnel described some preliminary analyses that they had conducted [3]. The emphasis of the CE modeling work was placed on bubble collapse, slug dynamics and impact. The trends of overpressure as a function of key parameters such as piping length were predicted. With appropriate adjustment of key modeling coefficients, the CE model was able to predict some of the major features of the pressure data recorded at Tihange (Figure 60) and Doel (Figure 70). No analyses were presented that treated either the initiating mechanisms or the pressure waves, forces, or stresses in piping.

None of this work has been reported publically. Further analytical model development is underway but has low priority. CE personnel also stated that they had not conducted or planned any subscale experimental modeling studies of the relevant phenomena. Although the CE models were developed at a level comparable to that achieved by Westinghouse, the CE evaluation was that their analytical modeling efforts were preliminary and at too early a stage to be applied to predict slug impact and waterhammer behavior in the feedwater piping of PWR steam generators. An opinion was stated that such work was necessary and primarily useful to guide PWR test programs.

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Westinghouse Modeling Efforts

Westinghouse analytical and experimental modeling efforts up to early 1975 are documented in the Research Memorandum by Roidt [5]. This report presents an analysis of steam void collapse, slug dynamics and impact, limited results from experimental modeling studies at 1/10 scale, a comparison of the analytical and experimental results, and an extension of the analysis to values of the parameters typical of PWR steam generators. No analysis is presented of slug initiation mechanisms or of forces, pressure waves, or stresses in piping. It is our understanding that Westinghouse had not published any reports other than [5] dealing with general analytical or experimental model studies of phenomena related to steam generator waterhammer prior to January 1, 1977. However, several other reports may have been prepared by Westinghouse to predict the dynamic behavior of the piping of specific plants or to document general recommendations.

Based on several meetings and discussions with Dr. Roidt, Creare is aware that Westinghouse has conducted additional analytical and experimental work since Reference [5] was released. A report on this work is expected to be released in early 1977. This work is limited to minor refinements to the analysis of Reference [5], application of this analysis to predict the Tihange data (Figure 60) and additional experimental work. Most of the experimental work has dealt with modeling void collapse and slug impact behavior inside the vessel of forthcoming steam generators of the "preheat" type. As such, it does not pertain directly to the present study although similar basic physics are involved. Dr. Roidt informally reported the recent measurement of pressures of the order of 40 to 50 psi in a feedring steam generator model with J tubes. Although these data are significantly higher than the pressures reported in Reference [5], they are a factor of 10 or more lower than the data in Section 4 of this report.

It is our assessment that the Westinghouse analytical model reported by Roidt [5] is far too primitive to be used to predict void collapse or slug motion and impact behavior in PWR steam generators. It is our understanding based on the meeting held September 1, 1976 [2] that Westinghouse personnel share this view. Unfortunately, both the general tone of the report [5] and its specific claims are so overly positive that its role in the justification of the Westinghouse criteria to preclude waterhammer may be misinterpreted. Several utilities have used this information directly.*

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*For example, the D.C. Cook response [B.1] to the NRC questionnaire [6] presents predictions of pipe stress based on the results of the Roidt report [5]. They state, "Thus it is concluded that the effects of waterhammer would not cause the inadvertent rupture of a feedwater line at the Donald C. Cook Nuclear Power Plant."

Critical Review of Reference [5]

Following a general introductory section, the results and conclusions of Reference [5] are presented. Figure 6 of Reference [5] compares the analysis with the data and the text indicates that "the agreement is good". The theory is extended to typical steam generator operating conditions and it is stated that:

> "The extension of the theory into the high pressure region requires mostly simplifying assumptions with respect to the behavior of the gas bubble and the resultant wave pressures and also is able to utilize better empirical data than is available for the condensing heat transfer coefficient in the presence of air corresponding to our experiment. For these reasons, as well as the fact that the theory predicts overpressure values which are reasonable in the light of some feedline damage we believe the results shown in Figures 7, 8, 9, and 10 to be reasonably accurate."

Figures 7, 8, 9, and 10 of Reference [5] present, respectively, calculations of the maximum overpressure after bubble collapse, the peak acoustic overpressure in the wave generated at slug impact, the total energy in the traveling wave, and the duration of the pressure wave moving through the feedline. Each of these parameters are presented as a function of piping length. A general prescription for the application of these results is then stated:

> "The calculations necessary for the determination of feedline integrity would seem to be: first; can the horizontal feedline sustain the loading of Figure 7 for the length of time in Figure 10 and second; can the impulsive loading obtained from Figures 8 and 10 deliver enough of the wave energy of Figure 9 (as the wave moves through an elbow) to cause system yielding. These aspects will not be considered further in this work."

In essence, the above quotes indicate that a well developed and confirmed analysis is being presented and is suitable as a direct component of PWR pipe stress calculations.

The analysis developed by Roidt is based on application of one-dimensional formulations of the continuity and momentum equations for the two phases, an energy equation, and an equation of state for the gas. Calculation of the rate of

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condensation is a critical step of any analysis of void collapse and subsequent slug impact. Roidt compares three models for condensation rate:

1. Condensation limited by an empirical "film" coefficient for heat transfer,

2. the model of item 2 augmented by an additional "sweeping" action, and

3. condensation limited by conduction heat transfer in the fluid, which is modeled as a semi-infinite slab.

All of these models are speculative and provide adjustable coefficients, such as the heat transfer coefficient h_{max}, which in effect are a direct assumption of the condensation rate. In addition to this major assumption, the model includes an arbitrary initial slug length L_o, and two other adjustable coefficients f and κ.

Experiments conducted on two facilities were reported in Reference [5]. Qualitative flow visualization was conducted with an air-water facility. These tests served only to demonstrate that water slugging and impact can occur. Quantitative experiments were conducted in a 1/10-scale steam generator model using steam and water. Unfortunately, apparently due to facility limitations, the steam contained over a 10% mole fraction of air according to the report. We question the accuracy of this estimate because the instrumentation is not described adequately in the report, the data uncertainties are likely to be of major importance, and because the reported calculations imply that only equilibrium physics were invoked. However, if the steam did contain a 10% mole fraction of air, then the tests are useless for any purpose and can only be misleading. The very low pressures (approximately 10 psi) reported by Roidt are evidence that air was present in significant quantities. The tests described in Section 4 of this report were conducted in a facility of similar size and vessel pressure and gave overpressures that are a factor of 10 to 100 larger than those reported by Roidt.

The good agreement between theory and data demonstrated in Figure 6 of the report by Roidt is entirely due to appropriate choice of the coefficients in the analysis. The midpoint of the curve was fitted exactly by choosing $\overline{h}_{max}$ and the slope of the curve at the midpoint was fitted by choosing L_o. Since the

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three condensation models used by Roidt give curves with slightly different curvature, this too has been fitted by the choice of model 2. It is not surprising that there is good agreement between theory and data when the value and the first two derivatives have been fitted at the midpoint of the curve.*

It is argued in the report by Roidt that the coefficients are reasonable. For example, the heat transfer coefficients are similar to the values given by Kern [B.2] which were derived from the 1923 experiments of Othmer for condensing heat transfer in metal tubes [B.3]. Such experiments are inapplicable to the determination of condensation rate on the disturbed surface of a highly subcooled fluid. Alternative bounding assumptions described in Section 5 of this report give overpressures that are a factor of 100 larger than those calculated by Roidt.

It is concluded that the analysis of Roidt [5] represents highly preliminary and unconfirmed modeling ideas that should not be applied to predict possible PWR behavior. Our main objection to this report is not that it presents preliminary ideas, but rather that the claims stated in the summary greatly exceed the value of the work.

Summary of PWR Vendor Modeling Efforts

Based on information acquired up to January 1, 1977, efforts by the PWR vendors to model phenomena related to steam generator waterhammer are at a very early stage. First-order analyses of bubble collapse, slug dynamics and impact have been developed, but are subject to major uncertainties. It is our understanding that no analytical models have been developed for slug formation initiating mechanisms. Pressure waves, forces, and stresses in piping have not been addressed in any general way, although specific piping systems have been analyzed.

This situation is unlikely to change until additional experimental work is conducted in larger, more extensively instrumented facilities than have been employed in the past. To our knowledge, such experiments are presently not underway or planned by the vendors.

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*It is noted that in this comparison the effect of the plastic piping employed in the experiments is overlooked in the theory. The wave speed, and hence the overpressure, was reduced by a factor of approximately three when plastic pipe was substituted for steel pipe in the experiments reported in Section 4, as would be expected from acoustic theory. Thus, the theory and data should disagree by a factor of three due to this effect alone.

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FOR APPENDIX B

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B.3 Othmer, D.F.; Ind. Engr. Chem., 21, 576, 1929.

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APPENDIX C

PWR TEST GUIDELINES

This Appendix offers a limited and very general commentary on PWR tests conducted to verify means to reduce the probability or severity of steam generatc waterhammer. This Appendix is provided in response to a request by the NRC for a general discussion of PWR testing. The reader should appreciate that although Creare has an understanding of the physical phenomena associated with steam generator waterhammer and is familiar with PWR plant design and operation, we have never conducted a PWR test of any kind and may overlook some obvious physical constraints. The comments below should be read with this in mind.

The main points made in this Appendix are:

1) PWR test objectives should be narrowed to reflect the facts that a PWR is ineffective as a research facility and that historically PWR tests of steam generator waterhammer have been, of necessity, very limited in number and conditions tested. The main objective should be to improve confidence in predictions of PWR system behavior.

2) Test success criteria are needed and may have to incorporate considerable engineering judgment due to the vagaries of single sample experiments.

3) Baseline and exploratory test conditions should be clearly identified and isolated. All plants should be tested under common baseline test conditions, without precluding limited exploratory tests to advance the state-of-the-art or to test unusual features of specific plants. A baseline test series is suggested.

4) Plants with unusual or potentially inadequate systems should be tested most thoroughly.

5) A few well chosen, high response instruments are more likely to provide needed quantitative data than the extensive, but ill-chosen instruments employed in the past.

6) Some improvements in test documentation are desirable.

Each of these topics is discussed in turn below.

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Test Objectives

Previous tests have demonstrated that PWRs are ineffective as research facilities. Of necessity, such tests have been highly limited in number and parameter variations. Yet the scatter and lack of replication in the available data from scale model and PWR tests show conclusively that the phenomena are whimsical, i.e., that uncontrolled (and perhaps uncontrollable) parameters strongly effect the results of any single test or limited test series. It is concluded that PWR tests are useful primarily as a means to support and enhance confidence in descriptions of phenomena and quantitative predictions derived by other means.

The current dilemma is that neither analytical model development nor scale model empiricism are sufficiently mature to predict PWR behavior with confidence. The resolution of this dilemma lies in advancing the predictive means, not in exhaustive tests of PWPs. In the interim, very narrow test objectives are proposed to:

1) contribute to system verification by a very limited number of tests under conditions intended to represent extreme but credible behavior during an abnormal operating transient,

2) provide quantitative data that describe the characteristics of any waterhammer events that occur, and

3) provide supportive quantitative data that describe related phenomena, e.g., feedring drainage rate in top discharge systems.

Acceptance Criteria

Prior to any testing, the Nuclear Regulatory Commission and each utility should agree on test procedures, instrumentation, and on results that will be acceptable. To make this possible, the utility should predict the test results and their uncertainty. In very general terms, such a prior agreement will define the acceptance criteria in each case. Similar systems should be subject to similar acceptance criteria.

The practical difficulty with this type of acceptance criteria is that quantitative predictions have not been possible with any confidence. Test predictions in the past have either been that "waterhammer will not occur" or no prediction has been made. Historically, the occurrence of any waterhammer event during a test, however mild, has usually been interpreted as a "failure" in the sense that additional hardware modifications have been performed by the utility. It would be desirable to predict the pressure pulse and piping response based on a conservative analysis and to demonstrate that the test results fall within the predicted range. However, since a truly conservative analysis presently predicts unacceptable overstress of typical piping, this general strategy cannot be employed until less conservative assumptions can be made.

At present, plant acceptance simply cannot be based solely on the results of limited tests. It would be most unfortunate if a potentially competent system incorporating the best of the current vendor recommendations were to "fail the test" because a small steam bubble collapsed and caused a measurable but entirely inconsequential pressure pulse. On the other hand, an unacceptable system should not be employed simply because it performed successfully in a few tests. Accordingly, it is our recommendation that PWR tests should play only a limited role in determining plant acceptance. The primary basis for plant acceptance at present must be engineering judgment founded on the available understanding of the phenomena. PWR tests should be expected only to improve confidence in these judgments.

Test Conditions

The concept is advanced here that at least two classes of tests need to be defined:

1) standardized "baseline" tests, and

2) exploratory tests:

   a) plant-specific tests,
   b) phenomenological tests and instrumentation.

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It is recommended that the Nuclear Regulatory Commission propose a standard series of baseline tests to be performed by each plant that is tested, regardless of the specific hardware and procedures in use at that plant. Suggestions are made below. It may also be desirable to perform additional exploratory tests of features unique to the plant or to contribute to understanding of key phenomena.

Suggested Baseline Tests. A minimum of seven tests are recommended to include:

1) four preliminary tests of one loop using pump heat and with a test matrix comprising all combinations of

   a) highest and lowest anticipated vessel pressures,

   b) "typical" and "longest credible" time to re-establish feedwater flow following feedring uncovering.

2) Three tests of full plant response to a main feedwater pump trip from three different power levels (e.g., 10%, 30%, and 100% power).

It may also be desirable, as an option, to perform an additional test at an intermediate pressure if there is a waterhammer event at the lowest pressure. In each test the following parameter values are recommended:

1) coldest anticipated auxiliary feedwater supply temperature,

2) minimum noncondensible gas content (shown to be typical of normal operation),

3) feedring(s) uncovered,

4) maximum anticipated feedwater flow rate (within existing procedures and as governed by automatic controls, if any).

It is anticipated that some plants may require additional tests. It is also suggested that it may be desirable to gradually increase the severity of the test conditions. Assuming that it can be adjusted significantly, increasing the feedwater supply temperature is most certain to reduce the probability of slug formation and soften the resultant impact.

Discussion of Parameters. There is a sufficient number and range of potentially relevant parameters that it is simply not possible to test everything on every plant. The rationale for the above choices is presented below.

Extreme values of certain parameters can be chosen with confidence. Specifically, minimum feedwater temperature and noncondensible gas content each tend to enhance condensation rates. It is demonstrated in Section 4 that small amounts of noncondensible gas in the steam, order 0.1% by mass, can inhibit slug formation and void collapse altogether. It should be possible to show that noncondensible gas content in PWR steam generators is typically much lower than this amount, but some thought should be given to ensure that the test procedures will not induce unrealistic noncondensible gas content.

Even the maximum possible auxiliary feedwater flow rate will generally be well below the approximately 1500 gpm per steam generator necessary to run a 16 inch feedwater pipe full of water. Under these circumstances, condensation rates, capacity for void collapse, and the propensity for hydraulic instability should tend to be greater at larger feedwater flow rates. Thus, there is qualitative justification for testing only at the highest possible feedwater flow rate. Exploratory tests at lower flow rates may be desirable in some plants, particularly those having unusual systems.

Vessel pressure is a parameter with potentially complex and multiple effects. Increased pressure increases the saturation temperature and thence the subcooling at fixed water temperature. This effect alone will tend to enhance condensation and the prospect of a waterhammer event. However, increased pressure also increases the vapor density, which in turn increases the mass of steam in a fixed volume and decreases the steam velocity at fixed steam mass flow. This effect alone tends to decrease the prospect of waterhammer and is likely to be more powerful than the effects of water subcooling. Finally, the pressure difference accelerating the water slug should depend on (and perhaps be roughly proportional to) the vessel pressure. The net result of these known and suspected parameter interrelations is that waterhammer events are expected to be most prone to occur at low pressure and most severe at high pressure. As a minimum, the anticipated pressure extremes should be tested. Moreover, there is justification for expecting that an intermediate vessel pressure will be the most severe test condition, so additional exploratory tests may be needed depending on the results of earlier tests.

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The timing of various events can have subtle effects on the phenomena. For example, the detailed timing and rate of feedwater delivery following an event that uncovers the feedring helps to determine the transient inventory and temperature distribution of water in the feedring and adjacent horizontal pipe run. In top discharge systems a two hour waiting period has been employed to drain the feedring completely. Yet this waiting period may also cause the initial charge of water entering the feedring to be quite hot. Conversely, a very short waiting period may not drain the system appreciably. It is conceivable that an intermediate waiting period may be the most severe condition. Moreover, either bottom discharge or top discharge systems may be subject to unanticipated effects due to hydraulic transients as the feedwater flow is reestablished or as the flow rate is adjusted. Bottom discharge systems may become particularly sensitive as the feedring is being covered. It is not possible to examine the full spectrum of potential thermal-hydraulic behavior in a limited series of tests on a PWR. For this reason only extreme timing conditions are recommended for the baseline tests. Additional exploratory tests and supporting quantitative data may be desirable.

Exploratory Tests. It is anticipated that the test program at each PWR plant may incorporate a few additional tests that address unanswered physical questions. Some potential exploratory parameter variations have been described above. To cite a current example of a related, but independent phenomenon, prediction of the rate of feedring drainage with top discharge is now highly uncertain. Direct measurements on a PWR of the drainage rates, both with the plant "cold" and in "hot standby" conditions, could reduce these uncertainties appreciably.

It is also anticipated that some classes of systems or specific plants may require special tests or measurements. For example, it may be necessary to test some CE plants under reactor trip conditions (i.e., without a main feed pump trip) in order to examine the thermal hydraulic behavior and the direct performance of the automatic controls and hardware that ramp the main feedwater flow rate down to approximately 5% of the nominal full-power flow rate.

Any exploratory tests can only be specified on a case by case basis. Plants with unusual systems should be tested most thoroughly.

Instrumentation

It is recommended that the Nuclear Regulatory Commission should encourage the use of better instrumentation and instrument descriptions than have been employed in the past. In particular, an estimated uncertainty of all data (which need be only a crude estimate) should be reported.

The three controlled variables that establish the sequence of events are:

1) vessel pressure, P_{SG},

2) feedwater supply temperature, T_{FW}, and

3) feedwater flow rate, Q_{FW}.

Each of these should be kept as constant as possible during the test. Their values and any transient fluctuations should be reported. Items 1 and 2 should be straightforward (although most test reports have neglected item 2). Special instrumentation may be required to ensure that Q_{FW} is measured accurately. Test reports to date have generally lacked a description of the instruments used to measure Q_{FW}, and have universally lacked an estimate of the uncertainty of the reported values of Q_{FW}.

Three key uncontrolled variables are:

1) vessel water level, h_{SG},

2) feedring/feedpipe water level, h_R, and

3) water temperature in feedring and feedpipe, T_{WR}.

Transient records of h_{SG} using existing instrumentation are routinely obtained and should be reported. Not only the initial level (which is usually established by the test procedure), but also the timing of feedring uncovering and recovering and the final water level are potentially important.

Means to measure the level and temperature of water in the feedring and associated piping during tests with the plant "hot" are not well developed at present. The best means available for both of these functions, consistent with not penetrating pressure barriers and avoiding fragile, costly, and unreliable instrumentation, has been thermocouples on the outside surface of the feedwater piping. It is recommended that these should be arrayed as required to provide some information on water temerature as it enters the drained horizontal pipe run and on water level in the piping. The main limitations of this method are that it cannot discriminate between saturated water and steam, and that the piping introduces thermal lags. Nonetheless, such data have been of considerable value in the past, particularly during the period of gradual refill of the feedring.

Some of the variables that can be measured to describe a waterhammer event (or lack of one) include:

1) pressures in the water,

2) gross piping displacements,

3) noise,

4) piping strain, and

5) piping aceleration.

The ease and cost of use of any device should be weighed against its ability and reliability as a means to 1) determine whether or not there was a waterhammer event, and 2) provide quantitative data describing the behavior of the system. These considerations are discussed briefly below. As a general guideline, the careful use of a few well chosen instruments is advocated in preference to the extensive instrumentation employed in some past tests.

A water pressure measurement at the elbow of the horizontal pipe run from the steam generator nozzle is most needed. Such data have never been competently obtained during a severe pressure transient. The absence of pressure fluctuations at this point is compelling evidence of the absence of a waterhammer event. One practical difficulty lies in finding an appropriate tap at which a pressure transducer can be installed in an existing plant without necessitating additional hydrostatic tests. Installation of the transducer in a convenient distant location is still of value, but is less desirable because the pressure wave can attenuate in an unknown way through compliant pipe systems. The pressure transducer should be capable of withstanding at least 15,000 psi. The transducer and read-out system should have at least 10 kHz response and should be set-up to measure a pulse with timing like that of the Tihange data (Figure 60). Parallel read-outs may be required to record over a range of possible overpressure magnitudes. Although equipment with these capabilities is common, its use is not trivial. Thus, only one or two such pressure measurements should be made.

Crude, rugged maximum-displacement sensors installed at key points throughout the plant (and perhaps left in place after the tests) can demonstrate the occurrence of a waterhammer event and can provide valuable pipe motion data. A very limited number of high-response displacement, acceleration, or strain transducers might also be employed, but these should not be allowed to detract from the careful performance of the tests or the other measurements noted above. Such detailed quantitative data can be useful to confirm structural calculations.

A microphone can be employed to provide audible and electrical read-out of sharp noises; it is likely to be preferable to stationing an observer within the containment. An observer outside containment at the feedpipe juncture may be helpful.

PWR Test Documentation

Test documents that Creare has examined during the course of this study have ranged from poor to good. Even the best test reports have contained significant oversights, some of which are pointed out above. It is recommended that the Nuclear Regulatory Commission encourage improved documentation to support the testing.

Two test documents will usually be needed, a "test plan" prior to the test and a "test report" after the test. The test plan should:

1) discuss the general test objectives,

2) describe the specific hardware and operating procedures employed to avoid unacceptable waterhammer (e.g., J-tubes, piping layout, flow limits, etc),

3) describe the rationale for necessary compromises in choosing the conditions to be tested, and the instrumentation,

4) provide a detailed step-by-step test procedure to be followed by plant personnel, including remarks to clarify terms that may be unique to the plant and comments on the rationale for key procedures, and

5) predict the test results and discuss any uncertainties pertaining to these predictions.

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It is anticipated that Nuclear Regulatory Commission personnel will evaluate the test plan, raise questions on its contents, and suggest revisions.

The test report should:

1) state that planned procedures were followed as intended or point out any deviations,

2) state that the test results were as predicted within the identified uncertainty,

3) provide records of quantitative data displaying the sequence of events and any significant quantitative results, and

4) reference or incorporate the test plan.

Our observation is that test plans have tended to be non-existent or limited to a listing of procedures. In general, it is recommended that more reporting be done before the test in order to avoid costly retests like those required in the past. (Probably less reporting will be needed after the test.) Other general observations are that test rationale and predictions have been lacking, that terminology has often been unclear without detailed knowledge of the specified plant, that instrumentation has been insufficiently described, that key data have not been reported, and that data uncertainties have not been reported.

APPENDIX D

1/4-SCALE HYDRAULIC FACILITY

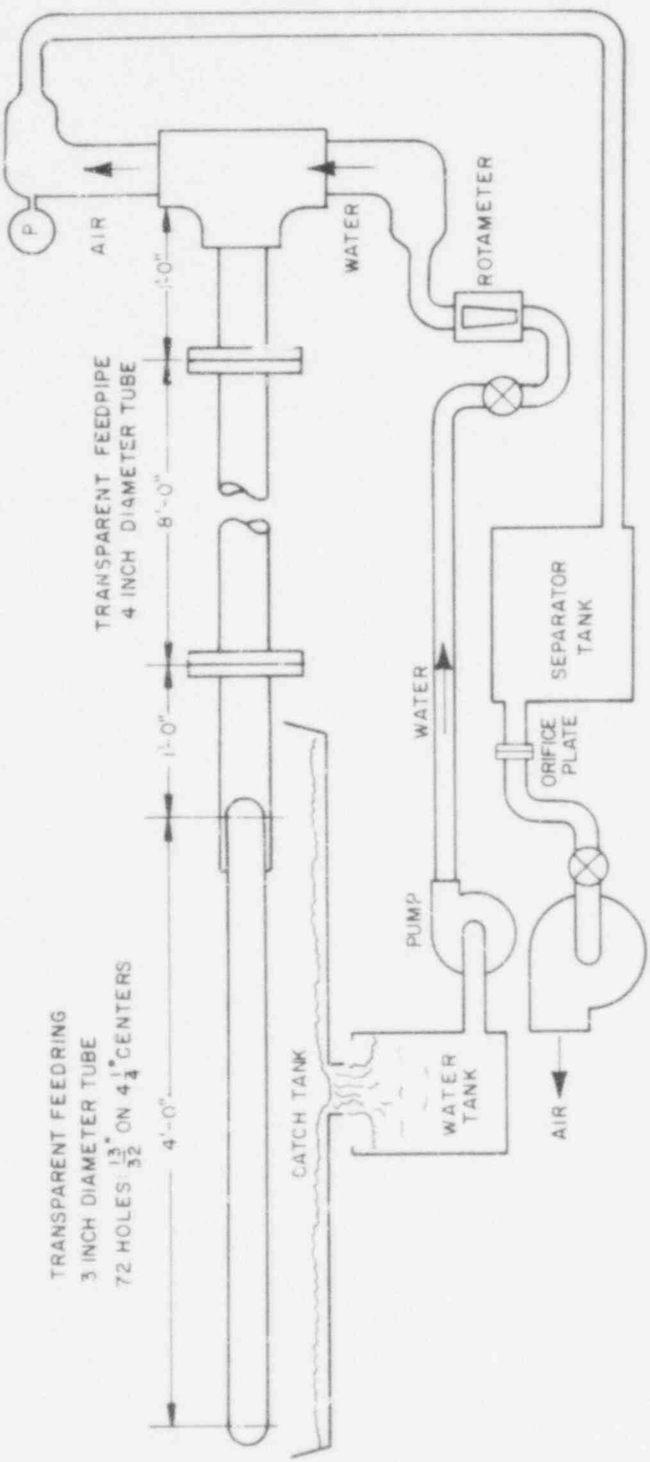
This Appendix presents a brief description of the hydraulic facility and tests in support of the data presented in Section 5.

The facility is shown in Figure D.1. It was designed to isolate the feedring hydraulic and multiphase flow behavior described in Section 5.1 from thermal phenomena and slug formation mechanisms. Tests were performed with waterflow into a stagnant air environment subject to a countercurrent flow of air through the system.

Water maintained in a storage tank below the test section was pumped at rates up to 30 gpm through a metering rotameter, a pipe tee, a long inlet feedpipe, and into the feedring test section. Parallel rotameters with range 0.5 to 5 gpm and 3 to 30 gpm were employed.

Air was sucked back through the system by a parallel pair of blowers with combined 400 cfm capacity and 40 inwg head. The air proceeded through the feedring, feedpipe, tee, a separator (used mainly to stop water slugs), a metering orifice plate, and the blowers. Several orifice plates were fabricated to ASME specifications and employed interchangeably to restrict the pressure drop to the range 2 to 25 inwg which was read on a vertical water manometer.

The feedring was designed to model the main features of typical full size feedrings. It was a simple circular ring as in CE systems, but was not split 180° away from the tee as in CE systems. The main system dimensions are tabulated below in comparison with typical full size dimensions. In particular, the area ratio of total hole area to feedpipe cross-sectional area was maintained. The larger CE holes were scaled to avoid surface tension effects with very small holes, as might occur with 1/4 scale Westinghouse holes (3/16 inch).



SCHEMATIC OF $\frac{1}{4}$-SCALE HYDRAULIC FACILITY

FIGURE D1

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TABLE D.1

FEEDRING DIMENSIONS

	FULL SCALE		Present Facility	SCALE RATIO	
	CE	W		CE	W
Feedpipe Diameter	14.4 in	14.4 in	4.0 in	28%	28%
Ring Pipe Diameter	11.6 in	10.0 in	3.0 in	26%	30%
Ring Circumference	710 in	450 in	157 in	22%	35%
Number of Holes	72	251	72	-	-
Hole Diameter	1.61 in	0.75 in	0.41 in	25%	55%
Area Ratio (Holes to feedpipe)	90%	68%	74%	-	-

Most of the quantitative data were obtained with a straight pipe sparger which was a 1/4-scale version of the sparger tested in the 1/10-scale steam generator model facility described in Section 4. It was a straight, four foot length of 4.0 inch diameter tube with ten 1.0 inch diameter holes on 2.67 inch centers. This sparger had a 63% area ratio (holes to pipe).

The quantitative data are provided in Section 5. They include:

1) water depth measurements in the feedpipe as a function of water flow rate,

2) number of holes flowing water and the water flow rate from each hole as a function of total water flow rate,

3) number of holes flowing water as a function of water flow rate and countercurrent air flow rate, and

4) demarcation of air and water flow rates corresponding to the transition from an orderly flow to a sloshing bubbling flow (see Section 5.1).

Quantitative data were also obtained with the feedring test section, but these were very limited and are not reported herein. The qualitative behavior, namely the occurrences of a transition from orderly flow to a sloshing flow with all holes covered, was similar in both test sections.

APPENDIX E

LIMITING RATE OF VOID COLLAPSE

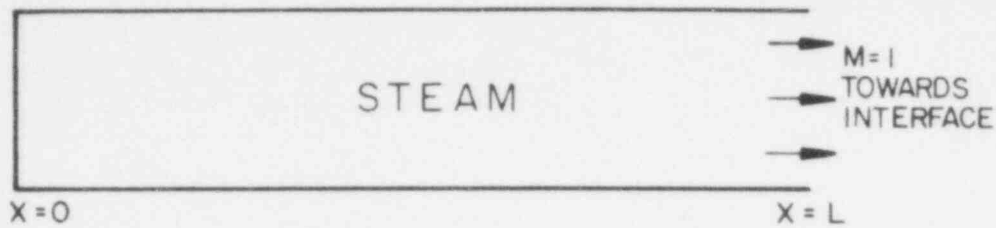
As discussed in Section 3, there is evidence from various sources of an upper limit to condensation rate determined by gas dynamics. This limit is set by the attainment of a Mach number of unity at the minimum cross-section normal to the flow.

If there is cold water lying on the bottom of a pipe, or droplets suspended in the steam as a spray, the area of condensation can exceed the pipe cross-section. This situation may occur in the feed ring, perhaps during a period in which steam is being sucked in through the drain holes and is bubbling through a layer of cold water. Void collapse in the feed pipe, however, probably does not involve the same potential for interfacial mixing except at the highly turbulent leading edge of the slug. We shall therefore develop a model of void depressurization in which steam is removed with a Mach number of unity at one end (Figure E1).

The purpose of this model is simply to predict the time required for the pressure in the void to be reduced from steam generator pressure to zero. This value may be compared with the Tihange data (Figure 60) and any other data that becomes available. A crude estimate of this time, which should have the right order of magnitude, is $2L/a$, the time required for a wave to travel twice the length of the pipe L at the sound speed a in the gas. For the conditions encountered at Tihange, $2L/a \approx 8$ ms, which is a factor of 2 briefer than the time interval measured at Tihange. A refined anlaysis that more accurately models the initial pressure transient in the void is developed in this Appendix. However, it should be recognized that both the crude estimate above and the Tihange data demonstrate that this depressurization time interval is brief by comparison with the total time required to collapse the void. Thus, this Appendix treats a minor detail merely for completeness.

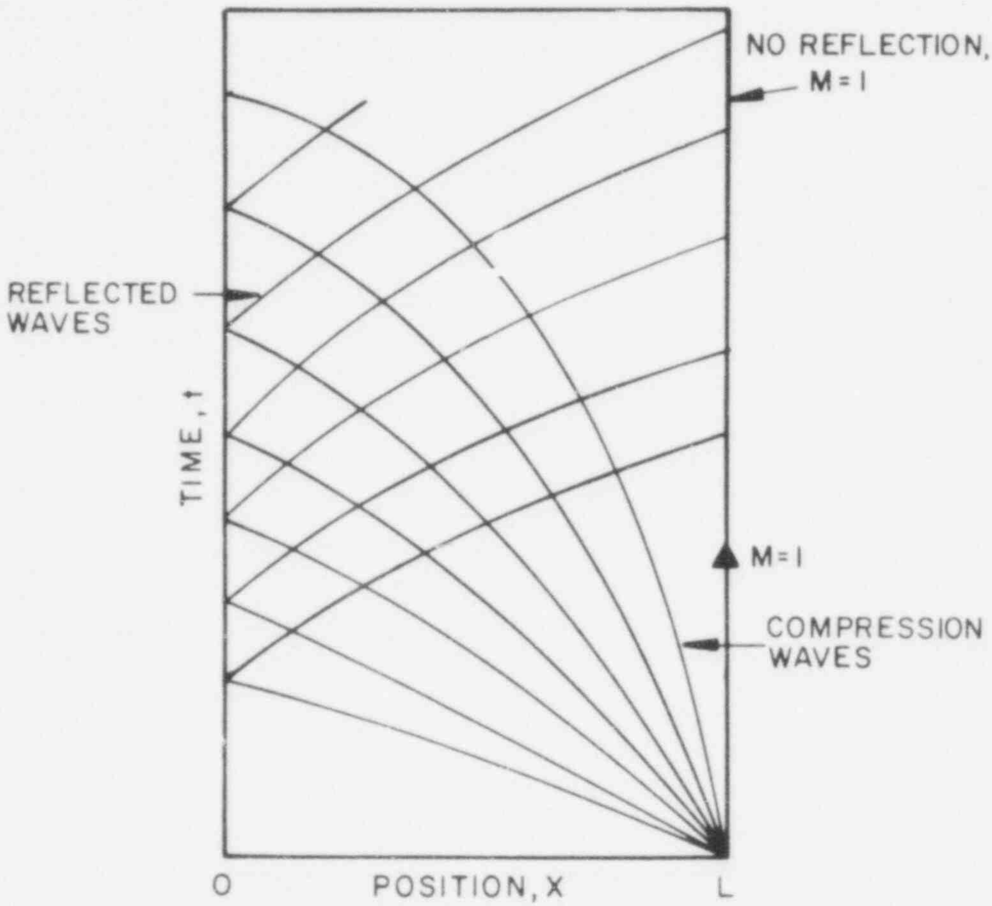
It is assumed that the reader is familiar with standard methods of analyzing unsteady flow of a compressible gas, as described for example by Shapiro.* The method of characteristics could be used. The properties of steam are awkard, but it should be adequate to model the steam dynamics as a perfect gas with constant $k = 1.4$ (air). The characteristic "net" would appear as in Figure E2.

*Shapiro, A. H.; COMPRESSIBLE FLUID FLOW; V2, Chapter 24, Ronald Press Company (1954).



IDEALIZED VOID DEPRESSURIZATION

FIGURE E1



SKETCH OF CHARACTERISTIC NET

FIGURE E2

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For the characteristic that stays at the exit until the first reflected wave gets there we have, in the usual way, (e.g., Equation 24.23 of Shapiro),

$$v + \frac{2}{k-1} a = \frac{2}{k-1} a_0 \tag{E.1}$$

where a_0 refers to the sound speed at the original (stagnation) temperature.

Since $v = a$ at $M = 1$, equation (E.1) gives

$$\frac{a}{a_0} = \frac{2}{2.4} = \sqrt{\frac{T}{T_0}} \tag{E.2}$$

Therefore: $T_0/T = 1.44$, and $p/p_0 = 0.2763$ from the isentropic law. The impact pressure on the liquid for an initial period of about $(2L/a_0)$ is,

$$p + \rho v^2 = p(1+kM^2) = (2.4)(0.2763)p_0 = 0.663p_0 \tag{E.3}$$

The density ratio $\rho/\rho_0 = 0.4$ at $p/p_0 = 0.2763$, therefore the exit mass flux at $M = 1$ is,

$$\rho a = \rho_0 a_0 \cdot \frac{\rho}{\rho_0} \cdot \frac{a}{a_0} = \frac{0.4}{1.2} a_0\rho_0 = 0.333\ a_0\rho_0$$

and is 1/3 of what is obtained by assuming stagnation properties at the exit.

The pressure history of points along the pipe will look like Figure E3.

After a time $t \gg L/a_0$, an "asymptotic solution" should be reached. As a first approximation, the exit pressure is $0.2763p_0$ at a time $ta_0/L = 2$ and the asymptotic solution starts immediately thereafter.

The Asymptotic Solution

After a period of time $t \gg L/a_0$, an asymptotic Mach number profile will be established and the temperature, pressure and velocity profiles will become "similar" as a function of x, with an amplitude which decays with time.

The basic equations to be solved are:

Continuity $$\frac{\partial \rho}{\partial t} + \frac{\partial}{\partial x} (\rho v) = 0 \tag{E.5}$$

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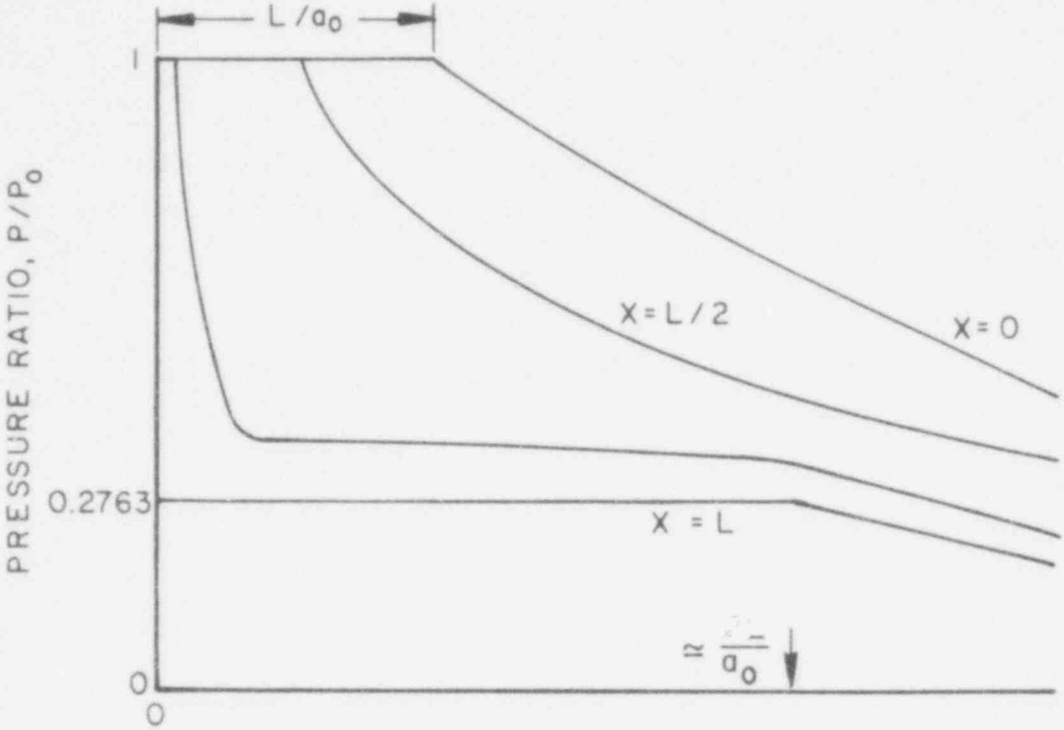

IDEALIZED PRESSURE HISTORIES

FIGURE E3

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Momentum $$\frac{\partial v}{\partial t} + v\,\frac{\partial v}{\partial x} = -\frac{1}{\rho}\,\frac{\partial p}{\partial x} \qquad \text{(E.6)}$$

State $$p = \rho RT \qquad \text{(E.7)}$$

In addition we have the isentropic constraint in various forms:

$$\partial p = a^2 \partial \rho \qquad \text{(E.8)}$$

$$\frac{\rho}{\rho_0} = \left(\frac{T}{T_0}\right)^{\frac{1}{k-1}} \qquad \text{(E.9)}$$

$$\frac{p}{p_0} = \left(\frac{T}{T_0}\right)^{\frac{k}{k-1}} \qquad \text{(E.10)}$$

where ρ_0, p_0 and T_0 refer to some "reference state", not necessarily at stagnation.

Some useful expressions involving the velocity of sound include:

$$M^2 = \frac{v^2}{kRT} \qquad \text{(E.11)}$$

$$a^2 = kRT, \quad a_0^2 = kRT_0 \qquad \text{(E.12)}$$

By various means, which need not concern us at present, it is possible to show that the "similarity" condition is satisfied if we choose

$$v = \frac{a_0 t_0}{t}\; v^*, \quad T = \frac{t_0^2}{t^2}\; T_0 T^* \qquad \text{(E.13)}$$

where t_0 is the, as yet arbitrary, time associated with the "reference state" and v^* and T^* are dimensionless variables that are functions of x only.

Using (E.13), (E.9), (E.10), and (E.8) in equations (E.5) and (E.6), we get, after some algebra:

$$(k-1)\,\frac{dv^*}{dx^*} + \frac{v^*}{T^*}\,\frac{dT^*}{dx^*} = 2 \qquad \text{(E.14)}$$

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$$v^* \frac{dv^*}{dx^*} + \frac{1}{k-1} \frac{dT^*}{dx^*} = v^* \qquad \text{(E.15)}$$

where x^* is the dimensionless space coordinate equal to $(x/a_0 t_0)$.

Equation (E.14) and (E.15) are now to be solved with the boundary conditions: $v^* = 0$, $T^* = 1$ (say) at $x^* = 0$. The solution proceeds as x^* increases until the determinant of coefficients on the left hand sides of (E.14) and (E.15) is zero, i.e., when $v^{*2} = T^*$. Using (E.13) this is equivalent to $v^2 = T\, a_0^2/T_0 = kRT$ and hence to a local Mach number of unity, as expected.

The Mach number is given by:

$$M^2 = \frac{v^{*2}}{T^*} \qquad \text{(E.16)}$$

Solving (E.14) and (E.15) we get:

$$\frac{dv^*}{dx^*} = \frac{\frac{2}{k-1} - M^2}{1 - M^2} \qquad \text{(E.17)}$$

$$\frac{dT^*}{dx^*} = -\frac{(3-k)\, v^*}{1 - M^2} \qquad \text{(E.18)}$$

The solution may now be obtained numerically. It is found that the dimensionless pipe length needed to achieve a Mach number of unity is $x^* = 0.133$. Since the pipe has a physical length L, this means that

$$\frac{L}{a_0 t_0} = 0.133 \quad \text{or} \quad t_0 = 7.52 \frac{L}{a_0} \qquad \text{(E.19)}$$

In other words, if we know L, (E.13) can be replaced by:

$$v = 7.52 \frac{L}{t} v^*$$

$$T = 56.5 \frac{L^2}{a_0^2 t^2} T_0 T^* = \frac{56.5 L^2 T^*}{kRt^2} \qquad \text{(E.20)}$$

and the dimensionless variables defining the problem solution could equally well have been chosen to be:

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$$\frac{vt}{L} = 7.52\ v^* \quad (\text{or } \frac{at}{L} = 7.52\ \sqrt{T^*})$$

$$\frac{kRTt^2}{L^2} = 56.5T^* \qquad (E.21)$$

and

$$\frac{x}{L} = 7.52\ x^*$$

The general solution, in terms of these parameters, is shown in Figure E4.

The temperature ratio from the center of the pipe to the end is about 0.85. In view of (E.10) the pressure ratio is therefore 0.566. The impact pressure on the liquid is equal to $p + \rho v^2 = p\ (1+kM^2)$ and is therefore equal to 1.36 times the centerline pressure (this is because the net rightwards moving gas momentum in Figure E1 is actually decreasing with time).

The rate of pressure decay follows by using (E.13) in (E.10) and shows that $p \propto t^{-7}$, a very rapid rate of depressurization indeed. If we apply the matching suggested earlier, we should set the centerline pressure at $0.2763/0.566 \simeq 1/2\ p_0$ after a time equal to $(2L/a_0)$, then follow a decay according to t^{-7}, so that at any subsequent time

$$p = \frac{1}{2}\ p_0 \cdot \left(\frac{2L}{a_o t}\right)^7$$

The pressure is essentially zero for times much greater than $(5L/a_0)$, say. The measured pressure transient can be compared with that predicted by the above analysis during the period of void collapse.

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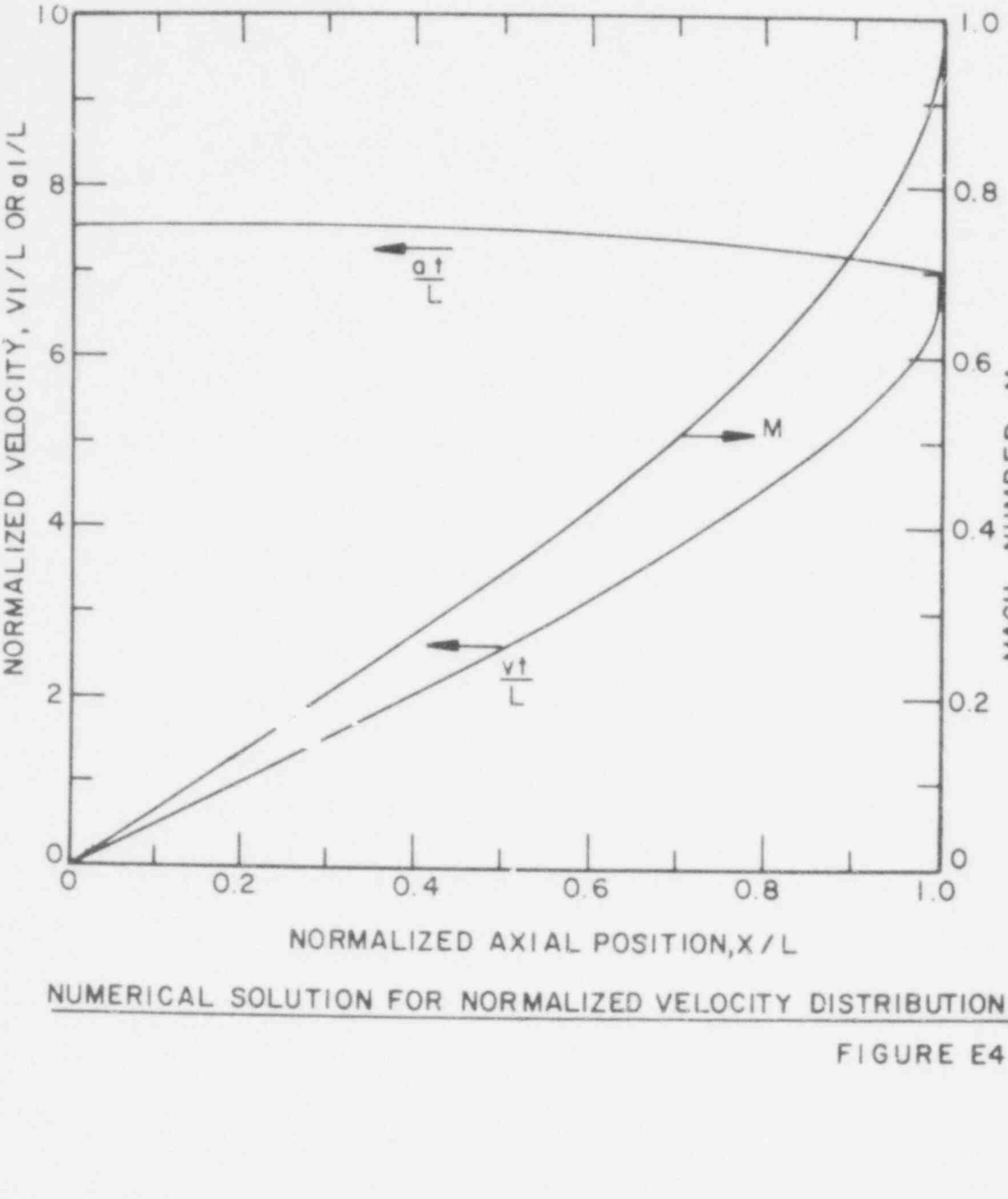

NUMERICAL SOLUTION FOR NORMALIZED VELOCITY DISTRIBUTION

FIGURE E4

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APPENDIX F

EVALUATION OF VENDOR RECOMMENDATIONS

A ranking of the vendor recommendations is described in Section 6.2 and presented as Table 19 of this report. This Appendix provides information in support of this ranking. A general discussion of the technical merits of each vendor recommendation is supplied. Also additional approaches are listed and discussed briefly.

Direct Suppression of Void Formation

If a void never forms, then the problem is eliminated altogether. The means to do this is to supply water in excess of any drainage at all times. This is not usually done with normal bottom discharge feedrings that are calculated to begin to drain at flow rates below approximately 1500 to 2000 gpm per steam generator (which corresponds to power levels below approximately 25% in a 4 loop plant with 750 megawatt electrical output). However, top discharge feedrings (i.e., with bottom holes plugged and J-tubes or comparable pipes installed) drain very slowly through the thermal sleeve clearance, perhaps at only 10 gpm typically with a full pipe, so that as long as some flow is running before the feedrings are uncovered there will be no drainage.* Thus, a small standby pump on line at all times, automatic controls to ensure that the existing auxiliary feedwater pumps are on before the feedring is uncovered, use of suitable signals and accumulators, or comparable means to maintain water flow in excess of drainage rate, in conjunction with top discharge and with adequate redundancy to provide high confidence, are together sufficient to prevent a void from forming at all so that the possibility of further events need not be considered.

In evaluating the potential costs, benefits and reliability of other means to suppress slug formation or reduce slug impact intensity, Creare has examined the vendor recommendations relative to the above solution, which to our knowledge is not employed on any plant.

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*To ensure that the pump is adequate, it may be necessary to determine the probable range of leakage rate and to test it periodically, since significant erosion is possible over planned 40 year plant lifetime.

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The B&W system comes closest to the above solution in the sense that their external feedring does not have a built-in leak at a thermal sleeve as in Westinghouse and Combustion Engineering internal feedrings. Thus, to first order there is no drainage. There may be second-order sources of leakage through check valves or failed components, or due to evaporation in the auxiliary feedring. These drainage means are beyond the scope of the present first-order, generic evaluation, although B&W should supply quantitative evidence of the probability and rate of such drainage. Plants supplied by B&W also have an enviable record of avoiding steam generator waterhammer; only one minor incident has been recorded (at Oconee #1) under low power start-up circumstances that were later indentified in tests and prevented from recurring by maintaining a bypass flow at all times during main feed.

The vendor recommendations are now reviewed in turn from highest-ranked combination to lowest ranked in terms mainly of the effectiveness of each approach as a means to reduce the probabiliity or intensity of water slug impact.

Combination of all Recommendations

In the context of the vendor recommendations the most effective approach is to combine all of the recommendations, i.e., top discharge, short pipes, feedwater flow limit, and rapid reestablishment of feedwater flow. Each contributes to reduce the frequency or severity of steam generator waterhammer in a way that is best clarified by discussing the limitations of systems that do not follow one or more of these recommendations.

To our knowledge, only Beaver Valley is presently following a combined approach employing all of the above recommendations in some form. Tests have not yet been conducted at Beaver Valley at this writing.

Top Discharge, Flow on Soon, Short Pipe

The main advantage of top discharge systems is the greatly reduced drainage rate into the vessel when the bottom discharge holes are plugged and top pipes such as J-tubes are installed to delay feedpipe drainage. The top pipes (i.e., J-tubes) are necessary to prevent rapid draining of the upper part of the feedpipe, as would occur with top discharge holes alone (and the bottom holes plugged). This can occur because the feedpipe is larger than the feedring as shown in Figure 8. In this report, top discharge means: 1) bottom holes plugged, 2) top holes drilled, and 3) top discharge pipes (e.g. J-tubes) installed.

The main objective of top discharge systems is to limit the size of the void in the feedring during abnormal operating transients. Top discharge acts to limit the void size only if the feedwater flow is maintained at all times or established soon after any event that uncovers the feedring.

Some quantification of "soon" is needed. It is our understanding from discussion with the vendors, that auxiliary feedwater flow is routinely established within 30 seconds to a minute after a main feedwater pump trip. Indeed, one Westinghouse requirement is that the system should be designed to ensure full auxiliary feedwater flow within one minute of a lo-lo level sense.* If the water leaks into the vessel at only 10 gpm, then the void will be five to ten gallons, about a cubic foot, which is relatively small compared with the 200 to 400 gallons in the feedring and feedpipe. Unfortunately, drainage rates cannot yet be predicted closely and it may be difficult to do so, as described in Section 5.1. Although immediate restoration of feedwater flow would be desirable to avoid any drainage, limiting the drainage period to one minute can be expected to reduce the magnitude of slug impact overpressure considerably. Accurate quantitative predictions of potential overpressure as a function of drainage period are not possible at present, however.

There is a need to increase confidence level by improving the prediction of leakage rates and by restricting drainage time more definitely through automatic controls or technical specifications with adequate redundancy.

In the rare instances when it is not possible to establish feedwater flow prior to or soon after the feedring is uncovered, amounting to a failure of the automatic control system or hardware, it is highly desirable to have means available to limit the maximum steam void that can develop by having only a short horizontal run of feedwater pipe adjacent to the steam generator.

The data in Section 5 demonstrate that in our experiments with the feedwater sparger drained initially, the combination of top discharge and a short horizontal run of feedwater pipe attached to the vessel nozzle was a much more powerful hardware modification than either approach taken alone. In systems that were drained fully prior to initiating feedwater flow, impact pressures and impulse recorded in our experiments with top discharge and a very short external pipe run were a factor of 5 or 10 less than those recorded with bottom discharge and a long

*Personal communication, Craig Fredrickson, Nuclear Plant Safety Division, Westinghouse Electric Corporation, January 12, 1977.

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external pipe run. Neither top discharge alone nor a very short external pipe run alone reduced either impact pressure or impulse appreciably (relative to bottom discharge and a long pipe run) in similar experiments. Although these data are limited to low pressure experiments in a 1/10 scale facility, they provide the best available information. It is emphasized that these considerations are significant only if the main purpose of top discharge, i.e., reducing drainage and resultant void size, has been defeated.

One Westinghouse operating plant (Trojan) and one Combustion Engineering Plant (St. Lucie) fall into this general category of plants presently operating with top discharge and with very short horizontal pipe runs (essentially the nozzle and a 90° downward turned elbow) adjacent to the steam generator vessel. Robinson #2 is expected to convert and join them. Most feedring steam generator plants now coming on stream and supplied by Westinghouse and Combustion Engineering will fall into this category. Since these are all recent plants, they are likely to have automatic controls to establish auxiliary feedwater flow soon after the feedring is uncovered.

The six operating plants supplied by B&W also satisfy these criteria, although as discussed in Section 2 their implementation is more positive in at least two significant respects. First, the external feedrings on B&W steam generators do not have a thermal sleeve to introduce a leakage path. Secondly, the horizontal pipe run is virtually non-existent since the feedpipe attaches to the lower surface of the external feedring on B&W steam generators. In contrast, the stack up of internal piping, nozzle, and elbow leads to a minimum total pipe length of approximately four feet on Westinghouse and Combustion Engineering systems.

Top Discharge, Flow on Soon, Flow Limit

This combination of recommendations retains the fundamental effectiveness of top discharge systems with the flow on soon as a means to limit significant drainage. It differs from the system above in that the feedpipe adjacent to the steam generator is relatively long (typically 8 to 12 feet) so that a large steam void can develop if the flow is not on soon, or soon enough. Accordingly, some other means are desirable as a backup for the rare instances when feedwater flow cannot be established soon after an event that uncovers the feedring.

One means to reduce the probability of slug formation in drained top discharge systems is to limit the maximum feedwater flow rate, at least until the feedring is refilled. The experiments reported in Section 4 demonstrated that the threshold flow rate for slug formation with top discharge was higher than that for bottom discharge in our study at 1/10 scale and low pressure. The reasons for this effect are explained in Section 5 and stem from suppression of the hydraulic instability that prevails with bottom discharge. Thus, there is some basis for applying the 150 gpm maximum flow limit (recommended by Westinghouse for bottom discharge systems) to systems with top discharge. If such a limit is applied only during the short time required to refill the feedring and associated feedpipe this approach would not be expected to constrain the vessel refill process excessively or cause overheating of the primary coolant. The period of application of the flow rate limit might be established as an absolute period prior to permitting manual control, perhaps two minutes, or might be indicated by rising water level in the vessel since direct measurement of feedring water level is not possible at present.

This combination of approaches is presently employed only at Indian Point #2 and #3 to the best of our knowledge. (A 150 gpm flow limit is employed only if the drainage period exceeds five minutes.) Although there are several other plants with top discharge planned and relatively long horizontal pipe runs (Salem #1, Surry #1 and #2, and Millstone #2), none of these plants have indicated any intention to limit the flow rate during refill of the feedring. Indeed, the installation of top discharge devices has generally been invoked as a means to remove feedwater flow limits, as was done at Trojan. These plants would benefit from flow limits during the limited period of refill of the feedring. Even the top discharge plants with a short horizontal pipe run would be expected to benefit from imposition of a limit on feedwater flow rate during refill of the feedring.

<u>Top Discharge, Short Pipe, Flow Limit</u>

This is a combination of recommendations that does not take credit for rapid establishment of feedwater flow. Thus, it should be assumed that the feedring and adjacent horizontal run of feedwater pipe are partially or fully drained when feedwater flow is established. Nonetheless, this approach still derives substantial advantages from top discharge, namely:

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1) suppression of the hydraulic instability described in Section 5 which has been observed only in bottom discharge systems,

2) refill of the feedring prior to raising the vessel level, which eliminates the possibility of a void being trapped by rising water level,

3) venting of the feedring to the vessel during refill which may tend to reduce the effective size of the void that can collapse, and

4) reduction of the refill period because the feedring can be refilled directly, rather than by refilling the entire vessel; this tends to be favorable because the initial charge of water stored in the piping is likely to be hotter than the cold auxiliary supply.

In addition, this combination of approaches includes a short pipe run, essentially a 90° downward elbow on the steam generator nozzle, which may be equivalent to a two to three foot external horizontal pipe run. It would be preferable to have no horizontal pipe run at all, but the cost of such a retrofit is likely to be substantial. The available quantitative evidence is scanty, essentially that of the Creare experiments at 1/10 scale described in Section 4, but the limited evidence suggests that the combination of a two foot external pipe run (at full scale) together with top discharge may be much more effective than the eight foot pipe run recommended by Westinghouse [7] together with top discharge. The data in Section 4 show that impact overpressure and impulse were reduced by a factor of five to ten in our experimental comparison at 1/10-scale of the behavior with scaled long and short pipe lengths in conjunction with top discharge, and an initially drained feedwater sparger.

In addition to the effects of top discharge and a short pipe run, this combination of approaches employs a flow rate limit during feedring refill which may be expected to further improve the effectiveness of the overall approach for reasons described previously.

There are presently no plants that fall into this category, because present plants with top discharge have all strived for rapid reestablishment of feedwater flow.

Top Discharge, Short Pipe

This more limited combination of approaches is significant because it is the level at which current tests of many new PWRs are being conducted. Specifically, plants coming on stream generally have or plan to have top discharge systems and short horizontal pipe runs. However, the Nuclear Regulatory Commission has prudently not allowed these plants to take credit for the expected performance of automatic and manual means to reestablish feedwater flow rapidly. Instead, a range of drainage times including up to a two hour waiting period has been a standard requirement of the test procedure. This two hour period may be expected to drain the feedring fully in most cases, and according to the PWR vendors is a very long time relative to anticipated system action subsequent to an abnormal occurrence. In addition, test procedures and completed tests have included high flow rates (order 400 to 600 gpm) comparable to maximum anticipated auxiliary feedwater flow because a feedwater flow limit is not employed presently with top discharge systems.

Creare's experiments at 1/10 scale suggest that as a minimum, occasional slug formation and mild impact would be expected during such tests. However, no waterhammer events have been reported to date during nine tests at Trojan and one test at St. Lucie. Similar tests at Beaver Valley are planned for the near future.

Top Discharge, Flow on Soon

Plants with top discharge but with a relatively long feedwater pipe must rely on means to reestablish feedwater flow rapidly subsequent to an event that uncovers the feedring. If such means could be made completely reliable, there would be no need to employ any back-up approach. The question here is whether or not procedures to get the flow on soon are reliable enough. The experience at Indian Point #2 (which also invokes a flow rate limit) is that over the past three years the flow has been reestablished within five minutes after every event that uncovered the feedring, the flow rate limit has not yet had to be employed in practice, and Indian Point #2 personnel have not reported any incidents of steam generator waterhammer since installation of the J-tubes at that plant. The number of times the feedrings have been uncovered in the past three years was estimated roughly as "dozens".

Despite the excellent record of re-establishing feedwater flow rapidly in practice, it is recommended that as a minimum, flow rate limits should be invoked during the period of feedring refill at those plants with top discharge and moderate to long horizontal runs of feedwater pipe extending from the steam generator nozzle.

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There are presently no plants in this category although four plants (Surry #1, Surry #2, Salem #1, Millstone #2) plan modifications which will place them in this category. If the tests to be conducted at these plants are similar to those conducted at other plants with J-tubes, such as Trojan, then the feedwater system will be drained initially and the main means to avoid waterhammer will be defeated intentionally during the test. Creare's experiments at 1/10 scale suggest that these plants may experience a waterhammer event during the test.

Top Discharge (Alone), Flow Limit

The experience with this combination of approaches is very limited for the reasons detailed above. A threshold flow rate was identified in Creare's experiments at 1/10 scale with a top discharge feedring drained initially. Two tests at Indian Point #2 with J-tubes (trips from 35% to 100% power) demonstrated the effectiveness of flow limiting procedures at that plant in these instances. However, the experience is too limited to support this procedure as a primary approach although it has merit in support of other combinations of approaches as described above.

Flow Limit, With or Without Short Pipe

It is our conclusion that the available evidence neither confirms, nor even significantly supports, the claim that a shortened feedpipe (alone) reduces either the probability or intensity of water slug impact. Furthermore, there is no evidence that a shortened feedpipe contributes significantly to the demonstrated effects of very low flow rates. Therefore, for the purpose of discussing the use of a maximum limit on feedwater flow rate, the feedwater pipe length is disregarded. The basis for our conclusions regarding the effects of feedwater pipe length are discussed separately below.

The use of a flow rate limit was based on, and now draws support from, 13 tests at Indian Point #2 and five tests at Doel #2. However, several waterhammer incidents have been experienced at reported flow rates below or near the 150 gpm limit recommended by Westinghouse [7]. These include two waterhammer incidents at Zion #2 and one at Zion #1 at or below 100 gpm feedwater flow rate, one incident at Doel #2 at approximately 40 gpm (the vessel pressure was only 45 psi in the latter instance), and measurement of an extreme depressurization at Tihange (Figure 60) at 176 gpm. The feedwater flow rate has not been measured or reported in most of the documented incidents, and it is suspected that other such cases would emerge if data were available.

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The available evidence indicates that under some circumstances a low flow rate is sufficient to prevent slug formation in bottom discharge systems. In addition to the evidence from PWRs, the 1/10-scale experiments at Creare demonstrate the existence of a threshold flow rate below which slug formation did not occur with bottom discharge. Analysis supported by quantitative data and direct flow observation at both 1/10 and 1/4 scale demonstrate further that the root cause of this behavior during these experiments was a hydraulic instability of the flow through the bottom discharge holes of the feedring, which led to covering of all of the holes by water, trapping of a steam void, and promotion of vigorous steam-water mixing in the feedring. It must be admitted, however, that analysis of some of the slug-formation phenomena is insufficiently developed and confirmed for predicting the behavior at larger scale. Moreover, there is a significant probability of additional effects in PWRs due to the vessel water level rising to cover the holes, although such effects were not observed in the small scale model experiments. For these reasons the effects of feedwater flow rates on slug formation in PWRs are uncertain.

It can be argued qualitatively that low water flow rates might also lead to reduced condensation rates, slower void collapse and lower overpressure at slug impact. However, there is presently no analysis that predicts such an effect and no quantitative data at any scale that support this claim. (In our own 1/10-scale experiments, the scatter in the data reported in Section 4 obscures any possible trend of overpressure magnitude as a function of flow rate, except for the threshold flow rate below which there was no impact.) Therefore, our evaluation of the effectiveness of a maximum flow rate limit during refill of the steam generator vessel has considered only its potential for reducing the probability of water slug formation.

The evidence for supporting the effectiveness of a flow rate limit in PWRs with bottom discharge systems is scanty and equivocal. First, use of a dimensional quantity (the flow rate) rather than appropriate dimensionless parameters is highly questionable because other parameters such as vessel pressure, water subcooling, and flow rate transients may be expected to play a major role in the behavior.* Secondly, the available

*For example, the present analysis and the air-water experiments at 1/10 and 1/4 scale (reported in Section 5) indicate that the dimensionless parameters j_g^* and j_f^* govern some of the multiphase flow phenomena. If these phenomena were solely responsible for slug formation, we would predict that slugs would be more prone to form at low pressure (i.e., low steam density) but that the resultant overpressure magnitudes would be reduced somewhat. Indeed, such an effect of pressure is plausible, but confident predictions at large scale cannot yet be made because additional thermal and transient flow processes also play a role (these phenomena are expected to depend on additional dimensionless parameters). These are some of the reasons that use of a dimensional flow rate limit is highly questionable.

analyses for predicting slug formation need significant further development and confirmation before they can be used with confidence to predict PWR behavior. In particular, condensation rates and resultant countercurrent flow rates cannot yet be predicted with any confidence, and possible combined effects due to rising water level in the vessel are ill understood. Third, the available evidence from PWRs is insufficient to resolve apparent conflicts in the data. Finally, questions can be raised concerning the reliability of adhering to a flow rate limit during an unexpected abnormal event, the reliability of avoiding significant flow rate transients, or even concerning the appropriateness of a flow rate limit in some plants under circumstances when additional flow is needed to remove the decay and residual heat.* Thus, there is good reason to question any approach that relies primarily on limiting the flow rate, regardless of the length of the horizontal pipe run.

Thus, the use of a feedwater flow rate is questionable in principal and demonstrates an equivocal operating history. Its use should be restricted to be only a back-up measure in support of superior approaches until better confirmation of its effectiveness is established.

There are four plants (Ginna #1, Kewaunee, and Point Beach #1 and #2) that have bottom discharge and are using a flow rate limit based on their response to the May 13, 1975 NRC questionnaire [6]. Two waterhammer incidents have been reported at one of these four plants (Ginna #1). Other plants may have employed flow rate limits at this time, but lack a clear statement to that effect. A recent informal survey of 27 operating nuclear power units (November 1976) indicated that a total of nine plants with bottom discharge currently employ a feedwater flow rate limit. Seven of these (D. C. Cook, Ginna #1, Kewaunee, Praire Island #1, #2, and Zion #1, #2) also have short pipes within the Westinghouse guidelines; Point Beach #1 and #2 employ a flow limit but have long pipe runs.

Top Discharge (Alone)

Experience under these conditions is very limited for the reasons described previously. To our knowledge, only one plant (Calvert Cliffs #2) has run a single test of a system functionally equivalent to top discharge (internal standpipes) with a moderate horizontal pipe run (10 feet) and with an appreciable feedwater flow rate (estimated 600 gpm) established following a t hour drainage period. A non-damaging waterhammer was experienced during that test, which also happened to be at relatively low pressure (145 psi). In the absence of confirmation of any kind, this approach cannot be recommended.

No plants currently employ top discharge alone.

*There are preliminary reports of two additional damaging incidents at D. C. Coc I in February 1977 (just as this report was being prepared for rinting). The indicated cause of these incidents is that the operator failed to adhere to the flow rate limit.

Short Pipe (Alone)

Limits on the maximum length of horizontal pipe run adjacent to the steam generator nozzle (e.g., Figure 7) have been advocated mainly as a means to reduce the magnitude of the overpressure due to slug impact in bottom discharge systems. There is presently no evidence of any kind that a short horizontal pipe run alone can reduce the potential for slug formation, although such an effect is plausible. Therefore, our evaluation is limited to the possibility that reducing the length of horizontal pipe run can reduce the overpressure magnitude.

In the most general terms, the volume of the void that collapses and the volume of the water slug are central factors in calculations of the overpressure magnitude and impulse (assuming that condensation proceeds rapidly enough). Crudely, these volumes can be restricted by limiting the drained volume of the system, i.e., the feedring and the horizontal run of feedpipe. In PWRs the feedring volume is equivalent in length to approximately 15 feet (Westinghouse systems) or 30 feet (CE systems) at the usual 16 inch feedpipe diameter. Thus, shortening the feedpipe by a few feet might be expected to have little effect based solely on a qualitative assessment of the change in volume of the portion of the system usually subject to draining.

An earlier analysis by Roidt [5] predicted that if the water slug formed in the horizontal run of feedwater pipe, then the overpressure magnitude was approximately proportional to the length of the horizontal pipe run. Four facts have since been demonstrated:

1. water slugs formed in the sparger, <u>not</u> in the feedpipe, of our 1/10 and 1/4-scale bottom discharge models,

2. the formation of water slugs in the model spargers can be explained and predicted quantitatively by an analysis of the hydraulic instability induced by two phase flow in the sparger,

3. whether or not an effect of feedpipe length is predicted depends on the assumptions made in the analysis of slug dynamics, even if the slug is assumed to form in the feedpipe, and

4. variation of feedpipe external horizontal run from 2 to 24 pipe diameters had a negligible effect on the overpressure magnitude recorded in our 1/10-scale bottom discharge model (during experiments at pressures near one atmosphere with a straight-pipe sparger).

These facts all support the view that shortening the length of horizontal pipe run in PWRs (alone) is likely to be an ineffective approach to reducing the magnitude of overpressure due to water slug impact. It must be admitted, however, that a truly definitive scale model experiment has not yet been performed. The 1/10-scale steam-water experiments were performed with a straight run of pipe simulating the feedring sparger. The 1/4-scale experiments were performed with a linearly scaled model feedring (i.e., a circular tube), tee section, and feedpipe, but only subject to countercurrent flow of air and water. Further scale model studies will be required if it is necessary to improve our understanding of the effects of feedpipe length in steam-water systems.

This information is by no means sufficient to demonstrate conclusively that shortening the feedpipe horizontal run is without value. However, two conclusions can be derived based on the available analytical and scale model evidence:

1. the effectiveness of shortening the feedpipe horizontal run (alone) has not been demonstrated at all, and

2. all of the available model evidence indicates that shortening the feedpipe horizontal run (alone) is ineffective as a means to reduce the probability of slug formation or the magnitude of the overpressure at slug impact.

There is little full scale evidence available to provide further enlightment on the effects of feedpipe length alone. There is some evidence from the tests at Indian Point #2 to suggest that the steam void was trapped by the vessel water level rising to cover the feedring. In this case, the collapse of the void in the feedring would be expected to play a significant role in the behavior. It has been observed that the four incidents involving the greatest pipe deformation have occurred on loops with moderate to long feedwater pipes (10 to 21 feet). This is the strongest evidence available to indicate that shortening the horizontal run of feedpipe may be useful. However, other systems with comparable long pipe lengths have reported only mild or no waterhammer incidents. The two mild waterhammer events recorded during tests at Indian Point #2 occurred in the loops with the shortest pipes, both of which were well within the Westinghouse eight foot guideline. Mild waterhammer events also occurred during two tests at Doel #2 with short pipes simulated by a water trap. The phenomena are simply too whimsical and poorly understood and the evidence is too limited and inconclusive to rely exclusively on any pipe length criterion at the present time.

Although this approach is at the bottom of our ranking, it is presently still a prominent vendor recommendation. Of eight plants with bottom discharge and no flow limit, three have very short pipes (essentially a 90° elbow on the steam generator nozzle) and three more plants have moderate plant lengths (five to nine feet). These plants are San Onofre, Turkey Point #3, Turkey Point #4, Yankee Rowe, Ft. Calhoun #1, and Maine Yankee.

Other Systems in Present Use

Two plants (Haddam Neck and Palisades) have not followed any of the vendor recommendations to our knowledge. Their position has been justified based on analysis and plant history. The piping at Palisades is unusual (it connects directly to the feedring from above) but there is no evidence to support the effectiveness of this configuration.

Two plants (Calvert Cliffs #1 and #2) employ a procedure that is unique to those plants and relies on introduction of auxiliary feedwater from a separate auxiliary sparger. The rate of level rise is restricted to 1.2 inches per minute which is presumed by the utility to prevent slug formation due to the trapping of a void in the drained main feedwater sparger as the level rises due to auxiliary feed. Slug formation in the auxiliary sparger is possible, but no such incidents have been reported to date.

The Calvert Cliffs system has the potential to be superior to other systems that control flow rate (alone), but its unusual hardware configuration makes it difficult to rely on test evidence from other systems. Its low ranking in Table 19 is due to this uncertainty.

General Remark on Evaluation

The subjective discussion above is the basis for the ranking of vendor recommendations displayed on Table 19. It represents our interpretation of the best available technical information. It may be necessary to revise or elaborate on this evaluation if additional evidence becomes available.

Alternative Systems

In any recurring behavior of this type it is possible to conceive a variety of solutions to the problem. Creare takes the position that any approach should be examined on its fundamental merits, tested and developed on small and intermediate scale facilities over a broad range of the relevant parameters and subjected to appropriate quantitative analysis before being implemented and tested on PWRs. Although some of the vendor recommendations were implemented without the advantage of such a building block approach, these recommendations now have been examined in various ways and accordingly have been emphasized in the present work. In contrast, alternatives that have been mentioned during various informal discussions or recommended in the literature are highly speculative and largely undeveloped.

Conception and development of alternate approaches to avoid unacceptable steam generator waterhammer is beyond the present workscope. Several approaches are listed below (in no particular order) and commented on briefly as appropriate. This list is not intended to be complete. Some of these approaches are merely revisions or refinements of existing approaches. Our main intent in providing this list is simply to document possible alternative approaches that have been discussed with various groups during the course of this project. No recommendation of any kind is intended.

Reduce Drainage With Top Discharge

1) Improve thermal sleeve (for top discharge) by closer control on tolerances and assembly, by incorporating a seal of some kind, by using internal sleeves as in CE systems to avoid pressure expansion, or by designing the sleeve to put the clearance at the top of the pipe. No practical improvements have been developed to date.

2) Leave a small pump running. Reliability unknown.

3) Employ accumulators. Possibly prone to failure.

4) Inject all feedwater at bottom of steam generator. Probably not a feasible retrofit.

5) Use better controls to reduce frequency of feedring uncovering. Helpful but probably insufficient.

Prevent Drainage With Bottom Discharge

1) Maintain flow rate above a critical value yet to be determined, but probably less than the 1500 gpm estimated conservatively in Section 5. (Note: there have been no waterhammer events in CE systems when the flow has been ramped down to 5%, i.e., $\approx$ 500 gpm.) May be unreliable; phenomena ill-understood.

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Restrict Slug/Void Formation

1) Use larger flow rates that will run the present pipes full. (Perhaps isolate all but one loop and supply all flow to that loop). Sufficient pump capacity may be unavailable, excess flow may overcool the primary coolant.

2) Redesign piping to employ multiple pipes to each loop that will run full at the minimum anticipated flow rate. Probably very expensive.

Reduce Condensation Rate

1) Inject saturated water from a stored supply. Probably very expensive.

2) Preheat the feedwater, perhaps by redesigning the auxiliary feedwater as an eductor. May not work, probably expensive.

3) Inject a non-condensible gas into the feedpipe prior to refilling the system. Phenomena are ill-understood, may detract from water chemistry control, may be unreliable.

Suppress Void Collapse

1) Reduce subcooled water volume stored near void by eccentrically mounting feedring at bottom of feedpipe. (First suggested by Vreeland [34].) Perhaps also apply a flow rate limit. May be ineffective, phenomena ill-understood.

2) Heat water entering feedpipe horizontal run by mechanical means (e.g., stirrers, vanes, pipes suggested by Vreeland [34]) or by spraying feedwater into piping.

3) Inject auxiliary feedwater through a separate system designed to run full and to heat the feed water (e.g., by spraying it) as it enters the vessel to mitigate collapse of the void trapped in the main feedwater system by rising vessel water level. May be ineffective or costly.

4) Vent the feedring, the piping, or both to the vessel. Modification is unproven, may be costly to retrofit.

5) Vent feedring and eliminate the horizontal pipe run by internal modifications. May be ineffective, may not be feasible, probably expensive.

6) Vent the feedring and use a short pipe, a water trap (as in Doel), a loop seal, or an internal check valve. May be ineffective.

7) Install a "surge pipe" (a straight, capped vertical section) at the elbow (or equivalent) in the horizontal pipe run to the steam generator nozzle. May be ineffective.

8) Impede vertical mixing (e.g., by pipes suggested by Vreeland [34]). Unlikely to be effective.

It is again stated that although any of the above alternative approaches (or possibly other approaches not on this list) might improve the situation, none of these approaches are sufficiently developed at present to recommend their implementation and use in PWRs with high confidence.

February, 1977

List of Operating Plants by Ranking

Below are listed the operating plants corresponding to the categories defined in Table 19 and reviewed in this Appendix.

1. Beaver Valley
2. Trojan
   St. Lucie #1
   Robinson #2
3. Indian Point #2
   Indian Point #3
4. ——
5. ——
6. Surry #1
   Surry #2
   Salem #1
   Millstone #2
7. ——
8. D. C. Cook #1
   Ginna #1
   Kewaunee
   Praire Island #1
   Praire Island #2
   Zion #1
   Zion #2
9. Point Beach #1
10. ——
11. San Onofre
    Turkey Point #3
    Turkey Point #4
    Yankee Rowe
    Ft. Calhoun #1
    Maine Yankee
12. Calvert Cliffs #1
    Calvert Cliffs #2
13. Haddam Neck
    Palisades

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