NUREG-0487

MARK II CONTAINMENT LEAD PLANT PROGRAM LOAD EVALUATION AND ACCEPTANCE CRITERIA

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Division of Systems Safety
Office of Nuclear Reactor Regulation
U.S. Nuclear Regulatory Commission
Washington, D.C. 20555

Summary

This report, prepared by the Office of Nuclear Reactor Regulation, addresses the portion of the Mark II owner's program that provides a generic methodology for establishing design basis LOCA and SRV loads for the lead Mark II facilities (Zimmer, Shoreham, and LaSalle), i.e., the lead plant program. This report includes an evaluation of the Mark II owner's load methodology, a description of load methodologies that we find acceptable for use in the individual plant unique assessments, and the basis for our conclusions.

The load evaluations were conducted by the NRC staff and our consultants, Brookhaven National Laboratory (BNL). The conclusions reached reflect the review efforts of the staff and our consultants. The work of BNL was conducted under the NRC Technical Assistance Contract A3098. In addition, we have made use of pressure suppression related experiments conducted under the auspices of the NRC Office of Reactor Safety Research. These programs include: 1) the Marviken Power Station Tests, 2) small scale tests conducted at the Massachusetts Institute of Technology, and 3) small scale tests conducted at the University of California.

A reassessment of nuclear power plant facilities with the General Electric Mark II pressure suppression containment system design has

been required because, during large scale testing of the subsequent Mark III containment system design, new suppression pool hydrodynamic loads associated with a postulated loss-of-coolant accident (LOCA) were identified that had not been explicitly considered in the original design of the Mark II containment system. These newly identified loads result from the dynamic effects of drywell air and steam being rapidly forced into the suppression pool during a postulated LOCA. Air injection results in a pool swell event of short duration in which a layer or slug of water rises and impacts certain structural components located above the pool. Subsequent steam injection results in oscillatory condensation loads due to the rapid formation and collapse of steam bubbles in the pool. In addition, recent experience at operating plants demonstrated that the dynamic effects of safety/relief valves (SRV) discharges to the suppression pool can be substantial. As in the case of the postulated LOCA event, SRV discharge is characterized by an initial short period of air injection followed by a longer period of steam injection into the pool

This report constitutes the first of two basic elements in the staff's evaluation of the Mark II facilities for pool dynamic loads. The other basic element is the NRC's evaluation of the individual plant unique Design Assessment Report (DAR). The function of the individual DAR's is to evaluate the structures, piping and equipment in each Mark II facility using NRC accepted pool dynamic load methodologies to demonstrate that the facilities can safely accommodate these loads.

This evaluation was conducted under the Nuclear Regulatory Commission's Generic Activities Program (See NUREG-0371). The applicable programs included: Mark II Containment Pool Dynamic Loads (A-8) and Determination of Safety/Relief Valve (SRV) Pool Dynamic Loads and Temperature Limits for BWR Containment (A-39).

In order to establish a common methodology for the prediction of LOCA and SRV pool dynamic loads and to expedite their response to NRC inquiries, the affected utilities formed an ad hoc Mark II Owners Group. The Mark II Owners Group developed a program consisting of a number of analytical and experimental tasks to support their pool dynamic loads application methods. They divided the overall program into two parts: a Lead Plant Program (LPP) intended to be complete during the second quarter of 1978 and a Long Term Program (LTP) scheduled for completion during the second quarter of 1980.

The objective of the Lead Plant Program was to establish design basis (conservative) loads appropriate for the anticipated life (40 years) of each Mark II BWR facility. The program was developed so as to demonstrate that a sufficient understanding of the pool dynamic phenomena exists to establish conservative loads for the lead Mark II plants (Zimmer, Shoreham, and LaSalle). As a result, the LPP includes bounding load specifications for certain loads. These loads were derived from tests and analyses completed in the LPP. The bounding loads were developed from a very conservative interpretation of the LPP tasks. This assures that conservative loads are used in the assessments of the lead plants. Preliminary results from the related and partially completed Long Term Program tasks indicate the conservative nature of the bounding loads of the LPP. In addition to our evaluation of the loads for the lead plants, we have also provided a status report of our review of the four arm quencher (a Safety/Relief Valve discharge device).

The objectives of the Long Term Program (LTP) are to: (1) provide justification through tests and analyses for a reduction in selected design basis bounding loads of the LPP; and (2) provide additional confirmation of certain loads utilized in the LPP. We expect that many of the conservative loads approved for use in the evaluation of the lead plants can be reduced as a result of LTP tasks. However, we will continue to monitor the progress of the LTP tasks to assure conservatism in the current lead plant load specifications. In addition, we will review proposed load reductions in light of the results of the LTP tasks. Our evaluation of the proposed load reductions will be reported in supplements to this Load Evaluation Report.

Based on our review of the information provided by the Mark II Owners Group, we find that sufficient information exists to establish conservative hydrodynamic loads for the plant unique evaluation of the response of the lead Mark II facilities to these loads. A substantial number of Loss of Coolant Accident (LOCA) and safety relief valve related hydrodynamic loads are possible in the event of a postulated accident. We can not arrive at a single conclusion regarding the acceptability of all the hydrodynamic loads proposed by the Mark II owners. We have arrived at different conclusions for the multitude of hydrodynamic loads. Our conclusions fall in one of three categories. Specifically, we have arrived at one of the following conclusions for each of the LPP loads: (1) the load methodology presented by the Mark II Owners Group is conservative and has been adequately defined for use in the individual DAR; (2) the load methodology specified by the Mark II Owners Group is provisionally acceptable, subject to load modifications and constraints developed by the NRC and our consultants; or (3) additional confirmatory information must be provided by the Mark II Owners Group prior to operation of the first Mark II plant.

With the identification of hydrodynamic loads that had not been considered in the original design of Mark II containment systems, several utilities with Mark II contianments have elected to perform modifications to their containments. Typically these modifications have consisted of removing, rerouting and redesigning piping and equipment in the suppression chamber. Other modifications include the use of SRV load mitigation devices, the reinforcement of containment structures, the redesign of the vent and vent support systems and changes in the design and placement of snubbers for primary system and balance of plant equipment in the drywell and the reactor building.

Plant modifications vary from plant to plant as a result of differences in plant designs. The lead plants implemented significant changes based on the bounding loads approach of the LPP. This was done to minimize licensing delays by incorporating additional load and structural margins in the plant over and above that which they consider necessary.

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I. Introduction

A. Problem Definition

There are 11 BWR facilities in various stages of construction with the Mark II containment system that are being built in the United States. None of the domestic facilities with Mark II containments is currently in operation. However, facilities with the Mark II containment in Japan and Italy are currently undergoing initial operational tests. A listing of the domestic BWR facilities with the Mark II containment system is provided in Table I-1.

The original design of the Mark II containment system considered only those loads traditionally associated with design basis accidents. These included pressure and temperature loads associated with a loss-of-coolant accident (LOCA), seismic loads, dead loads, jet impingement loads, hydrostatic loads due to water in the suppression chamber, overload pressure test loads, and construction loads. However, since the establishment of the original design criteria, additional loading conditions have been identified that must be considered for the pressure suppression containment system design.

TABLE I-1

DOMESTIC MARK II UTILITIES AND PLANTS

UTILITY NAME	PLANT NAME
Northern Indiana Public Service Co. Chesterton, Indiana	Bailly 1
Washington Public Power Supply System Richland, Washington	WPPSS-2
Commonwealth Edison Company Chicago, Illinois	LaSalle 1 and 2
Philadelphia Electric Company Philadelphia, Pennsylvania	Limerick 1 and 2
Niagara Mohawk Power Company Syracuse, New York	Nine Mile Point 2
Long Island Lighting Company Hicksville, New York	Shoreham
Pennsylvania Power and Light Company Allentown, Pennsylvania	Susquehanna 1 and 2
Cincinnati Gas and Electric Company Cincinnati, Ohio	Zimmer

In the course of performing large scale testing of an advanced design pressure suppression containment (Mark III), and during in-plant testing of Mark I containments, new suppression pool hydrodynamic loads were identified that had not been included explicitly in the original Mark II containment design basis. These additional loads result from dynamic effects of drywell air and steam being rapidly forced into the suppression pool during a postulated LOCA and from suppression pool response to safety/relief valve (SRV) operation, which is generally associated with plant transient operating conditions. Since these new hydrodynamic loads had not been considered explicitly in the original design of the Mark II containment, the NRC staff determined that a detailed reevaluation of the Mark II containment system was required. A similar reevaluation is being conducted for the Mark I containment system design. The results of the short term Mark I reevaluation were documented in December, 1977.[1]

The Mark II containment design was based on the experimental technology obtained from testing performed on a pressure suppression concept for the Humboldt Bay Power Plant and from testing performed for the Bodega Bay Plant concept.[2,3] The purpose of these initial tests, performed during 1958 through 1962, was to demonstrate the viability of the pressure suppression concept for reactor containment design. Tests were designed to simulate a LOCA with various equivalent piping break sizes up to approximately twice the cross-sectional break size of the design basis LOCA. The tests were instrumented to obtain quantitative information for establishing containment design pressures. Data from these tests were the primary experimental bases for the design and the initial staff approval of the Mark II containment system.

During the large scale testing of the Mark III containment system design in the period 1972 through 1974, new suppression pool hydrodynamic loads were identified for the postulated LOCA event. GE tested the Mark III containment concept in its Pressure Suppression Test Facility (PSTF).[67] These tests were initiated for the Mark III concept because of the geometrical configuration differences between the previous containment concepts and the Mark III design, principally in the utilization of horizontal vents. (Steam had been ejected vertically downward into the suppression pool in the previous BWR containment designs, whereas the Mark III design ejects steam horizontally into the suppression pool). More sophisticated instrumentation was available for the Mark III tests as well as computerized methods for data processing.

It was from the PSTF testing that the short term dynamic effects of drywell air being forced into the pool in the initial stage of the postulated LOCA event were first clearly identified.

In addition to the information obtained from the PSTF data, other LOCA-related dynamic load information was obtained from foreign testing programs[(5)] for similar pressure suppression containments. It was from these foreign tests that oscillatory condensation loads on the vent system downcomers and suppression pool boundaries during the later stages of steam vent flow were identified.

Also, recent experience at operating plants indicated that the dynamic effects of SRV discharges to the suppression pool could be substantial. Although the SRV discharge and the design basis LOCA events may not be directly related, both events are characterized by an initial short period of air injection into the suppression pool followed by an extended period of steam blowdown.

The staff recently issued a report providing a technical update on pressure suppression type containment in U.S. light water reactor nuclear power plants.[(66)] This report includes additional information relative to the historical development of pressure suppression containment technology, a description of the various suppression containment designs, and a discussion of the review areas and technical bases for licensing suppression containments.

B. Program for Resolution

The NRC sent letters to each of the domestic utilities owning BWR facilities with Mark II containment system designs in April 1975*

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*The significant events related to the staff review of the Mark II program are described in Appendix A.

requesting that they provide information demonstrating the adequacy of their containment designs. These letters reflected NRC concerns about the dynamic loads from SRV discharges and the need to evaluate the containment response to the newly identified dynamic loads associated with postulated design basis LOCA events.

The domestic Mark II containment owners formed an ad hoc Mark II Owners Group to develop responses to these NRC requests. The utility owners recognized that the additional evaluation would be very similar for all plants. Formation of the Mark II Owners Group was beneficial because it established a uniform program for responding to the NRC inquiries as quickly as possible.

In November, 1975 we received Revision 0 of the Dynamic Forcing Function Reports (DFFR).[6] This report describes a generic methodology for determining Mark II pool dynamic loads. In addition, in May 1976 we received a copy of Revision 0 of the Mark II containment supporting program report. This report describes four LOCA and six SRV experimental and analytical tasks established to provide the necessary justification to support the methodology presented in the DFFR.

The Mark II owners' program was modified in May 1977 to accommodate the licensing needs of the lead Mark II plants. This modification consisted of dividing the program into a Lead Plant Program (LPP) and a Long Term Program (LTP). The objectives of these programs are described below. Documentation for the LPP was completed in the second quarter of 1978 and documentation for the LTP is scheduled for completion in the second quarter of 1980.

1. Lead Plant Program

The objective of the Lead Plant Program was to establish design basis (conservative) loads appropriate for the anticipated life (40 years) of each Mark II BWR facility. Licensing activities for certain designated Mark II lead plants (Zimmer, Shoreham, and LaSalle) will precede completion of the entire Mark II containment supporting program. Consequently the LPP was developed so as to demonstrate that sufficient information and understanding of the pool dynamic phenomena of interest exist to establish conservative loads for the lead plants. Because of the LPP emphasis on developing loads consistent with the licensing requirements of the lead plants, a bounding interpretation of the available test data was utilized for many of the pool dynamic loads. This ensures that conservative loads are used for the lead plant evaluations.

Those tasks of the Mark II containment supporting program which are a part of the LPP are designated accordingly in Table E-1 of Appendix E. The Mark II owners' specification of the LPP loads are documented in the lead plant reports referenced by Table E-1. The key load definition reports are Revision 2 of the DFFR, the June 1976 GE/4T applications memorandum and the January 1977 GE/4T applications memorandum. Additional load specifications are provided in the LPP documents.

2. Long Term Program

The Mark II Long Term Program (LTP) includes a number of analysis and test programs which extend beyond the LPP efforts. These additional tasks are listed in Table E-1 in Appendix E and described in the Mark II Containment Supporting Program Report.(7) These additional tasks in the LTP complete the Mark II owners' generic supporting program.

The objectives of the LTP are to: (1) provide justification through tests and analyses for a reduction in selected design basis loads of the LPP; and (2) provide additional confirmation of certain loads utilized in the LPP.

The Mark II owners have recently designated selected tasks in their LTP as Intermediate Program, IP, Tasks. The purpose of the proposed IP is to support a reduction in the conservative LPP loads for use in the licensing evaluation of the next group of Mark II plants which follow the lead plants. This next group of plants includes WPPSS-2, Susquehanna 1 & 2 and Limerick 1 & 2. These revised loads for the IP are described in Revision 3 of the DFFR. Loads addressed by the IP include most of the significant loads of the LPP. IP tasks are designated accordingly in Table E-1 of Appendix E. Documentation for approximately 25% of these tasks was complete as of August 1978. The documentation for the remaining IP tasks is scheduled by the Mark II owners for completion in the second quarter of 1979.

3. Design Assessment Reports

The purpose of the generic Mark II owners' program is to provide a generic methodology, where possible, for use in the plant unique analysis of Mark II plants.

All Mark II plants are basically similar (See Section II.A) in that the wetwell is configured in the shape of a right circular cylinder, with the drywell configured as a truncated cone situated directly above the wetwell. The two volumes are connected by a matrix of long vertical vents. However, significant differences exist between individual Mark II containment designs. These differences include: containment construction, characteristic dimensions of the containment, equipment supports and piping and equiment differences. Some of the significant design variations are presented in Table I-2. These plant unique differences make a completely generic evaluation of the Mark II containment design impractical. Therefore, in addition to the Mark II owners' generic programs, each Mark II owner provides to the NRC a plant-unique Design Assessment Report (DAR).

The function of the individual DAR's is to:

1) Describe the plant unique application of the generic Mark II pool dynamic loads methodology;

2) Establish pool dynamic loads excluded from the Mark II owners' generic programs and;

3) Provide an evaluation of the response of the structures, piping and equipment in each Mark II plant to pool dynamic loads to demonstrate that the facility can safely accommodate these loads.

Table I-2
Comparison of Mark II Containment Designs

	WPPSS - 2 (Hanford 2)	LaSalle 1/2	Limerick 1/2	Nine Mile Point 2	Shoreham	Susquehanna 1/2	Zimmer	Bailly 1
Thermal power (MWt)	3323	3293	3293	3489	2436	3293	2436	1931
Type of construction	steel	steel lined, post tensioned concrete	steel lined, reinforced concrete	steel lined, reinforced concrete	steel lined, reinforced concrete	steel lined, reinforced concrete	steel lined, post tensioned concrete	steel lined pre-stressed concrete.
Drywell floor construction	structural connection + seal to wall	structural connection	structural connection	separated + seal to wall	separated + seal to wall	structural connection	structural connection	structural connection
Design Pressures								
Drywell (psig)	45	45	55	45	45	53	45	45
Wetwell (psig)	45	45	55	45	48	45	45	45
Drywell floor (psid)	25	25			30	28	25	25
Volumes (ft^3)								
Drywell, free air	200,500	221,500	234,700	230,000	192,500	239,600	180,000	160,800
Wetwell, free air	143,000	166,400	155,750	143,000	138,500	154,000	93,000	103,000
Pool Water	112,197	142.160	129,800	160,000	81,350	127,000	102,000	73,500
Vent System								
Bracing *	horizontal bracing	horizontal girders	structural restraint	horizontal bracing	horizontal bracing	horizontal bracing	no bracing	--
Number of Downcomers	84/18	98	85	100	88	87	88	60
Diameter (in.)	24/28	23.5	24	23.25	23.25	24	24	
Total length (ft.)	45	49.3	44.5		45.2	43	37.25	

Table I-2
Comparison of Mark II Containment Designs

	WPPSS - 2 (Hanford 2)	LaSalle 1/2	Limerick 1/2	Nine Mile Point 2	Shoreham	Susquehanna 1/2	Zimmer	Bailly 1
Submergence (ft)	12	12.8	11	11	9	11	10	11
Safety Relief Valves								
Total number	18	18	11	24	11	16	13	10
ADS actuated	7	7	5		7	6	6	5
End device *	4-arm quencher	plant unique T quencher			KWU T-quencher	KWU T-quencher	KWU T-quencher	--
Other								
Pool depth (ft)	31	26.5	23	24	18	23	22.5	
Pool Surface, area (ft^2)	4520	4685	5640		4250	5277	4440	
Break area/vent area	0.0105	0.0103	0.0194		0.0167	0.0152	0.0031	0.012
Total vent area (ft^2)	309	295	249	--	259.5	257	276	189
NSSS Product Line (BWR-)	5	5	4	5	4	4	5	5

*added for pool dynamic loads

Individual Mark II owners issued these reports beginning in late 1975 through the first quarter of 1976. These reports will be updated and revised as additional information becomes available from the Supporting Program.

Modifications in the pool dynamic loads from the time Revision 0 of the DFFR was submitted have outpaced the rate at which these modifications were factored into the plant unique DAR's. Major revisions to the lead plant DAR's reflecting loads acceptable to the staff could not be made without impacting the licensing schedules for these plants. As a result of these schedule considerations, the owners of each of the lead Mark II plants have submitted a "closure" report in the form of an amendment to the plant Final Safety Analysis Report (FSAR). The function of the closure report is to document the evaluation of each lead plant against acceptable loads.

As noted above, a few pool dynamic load related areas have been excluded from the Mark II owners' generic program. These loads are provided and evaluated on a plant unique basis. They are noted in Table IV-1. Most of these loads fall in the category of secondary loads. Signficant loads that are being treated on a plant-unique basis are the SRV related loads. The Mark II owners' generic program includes several SRV Tasks that apply to one or more of the Mark II plants. However, all the owners of Mark II lead plants have proposed utilizing SRV quencher discharge devices not included in the owners' generic program. The supporting programs for these devices are sponsored by individual Mark II owners and in the case of the KWU "T" quencher by a sub-group of the Mark II owners. Conservative SRV loads that we find acceptable for use in the plant unique evaluation of Mark II plants are discussed in this Load Evaluation Report. The final load specification for these devices will be made as part of the NRC's A-39 generic program.[8]

The staff review of each DAR and closure report will be covered in each Safety Evaluation Report prepared for the specific plants. This review is conducted as a part of the overall staff review of each individual plant and the results are presented in the plant Safety Evaluation Report published prior to issuance of an operating license.

C. Modifications to Mark II Plants

During the course of the Mark II owners' pool dynamic program, several utilities have performed modifications to their containment to provide additional safety margins. These modifications vary considerably from plant to plant. Table I-3 is a listing of typical modifications. The variations in containment system modifications are a result of three factors:

Table I-3

LISTING OF TYPICAL POOL DYNAMIC RELATED MODIFICATIONS RELATED TO MARK II FACILITIES

1. Ring stiffeners welded to interior walls of steel containment, additional reinforcement added to concrete containment walls and additional reinforcement bars added to drywell floor.

2. Safety/relief valve (SRV) lines rearranged symmetrically around the suppression chamber, horizontal runs of SRV lines rerouted up close to the drywell floor, steel framing system redesigned for support of the SRV lines in the pool and increased thickness of SRV lines.

3. SRV discharge devices changed from ramshead to quencher device.

4. Reactor support pedestal modified by filling inner core with concrete and reinforcing bars. For some designs, large holes in pedestal eliminated.

5. Modifications in the vent and vent support system. Vent bracing system redesigned, and flanges removed, vents shortened and vent wall thickess increased.

6. Equipment and piping in the suppression chamber removed, rerouted and redesigned. Includes gallery platforms, HVAC ducting, vacuum breakers.

7. Drywell steel framing stiffened or replaced, steel framing support modifications.

8. Pipe restraints in the suppression chamber redesigned.

9. Snubbers for primary system and BOP equipment in the drywell and reactor building upgraded or relocated in a number of locations.

10. Additional suppression chamber instrumentation for SRV in-plant testing and pool temperature monitoring.

1) signficant design differences between individual Mark II plants;

2) the magnitude of the variation in pool dynamic loads from plant to plant; and

3) the construction status for each Mark II plant.

The first two factors were previously discussed. The third factor arises because the 11 domestic Mark II plants were at different stages of construction at the time pool dynamic concerns were first identified. Several of the owners of lead Mark II facilities have implemented significant changes based on the bounding loads approach of the LPP, to minimize potential licensing delays by incorporating additional load and structural margins in the plant over and above those that they consider necessary. Owners of later Mark II facilities might elect not to include all of these modifications if they are shown to be unnecessary utilizing the anticipated reduced loads of the LTP.

D. Load Definition Criteria

As a result of the staff review including the efforts of our contractors at BNL of the many test data and analytical results, the staff has developed appropriate and acceptable criteria suitable for use by the lead Mark II plant owners. The staff intends to assess the safety of the design using these criteria as given in Appendix D to this report. Modifications on the basis of suitable justification can be accommodated when appropriate. It is expected that some changes will result as further evaluation and information become available for the designated intermediate plants.

II. The Mark II Containment Design and Its Hydrodynamic Loads

A. Description of the Mark II Containment System Design

The function of the Mark II containment system is to condense the steam released during a postulated LOCA event, to limit the release of fission products associated with an accident and to serve as a source of water for the Emergency Core Cooling System (ECCS).

The Mark II containment system includes a primary containment structure and a secondary containment building. The primary containment structure is a vapor suppression system which encloses the reactor vessel, the reactor coolant recirculation loop, and other portions of the Nuclear Steam Supply System (NSSS). The primary containment structure, which is the subject of this report, is shown in Figure II-1. It consists of a drywell, a suppression chamber, a vent system connecting the drywell to the water pool, isolation valves, containment cooling systems and other service equipment. An additional structure called the reactor building surrounds the primary containment. This building serves as a secondary containment.

The drywell is configured in the shape of a truncated cone, closed by a dome. The suppression chamber is a cylindrical structure located directly below the drywell. The primary containment may be of reinforced concrete, prestressed concrete or steel construction.

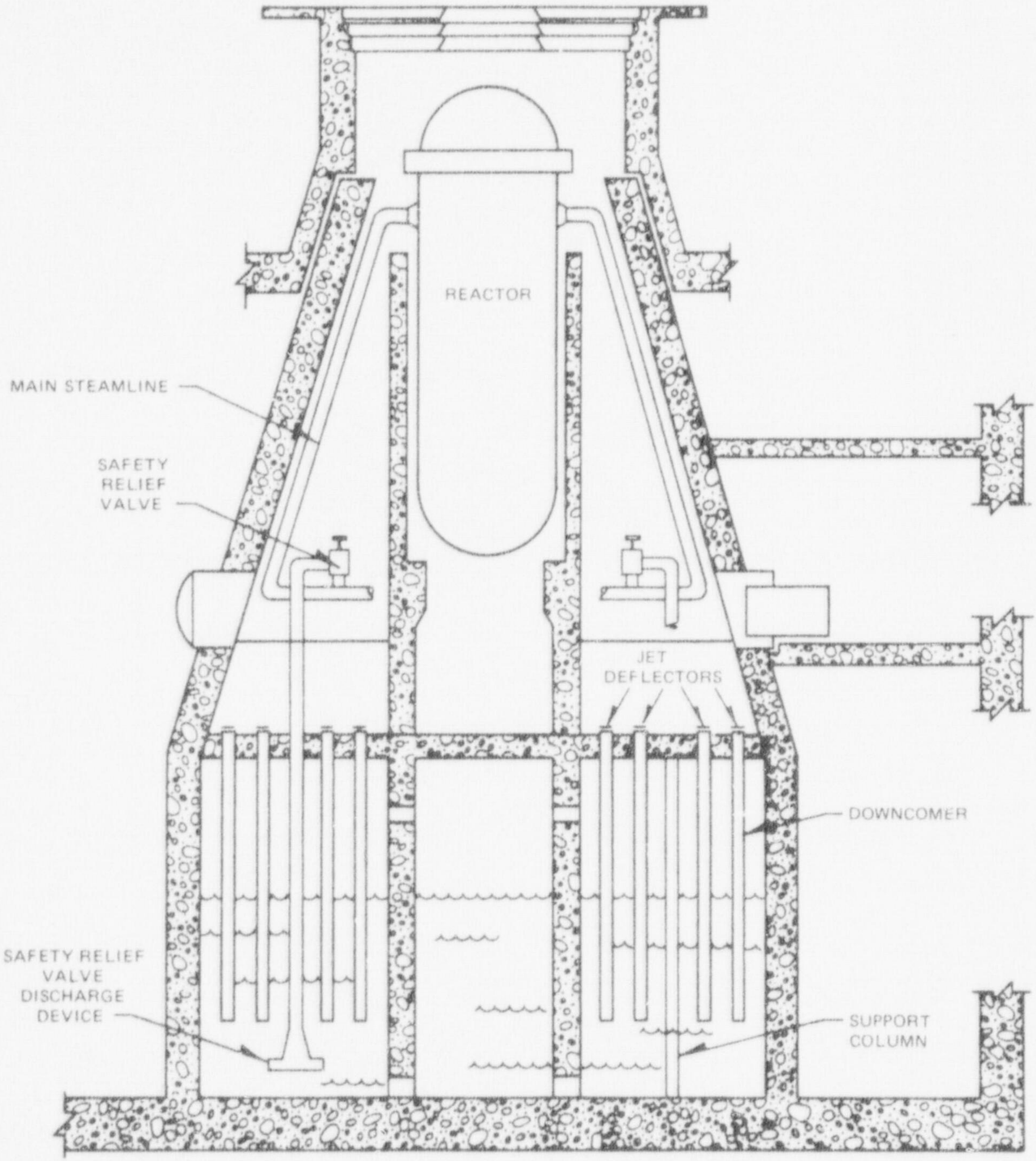


Figure II-1 Typical Mark II Pressure Suppression Containment

The suppression chamber is separated from the drywell by a structural diaphragm. Loadings are transmitted from the suppression chamber to the reinforced concrete foundation slab of the reactor building. The drywell-to-wetwell vents consist of circular downcomer pipes which project downward from the diaphragm into the suppression pool. Dimensions of the Mark II plant designs vary from plant to plant. Table I-2 presents a comparison of the containment design features for the domestic Mark II plants.

In the event of a postulated LOCA, water and steam from the reactor system would be released into the drywell atmosphere. As a result of increasing drywell pressure, a mixture of drywell atmosphere, steam, and water would be forced through the vent system into the pool within the suppression chamber. The steam vapor would condense in the suppression pool. Noncondensible gases and fission products would be collected and contained in the suppression chamber air space. The initial drywell atmosphere would transfer to the suppression chamber and pressurize the chamber. At the end of the blowdown subcooled ECCS water from the postulated pipe break would rapidly quench the steam within the drywell reducing the drywell pressure. The suppression chamber would be vented to the drywell through installed vacuum breakers to equalize the pressures between the two vessels. The ECCS cools the reactor core and transports the heat to the water in the suppression chamber. Cooling systems are provided to remove heat from the water in the suppression chamber, thus providing continuous removal of decay heat from the primary containment under accident conditions following the initial deposition of energy to the suppression pool from the blowdown.

In addition to the suppression function under postulated LOCA conditions, the Mark II containment provides a similar pressure suppression function for safety/relief valve discharge which may occasionally occur to control pressure in the BWR reactor vessel and other components of the NSSS during transient events. The safety/relief valves, which relieve the NSSS pressure, discharge into pipes which are routed to the suppression chamber and terminate below the pool water surface. The mechanism of this pressure suppression function is similar to that for the postulated LOCA event.

B. Description of LOCA - Related Hydrodynamics Phenomena

The following is a qualitative description of the various phenomena that could occur during the course of a postulated design basis LOCA in a BWR with the Mark II containment system and a description of the hydrodynamics loads which these phenomena could impose upon the suppression chamber and related structures.

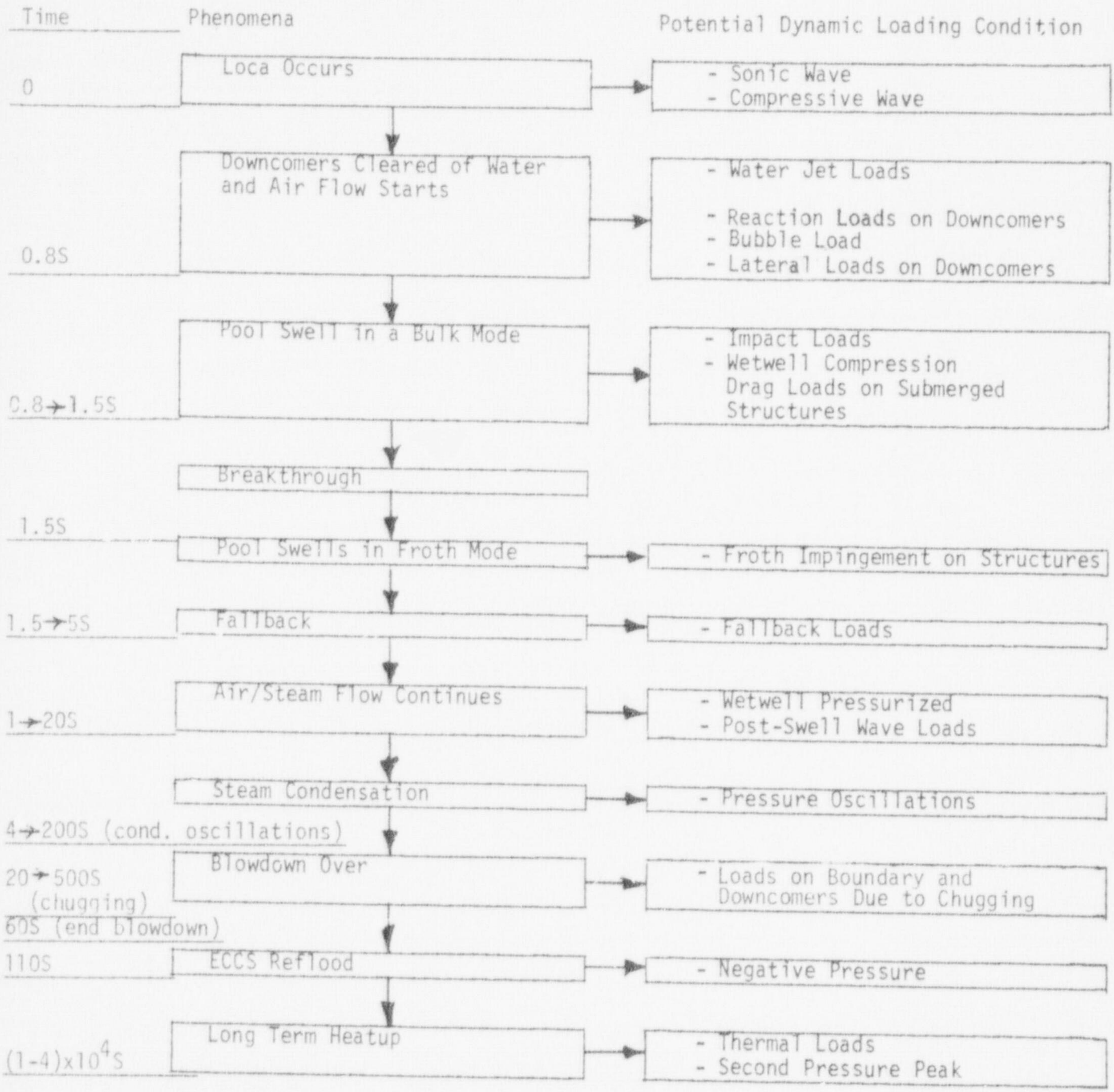
Figure II-2 shows the sequence of events following a postulated LOCA, the potential loading conditions associated with these events and typical times associated with each event.

Assuming the instantaneous rupture of a steam or recirculation line, a sonic wave exits the broken primary system pipe and expands into the drywell atmosphere. This wave rapidly attenuates as a front expanding spherically outward into the drywell. The wave then enters the vent system, progressing into the pool.

Since there would be a very rapid drywell pressure increase associated with the postulated LOCA, a compressive wave could be formed in the water that initially occupies the downcomers. Prior to clearing of this water from the downcomers, this compressive wave could propagate through the suppression pool and result in a dynamic loading on the suppression chamber and structures within the suppression pool.

As the drywell pressure increases, the water initially in each main vent downcomer accelerates into the pool clearing the vents of water. During this water clearing process, a jet forms in the suppression pool which creates water jet impingement and drag loads on structures near the vent outlet and on the suppression pool basemat. In addition, jet formation can occur asymmetrically leading to lateral reaction loads on the vents. During the vent-clearing transient, the diaphragm will be subject to a downward

Figure II-2 LOCA Sequence of Events



Peak drywell and wetwell pressure @ 50S

Maximum diaphragm Δ P down @ 0.7S

Maximum diaphragm Δ P up @ 2.0S

pressure differential. Immediately following vent water clearing, a bubble of air from the drywell starts to form at the vent exit. The steam in the air/steam mixture flowing through the vents condenses in the pool. As the air bubble forms, its pressure is nearly equal to the drywell pressure at the time of vent clearing. This results in a pressure disturbance in the pool. The dynamic bubble pressure is geometrically attenuated through the suppression pool water and results in loads on submerged structures and on the suppression pool structure.

When the air flows from the drywell through the vent system, the bubbles initially formed expand. Continued injection of drywell air and expansion of the air bubble results in a rise of the suppression pool surface. Structures close to the pool surface experience impact loads as the rising pool surface strikes the lower surface of the structures, followed by drag loads as the pool surface continues to rise past the structures. In addition, the rising pool surface compresses the air in the upper half of the suppression chamber causing a net upward load on the diaphragm.

As the pool surface rises, the air bubble collapses, terminating the potential for the upward loading, and the water slug breaks up. Breakup of the slug occurs at a height of about 1.5 times the initial submergence of the vents. Subsequent pool swell evolves into a two-phase air water froth. There is no substantial froth pool swell due to the compression of the air space above the pool surface. Gravity induced fallback of the froth returns the pool to the original pre-LOCA elevation. The pool swell transient associated with drywell air venting to the pool, typically lasts for about five seconds. The volumes of the drywell and suppression chamber are such that purging of the drywell air into the suppression chamber will lead to a static pressure increase of about 35 psig.

Following air carryover, there will be a relatively long period of decreasing steam flow through the vent system. During this time, vent flow occurs in three distinct phases:

1. High mass flux, characterized by nearly steady-state condensation;

2. Medium mass flux, characterized by periodic variations in condensation rate; and

3. Low mass flux chugging, characterized by intermittent condensation.

During steam condensation, the vents experience a lateral loading caused by random movement of the steam-water interface. The magnitude of this load varies with steam mass flux and suppression pool temperature. Maximum lateral loads in a postulated LOCA occur toward the end of blowdown. The same condensation phenomenon also results in pressure loadings on the suppression pool boundary.

Shortly after a postulated LOCA, the ECCS will automatically pump condensate water and/or suppression pool water into the reactor vessel. This water floods the reactor core and subsequently cascades into the drywell through the postulated break in the pipe. The time at which this will occur depends upon break size and location. Because the drywell will be full of steam when the vessel is reflooded, the sudden introduction of water into the drywell causes steam condensation and depressurization. As the drywell pressure falls below the suppression chamber pressure, the vacuum relief system will allow air from the suppression chamber to reenter the drywell. Eventually, sufficient air will return to equalize the drywell and suppression chamber pressures.

Following vessel flooding and drywell/suppression chamber pressure equalization, suppression pool water will be continuously recirculated through the core by the ECCS pumps. The energy associated with the core decay heat will result in a slow heatup of the suppression chamber pool. To control suppression pool temperature, operators will activate the suppression pool cooling mode of the Residual Heat Removal (RHR) system. After several hours, the RHR heat exchangers will terminate the suppression pool temperature increase.

Drywell and suppression chamber pressure increase is associated with this post-LOCA suppression pool temperature increase; however, the resultant maximum will not exceed the pressures that occur during the short-term blowdown phase of the accident.

The magnitude and timing of LOCA pool swell and steam condensation pool dynamic loads depends on the break size. A spectrum of LOCA break sizes was considered in order to establish the limiting design conditions for Mark II containments. The LOCA conditions which were considered include the following accident conditions:

1. Design Basis Accident (DBA), a double-ended break of a recirculation line or main steam line.

2. Intermediate Break Accident (IBA), a break such that the high pressure subsystem of the ECCS cannot maintain reactor water level; however, vessel depressurization does not occur. An IBA corresponds to a liquid or steam line break of about 0.1 ft^2.

3. Small-Break Accident (SBA), a break that will not result in reactor depressurization due either to loss of reactor fluid or automatic operation of the ECCS.

The DBA is the design limiting case for the pool swell related pool dynamic loads including jet, drag, impact and fallback loads. The IBA and SBA cases have a much lower rate of drywell pressurization. Therefore, for these cases the IBA and SBA pool swell loads are correspondingly lower. However, LOCA related steam loads can occur over a wider spectrum of breaks since the maximum condensation loads occur at low vent mass flux. Condensation oscillations and chugging may occur over an extended period of time for small breaks as a result of the reduced reactor vessel depressurization rate compared to a DBA.

C. Description of SRV-Related Hydrodynamic Phenomena

BWR plants are equipped with safety/relief valves (SRV) to control primary system pressure. Small pressure variations can be controlled by changing power level and/or load. However, more rapid transients such as a turbine trip cannot be handled by such means. For these transients, SRV's mounted on the main steam line are actuated to divert either a portion or all of the generated steam into the suppression pool. These valves are actuated at individual pre set pressure levels or by an external signal (ADS). The series of SRV's are indivdually set at pressures over a range, such that only the required number of valves to control the pressure transient will actuate. Upon SRV actuation, the air column within the partially submerged SRV discharge line is compressed by the high pressure steam and, in turn, accelerates the water column into the suppression pool. The water jet or jets thus formed create pressure and velocity transients which are manifested as drag or jet impingement loads on submerged structures.

Following water clearing, the compressed air is also accelerated into the suppression pool forming a high pressure air bubble. This bubble executes a number of oscillatory expansions and contractions before rising to the suppression pool surface. The associated transients again create drag loads on submerged structures as well as pressure loads on the submerged boundaries. These loads are referred to as SRV air clearing loads.

Following the air clearing phase essentially pure steam is injected into the pool. Experiments indicate that the steam jet-water interface which exists at the discharge line exit during this phase is relatively stationary so long as the local pool temperature is low. Thus, the condensation proceeds in a stable manner and no significant loads are experienced. Continued steam blowdown into the pool will increase the local pool temperature. The condensation rates at the turbulent steam/water interface are eventually reduced to levels below that needed to readily condense the discharged steam. At this "threshold" level, the condensation process becomes unstable; i.e.,: steam bubbles are formed and shed from the pipe exit, the bubbles oscillate and collapse giving rise to severe pressure oscillations which are imposed on the pool boundaries. Current practice to deal with this phenomenon in BWR plants is to restrict the allowable operating temperature envelope via the Technical Specifications such that the threshold temperature is not reached. This restriction is referred to as the pool temperature limit.

III. Hydrodynamic Load Evaluation

A. Introduction

In this section, we describe our evaluation of the methodology employed by the Mark II Owners Group in their Lead Plant Program (LPP) to quantify the hydrodynamic loads associated with the suppression pool dynamics following either a postulated design basis LOCA event or SRV discharge. These dynamic loads, in combination with the LOCA-related loads previously identified for each plant in its Final Safety Analysis Report (FSAR), are utilized by the owners of the lead Mark II plants to perform a structural assessment of the containment systems as reported in their Design Assessment Report (DAR).

In addition to the LPP, the Mark II owners are conducting additional tests and analyses as part of their Long Term Program (LTP). We anticipate that the LTP tasks will confirm the large conservatisms in many of the loads in the LPP and will provide a basis for the reduction of selected LPP loads. Our review and evaluation of the proposed LTP tasks is also included in this section.

Each of the dynamic loads identified for the Mark II containment system is evaluated separately in the subsections below. The evaluation includes the data base and analytical tools that were used to establish the individual load magnitudes. Further description of the test programs and analyses which serve as the basis for the load specification is given in Appendices B and C, respectively.

B. LOCA - Related Hydrodynamic Loads

1. General Considerations

The phenomena and dynamic loads which can occur following a postulated design basis LOCA event in a Mark II containment were previously identified (See Section II.B). These loads have been reviewed to determine which were significant, based either on their magnitude as compared with non-hydrodynamic loads or in terms of the structural response of the containment system. These loads required detailed consideration. Such loads are designated as primary loads. The remaining loads are considered to be of secondary importance. Our evalation of the secondary loads is also provided in this report. The primary and secondary LOCA loads for the lead plants are identified below.

a. Primary Loads

1) Wetwell Structural Loads

The wetwell structural loads include:

a) Jet Loads on the Basemat During Vent Clearing;

b) Pressure Loads on the Submerged Walls (Including Pedestal) During Vent Clearing;

c) Pressure Loads on the Basemat and Submerged Walls During Air Bubble Formation and Pool Swell;

d) Pressure Loads on Walls Above Pool Surface During Pool Swell Due to Airspace Compression;

e) Pressure Loads on the Diaphragm During Pool Swell Due to Airspace Compression; and

f) Pressure Loads on Submerged Walls and the Basemat During Steam Condensation and Chugging.

2) Wetwell Component Loads

The wetwell component loads include:

a) Lateral Loads on Downcomers During Steam Condensation and Chugging;

b) Drag Loads on Submerged Structures During Vent Clearing, Air Bubble Formation, Pool Swell, Fallback and Steam Bubble Collapse;

c) Impact Loads on Structures Located Above Initial Pool Surface During Pool Swell.

b. Secondary Loads

The secondary loads for the Mark II containment system include:

1. Sonic Wave Loads;

2. Compressive Wave Loads;

3. Post Swell Wave Loads;
4. Seismic Slosh Loads;
5. Fallback Loads on Wetwell;
6. Thrust Loads;
7. Friction Drag Loads; and
8. Lateral Loads on Downcomers During Vent Clearing.

With the exception of the drag loads on submerged components, the staff's evaluation of each of the primary dynamic loads listed above is presented below. The drag loads are evaluated in Section III.D where they have been combined with similar loads which are SRV-related. Our review of secondary loads is presented in Section III.E.

2. <u>Vent Clearing Loads on Submerged Boundaries</u>

The submerged jet formed by expulsion of the water leg in the downcomers during vent clearing creates jet impingement loads on the basemat. In addition, the induced pressure transients in the pool outside the jet proper result in pressure loads on the submerged wetwell walls.

The methodology proposed by the Mark II Owners Group for specification of these loads models the jet as both steady-state and axi-symmetric. Thus, velocity attenuation and spreading of the jet are estimated by conventional methods.[9] The proposed methodology further assumes that the initial jet velocity is equal to the maximum vent clearing velocity. Total momentum transfer is applied to define an overpressure at the basemat relative to ambient conditions.

Based on this methodology, a generic value of 33 psi overpressure was obtained for application to all lead Mark II plants. This value corresponds to a vent clearing velocity of 60 fps and a vent exit-to-basemat clearance of 10 feet. Although the loading is dynamic, this overpressure is applied statically as a uniform load on the basemat and wetwell walls below the vent exit with a linear attenuation to zero at the pool surface.

The staff finds that the proposed methodology is conservative with respect to the prediction of maximum overpressure. First, the selected values of vent clearance and vent clearing velocity represent bounding values of these parameters for the Mark II plants. Also, the corresponding specified value of overpressure has been found to be well in excess of any observed during the 4T test series (maximum of 10 psi) as reported in Reference 15. A comparable trend was observed during the EPRI tests.[10] With regard to the static application of the load, the staff finds this acceptable in view of the fact that the characteristic period of loading is large (about 1 second) relative to the corresponding natural periods for typical Mark II containment structures (30-50 msec). The 4T test results suggest that uniform application of the overpressure on the boundaries is appropriate. Only slight attenuation from the peak values recorded directly below the downcomer exit was observed on the boundaries during the vent clearing phase.

Accordingly, the staff concludes that the Mark II Owners Group methodology for estimating loads on the submerged boundaries during vent clearing is acceptable.

3. Pool Swell Loads

After the downcomers have been cleared of water, a mixture of air and steam from the drywell is driven thru the downcomers into the suppression pool. Initially, the noncondensible air, which is at an elevated pressure relative to local hydrostatic, forms a quasispherical bubble at the end of each vent. The individual bubbles grow due to this overpressure and the continuing inflow of noncondensibles. Eventually, the indivdual bubbles coalesce with bubbles from adjoining downcomers or contact the submerged boundaries. This coalescence proceeds until, as has been observed experimentally,[10] a blanket of air of relatively uniform thickness extends across the entire suppression pool. The water ligament or slug above this air blanket is accelerated upward by the continuing expansion of the air bubble. Eventually the upward motion is slowed by the increasing pressure in the air space above the pool. This deceleration results in breakup of the water slug allowing communication of pressure between the bubble and wetwell air space. The sequence of events which occur between the initial formation of the air blanket and breakup of the water slug, including the subsequent froth activity, constitutes the pool swell phase of the LOCA blowdown. Evaluation of the loads associated with this phase is given in the ensuing subsections. The loads which are associated with the earlier discrete air bubble formation phase as well as those related to the fallback event which occurs after breakthrough are addressed separately in Section III.D.

a. Pool Swell Analytical Model

To develop load specifications for the wetwell and wetwell components during pool swell, a number of parameters related to the motion of the water slug must be established. These include the slug velocity and acceleration as a function of elevation, the pressure in the air bubble driving the pool upward and the pressure in the wetwell air space which is compressed by this upward motion. To define these parameters, the Mark II Owners Group employ the General Electric Pool Swell Analytical Model (PSAM).

A description of the PSAM is presented in Appendix C.1. The model has been qualified by a series of comparisons between predictions and experimental results. The experimental results which were employed include full scale single cell data obtained in the 4T test facility (Appendix B.1) and the sub-scale multivent tests conducted by the Electric Power Research Institute (EPRI-See Appendix B.3). Comparisons have also been made with measurements obtained during the second series of the Marviken Power Station tests (Appendix B.7). These comparisons are documented in References (11), (12) and (13). In addition to the above, independent comparisons have been made by our consultant, the Brookhaven National Laboratory (BNL). The results of BNL's study are reported in Reference (14).

Based on a review of these comparisons the staff concludes that the PSAM is provisionally acceptable for the development of conservative Mark II pool swell loads. The modifications which will be required for complete acceptability are delineated below. Additional confirmation of the conservatism of the PSAM with the required modifications will be provided during the LTP by additional comparisons of predictions with the test results from the EPRI sub-scale single vent test program (Appendix E).

1) Air Bubble Pressure

This parameter is used to develop the specification for submerged boundary loads during pool swell. Good agreement between the PSAM prediction and EPRI measurements throughout the transient up to bubble breakthrough has been observed. In these tests air blowdowns were utilized for drywell charging. On the other hand, the model consistently overpredicted this parameter for the 4T tests in which steam blowdowns were employed. In terms of maximum values, for example, the bubble pressure rise over ambient was overpredicted by as much as 25%. It is important to note that the experimental and predicted maxima did not occur simultaneously. In general, the measurements indicated that the bubble pressure was maximum at vent clearing followed by a monotonic decrease. The PSAM, on the other hand, showed a distinct increase in bubble pressure above the initial value during the transient. Thus, the degree to which the model overpredicted the measured pressure history tended to increase during the blowdown up to breakthrough. This conservatism can be attributed to the "all air" assumption used in the PSAM. We conclude that the use of the PSAM for predictions of the air bubble pressure history during pool swell is acceptable.

2) Pool Elevation

This parameter is used together with the corresponding values of pool velocity and acceleration to develop drag and impact loads on submerged wetwell components during pool swell. The Mark II Owners Group methodology employs the PSAM to prescribe the pool elevation transient only during the acceleration phase of the pool swell event, i.e., up to maximum pool velocity. Thereafter, it is assumed that the pool continues to swell at a constant rate (equal to the maximum velocity) up to a maximum elevation equal to 1.5 times the initial vent submergence. This conservatism is introduced to account for froth loads which occur after breakthrough of the air bubble and which are not accounted for explicitly by the PSAM.

The Mark II Owners Group basis for specification of a maximum pool swell of 1.5 times submergence derives directly from the 4T test results (Appendix B.1) and is documented in References 15, 16 and 17. The staff has reviewed this basis and has concluded that the specification is not acceptable in all circumstances. Our conclusion is based on the fact that: (a) instances where the indicated pool swell exceeded the specification have been observed; and (b) sufficient uncertainty in the determination of pool elevation exists to suggest that an additional margin is required.

With regard to (a) we note that during Run 31 of the 4T tests a maximum pool swell equal to 1.63 times the initial submergence (9 ft) was observed (see Figure 3.2 of Reference 15). This result is considered irrelevant for Mark II application by the Mark II Owners Group on the grounds that the drywell blowdown conditions were not representative of Mark II conditions (See Section 3.2 of Reference 15). The staff finds this position unjustified based on the comparison shown in Figure III-1 between the drywell pressure history observed during Run 31 and that taken from the Shoreham FSAR[18] corresponding to the containment response for the DBA LOCA. In addition to this 4T result, pool swell

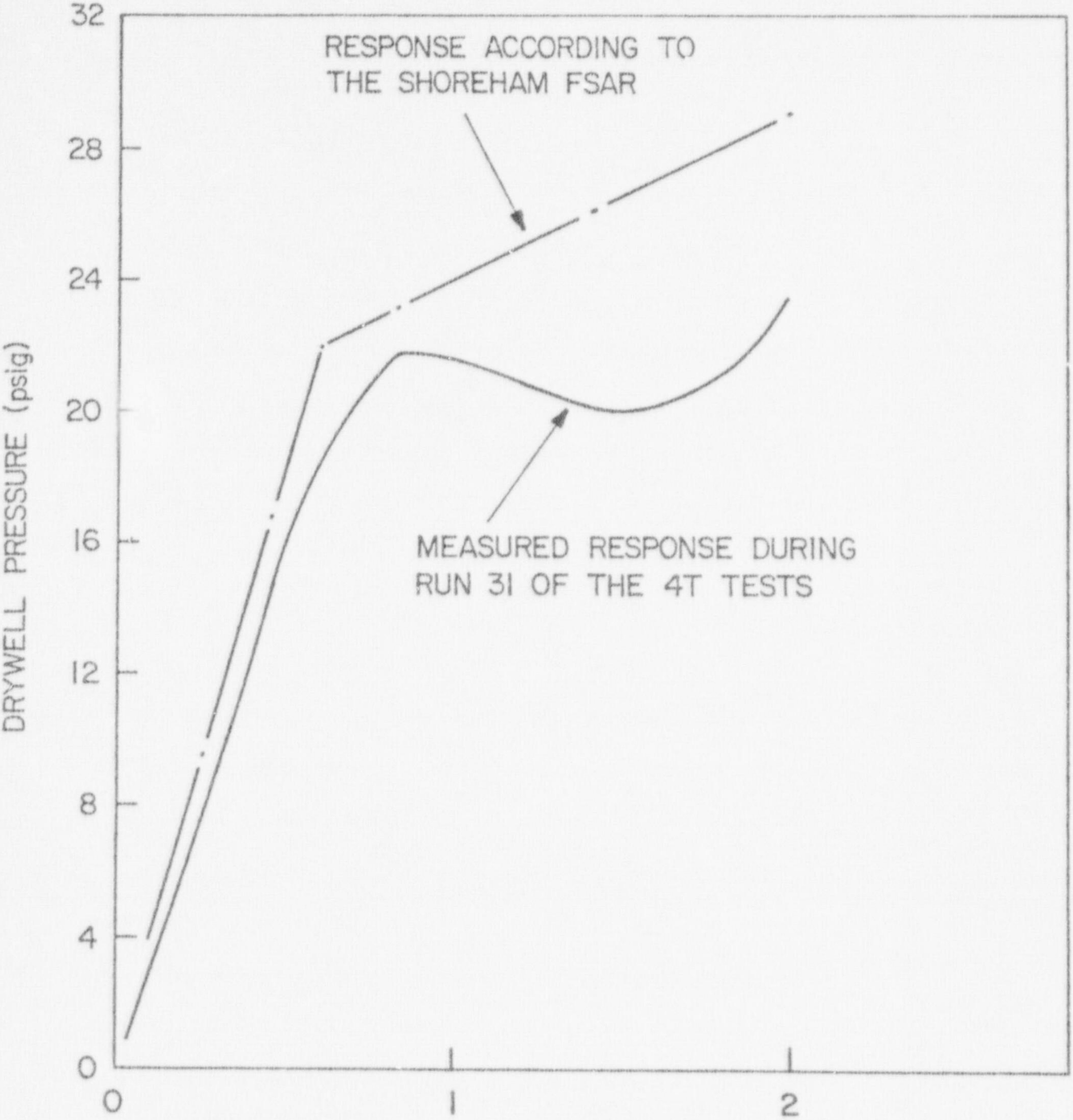


FIGURE III-1 COMPARISON OF 4T AND SHOREHAM DRYWELL PRESSURE RESPONSE TO THE DBA LOCA exceeding the current specification was also observed during Run No. 80 of the EPRI sub-scale tests (Appendix B.3). In this case, the maximum pool elevation was observed to be 1.69 times the initial submergence which also corresponds to 9 feet in full-scale.

With regard to the uncertainty in the measurement of pool swell elevation, it is relevant to note that this quantity is deduced indirectly from the wetwell pressure transients by assuming a polytropic relation between the instantaneous wetwell free space volume and pressure.[17] The staff considers this indirect procedure acceptable only during the acceleration phase of the pool swell transient. In particular, determination of the maximum pool swell elevation by this method introduces considerable uncertainty due to the occurrence of breakthrough and froth activity at that stage of the transient. This concern is borne out by the results observed during Run 29 of the 4T tests. In response to a formal NRC request, raw data output for this blowdown was made available to the staff.[19] Our examination of these data revealed the presence of froth activity (as indicated by water level probes) at the 38 foot level. This is approximately two feet above the maximum pool elevation indicated by the wetwell pressures. It is also relevant to note that the next highest water level probe was at a 40 foot elevation. Therefore, it can be argued that froth activity may have occurred as high as four feet above the maximum inferred from the pressure measurements. A similar result was observed during the EPRI sub-scale tests. In this case, froth on the diaphragm floor was encountered. This observation is presented to demonstrate the inherent uncertainty in the determination of maximum pool swell including froth from the wetwell compression transient. It should be noted that the staff agrees with EPRI in their assessment that the froth loading on the diaphragm observed during the EPRI tests is not representative of Mark II conditions. Results of the scaled tests are valid up to the point of maximum pool elevation. Beyond this point observations are meaningless as a result of improper scaling for the Rayleigh-Taylor instability.[10]

In view of the inadequacies cited, the staff has developed an alternative specification for pool swell elevation. We shall require that:

a) he pool swell transient be determined using the PSAM with the polytropic exponent for the wetwell compression taken at a value equal to 1.2; and

b) the maximum pool elevation be taken equal to the value computed by the PSAM with a polytropic exponent of 1.2.

The basis for this specification is a series of calculations and comparisons with the 4T experiments that have been carried out by BNL.[14] These have shown that the acceleration transient is conservatively predicted by this methodology. Also, the use of a polytropic exponent of 1.2, which corresponds to the "effective" value observed during the 4T tests, results in greater pool elevation relative to that obtained with a standard air value of 1.4. This provides the additional margin needed to bound the uncertainty in the measurements which has been cited above.*

3) Pool Velocity

This parameter is used to determine impact and drag loads on wetwell components during pool swell. The Mark II Owners Group methodology employs the PSAM to prescribe the variation of pool velocity with pool elevation during the acceleration phase of pool swell. Thereafter, pool velocity is maintained constant at the maximum value up to the maximum pool swell elevation.

In most of the comparisons for pool velocity, the PSAM predictions have either been in good agreement with, or conservative relative to, the measured results throughout the acceleration transient. However, there have been a few isolated instances in the case of 4T results where the model has underpredicted the maximum velocity by as much as 7%. (See Figure 6.43 of Reference 11). These cases generally occurred

*The margin between predicted and measured values increased from 1.9 feet to 3.5 feet by the use of a polytropic exponent of 1.2 for the representative 4T cases examined in detail by BNL.

at lower pool velocities ($\sim$ 20 fps) corresponding to deeper submergence and low drywell charging rate. It appears therefore that the model is not uniformly conservative over the entire parameter range of interest in terms of velocity.

A certain amount of uncertainty also exists in the experimental determination of the pool velocity, which to date, has not been quantified by the Mark II owners for the 4T test results. As in the case of pool elevation, this determination is made indirectly from the wetwell pressure transients. The procedure involves discrete numerical differentiation of the pool elevation history (obtained from the wetwell pressure transient as described in the previous section) followed by a polynomial curve fit of the resulting data points to provide a continuous representation of the velocity transient. A second polynomial fit restricted to points near the maximum value is used to define the maximum velocity more accurately. Since this procedure is applied only during the acceleration phase of the pool swell when the water slug is still intact, the staff considers it to be sound, in principle, but inherently subject to error. Some indication of the magnitude of this error is provided by the fact that the maximum velocities derived from the two different polynomials generally differed by about 5%.[11] The error analysis performed for the EPRI velocity data,[10] which are obtained in a somewhat similar fashion, indicates an uncertainty of ±5%. These observations suggest that a multiplier be applied to the results generated by the PSAM to provide a conservative margin for velocity prediction throughout the range of interest. The staff will require that a multiplier of 1.1 be applied uniformly over the velocity transient to provide this margin. This multiplier results by interpreting the underprediction of 7% as scatter about the theoretical values and combining this with a 5% error in velocity determination using a root-mean-square approach. (The multiplier which results is actually about 1.09 which we have rounded off to 1.1). In addition we require a more complete error analysis of the 4T velocity measurement during the Mark II owners group LTP to confirm our estimate of the error in the 4T velocity measurements.

We conclude that the method of calculating velocities based on the PSAM multiplied by 1.1 provides a conservative prediction of pool velocity up to maximum pool elevation; i.e., including the deceleration phase of pool swell.

4) Pool Acceleration

The pool acceleration parameter is utilized in determining the acceleration drag loads on submerged wetwell components during the pool swell phase of the LOCA event as described in Section III.D. No direct comparison of model prediction with experimental results has been made for this parameter. However, from an examination of the velocity transient comparisons, it can be inferred that the model substantially overpredicts the pool acceleration, particularly at early times when pool acceleration is maximum. Consequently, a conservative estimate of total drag will be established. We conclude that the use of the PSAM for prediction of pool acceleration during the pool swell phase of the LOCA is acceptable.

5) Wetwell Air Compression

This parameter defines the pressure loading on the wetwell boundary above the pool surface during pool swell. The PSAM predictions for this parameter have been shown to be conservative relative to both the 4T and EPRI experimental results. Among all of the 4T cases examined, the wetwell pressure rise has been overpredicted by at least 50% with an extreme case corresponding to Run 36 yielding an overprediction of 130%.[14] The corresponding range of overprediction for the EPRI tests was from 30 to 50%.[12] This conservatism can be attributed in part to the constant thickness slug assumption used in the PSAM, with additional conservatism provided in the case of the 4T results by the assumption of "all air" blowdown. We conclude that the use of the PSAM prediction for specification of wetwell boundary loads above the pool surface during pool swell is acceptable.

6) Drywell Pressure History

To generate predictions for the various pool swell parameters cited above a drywell pressure history must be specified as input to the PSAM. The Mark II Owners Group methodology employs the pressure history given in the individual FSAR's for this purpose. These histories are derived from currently existing containment response codes[20, 21, 22] which neglect suppression pool inertia during pool swell.*

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*These codes all use the common assumption that air carryover following vent clearing is instantaneous.

During the qualification of the PSAM, the pressure histories which were utilized were the actual measured histories as observed during the particular tests being analyzed. In general, these measured transients will not be identical to those that would be predicted using the aforementioned containment response codes. Accordingly, an uncertainty exists regarding whether the FSAR history represents a conservative forcing function for development of suppression pool response to the postulated LOCA.

To ensure conservatism in this regard, the staff has requested[23] that the Mark II Owners Group perform a comparison between predicted pool swell response using both the measured pressure transients and those given by a representative containment response code for selected 4T tests. The specified runs were the two saturated liquid blowdowns conducted during Phase II of the 4T tests (Runs 36 and 37). The results of these comparisons were provided to the staff via Reference 24. Results are presented for pool swell elevation, pool swell velocity and air bubble pressure. In all cases, greater response is predicted using the pressure history calculated according to the method described in Reference 21.

Based on these results, we conclude that the use of the FSAR pressure history is acceptable for determination of pool swell response, provided that this transient is calculated by the method of Reference 21. For those plants which employ other methods for this calculation, the staff will require that comparisons similar to those presented in Reference 24 be made using their individual calculation procedure. The staff will require that these comparisons be included in the individual DAR's.

b. Loads on Submerged Boundaries

During the pool swell transient, the high pressure air bubble which forms in the vicinity of the vent exit creates an increase in pressure on all suppression pool boundaries below the vent exit as well as those walls with which it is in direct contact. Boundaries which are above the bubble location but below the point of maximum pool elevation also experience increased pressure loads corresponding to the increased pressure in the wetwell airspace as well as the hydrostatic contribution of the water slug.

The Mark II Owners Group methodology for specification of these loads employs the PSAM to determine the maximum values of bubble pressure and wetwell airspace pressure. The methodology also takes the maximum pool elevation as 1.5 times the initial submergence. Using these data, a static loading is applied to the submerged containment structures as follows:

1) for the basemat - uniform pressure equal to the maximum bubble pressure plus hydrostatic head corresponding to vent clearance from basemat;

2) for the containment walls below vent exit - maximum bubble pressure plus hydrostatic head corresponding to vertical distance from vent exit; and

3) for the containment walls between vent exit and maximum pool elevation-linear variation between maximum bubble pressure and maximum wetwell airspace pressure.

The staff finds the methodology outlined above acceptable, subject to the modifications recommended in Section III.B.3.a.2 relative to maximum pool elevation. The maximum values of bubble and wetwell airspace pressure are conservatively predicted by the PSAM as discussed in Section III.B.3.a.1 and III.B.3.a.5. With regard to the static application of the pressure load, the staff finds this acceptable in view of the large characteristic period of loading (1 second) relative to the corresponding natural periods for typical Mark II containment structures (30-50 msec). The spatial distributions selected are appropriate and conservative based on an examination of the 4T test results.

c. Impact Loads

Impact loads are a consequence of pool swell. As the pool rises, any structures or components located above the pool but lower than its maximum elevation will be subject to water impact. The structures and components in question are the various pipes, beams, braces and platforms. The DFFR separates all impacted structures into two classes: 1) small structures such as pipes, I-beams and other similar structures having one dimension of the structure less than or equal to 20 inches and 2) large structures which have both dimensions of the structure greater than 20 inches.

1) Impact Loads on "Small" Structures

The Mark II Owners Group methodology for predicting impact loads on "small" structures, as described in the DFFR, is based on data from the Pressure Suppression Test Facility (PSTF) (Appendix B.2). The DFFR defines a universal normalized force history, somewhat similar to a versed-sine shape, having a constant pulse duration of 7 milliseconds. The maximum amplitudes of these pulses (force/projected area of target), obtained directly from PSTF data, are presented as linear functions of pool velocity. A single correlation is presented for all cylindrical targets and two separate correlations (depending on size) are presented for flat targets. These force histories would then be used to calculate stresses in the impacted structures under dynamic conditions.

The staff has reviewed the impact load specification in the DFFR and has concluded that it is incomplete. We conclude that neither the maximum pressure correlations nor the constant pulse duration of 7 msec can be considered applicable to all Mark II structures without further qualifications. The staff has formulated a revised impact load specification which we find acceptable. This specification, essentially, makes use of the PSTF impulse data (instead of pressures) and assumes impact by a flat pool. The pulse duration is considered a variable which depends on the target geometry, size and pool velocity. The justification for this load specification is described in Appendix C.7 and the specification itself is described below.

The staff's load acceptance criteria stipulates that the hydrodynamic loading function that characterizes pool impact on small* horizontal structures shall have the versed sine shape

$$p(t) = P_{max} \times \frac{(1 - \cos 2\pi \frac{t}{\tau})}{2}$$

where:

p = pressure acting on the projected area of the structure;

P_{max} = the temporal maximum of pressure acting on projected area of the structure;

t = time; and

τ = duration of impact.

*Small structures, in the present context, are defined as pipes, I-beams and other similar structures having one dimension less than or equal to 20 inches. The acceptance criteria for impact are not applicable to the determination of ovaling stresses in cylindrical pipes.

For both cylindrical and flat structures, the maximum pressure P_{max} and pulse duration τ will be determined as follows:

(a) The hydrodynamic mass per unit area for impact loading will be obtained from the appropriate correlation for a cylindrical or flat target in Figure 6-8 of Reference 4.

(b) The impulse will be calculated using the equation

$$I_p = \frac{M_H}{A} \times \frac{V}{(32.2)(144)}$$

where:

I_p = impulse per unit area, psi-sec;

$\frac{M_H}{A}$ = hydrodynamic mass per unit area, lbm/ft^2, from (a) above; and

V = impact velocity, ft/sec.
(From Section III.B.3.a)

(c) The pulse duration τ will be obtained from the equation

<u>Cylindrical Target</u>

$$\tau = \frac{0.0463D}{V}$$

<u>Flat Target</u>

$$\tau = \frac{0.011W}{V} \text{ for } V \geq \frac{7 \text{ ft}}{\text{sec}}$$

$$= 0.0016W \text{ for } V < \frac{7 \text{ ft}}{\text{sec}}$$

where:

τ = pulse duration, sec;

D = diameter of cylindrical pipe, feet;

W = width of the flat structure, feet; and

V = impact velocity, ft/sec.

(d) The value of P_{max} will be obtained using the following equation:

$$P_{max} = 2I_p / \tau .$$

For both cylindrical and flat structures, a margin of 35% will be added to the P_{max} values (as specified above) to obtain conservative design loads.

The load acceptance criteria, as specified above, corresponds to impact on rigid structures. The effect of finite flexibility of real structures will be accounted for in the following manner. When performing the structural dynamic analysis, the "rigid body" impact loads will be applied; however, the masses of the impacted structures will be adjusted by adding on the hydrodynamic masses of impact (see Appendix C.7). The numerical values of hydrodynamic masses will be obtained from the appropriate correlations for cylindrical and flat structures in Figure 6-8 of Reference 4.

These load acceptance criteria are considered conservative on several counts. First, the load specification is based on flat pool impact. This results in the shortest possible impact duration and, consequently, largest dynamic stresses in the structures. Secondly, the versed-sine pulse is rather severe from the standpoint of dynamic stresses. For the same total impulse and pulse duration, the versed-sine pulse results in greater stresses than a rectangular, half-sine or triangular pulse. Although these two conservatisms may be significant, they are difficult to quantify for an actual Mark II plant. For this reason, an additional 35% margin is required on p_{max}

to account for the scatter in the PSTF data base and approximations involved in the fluid-structure interaction methodology.

2) Impact Loads on "Large" Structures

The DFFR does not offer any generalized impact load information for "large" structures (all dimensions > 20 inches). The limited number of large structures that may be present above Mark II pools will be treated on a case-by-case basis in the DAR's for individual plants.

3) Impact Loads on Gratings

The Mark II Owners Group does not identify an impact load for gratings. This position is justified on the grounds that none were detected during PSTF tests on a prototypical grating target (Appendix B.2). Thus, the only loads identified for gratings during pool swell is a steady state drag load to be determined from the product of a differential pressure across the grating and the solid area of the grating. The differential pressure for a velocity of 40 fps is provided in Figure 4.40 of Reference 26 as a function of "open area fraction".

The staff concurs that for gratings which are similar (in terms of % solid area, bar thickness-to-length ratio, etc.) to that tested in the PSTF, the loads induced by impact are small. Nevethless, the proposed load specification based on a steady state drag load is considered unacceptable on several grounds.

(a) application of the drag as a static load as suggested in Reference 26 is inappropriate. The actual loading is dynamic in nature and this effect should be accounted for in developing the structural response of grating.

(b) the proposed calculation of drag load is incorrect in that the total area of the grating, rather than the solid area, must be used to compute the total load. This follows from the fact that the pressure differential across the grating corresponds to the total head loss incurred by a stream tube defined by upstream density and velocity and a cross sectional area equal to the total area of the obstacle.

(c) the particular pressure differential-vs-open area curve presented in Reference 26 is inconsistent with conventional correlations such as those given by Idel'chik.[27] The given curve is nonconservative (ΔP too low) for open area less than 60%.

To account for both the dynamic nature of the initial loading and for impact loads which may be significant for gratings different from those tested, the staff will require that the drag load be increased by a multiplier given by:

$$F_{SE}/D = 1 + \sqrt{1 + (0.0064\ Wf)^2} \quad \text{for } Wf < 2000 \text{ in/sec}$$

where F_{SE} is the load to be applied to the grating (i.e.: the static equivalent load), W is the width of the bars in the grating (inches), f is the natural frequency of the lowest mode* (Hz) and D is the static drag load. The detailed basis for this multiplier is presented in Appendix C.4. For the gratings tested during the PSTF tests ($W \simeq 1/4$ in., $f \simeq 100$ Hz), the multiplier takes on a value of approximately two which is in good agreement with the values actually observed (See Figure 5.14 of Reference 4).

For Wf > 2000 in/sec**, the above equation may still be good, leading to conservative loads, alternatively the methods for "small structures" (Section III.B.3.c.1) may be used but with a 10% increase for the hydrodynamic mass coefficient for all gratings with open area greater than 50% (to account for interference).

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*It should be verified that the higher modes do not significantly contribute to the load.

**For practical grating configurations it is highly unlikely that Wf will be greater than 2000 in/sec.

For grating less than 50% open area, evidence must be presented to show that the hydrodynamic masses used are appropriate. For gratings with open area greater than or equal to 60% the value of D is to be determined by forming the product of pressure differential as given in Figure 4-40 of Reference 26 and the total area of the grating. For gratings with smaller open area the pressure differential is to be determined from the correlations of Reference 27 using an approach velocity of 40 fps.

d. Wetwell Air Compression

1) Wall Loads

The upward motion of the water slug during pool swell causes compression of the air above the suppression pool. As a result of this compression, additional pressure loads are experienced by the wetwell boundaries located above the pool surface.

The Mark II Owners Group methodology for specification of this pressure history employs the predictions of the PSAM directly. The predicted values of pressure are applied uniformly about the periphery of the wetwell boundary.

The PSAM has been found to provide a conservative estimate of the wetwell compression history as discussed earlier in Section III.B.3.a.5. Accordingly, the staff finds the methodology outlined above acceptable.

2) Diaphragm Upward Loads

The wetwell air compression during pool swell also causes the drywell floor or diaphragm to experience an increased pressure loading. These pressures may exceed the drywell pressure for a short period of time resulting in a net upward load on the diaphragm.

The Mark II Owners Group specification for upward diaphragm loads is a pressure differential of 2.5 psi. The basis for this specification is a direct application of the maximum observed value during the entire 4T test series.

Our consultants at BNL have examined all the available data for upward pressure differential, including those from the 4T and EPRI tests, and have concluded that the specification of 2.5 psi for all Mark II plants is not justified. In particular, a regression analysis carried out by BNL in terms of plant unique parameters indicates that, for plants with small wetwell free space volume and break size, the specification of 2.5 psi can be exceeded. The details of this analysis are given in Reference 28. The analysis is summarized in Appendix C.5. The resulting correlation of the 4T and EPRI results takes the form:

$$\Delta PUP = 8.2 - 44\,F \text{ (psi)} \qquad 0 < F \leq 0.13$$

$$\Delta PUP = 2.5 \text{ (psi)} \qquad F > 0.13$$

$$F = \frac{AB \cdot AP \cdot VS}{VD \cdot (AV)^2}$$

where:

AB = break area;

AP = net pool area;

AV = total vent area;

VS = initial wetwell air space volume; and

VD = drywell volume.

The staff will require that this correlation be employed by the Mark II Owners Group to develop a specification for maximum upward pressure differential on the diaphragm. Additional confirmation of the conservatism associated with this specification will be provided during the LTP by examination of the EPRI subscale single vent test results.

e. Asymmetric Loads

The potential for asymmetric pool dynamic loads on the wetwell walls exists under a number of conditions, including safety/relief valve load cases, variable vent submergence within a given plant resulting from seismically induced pool motion, spatial variations of the pool area to vent area ratio within a given plant and asymmetries in the vent flow yielding an unequal containment bubble load profile.

Pool swell related asymmetries resulting from the variation of the pool area to vent area ratio within a given plant were considered in the EPRI three dimensional (3D) tests[10] described in Appendix B.3. These tests simulated a 90° sector of a Mark II containment. No significant 3D effects were noted in these tests. The Mark II owners excluded an asymmetric loading specification for this condition on the basis that it is negligible. The staff concurs with this conclusion that asymmetries due to variations in the vent area to pool surface area typical of Mark II containment may be considered negligible.

Asymmetric loads for multiple safety/relief valve load cases are discussed in Section III.C.2. Asymmetric pool swell loads resulting from seismically induced variable vent submergence as discussed in Section III.E.4 will be evaluated on a plant unique basis in our review of the individual plant DAR's.

Circumferential variations in the air flow rate can occur due to drywell air/steam mixture variations and would result in variations in the bubble pressure load on the wetwell wall. We believe that large variations in the drywell pressure and vent flow compositions are unlikely. Past testing has indicated that there is reasonably good mixing of the air and steam in the drywell, precluding large variations in the vent flow composition. However, vent flow variations can occur and we have not been provided with information to justify neglecting this condition. A conservative assumption is that all air is vented on one-half of the drywell periphery and steam is vented on the other. The pool swell analytical model (PSAM) should provide the basis for specifying a maximum asymmetric load. Section III.B.3.a.1 describes an acceptable method for calculating the maximum air bubble pressure for each plant. The staff finds acceptable the use of a vent flow asymmetry specification consisting of a maximum increase in the air bubble pressure calculated by the PSAM at the time of vent clearing and a minimum increase of zero. This asymmetric condition should be applied to the wetwell in a worst case distribution to yield a bounding specification.

4. Steam Condensation and Chugging Loads

When the bulk of the drywell air has been "carried over" to the wetwell, essentially pure steam is forced thru the downcomers into the suppression pool where the steam condenses. At the early stages of this steam blowdown phase of the LOCA the local flux rates are high ($\sim$ 50 lb/ft^2/sec) and the steam-water interface at the vent exit is relatively stationary in time.

As the blowdown proceeds and the pressure differential between the drywell and vent exit is reduced the steam flux rates decrease and, as observed experimentally, the steam-water interface takes on an oscillatory character.

The displacement effect of this motion creates pressure transients in the suppression pool which are transmitted to the pool boundaries. At sufficiently low rates of steam flux, a more erratic motion of the steam-water interface is superimposed on the relatively systematic sinusoidal motion. Specifically, complete and rapid collapse of the steam bubble is observed. These events, which are referred to as chugging, occur in a relatively random fashion, both in terms of the intensity of the collapse and in time. Pressure loads are also associated with these chugging events and, in addition, due to the asymmetry of the bubble collapse, the ends of the downcomers also experience sizable lateral loads.

In the following sub-sections, the staff's evaluation of the Mark II Owners Group methodology for specification of the hydrodynamic loads on the submerged boundaries and downcomers during steam blowdown is presented. Drag forces on submerged structures which are associated with this phenomenon are discussed in Section III.D.3 where they have been combined with similar loads arising during SRV actuation.

a. Downcomer Lateral Loads

1) Single Vent Loads

Lateral loads on a downcomer occur intermittently during chugging. They are caused either by the impact of the inflowing water on the interior downcomer walls or by the trapping and collapse of steam bubbles in the water near the downcomer exit. In either case, a net lateral load results if the phenomenon is asymmetric with respect to the downcomer centerline, as is usually the case.

The lateral loads are applied near the downcomer exit, and have been observed to be impulsive in nature, stochastic in magnitude, random in direction, and apparently not affected by the presence of another downcomer as close as three diameters away, center-to-center. Typically, a lateral load is applied in a time of order 1-10 msec, while the interval between successive lateral loadings is the interval between chugs, typically 2 sec.

The Mark II Owners Group specification for the lateral load on a single downcomer is a maximum equivalent static load of 8.8 KIPS. This load corresponds to the maximum observed during foreign licensee tests on a single prototypical downcomer (Appendix B.5.a). The justification and basis for the use of this load is detailed in References 15, 26, and 29. The staff has reviewed this documentation and has concluded that the specification is acceptable only for downcomers which closely resemble the tested configuration in terms of stiffness and geometry. A precise characterization of these similarities is given below. Acceptable load specifications for other configurations have been developed by the staff and are also presented. A detailed description of the staff's basis for these specifications is given in Appendix C.3.

(a) A static equivalent load* of 8.8 KIPS is acceptable provided that: (1) the downcomer is 24 inches in diameter; (2) the downcomer has a dominant natural frequency of lateral oscillation of 7 Hz or less in its submerged state; and, (3) the downcomer is either cantilevered (unbraced) or braced at approximately 8 feet or more above the downcomer exit as in the 4T (Appendix B.1) and GKM I (Appendix B.5.a) tests.

(b) A static equivalent load of 8.8 KIPS multiplied by the ratio of the downcomer natural frequency and 7 Hz is acceptable for downcomers with natural frequency greater than 7 Hz but less than or equal to 14 Hz. This specification is acceptable only if the other restrictions outlined in item (a) are satisfied.

(c) If the natural frequency of the downcomer is above 14 Hz, or if the downcomer is braced at a point closer than 8 feet above the vent exit, a dynamic strutural analysis of the downcomer response will be required on a plant specific basis. For such an analysis it will be required that the dynamic load be taken in the form:

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*The static equivalent load is defined here as the static load which would produce the maximum strain, or deflection, actually induced by the dynamic load. This load is applied laterally (in the horizontal plane) to the tip of the downcomer, with a random direction of application.

$$F(t) = F_o \sin \frac{\pi t}{\tau} \quad 0 < t < \tau$$

$$= 0 \text{ for } t > \tau \qquad (1)$$

where 2 msec $< \tau <$ 10 msec and the impulse $I = 2F_o \tau / \pi$ is 200 lbf-sec. This specification is also subject to restriction (1) listed in item (a) above.

This is an interim specification which represents the upper bound of all observed loads (See Appendix C.3). A more realistic dynamic loading specification is to be developed by the Mark II Owners Group during the LTP. The staff will monitor and evaluate this development as it proceeds and will require a dynamic evaluation of downcomer response to lateral chugging loads for all plants during the LTP to provide additional confirmation that the static load specification is conservative.

(d) For downcomers other than 24 inches in diameter the Mark II Owners Group has not provided an acceptable load specification. These cases will be treated on a plant unique basis, as required, in the individual DAR's.

2) Lateral Loads on Multiple Vents

To evaluate the structural response of the diaphragm floor and vent bracing system to the chugging loads imposed at the end of the downcomers, the total load experienced by groups of downcomers in a given direction is required. These groupings are arranged in various ways both with regard to number of downcomers and force direction within an "influence" zone until structural parameters of interest (force, moment, stress) are maximized (See Section 4.3.2.4 of Reference 26 for additional details).

To quantify the magnitude of these loads, the Mark II Owners Group has developed a methodology based on a probabilistic approach.[(26)] This approach is justified on the grounds that the chugging event is stochastic in nature with respect to force magnitude, direction and time.

The crucial assumptions employed in this methodology are as follows:

(a) The magnitude of the force on any downcomer is random and follows the frequency distribution observed during foreign tests on a single, full scale prototypical downcomer (See Appendix B.5).

(b) The frequency distribution is the same for all downcomers; i.e., the forces on any p[illegible]lar downcomer are statistically independent of [illegible]ning downcomers in the group. Justification for th[illegible]ssumption is provided by results observed during foreign licensee tests (Appendix B.5).

(c) The direction of the force on each downcomer is random and is uniformly distributed about the horizontal plane. The randomness for direction has been documented in the 4T tests for a single downcomer (Appendix B.1) and during foreign licensee tests (Appendix B.5) for both single and multi-vent configurations.

(d) The chug event occurs simultaneously at all vents. That is, the randomness in time is neglected. This is a conservatism since experiments (Appendix B.5) have demonstrated that vents do not chug exactly in phase.

(e) The maximum number of chug events which can occur during any blowdown is 265. This choice is in excess of the number which would be anticipated for the DBA LOCA and corresponds to a "small" liquid break with an estimated flow duration on the order of 500 seconds.

Relevant features of the calculation procedure are as follows:

(a) The force magnitude distribution is multiplied by the distribution of the cosine of the angle of force to obtain the component of chugging force in a given direction for a single downcomer.

(b) For N downcomers, the resultant force distribution for one chug is calculated as the sum of N distributions of the product of the force magnitude distribution and the cosine of the force angle.

(c) For plant unique applications, the downcomer loading per number of downcomers is established for a probability level of 10^{-4} (Probability of exceeding a given resultant force magnitude at least once in 265 chugs). This loading distribution is given in Figure 4-10.a of Reference 26.

Based on our review of the relevant documentation it is the staff's position that the methodology proposed by the Mark II Owners Group is provisionally acceptable subject to the following modifications.

(a) The particular force magnitude frequency distribution selected for use in the calculations is not acceptable. Specifically, this distribution corresponds to steam blowdowns in which significant air admixture was employed (Tests 5 and 7 of the foreign licensee tests reported in Reference 5 and described in Appendix B.5). Thus, it does not correspond to the "worst" loading case of 0% air admixture, which is the basis for the 8.8 KIP specification for a single downcomer lateral load. To account for this discrepancy the staff will require that a multiplier equal to the ratio of 8.8 KIPS and the maximum load (7 KIPS) observed during tests 5 and 7 be applied to the loading distribution which is currently employed. The required multiplier is 8.8/7.0 = 1.26.

(b) The force magnitude distribution must also be modified in accordance with the requirements outlined in Section III.B.4.a.1. Thus, for downcomers which satisfy requirement (a) of Section III.B.4.a.1, no additional modification is required. For those downcomers in category (b) we will require that the downcomer loading be scaled up according to the appropriate frequency ratio. For downcomers in category (c) our evaluation of the methodology for multi-vent loads has not been completed. It will be addressed in a supplement to this LER.

(c) The multiplication of the force magnitude by the cosine of the direction angle is not justified in the generation of a force histogram for a group of downcomers. This procedure generates a probability distribution for the component of the force along some arbitrary but preselected direction averaged over all possible values of the component perpendicular to that direction. While the use of a histogram generated in this way to obtain a probability of exceeding a given force in an arbitrary direction will be underestimated, the choice of 10^{-4} as the desired probability level limits the errors produced to small values. The force per downcomer in Figure 4-10a of Reference 26 is estimated to be too low by about 7% for 5 downcomers and 8.5% for 100 downcomers (see Appendix C.6). Because of substantial conservatism in other parts of the methodology this correction can be neglected.

b. Submerged Boundary Loads

The origin of these loads is the motion of the steam-water interface formed at the vent exit during the steam-blowdown phase of the LOCA event. This motion acts as an effective "source" creating volumetric displacements in the suppression pool and giving rise to pressure transients which are transmitted to the pool boundaries.

Experiments indicate that the detailed behavior of the source is primarily dependent on the vent steam flux rate. Thus, at very high rates the interface is well outside the vent exit and is essentially stationary. In this case pressure fluctuations are not generated. At intermediate levels of steam flux rate the interface is located at the vent exit and its motion is relatively stable and sinusoidal in character. At very low steam flux rates (less than about 8 $lb/ft^2/sec$), the motion of the interface is unstable, i.e., complete collapse of the steam bubble is observed, leading in some cases to reentry of suppression pool water into the vent The intensity of these events, which are referred to as chugging, is random but bounded.

The Mark II Owners Group specification for these hydrodynamic loads distinguishes between the various phases which are delineated above. Specifically, pressure fluctuations (from local hydrostatic) are prescribed in terms of frequency content and amplitude for three distinct ranges of steam flux corresponding to high (greater than 12 $lb/ft^2/sec$), medium (between 8 and 12 $lb/ft^2/sec$) and chugging (less than 8 $lb/ft^2/sec$). Specification of the spatial pressure distribution on the Mark II containment also varies according to the steam flux regime. The staff's evaluation for each of these is presented separately below. However, several features of the methodology which are common to all three regimes are reviewed first.

1) Basis for Load Specification

The basis for specification of submerged boundary loads during steam blowdown is a direct application of the "worst" loads observed in the 4T full-scale single cell test facility (See Appendix B.1). The dynamic pressure loads which are specified are derived from the pressures measured on the surface of the 4T tank and, more specifically, from those observed at the tank bottom directly below the single downcomer. The values recorded at this location were consistently higher than at any other point on the submerged boundary during any particular blowdown. The selected loads are also "worst" in the sense that the maximum observed values with respect to test parameter variation are selected. This aspect will be discussed in detail below.

In addition to the use of the "worst" case loads, the Mark II Owners Group has cited a number of additional conservatisms related to certain features of the 4T facility to demonstrate that the observed loads are bounding for the Mark II prototypical containment. One of these is the pool-to-vent area ratio of the 4T facility which is small relative to the corresponding average value of domestic Mark II plants (12 vs 19 for a 24" downcomer). Both experiment and theory indicate that boundary loads decrease with increasing pool area. Another feature is the configuration of the 4T drywell (Appendix B.1) which is designed to enhance the mixing of steam with the drywell air. This would be expected to result in very low air content during the steam blowdown phase of the tests relative to that which could exist during an actual LOCA. Since it has been shown experimentally[5, 30] that the presence of even a small amount of air in the vent flow significantly reduces condensation loads, it can be anticipated that the loads observed during the 4T tests will be higher than those that would occur under prototypical conditions.

An additional conservatism accrues from the way in which the loads are actually applied to the containment structure. Generally speaking, this involves the assumption of exact synchronization of the condensation or chugging oscillations at all vents and results in a conservative specification of boundary loads. A more detailed discussion of this feature of the methodology will be presented below.

In general, the staff is in agreement with the implications of the items enumerated above. Nevertheless, the appropriateness of applying the pressures measured in the 4T tests directly to the Mark II suppression pool boundaries can still be questioned on several grounds.

One concern is with regard to multi-vent effects, viz., how would loads deduced from single cell tests differ from, or be modified in, a multiple vent configuration? Another concern is related to so called fluid-structure-interaction (FSI) effects on the 4T measurements. Here the issue is the determination of how measurements have been affected by FSI.

With regard to multi-vent effects, the Mark II Owners Group's justification for considering the 4T single cell results bounding for the lead Mark II plants resides primarily in the experimental results obtained during foreign licensee tests

(Appendix B.5). These results indicate that no amplification of peak pressures will occur with multiple vents and further, that the most probable (mean) values of pressure fluctutions will be attenuated. This can be attributed to the randomness of the condensation event. A multi-vent analytical model[(31)] developed by the Mark II Owners Group suggests that this effect can be demonstrated theoretically.

Based on our review of the available experimental and theoretical evidence, we conclude that the 4T single vent loads are bounding for Mark II submerged boundary loads during steam blowdown. Additional confirmation of their conservatism will be provided during the LTP by the sub-scale multi-vent test program (Appendix E). With respect to the FSI issue, it is the staff's position that, although the applicability of the 4T measured wall loads to Mark II plants has not been demonstrated on completely rigorous grounds, the manner in which the loads are applied provides sufficient conservatism to justify the use of the current load specification. We are reviewing several ongoing Mark II owner tasks to provide rigorous confirmation of the conservatism in the lead plant load specification. We anticipate that these tasks will not only confirm this conservatism but will also justify a substantial reduction in the load during the Long Term Program.

2) <u>High Steam Flux Loads</u>

The Mark II Owners Group specification for submerged boundary loads during high steam flux is presented in an Applications Memorandum.[15] The specification is a sinusoidal pressure fluctuation with frequency range 2-7 Hz and peak-to-peak amplitude of 4.4 psi. This fluctuation is superimposed on local hydrostatic pressure uniformly below the vent exit with a linear attenuation of the maximum amplitude to the pool surface. For the DBA LOCA, the duration of application is about 10 seconds. It is noted however that for break areas in excess of approximately 1.2 square feet, high steam flux pressure oscillations could persist for as long as 100 seconds. In summary, the specification of submerged boundary loads during high steam flux condensation is characterized by:

(a) peak-to-peak amplitude;

(b) frequency range;

(c) spatial variation; and

(d) duration of load application.

The staff's evaluation of each of these features of the specification is presented individually below.

a) <u>Evaluation of Peak-to-Peak Amplitude</u>

The proposed amplitude (4.4 psi peak-to-peak) is higher than the maximum observed (3.4 psi peak-to-peak) during the entire 4T test series[16, 17] which includes variations in initial pool temperature, submergence and pool-to-vent area ratio. A detailed examination of the trend of amplitude with these parameters has been made by our consultants at BNL and it has been concluded that the maximum observed value would be conservative for Mark II application. This has also been confirmed by data from foreign licensee tests[30,34] and the Marviken[32] tests conducted in Sweden in a full scale containment facility similar to a Mark II plant. Because of the low frequency content of the observed signals relative to the characteristic frequency of the 4T water-tank system, it is not expected that any modification will be required to account for FSI effects (See Section III.B.4.b.5). On the other hand, since the sinusoidal oscillations have been identified as being associated with the vent acoustics,[33] some modification may be required to correct for the difference in vent configuration between the 4T facility (See Figure B-1 of Appendix B.1) and the prototypical Mark II containment. In the staff's judgement any such modification is expected to be relatively small in the sense that the chugging load specification (Section III.b.4.b.4) will remain design controlling. Accordingly, the staff finds the specification for peak-to-peak amplitude acceptable for Mark II application. The Mark II owners' LTP includes a task that calls for the investigation of the effect of vent acoustics on condensation loads. This task includes a sub-scale multi-vent test program wherein steam tests are conducted at several different scales. Tests over a range of vent lengths will yield results for establishing the influence of vent acoustics on condensation loads. Preliminary results from these tests indicate that this effect is small.

b) <u>Evaluation of Frequency Range</u>

The proposed frequency range (2-7 Hz) covers the range of principal values observed during the entire 4T test series (2, 6, 7 Hz). No distinct trends with vent diameter, submergence or initial pool temperature are reported[17] although the higher frequencies are associated with the 24" downcomers. Based on considerations identical to those presented in the previous section, the staff concludes that the specification for frequency range is acceptable for Mark II application.

c) Evaluation of Spatial Distribution

The proposed uniform application of the load throughout the containment provides a considerable measure of conservatism. Since the observed amplitudes showed substantial attenuation in the vertical direction, uniform application of the maximum values recorded at the bottom is conservative. Uniform application in the azimuthal direction implies that the condensation pulses from all vents are exactly synchronized providing additional conservatism. The staff finds the proposed specification of uniform spatial distribution for the high mass flux condensation loads acceptable.

d) Evaluation of Load Duration

The total duration of high steam flux condensation loads depends not only on break size but on plant unique parameters. Accordingly, the staff's evaluation of total duration will be conducted on a plant unique basis during its review of the individual DAR's.

3) Medium Steam Flux Loads

The Mark II Owners Group specification for submerged boundary loads during medium steam flux is presented in an Applications Memorandum.[15] The specification is a sinusoidal pressure fluctuation with frequency range 2-7 Hz and peak-to-peak amplitude of 7.5 psi. This fluctuation is superimposed on local hydrostatic pressure uniformly below the vent exit with a linear attenuation of the maximum amplitude to the pool surface. For the DBA LOCA, the duration of application is about 10 seconds and follows immediately after the high steam flux loads. It is noted however that for intermediate break sizes, in the range 0.6 to 1.2 ft^2, medium steam flux loads could last for up to 200 seconds.

The staff evaluation of each of the features that characterize the load specification for medium steam flux rates is given below.

a) Evaluation of Peak-to-Peak Amplitude

The proposed amplitude (7.5 psi peak-to-peak) exceeds all but one of the values observed during the 4T tests[16, 17] which includes variations in initial pool temperature, submergence, and pool-to-vent area ratio. The one exception occurred during Run 55 of the test series where the maximum peak-to-peak amplitude was 9.7 psi. However, it is believed that this singular event was associated with incipient chugging rather than harmonic steam condensation (See Section 5.4.5 of Reference 17). The trends suggested by the balance of the data indicate that the proposed amplitude is conservative for Mark II application. This is also confirmed by data from a foreign licensee[30, 34] and from Marviken[32] tests which indicate harmonic pressure amplitudes significantly lower than the Mark II Owners Group specification. Because of the low frequency content of the observed signals relative to the characteristic frequency of the 4T water-tank system, it is not expected that any modification will be required to account for FSI effects (See Section III.B.4.b.5). On the other hand, since the sinusoidal oscillations have been identified as being associated with the vent acoustics,[33] some modification may be required to correct for the difference in vent configuration between the 4T facility (See Figure B-1 of Appendix B.1) and the prototypical Mark II containment. In the staff's judgement, any such modification is expected to be relatively small in the sense that the chugging load specification (Section III.B.4.b.4) will remain design controlling. Accordingly, the staff finds the specification for peak-to-peak amplitude acceptable for Mark II application. The Mark II owners' LTP includes a task that provides for the investigation of the effect of vent acoustics on condensation loads. This task includes a sub-scale multi-vent test program wherein tests are conducted at several different scales. Tests over a range of vent lengths will yield results for establishing the influence of vent acoustics on condensation loads. Preliminary results from these tests indicate that this effect is small.

b) Evaluation of Frequency Range

The proposed frequency range (2-7 Hz) is actually greater than the range of dominant frequencies which were recorded during the 4T tests (5, 6 Hz). No distinct trends with vent diameter, submergence or initial pool temperature are reported.[17] Based on considerations identical to those presented in the previous section, the staff concludes that the specification for frequency range is acceptable for Mark II applications.

c) Evaluation of Spatial Distribution

The proposed uniform application of the load throughout the containment provides a considerable measure of conservatism. Since the observed amplitudes showed substantial attenuation in the vertical direction, uniform application of the maximum values recorded at the bottom is conservative. Uniform application in the azimuthal direction implies that the condensation pulses from all vents are exactly synchronized providing additional conservatism. The staff finds the proposed specification of uniform spatial distribution for medium mass flux condensation loads acceptable.

d) Evaluation of Load Duration

The total duration of medium steam flux condensation loads depends not only on break size but on plant unique parameters. Accordingly, the staff's evaluation of total duration will be conducted on a plant unique basis during its review of the individual DAR's.

4) Chugging Loads

a) Load Specification

The Mark II Owners Group specification for submerged boundary loads during the chugging phase of the steam blowdown is presented in the Mark II Lead Plant Topical Report - Chugging Loads Justification.[29] The specification varies according to load application. Two cases are considered: symmetric and asymmetric. For each case the loading is characterized by a dynamic pressure pulse or fluctuation which is added to the local hydrostatic pressure. The maximum amplitude of the pulse varies according to the load application. Both over and underpressure maxima are imposed. For the symmetric loading case the maxima of overpressure and underpressure are taken as +4.8 and -4.0 psia, respectively. For the asymmetric case the corresponding values are +20.0 and -14.0 psia. The spatial distribution for the symmetric loading condition is axi-symmetric with respect to the suppression pool vertical axis. For the asymmetric case, the spatial distribution is symmetric with respect to a vertical plane (mirror symmetry) but the maximum amplitude of the pressure pulse varies between 0° and 180° according to the shape of the cumulative probability density curve for over and under-pressure as observed during the 4T tests. Thus, the lowest and highest values occur at diametrically opposed locations. The actual variation is shown in Figure I.B-1 of Reference 29. For both loading cases, the vertical distribution is taken to be uniform between the basemat and vent exit with a linear attenuation of maximum amplitude to zero from the vent exit to the suppression pool surface. The pressure signature needed to develop the chugging boundary loads is taken from among those recorded at the bottom center of the 4T facility. Since the maximum amplitudes are determined by other considerations (to be discussed below) the signatures are selected solely on the basis of their frequency content. Specifically, a signature with significant frequency content in the 20-30 Hz range was selected since this tends to maximize the structural response of the prototypical Mark II containment. This pressure signature is depicted in Figure III-2. These pulses are on the order of 500 msec in duration (including "ringout" - see Section III.B.4.b.4.) and are assumed to occur at 2 second intervals. The current specification for overall duration of chugging loads is the time interval 4-60 seconds corresponding to the DBA LOCA. No chugging loads are specified for other break sizes.

In summary, the specification of submerged boundary loads during chugging are characterized by:

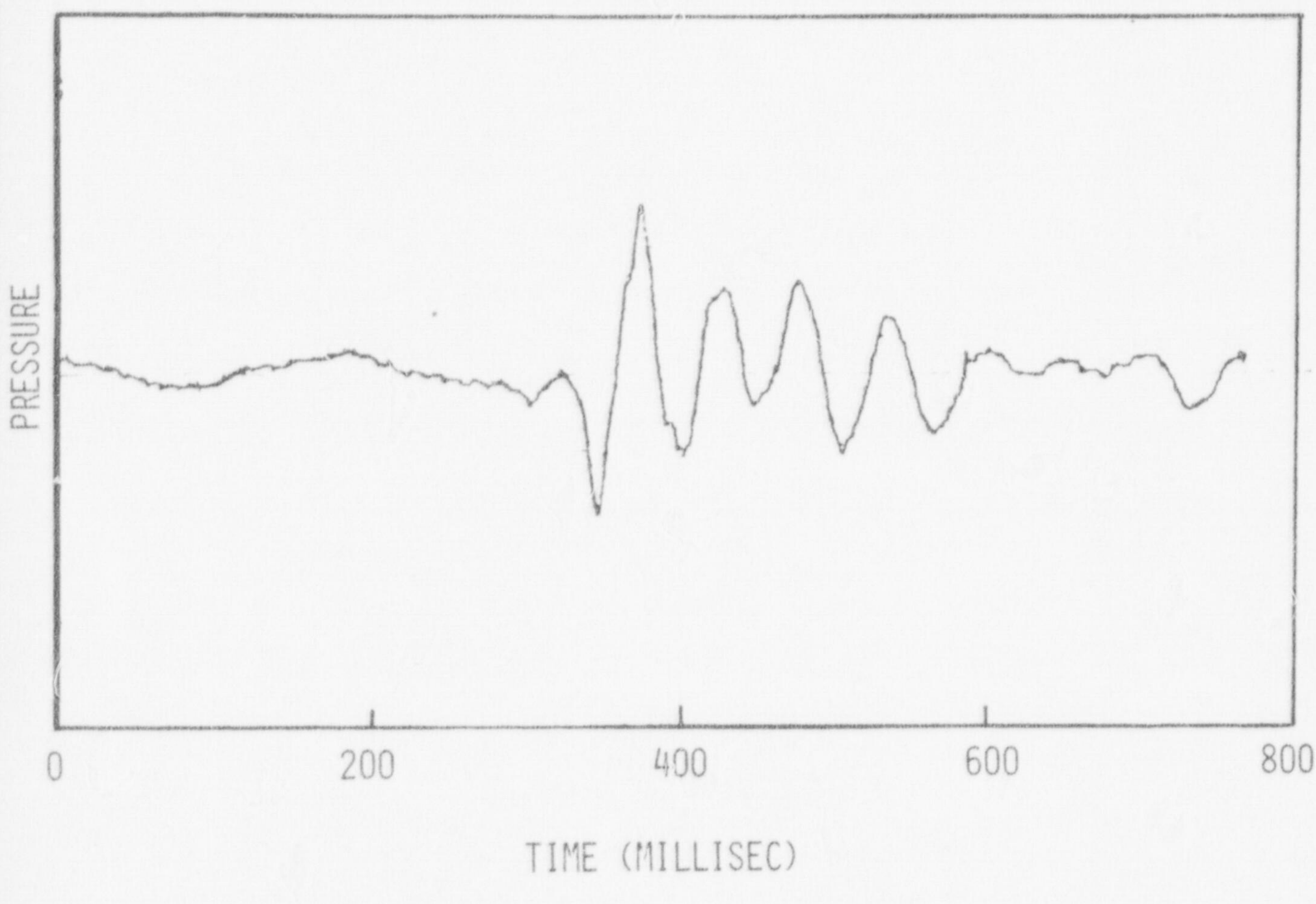

FIGURE III-2 TEMPORAL VARIATION OF CHUGGING WALL LOAD

(1) a maximum amplitude for both over and underpressure;

(2) a temporal variation;

(3) a spatial variation;

(4) interval between pulses; and

(5) duration of load application.

The staff's evaluation of each of these features of the specification will be presented individually below.

b) <u>Evaluation of Maximum Amplitude</u>

For the symmetric loading case the proposed values (+4.8, -4.0 psia) of maximum amplitude correspond to the mean values for over and underpressure of all closed tank Phase I 4T tests.* The Phase I tests were conducted with a 24" downcomer and thus correspond to a pool-to-vent

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*During the Phase I tests, blowdowns were conducted with and without venting of the wetwell to atmosphere. The pressure fluctuations observed with venting (suppression pool pressure at ambient) were substantially higher than for the pressurized cases. For example, the peak overpressure recorded with open tank was 50 psi (16) compared with only 20 psi in a pressurized wetwell. The open tank results, however, are not considered to be representative of Mark II containment conditions during the steam blowdown phase of the LOCA when the suppression pool is pressurized to about 3 atmospheres. Accordingly, the open tank results are not used in the development of the chugging boundary loads.

area ratio which is significantly smaller than that existing during the Phase II, III tests which employed a 20" downcomer. A comparison of mean pressure amplitudes between the two test series indicates a strong dependence on this parameter with the higher values observed during the Phase I tests. We have examined all of the 4T data and are satisfied that the trends for mean over and underpressure with the variation of pool temperature and submergence are secondary over the range of these parameters encountered in Mark II plants. The dominant effect is due to pool-to-vent area ratio. Therefore, the use of the mean values taken from the Phase I tests (24" downcomer) is justified. We also find that these values are higher than any observed during foreign licensee[34, 30] or Marviken[32] tests. Accordingly, we judge these values to be conservative and acceptable for Mark II application.

For the asymmetric case the proposed values (+20, -14 psia) correspond to the maximum values observed during the closed tank Phase I test series. Here again, for otherwise identical conditions of pool temperature and submergence, the Phase I peak overpressures were significantly higher than those recorded during the 20" downcomer tests (Phase II, III). They are also higher than any reported from foreign licensees[34, 30] or Marviken[32] tests. On the other hand, in contrast to the results for mean pressure, a strong effect of pool temperature on peak pressures was observed during the Phase III 4T tests. Similar tests with a 24" downcomer that were performed in support of independent work for BWR systems[35] showed essentially no temperature effect while the results of foreign licensee tests[34] suggest that the effect of pool temperature is to decrease peak pressures. These contradictory results can probably be attributed to the inherent randomness of peak values of a stochastic variable. This is borne out by the fact that, in all cases, the mean values of peak pressures were relatively insensitive to pool temperature effects. Nevertheless, the staff has examined the potential impact on the loading specification of the trend with temperature which is suggested by the Phase III 4T results. We find that for the small pool case (Phase I) a peak overpressure of 29 psia would be expected for pool temperatures on the order of 150°F. Evidently, this value is substantially higher than the Mark II Owners Group specification. On the other hand, if one takes credit for the reduction in pressure which can be expected due to the larger pool-to-vent area ratios associated with the prototypical Mark II plant, this value would be reduced to about 16 psia. Thus the staff concludes that the Mark II Owners Group specification is conservative and acceptable for Mark II application provided the plant average pool-to-vent area ratio is no less than 17.

c) Evaluation of the Temporal Variation

As indicated earlier, the essential feature characterizing the specified temporal variation is the frequency content of the signal. Since these signals are recorded at the walls of the 4T facility, their power density spectrum (PDS) will contain some modified version of the content inherent in the chug pulse itself plus additional contributions due to the response of the 4T water-tank system (ringout). A number of analytical and experimental studies carried out by the Mark II Owners Group[33] suggest that the chug pulse itself consists of a single expansion (corresponding to bubble collapse) followed by a more rapid overpressure superimposed on a low frequency ($\sim$ 5 Hz) sinusoidal fluctuation associated with the vent acoustics. The signals observed on the 4T walls are consistent with this interpretation but exhibit additional frequency content in the 20-50 Hz range. The Mark II Owners Group associates this content with the "ringout" or response of the 4T facility to the chug pulse. Assuming that:

(1) the geometric and mass-elastic characteristics of the 4T facility have not affected the magnitude of the single under-overpressure and the frequency and magnitude of the vent acoustic contribution;

(2) the high frequency content is totally due to ringout; and

(3) the Mark II containment fundamental natural frequency is in the range 20-30 Hz,

the staff concludes that the specification for temporal variation is conservative and acceptable for Mark II application. Additional justification for this conclusion is provided in Section III.B.4.b.5.

d) Evaluation of the Spatial Variation

The proposed uniform application of the pressure pulses in the vertical direction between the basemat and vent exit is judged to be conservative. Since the 4T measurements indicate a continuous attenuation of pressure amplitudes from the maximum values observed at the bottom to any higher point on the submerged boundary, the specification will result in higher total loads at any point on the boundary relative to what would actually be expected. This will tend to maximize both the symmetric and asymmetric loading conditions on the containment.

The proposed uniform application of the mean pressure throughout the periphery of the containment implies that all downcomers are chugging exactly in phase at the most probable chug intensity. This specification is judged to be sound in that it represents a conservative interpretation of the random nature of the chug phenomenon.

The specified variation of the maximum pressure amplitude over the periphery of the containment implies that all downcomers are chugging exactly in phase and that the individual intensities of the chugs are distributed in a manner which concentrates high and low intensity events at opposite sides of the containment. The probability for occurrence of such a distribution is small. Support for this finding is provided by the analytical model of Reference 31 which represents an important element of the Mark II Owners Group LTP. We conclude that the prescribed uniform spatial variation is conservative and acceptable for Mark II application.

e) Evaluation of Interval Between Pulses

The 4T test results indicate that the interval between chug events is itself a random parameter. In general, consecutive intervals are never identical in time and have been observed to vary from as short a duration as 0.83 seconds to greater than 10 seconds. Histograms of the available data indicate however that there is a most probable value which depends on pool temperature and vent diameter. The most probable interval observed in the Phase I 4T tests is two seconds.[16] The corresponding values deduced from the Phase II, III tests were about 1.2-1.4 seconds for a cold pool (initial temperature 70°F) increasing to about 3 seconds for elevated pool temperature (150°F). The apparent dependence on vent diameter indicated by the data does not appear to be consistent with the Single Vent Chugging Model (Appendix E) which has been developed by the Mark II Owners Group. This model suggests that the effect of diameter is much less than that observed experimentally. The staff estimates that the most probable interval observed during the Phase II, III tests is more representative and that the relatively long interval (2 seconds) inferred from the Phase I data is probably due to the incorporation of both open and closed test tank results in the development of the histogram. Nevertheless, taking into account the temperature dependence indicated by the data, we conclude that the selected value of 2 seconds for most probable interval between chugs is acceptable for Mark II application.

f) Evaluation of Load Duration

The Owner's Group proposed specification (4-60 seconds) is not acceptable. Considerable variation in this parameter may be anticipated depending on postulated break size and plant unique parameters. The staff will evaluate the total duration of this load on a plant unique bas[illegible] [illegible]g its review of the individual DAR's.

5) <u>FSI Effects on Load Specification</u>

As indicated in Section III.B.4.b.1, a concern exists which is related to the uncertainty which is introduced due to fluid/structure interaction effects on the wall pressure measurements obtained, during chugging, in the 4T facility. Since these pressures are used directly to develop the load specification for Mark II application, the extent of this uncertainty needs to be established.

The current position of the Mark II Owners Group, vis-a-vis the FSI concerns expressed by the staff, is detailed in the 4T-FSI Report.[36] This report provides a description of the modal survey performed by Anamet Laboratories, Inc. (Appendix B.9) on the 4T facility to establish the qualitative and quantitative effects of FSI on the measurements that define submerged boundary loads during the steam blowdown phase of the LOCA event. The report also includes a description of several analytical results which provide additional support for the current positi This position can be expressed as follows:

(a) the 4T geometry and tank-water elastic characteristics affect the wall pressure histories through a "ringout" response to the chug excitation;

(b) the frequency spectrum of wall pressures below 20 Hz is not greatly affected by the 4T response;

(c) in the frequency range from 20-50 Hz, the wall pressure is amplified by the 4T characteristics;

(d) the pressure is affected by the 4T characteristics for the frequency range above 50 Hz but the signal power in this range is only 10% of the total power;

(e) the 4T characteristics have only about a 10% effect on the measured pressure amplitudes; and

(f) for chug pulses of 10-20 msec duration the 4T system simulates the prototypical Mark II containment with all vents in exact synchronization. For shorter duration pulses (1 to 3 msec), the 4T system does not affect the pressure history.

Based on these considerations, the Mark II Owners Group concludes that the submerged boundary loads derived from the wall pressure measurements on the 4T are bounding and conservative for Mark II application.

Our BNL consultants have reviewed these arguments in detail and believe that they have merit. However, our consultants believe, and we agree, that additional analyses of the 4T results should be performed to provide a more conclusive argument that the current chugging load specification is conservative with regard to FSI effects. Our recommendations for additional analyses were discussed with the Mark II owners.

The recommedation recognizes that a direct application of the true forcing function to the containment requires a complete time-history specification of the motion of the steam-water interface. If this, or the equivalent pressure at the vent exit were known, a straight-forward application via potential flow techniques to the actual structures and geometries of interest would yield the true loading history.

In the course of the modal survey conducted by the Anamet Laboratories an effort was made to infer just this information from the available pressure measurements. The complexity of the 4T system acoustic-elastic characteristics precluded any success in this regard. However, it has been noted that a practical and useful alternative would be to deduce the net specific impulse associated with any given chug. In principle, this can be found by integrating the entire pressure time history over the walls of the 4T with respect to time. Since this same impulse emanates from the vent, it should yield a good estimate of vent net impulse. Though practical difficulties may arise in obtaining this value, once it is at hand, a synthetic pulse of correct amplitude at the vent can be derived by assuming a reasonable form for it in time. With the assumed pulse at the vent, analytical studies of the response of the prototypical Mark II containment to these impulses can proceed.

An inverse manner of arriving at such a reasonable pulse definition is to assume vent pulses of various shapes and to investigate analytically the response they would elicit from the 4T. When such responses resemble to a sufficient degree those of the 4T tests, it may be assumed that a good estimate of source chug pulse has been found.

In response to the staff's recommendations, the Mark II Owners Group has undertaken studies of this general nature.[64] These results confirm items (a) thru (e) of the Owners' FSI position, as stated above, for a triangular shaped chug pulse of representative duration (10 msec).

The staff concluded that the above study should be expanded[65] to provide: 1) a demonstration that the same conclusions apply for other pulse shapes; and 2) a comparison between calculated and observed pressure traces at the 4T facility boundary. The Mark II owners have committed to provide this information in the first quarter of 1979. We will review the progress of this task to determine if modifications to the current load specification are necessary.

Insofar as the applicability of the 4T measured wall loads to Mark II plants (item f) is concerned, it is the staff's position that, although this has not been demonstrated on completely rigorous grounds, the manner in which the loads are applied (see discussion in Section III.B.4.b.4) provides sufficient conservatism to justify the use of the current load specification. It is the intent of the Mark II Owners Group to provide rigorous confirmation of this conservatism by development of a hydrostructural analytical model of the Mark II suppression pool which will permit evaluation of containment structural response directly to chug impulses located at the vent exits. This effort will be carried out during the LTP.

C. SRV-Related Hydrodynamic Loads

As discussed in Section II.C, the primary hydrodynamic loads associated with SRV operation include oscillating pressure loads on the submerged boundaries due to air-clearing and similar loads induced by unstable steam condensation. Drag and water jet loads on submerged structures also occur. Our evaluation of the submerged boundary loads is presented in the ensuing subsections. Drag and water jet loads which arise in connection with SRV actuation are evaluated in Section III.D where the discussion of these loads has been combined with similar loads which are LOCA related.

The magnitude of both the air-clearing and unstable steam condensation loads are strongly dependent upon the type of discharge device utilized on the SRV lines. The devices currently used or proposed for the Mark II facilities include both the four-arm quencher and T-Quencher. Originally the Mark II plants had proposed use of a ramshead device. The various discharge devices are shown in Figure III-3.

As a result of the staff's evaluation of the performance of the various devices, it has been concluded that the use of the ramshead device is unacceptable for Mark II plants. This conclusion is based on a large body of test data which shows that (1) use of a quencher results in a reduction of air clearing loads relative to the ramshead and (2) steam quenching performance with the quencher is stable at local pool temperature levels approaching boiling whereas, for the ramshead

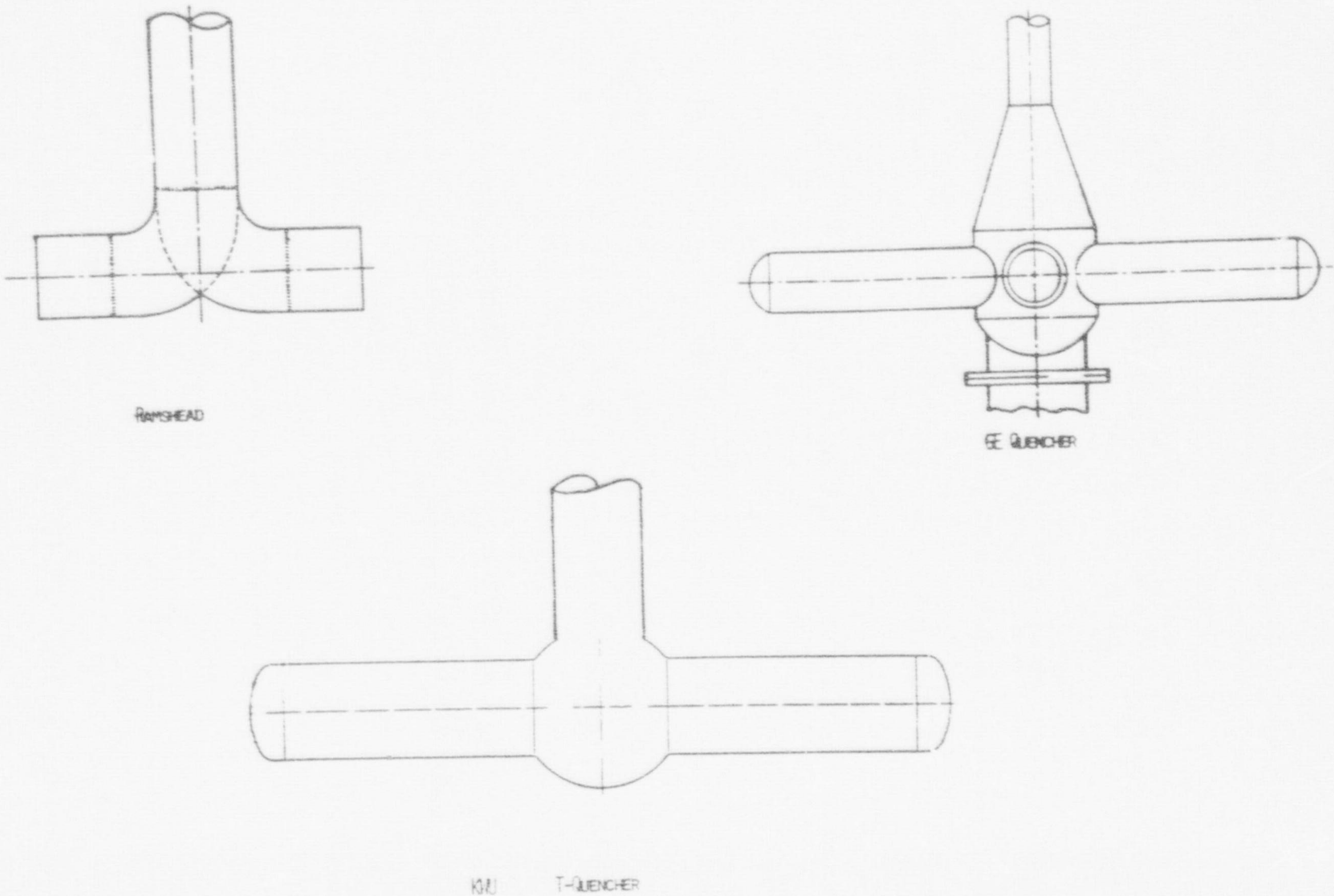

Figure III-3, Safety Relief Valve Discharge Devices device, instability occurs at a lower and, as yet, unestablished threshold level. The detailed basis for our conclusion is given in Sections III.C.1 and III.C.2.

The staff has informed the Mark II Owners of their conclusions and, in response, the lead plants have proposed the use of the T-quencher device. A supporting program to justify use of this device is currently being evaluated by the staff. However, a complete data base needed to support the load methodology is not expected to be available until early 1979.

Since the current schedule for lead plant licensing requires that conservative loads be established in advance of the T-quencher qualification program, the staff has developed an interim load specification for air-clearing loads which makes use of the DFFR loads methodology for a ramshead. The staff has determined that this approach will provide conservative load criteria for the evaluation of plant structures and components. The rationale for this approach is detailed in Section III.C.2.

In addition to the development of an interim air-clearing load specification, the staff has also developed a pool temperature limit criterion based on the existing four-arm quencher data base. In the staff's judgement sufficient similarity in steam quenching performance between the four-arm and T-quenchers has already been demonstrated to justify this approach.

The detailed basis for the development of this criterion is given in the ensuing subsection. It is reiterated here that restricting plant operations so that local pool temperatures remain below the pool temperature limit precludes the occurrence of unstable steam condensation loads. Thus, specification of a pool temperature limit eliminates a requirement for a load specification for this condition.

1. Pool Temperature Limit

   As we have discussed previously, SRV actuation at elevated pool temperatures could result in severe vibratory pressure loads. The current practice to eliminate this concern is to limit the pool temperature so that the threshold temperature for severe vibrations will not be achieved during all operational modes and upset modes such as a stuck-open SRV event. The pool temperature limit, along with the distribution of the air/steam mixture discharging from the SRV line, however, is strongly dependent on the type of SRV discharge device used. In this section, we will discuss the results of our evaluation of the pool temperature limit for the ramshead and quencher devices.

   It should be noted that the Mark II lead plants have used the GE proposed ramshead pool temperature limit with the supporting documentation. The results of our evaluation of the limit proposed by GE provided the basis for our recommendation to use quenchers in all Mark II plants.

a. Local and Bulk Temperature Difference

Local temperature denotes an average water temperature in the vicinity of the discharge device and represents the relevant temperature which controls the behavior of the condensation process occurring at the pipe exit. In general, this will differ from both the temperature of water in contact with the steam as well as the bulk temperature of the entire wetwell pool. The latter, of course, is a calculated value based on the total energy and mass release into the pool with the latter assumed to act as a uniform heat sink. Since bulk temperature is used in plant transient analyses, the difference between the bulk and local value must be specified in order that the analysis can demonstrate operation within the prescribed limits.

In a test facility, the volume of water associated with a single discharge device is, of course, only a small fraction of the volume which would exist under prototypical conditions. In such a confined pool, differences between local and bulk conditions are minimal. Tests indicate[5] that temperature distributions in a confined pool are relatively uniform with generally no more than a 2 to 3°F variation. Thus, under test conditions the measured temperature can generally be interpreted as local temperatures.

To determine the difference between bulk and local conditions for a ramshead, the Mark II Owners conducted a series of in-plant tests at the Quad Cities plant.[37] This plant utilizes a Mark I type containment. The pool was instrumented with 18 thermocouples six of which were located in the vicinity of the discharge device to determine local pool temperatures. The test was conducted by continuously discharging an SRV into the suppression pool for 27 minutes. The results showed that, through-out the transient, the measured local temperature did not deviate from the calculated bulk temperature by more than 10°F. Based on these results, GE proposed that a difference of 10°F between local and bulk conditions for the ramshead device be used for Mark II application. Although we concur with GE's interpretation of the test data, the applicability of this result for Mark II plants has not been established.

For quencher devices no data base exists for the determination of the difference between local and bulk temperature. For plants using quencher devices, the applicants will be required to provide a data base based on in-plant tests to establish the difference between local and bulk pool temperature.

b. <u>Temperature Specification for Ramshead Device</u>

Based on the interpretation of the data base, GE proposed a 150°F bulk temperature limit for plants using the ramshead device, which is equivalent to 160°F local temperature limit.

In late 1975, GE submitted a topical report[5] to support the temperature limit for the ramshead device. The report, however, was based on test data for SRV's having a straight down pipe discharge device and not on data for the ramshead device. As a result of our evaluation, we concluded that the data base did not support the proposed limit. Additional information was requested.

1) <u>Sub-scale Ramshead and Elbow Data</u>

Sub-scale tests were performed at the Moss Landing Test Facility and in a test facility in San Jose, California. These consisted of seven tests using a ramshead device and 37 tests using a 90° elbow. The mass flux ranged from 50 to 195 $lbm/sec\text{-}ft^2$. The local threshold temperature for unstable steam condensation determined for these tests ranged from 152 to 176°F for the ramshead device and 146 to 172°F for the elbow device.

The staff finds the data base cited to be insufficient for establishing the ramshead threshold temperature due to the following concerns:

(a) <u>Scaling Law Application</u>: The applicability of subscale test results requires justification based on rigorous scaling laws. These must be established from fundamental principles and carefully applied in model testing. Such scaling laws have not been derived for the SRV discharge phenomenon. Test facilities were not scaled to simulate an actual plant. Therefore, neither dynamic nor geometrical similarities can be established for the tests. Furthermore, GE has not justified the assumption that scaling has no effect on temperature threshold.

(b) Lack of Threshold Temperature Definition: The definition of the "threshold temperature" in the tests has not been well established and cannot be related to full-scale conditions. The threshold was determined by visual inspection of measurements at a few locations, often different in different tests. Without pressure amplitude criteria, prediction of the threshold temperature for full-scale containment, therefore, cannot be made.

(c) Data Scattering: Not only is there a difficulty in interpreting what the measured threshold temperature means for full-scale systems, but also substantial data-scattering appear in the sub-scale test results. As noted previously, the temperature threshold ranges from 146 to 176°F. With such a wide range of data scattering, the probability for the temperature threshold to be below the GE proposed threshold of 160°F is high: 16% of the sub-scale data points fall below the limit.

We therefore conclude that the applicability of the sub-scale test data cannot be supported without additional tests.

2) Small Scale Straight Down Pipe Data

The data set for the straight down pipe was obtained from tests performed in Germany. A total of 12 data points was obtained. The threshold was defined as the pool temperature at which the peak-to-peak pressure amplitude first reached 2 bar (29 psi) outside a circular projection with twice the pipe diameter on the floor of the tank. The results show that all data points fall below the 160°F limit. Therefore, the data does not support the proposed limit.

3) Plant Operational Data

Five plants have experienced SRV discharges into the suppression pools where temperatures in excess of 100°F were reached with no reported instabilities. Specifically, the highest pool temperature from these events ranged from 122 to 165°F. However, the report only provides detailed data for two plants which were identified as Plant A and Plant C.

The data indicate that Plant A was manually scrammed before the suppression pool temperature being monitored reached 110°F following an SRV stuck-open event. This temperature increased rapidly and reached 165°F when the reactor pressure was 184 psig. Plant C only reached 146°F because the reactor was scrammed at a lower pool temperature.

Figure III-4 shows the loci of the Plant A and C events on a plot of pool temperature versus SRV steam mass flux during blowdown. Also shown in the figure is the GE proposed pool temperature limit. It is clear that these plants experienced SRV discharges far below GE's proposed temperature limit at virtually all mass fluxes except the lowest. Thus, the experiences do not provide support for the proposed limit at higher mass fluxes.

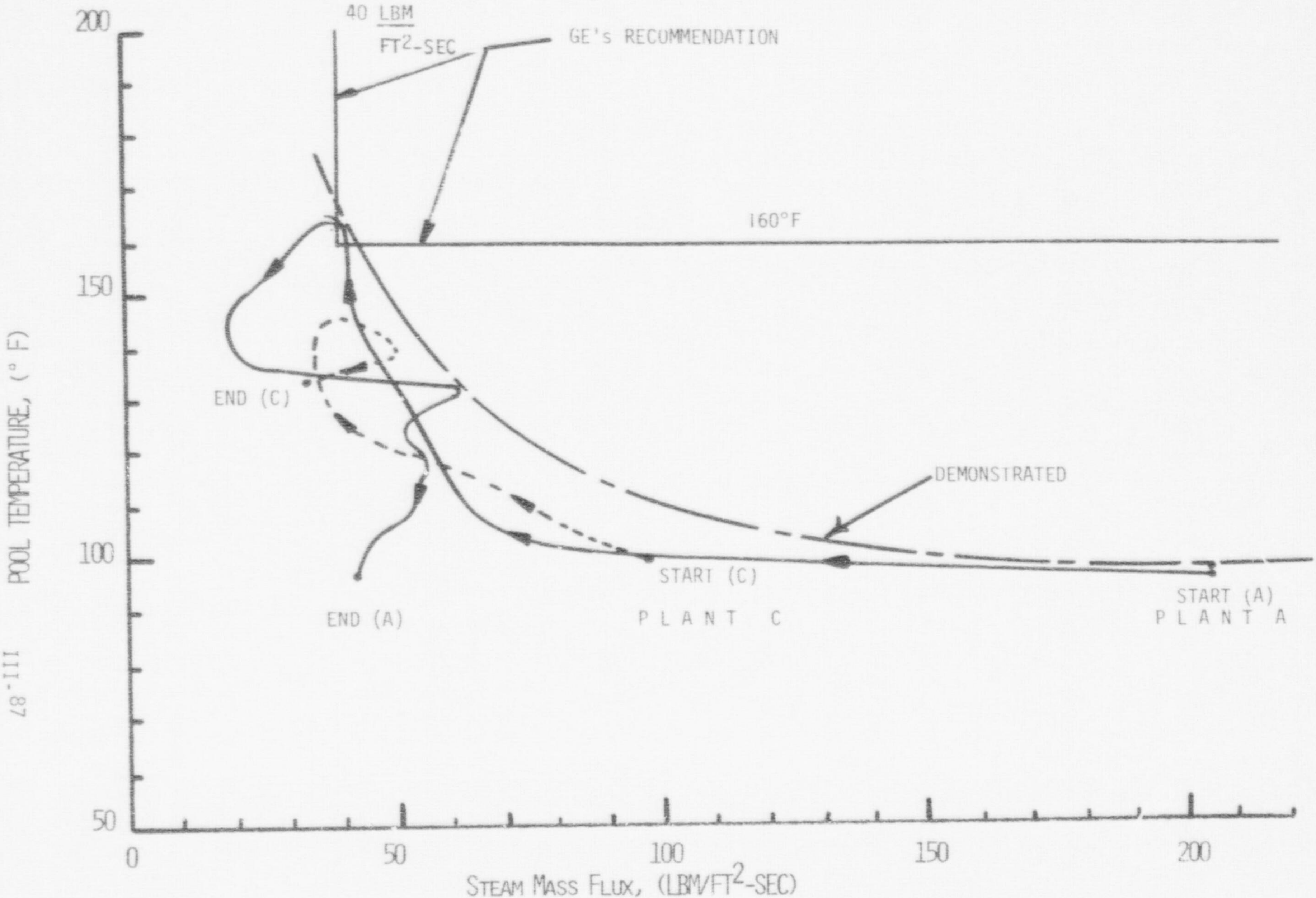

FIGURE III-4 PLANT SRV OPERATIONAL DATA

4) Conclusion on Ramshead Temperature Limit

We conclude that a suprpession pool temperature limit has not been adequately established for the ramshead device. The data base showed substantial data scatter. In addition, no scaling analysis has been performed to show the direct applicability of such data. The limited amount of plant operational data may be considered as supporting data for some specific zones of mass flow and pool temperature. However, these data are not sufficient to define an operational basis for all potential events.

In light of this conclusion and in consideration of the air-clearing loads mitigation provide by quencher device, we recommend that an alternative device such as quencher should be used. Our evaluation of quencher devices is discussed in the following sections.

c. Temperature Specification for Quencher Device

As indicated earlier, the quencher device differs significantly from the ramshead wherein the steam discharges in only two relatively large jets. In contrast, the quencher ejects the steam through a very large number of small jets each of which is surrounded by cooling water. As a result, stable steam condensation can occur at much higher pool temperatures leading to significantly improved performance.

Small scale tests on selected quencher devices were performed by Kraftwerk Union AG (KWU) in West Germany.[5] The results of these tests indicate that the hold pattern in a perforated-pipe quencher is the controlling parameter for effective steam condensation. Using a quencher device with an optimized hole pattern, KWU conducted tests at elevated pool temperatures. Steam condensation instability did not occur even as the local pool temperature approached the boiling point.

In-plant tests were also performed at a European BWR plant.[37] The tested discharge devices were the four-arm quenchers with an optimized hole pattern. The results of the tests indicate that smooth steam condensation over a wide range of reactor pressure (1100 psia to 100 psia) and pool temperature (140 to 176°F) was observed. The tests also showed good pool mixing which was attributed to the bulk pool motion induced by the air or steam jets discharging through special holes in the end of two adjacent quencher arms. The maximum variation of pool temperature was not more than 10°F.

Based on our evaluation of this test data, we find that:

1) The hole pattern is the primary design feature for achieving smooth steam condensation. Therefore, all quencher devices should be designed with the exact hole pattern described in NEDE-11314-08,[38] or supporting data should be provided to justify different hole patterns.

2) There exists satisfactory justification for raising the limit of suppression pool local temperature of 200°F for all plant transients involving SRV operations. As indicated earlier, the small scale test results showed that steam condensation instability did not occur when the maximum local temperature reached 210°F. In the staff's judgement a 200°F temperature limit will provide additional conservatism and will assure that unstable steam condensation will not occur with a quencher device.

3) The applicants will have to provide plant unique analyses for pool temperature responses to transients involving SRV operations to demonstrate that the plants will operate within the limit of 200°F.

It is emphasized that the above limit on maximum suppression pool local temperature was established on the basis of test data that are currently available to the staff. We will continue our evaluation as additional data becomes available.

d. Suppression Pool Temperature Monitoring System

The suppression pool temperature monitoring system is required to ensure that the plant is always operated within limits of the Technical Specifications. It is our position that the applicants should satisfy the following general requirements for the design of this monitoring system:

1) Redundant sensors shall be provided at each monitoring location.

2) The total number of monitoring locations shall not be less than eight. Monitoring locations shall be distributed evenly around the pool.

3) The sensors shall be installed sufficiently below the minimum water level prescribed in the Technical Specifications to assure that the sensor properly monitors pool temperatures.

4) The pool temperature shall be recorded in the control room. Redundant sensors from each sensor group shall be recorded.

5) Instrument set points for alarm shall be established so that the primary system can be shutdown and depressurized to less than 200 psia before the suppression pool temperature reaches the temperature limit as specified in Section III.C.1.c above.

6) All sensors shall be designed to seismic Category I criteria, quality group B, and shall be energized from onsite emergency power supplies.

2. SRV Air Clearing Loads

a. Introduction

This section summarizes our evaluation of the methods proposed for quantifying the hydrodynamic loads associated with the discharge of air from an SRV line following SRV actuation. Several types of proposed discharge devices are considered. These are: the "ramshead,"[26] "four-arm quencher,"[26] and "two-arm" or "T-quencher".[39] Within this section, we consider the pool boundary loads due to the oscillation of the air bubbles formed in the vicinity of the discharge device as a result of SRV actuation. Other sections of this report will assess the loads associated with the later steam discharge through the device, as well as loads on the discharge device support structures.

The steps involved in obtaining a load specification for a given discharge device are as follows. The initial state and position of the bubble are deduced from the reactor conditions and SRV line characteristics using analytical or empirical relationships developed for the different discharge devices. The subsequent pressure history of the bubble prior to break through is then predicted and, from this prediction, the pool boundary loads are deduced. The frequency of the periodic boundary loads is the same as that of the bubble oscillation; the total number of load cycles resulting from a single discharge depends also on the bubble rise time. The simultaneous actuation of several valves could result in higher boundary loads due to the superposition of load contributions of multiple bubbles. In addition, when a single valve is actuated several times in rapid succession, changes in the initial SRV line conditions (wall temperature, steam fraction, water leg length, etc.) may change the resultant air clearing loads. Similar changes may also be caused by leaks in a malfunctioning SRV. In-plant tests of these conditions have demonstrated that the changes are of sufficient importance to require separate assessment.

b. Load Calculation Methodologies and Supporting Programs

The Mark II Containment Dynamic Forcing Function Information Report[26] describes the proposed methods for calculating expected pool boundary loads resulting from SRV discharges through ramshead and four-arm quenchers. We will discuss: (1) the elements of these methodologies that are currently acceptable; (2) the bases for these assessments; (3) the restrictions, if any, on the ranges of applicability of the proposed methods; and (4) further work planned or in progress. In addition, we will discuss the confirmatory program that is being conducted to obtain a final load specification for T-quenchers.

We find that the owners' ramshead load specification is acceptable (for the reasons discussed in the next Section), but only for the limited range of plant-specific parameters. However, most Mark II plants have line lengths and air volumes (parameters in the load specification) outside this limited range. We therefore find there is no generally acceptable load specification for ramshead air discharge loads for Mark II configurations. The proposed Mark II ramshead load specification serves a second function, however. For the reasons discussed in Sections III.c.2.b.3 and III.c.2.c, we find the proposed ramshead load specification acceptable as a load specification for Mark II T-quencher air discharge loads. The ongoing experiments will confirm the conservatism of this specification.

1) Ramshead Air Discharge Loads

The load specification for ramshead air discharge loads is viewed by the staff as a calculational procedure that is based on plant-specific input. The Loads Methodology

Summary[40] provides an overview and update of the procedure described in the original DFFR.[26] Extensive additional references cited in the text below provide the details on the analytical background. Two series of in-plant tests were conducted at the Quad Cities and Monticello Mark I BWR plants[41,42] to substantiate the proposed load specification. A comprehensive review of the results of these tests and their application has been conducted by the staff and their BNL consultants. The key results are described in the following sections where the bases are summarized for the calculation procedure. Predictions based on the load calculation methodology have been compared with all in-plant results.

The main elements of the calculational procedure are as follows. The pressure rise in the SRV line air space and the resultant dynamics of the water leg in the SRV line following valve actuation are computed using the line transient model. When the line clears of water a bubble formation model is used to compute the initial conditions of the air bubble formed by the complete expulsion of air from the line. A bubble dynamics model is then used to calculate the subsequent oscillatory bubble pressure history. Loads on the containment walls caused by the bubble transient are computed using the method of images and potential flow theory. Finally, the results of the model calculations for a range of input parameters are presented as "influence coefficients" that predict the perturbations from a base case boundary load prediction for given changes in the input.

a) Line Transient Model

The pressure transient in the discharge line after SRV actuation and the dynamics of clearing the water leg are predicted with this model.[43]

It simulates the transient flow of gas and water in the safety/relief valve discharge line for the short period (0.2 to 0.5 seconds) after SRV opening. The appropriate conservation equations for mass, momentum, and energy are solved numerically. A properly formulated submodel accounts for steam condensation on the pipe wall. Calculated transient pressures in a Mark I configuration discharge line compare very favorably with in-plant test data.[43] The model yields over-predictions on the order of 20%, which represent deviations that are consistent with the inherent conservatism in the model. Also the model does not contain parameters that are adjusted to yield agreement between prediction and experiment.

Based on our review of the analytical bases and the supporting in-plant experiments, we find the model acceptable.

b) Bubble Initial Conditions

The bubble formation process is complex and has been modeled using several reasonable simplifications.[44]

Initial conditions for the bubble formation calculation are required. The line transient model yields the stagnation pressure and total enthalpy of the air in the SRV line at the time of vent clearing. The initial bubble pressure is taken to be the stagnation line pressure at this time. Initial bubble radius is assumed to be the same as that for the ramshead discharge pipe. The initial radial velocity at the bubble surface is deduced from the water jet velocity at the time of vent clearing. While necessarily approximate, these proposed initial conditions are acceptable.

During the bubble formation process, the enthalpy of the air arriving at the bubble is taken to be a known fraction of the enthalpy of the compressed air in the discharge line at the time of vent clearing.(44) This fraction, termed the "bubble formation efficiency," accounts for losses of enthalpy during the discharge process and bubble formation. On the basis of in-plant tests in Mark I configurations, it was determined that for a single actuation of a properly functioning valve, a formation efficiency of 0.1 gave adequate, conservative comparisons between model predictions and experimental results.(45, 46) Again, the possible sensitivity of the bubble formation efficiency to plant-sepcific parameters has been recognized(47) but not quantified, We therefore, find the chosen value of the formation efficiency to be acceptable only for vent line configurations and plant conditions similar to those actually tested. The model has not been jusitfied, and is therefore not acceptable for air line lengths and volumes that differ significantly from those associated with the in-plant tests.

An initial bubble position four feet from the ramshead exit is selected on the basis of in-plant tests at Quad Cities and Monticello.[41,42] It is recognized[19] that the initial bubble position may depend on the vent line transient and plant specific parameters. Since these effects have not been quantified, the assumed bubble position is acceptable only for vent line geometries and plant conditions similar to those actually tested.

c) Bubble Dynamics Model

The initial bubble pressure is higher than the ambient pressure in the pool. As the bubble expands it accelerates the surrounding water. The inertia of the water then causes overexpansion. This motion reverses itself, and hence developes its oscillatory character. This is a well-understood, analytically tractable phenomenon (the classical "Rayleigh bubble problem"). We have reviewed the proposed analytical model and find it generally acceptable. However, the difference between the predicted bubble frequencies and the frequencies measured during in-plant tests[46] (4-12 Hz) requires an error band on the frequency predictions. Based on the observed discrepancies between the in-plant data and the model frequency predictions, we require the use of an error band of $\pm$ 50% on the model frequency predictions.

A model exists for predicting vertical motions and hence the rise time of the bubble in the pool, but there is not sufficient agreement with the in-plant test results.[46] Since in-plant data are available for rise time and the number of bubble oscillations before breakthrough at the pool surface, these data provide a better basis for predicting the number of load cycles. However, use of these data must be limited to discharge conditions and plant geometries similar to those actually tested.

d) Pressure Attenuation and Method of Images

The pool boundary loads induced by the bubble oscillation are computed analytically using the method of images.[26]

This method is based on the inherently conservative assumption of inviscid potential flow. No mutual interference between bubbles or between bubbles and the walls is assumed. This assumption is acceptable only if at least five bubble radii separate bubbles and walls.[48] Pool pressures are found to vary inversely with distance from the bubble in this analysis, provided the bubble dimensions are small compared with those of the pool.

The method of images and the associated pressure attenuation are confirmed by the results of the in-plant tests.[46] Based on our review of the basis for the method of images outlined above, we find the proposed method to be acceptable.

e) Model Output: Influence Coefficients

To present the model output in a generic format which can be used directly for plant-unique applications, the influence predicted by the model of each independent variable on each dependent variable of the vent clearing and bubble dynamics phase is computed. This methodology assumes that the perturbations in model predictions from a base case prediction are linear functions of the perturbations of the independent variables and are superimposable. Redundant computational checks have established the validity of this approach over the ranges for which it is applied.(26) We conclude that the use of the method of influence coefficients for the ranges of independent variables considered is acceptable.

f) Superposition of Loads Due to Multiple Bubbles

Discharge through a single ramshead generates two bubbles in the pool. Simultaneous actuation of a number of SRV's will further increase the number of bubbles simultaneously entering the pool. The superposition of the load combinations from multiple bubbles will depend on the phase difference, if any, between the bubble oscillations. The most conservative method of superposition assumes that all bubbles oscillate in phase, so that their load contributions are directly additive (so-called "ABSS" superposition). At present there is no adequate experimental or theoretical basis for accepting any method of superposition other than this most conservative scheme in the case of ramshead bubble discharges. The in-plant test data from the Monticello and Quad Cities plants show that the assumption of direct load superposition conservatively bounds the loads measured during multiple valve actuations. On the basis of our evaluation of the in-plant test data, we conclude that the ABSS method for superposition should be used for analyses of ramshead discharges.

g) Consecutive Valve Actuations

In-plant tests at Monticello have demonstrated that air discharge loads may be significantly increased under "second pop" conditions. These conditions occur when a single valve is actuated two or more times in rapid succession. These changes are attributed to changes in the vent line conditions that affect the line transients during subsequent actuations. By comparing the in-plant measured loads, with the predictions for the single actuation conditions, "load multiplier" factors of 1.6 on predicted peak negative loads and 1.4 on predicted peak positive loads are proposed.[40] These multipliers assure that the model predictions so modified will bound the in-plant test data obtained for consecutive valve actuations. We consider this approach to be acceptable only for the limited range of plant conditions, vent line geometries, and consecutive actuation sequences actually tested.

h) Leaking Valves

In the course of the in-plant tests, it was found that the effect of a leak in a safety/relief valve increases air discharge loads. The increases were in all cases less than the maximum observed under consecutive actuation conditions. Because of the conservatism in model predictions for the no-leak case, the single actuation predictions bound or come very close to matching all of the leaking valve data.

Based on our review of the in-plant test data, we conclude that the single actuation predictions provide a best estimate for loads from leaking valves.

i) Summary of Conclusions

The calculation procedure for ramshead air discharge loads has been confirmed by extensive in-plant tests in Mark I BWR's. A number of elements of the methodology, i.e., choice of initial bubble position and bubble formation efficiency, load multipliers for consecutive actuations, and bubble rise time in the pool, are empirical results from the in-plant tests. Thus the load specification is acceptable only for vent line and discharge conditions in the same ranges as those actually tested. The ranges of these governing parameters are listed in Table III-1.[46] The SRV line lengths and air volumes of most Mark II plants, however, do not fall in this limited range of governing parameters for which the ramshead load specification is acceptable. Thus the DFFR[26] does not provide a calculation procedure that is acceptable for predicting ramshead loads for most Mark II plants.

TABLE III-1 (From Ref. 46)
RANGES OF VARIABLES FOR SRV TESTS

	DFFR MINIMUM	DFFR MAXIMUM	TEST MONTICELLO	TEST QUAD CITIES
D	.666	.994	.797	.635*
L	50.0	250	103	90.5
L_S	10.0	30.0	13.5	16.5
F_e	.01	.09	.023	.019
P_S	1050	1250	1000*	979*
$\dot{M}_S$	150	350	200	152.7
V_{DL}	300	600	451	510
P_D	50	250	135	188
ρ_D	.10	.50	.242	.382
H_o	10	30	6.54*	9.4*

D - Line I.D. (ft)

L - Line Length (ft)

L_S - Initial Length of the Water Column (ft)

F_e - Equivalent Friction Factor

P_S - Absolute Pressure of Steam Upsteam of SRV (psia)

$\dot{M}_S$ - Mass Flowrate of Steam (lb/sec)

V_{DL} - Water Discharge Velocity (ft/sec)

P_D - Absolute Pressure in Vent Line at Discharge (psia)

ρ_D - Air Density in Vent Line at Discharge (lb/ft^3)

H_o - Line Submergence (ft.)

*Out of DFFR range

It should be noted, however, the DFFR methodology for predicting the ramshead load provides a calculational procedure by which conservative load criteria for plants using T-quenchers can be established. Refer to Section III.C.2.c for a detailed discussion of the basis for this conclusion.

2) Four-Arm Quencher Air Discharge Loads

a) Statistical analysis of experimental data

The calculation procedure for predicting air clearing loads for discharges through four-arm quenchers is based primarily on the results of an extensive series of reduced-scale and in-plant tests.[5] Because of the complex nature of the flow through the quencher, the initial conditions of the large air bubbles formed by the many air jets from the quencher are calculated on the basis of a statistical analysis of the experimental data. In general terms, this approach is the empirical analog to the analytically derived influence coefficients used in the ramshead load methodology. The important governing parameters that influence the loads were identified from half-scale tests.[5] They were found to be the line air volume, the pool temperature, the submergence depth of the quencher, and the pool area. Influence coefficients were then deduced on the basis of the experimental data alone.

The load calculation procedure based on the statistical model involves two elements: calculation of mean values of peak positive loads using the experimentally derived influence coefficients, and calculation of a confidence margin. The confidence margin is determined by the mean value of the data, the number of data points available and the chosen confidence level. For a typical case the confidence margin is approximately 40% of the mean value. This approach provides, in effect, a rational method for determining design value margins. A confidence level of 90%-90% was selected for calculating margins. This implies that there would be a 90% confidence that 90% of all new data would fall below the calculated design value. Using this confidence level in conjunction with the present data base leads to design values that are substantially higher than the mean values actually measured during in-plant tests and clearly bound the highest loads ever measured in plant.

The 90%-90% confidence level that we find acceptable for Mark II applications leads to slightly lower design values (typically of order 10% to 20% lower) than would a 95%-95% level, which is the level found acceptable for the Mark III quencher statistical model. This change is acceptable for the Mark II DFFR because the preliminary data of CAORSO[62] in-plant tests demonstrate the conservatism of the statistical approach using the 90%-90% confidence level. We also learned that the Tokai II in-plant test[63] results indicated a similar degree of conservatism in the statistical approach. We, therefore, find the 90%-90% confidence level used in conjunction with the presently available data base acceptable.

A considerable expansion of the air discharge load data base will be possible when the final results of in-plant tests become available. The CAORSO containment is of the Mark II design and uses the GE four-arm quencher. The results of the tests will be used to confirm the statistical model data base. Assessment of these data is expected to be completed before the issuance of an operating license to the WPPSS-2 plant, which is the only Mark II plant presently proposing to use a GE four-arm quencher.

b) Bubble Dynamics Model

The statistical analysis of the experimental data yields a prediction for the peak positive bubble pressure under given discharge conditions. The associated peak negative pressure is then calculated using a conservation of energy analytical model for the bubble.[26,42] We have reviewed this analysis and the small-scale test data that support it[26,42] and find the method acceptable.

The frequency of the bubble oscillation in a large number of in-plant tests was found to range from 5 to 11 Hz.[26,42] This entire range of frequencies is considered for load specification purposes. The load-time history from a single quencher is assumed to be a combination of two half-sine waves with amplitudes corresponding to the peak positive and negative pressures. Both of these elements of the bubble dynamics model are acceptable.

c) Load Attenuation

The pressure distribution on the pool boundaries is taken to be inversely proportional to the distance from the centerline of the quencher, except in the immediate vicinity of the quencher. The justification for this pressure attenuation is the same as for the ramshead analysis. For certain areas close to the quencher, no attenuation is assumed; i.e., the load is set equal to the bubble pressure. The areas are:

(1) points on the basemat within a circle centered on the quencher centerline and of diameter* twice the quencher diameter; and

(2) points on the pedestal and containment wall corresponding to a projected quencher area.

Pressure distributions measured during in-plant tests show that this attenuation scheme is conservative in all cases.[5]

Based on our review of the favorable comparison between the load attenuation methodology and in-plant test data, we find the osed approach acceptable.

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*Quencher diameter denotes the diameter of the circles which circumscribe the quencher arm.

d) Superposition of Loads Due to Multiple Bubbles

In-plant tests of loads developed during the simultaneous actuation of adjacent and remote valves has demonstrated that peak boundary loads due to air discharges are no higher than the peak bubble pressure from a single quencher discharged in a pool area equivalent to the in-plant pool area per quencher. At points beyond the peak load regions, superposition of loads by a root mean square method ("SRSS") has been found by comparison with the in-plant test data[5] to be acceptable.

e) Consecutive Valve Actuation

The higher initial bubble pressures developed during rapid sequential actuation of a single valve were investigated in a series of in-plant tests.[5] The average ratio of the consecutive actuation loads to single actuation loads was obtained, together with a new error band for the consecutive actuation predictions. All other elements of the load specification are unchanged. The use of these in-plant results in predicting load increases during consecutive valve actuations is acceptable.

f) Leaking Valve Actuations

The single valve first actuation data base may contain data obtained under leaking valve conditions; but these data have not been separately identified. A separate assessment is, therefore, not possible at this time. In-plant tests currently underway [49] will be monitored for leaking valve conditions and may provide the necessary data base.

g) Summary of Conclusions

Based on the currently available test data, we conclude that the calculational procedure for four-arm quencher air discharge loads described in the DFFR with 90%-90% confidence level is acceptable. This procedure can be used to establish the design value for plant using the four-arm quencher device. We will require that the CAORSO in-plant tests confirm the acceptability of the calculational procedure.

3) Confirmatory Program for Two-Arm T-Quencher Air Discharge Loads

The T-quencher is a modified version of the four-arm quencher design. Most features of the T-quencher are similar to the four-arm quencher, although the geometry of the bubble formed and the total discharge area of the quencher may be different. Among the designs currently being tested are the "long-arm" T-quenchers [39] for possible use in both Mark I and Mark II plants and "short-arm" T-quenchers [50] for possible use in Mark II plants. A final load specification for T-quencher air discharge loads is still under development. However, an interim load specification based on the predictions of the loads for ramshead air discharges extrapolated to Mark II configurations is acceptable for the reasons discussed in the next Section. Outlined below is the confirmatory work, planned or in progress, that is intended to provide the necessary basis for a final load specification for T-quencher air discharge loads for Mark II applications.

Although the behavior of T-quenchers is expected to be qualitatively similar to the four-arm quencher performance, the geometric differences between the two devices requires a separate study of T-quencher loads. A method of analysis based largely on full-scale testing similar to the four-arm quencher methodology is being used.

The tests currently underway are expected to resolve a number of outstanding questions, including:

a) Expected load trends that have been identified on the basis of reduced-scale tests. These tests will also provide a basis for comparison of ramshead and quencher loads under identical (though reduced-scale) discharge conditions.

b) Large-scale tests in "single cell" configurations will provide a body of experimental data that will serve as the foundation for a "statistical model" similar to that developed for the four-arm quencher. These tests will include consecutive actuation conditions. Effects of leaking valves on the air discharge loads must be considered.

c) In-plant tests will verify the load specification as developed, and will provide further information on load attenuation and superposition due to multiple bubble actuations.

The ranges of conditions actually tested at full scale will determine the limits of applicability of the statistical model so developed.

The in-plant tests of a Mark I 19-ft T-quencher are under way at Monticello[39]. The test plan includes single valve actuations, multiple valve actuations (three adjacent valves), and consecutive actuations of a single valve. Twenty-six tests are planned. Measurements of pool boundary pressures and a number of other variables will be made. Geometrically similar 1/12- and 1/4-scale T-quencher tests are also being conducted. The investigation of a wider range of independent variables such as steam flow rate, discharge line volume and line length, water leg, quencher submergence, and location in the pool is planned.

1/12-scale tests of ramshead, four-arm quencher, T-quencher and a so-called "drum" quencher have been conducted by one of the lead plant applicants.[51] Although the data are still in preliminary form, certain trends have been demonstrated: peak boundary loads for T-quencher discharges were up to 50% lower than for ramshead discharges under otherwise identical conditions.

Comparisons are also to be made on 1/4-scale models. In addition full-scale tests in "single cells" similar to those conducted for the four-arm quencher are planned.[51]

Full-scale "single cell" tests for a short-arm T-quencher are being conducted by another lead plant applicant.[50] A data base similar to the four-arm quencher data base is to be developed.

We find the experimental program that is currently underway to provide the basis for quantifying T-quencher loads to be acceptable. We may require in-plant T-quencher tests in a Mark II plant.

c. <u>Comparison Between Ramshead and Quencher Air-Clearing Loads</u>

A variety of reduced-scale and full-scale in-plant tests have established that the quencher design has air discharge load mitigation characteristics superior to those of a ramshead. This is attributed to the formation of larger, lower-pressure air bubbles and enhanced air/water mixing in quencher discharges. In this section, we outline briefly the quencher/ramshead air discharge load comparisons that have been developed through tests.

1) <u>Ramshead/Four-Arm Quencher Comparison</u>

The load calculation procedure for four-arm quenchers is well established (Section III.C.2.b.2 above). A direct comparison between air clearing loads calculated for the four-arm quencher and for a ramshead under otherwise identical discharge conditions demonstrates significantly better performance by the quencher. Reduction factors for peak positive and peak negative loads for a single valve discharge are of order four and two, respectively.[53]

2) <u>In-Plant and Small-Scale Tests of a Long Arm T-Quencher (Monticello)</u>

Following the in-plant tests of a ramshead at Monticello,[42] a series of tests under essentially identical discharge conditions were conducted with T-quenchers replacing three of the ramsheads.[52] A total of 26 firings were conducted, including tests of single, and multiple (3 valves) and single consecutive actuations. These tests provide an excellent basis for comparison of T-quencher and ramshead performance, although it must be noted that the type of T-quencher tested is designed for Mark I applications and differs somewhat from the Mark II design. Nevertheless, the load trends observed are significant. Peak positive and negative loads for single valve actuations through the T-quencher were about 25% and 50% of corresponding loads developed with a ramshead.[53] The multiple valve and consecutive valve actuation tests showed similar reductions. In no case did the air discharge loads through the quenchers equal or exceed the ramshead loads under equivalent discharge conditions.

Prior to the in-plant Monticello tests, a series of small-scale T-quencher tests were conducted in a 1/4-scale Facility.[54] The results of these tests demonstrated superior performance qualitatively similar to that later observed during the in-plant tests.

3) Reduced-Scale Tests of T-Quenchers (LaSalle)

In an extensive series of 1/12 -scale tests conducted by NUTECH,[51] six different quencher designs (including a four-arm quencher, T-quencher, and "drum" quenchers) were compared with a ramshead to assess relative air discharge load performance. Comparisons with peak positive pressures developed by the ramshead showed significant load reductions for all the quencher-type devices tested, the reductions ranging for 28% to 86%. Peak negative pressures for the T-quencher and the drum quenchers were also significantly lower than those developed by the ramshead. The trends shown by these small-scale tests can only be taken as qualitative indications of expected full-scale performance. The results do, however, provide a further indication that for air discharge loads the T-quencher performance is distinctly superior to that of a ramshead.

d. Load Specification for a Mark II T-Quencher

A final load specification for the Mark II T-quenchers must await the completion of the confirmatory program outlined in Section III.C.2.b.3. For the present, a considerable body of experimental data demonstrates that T-quencher air discharge loads can be expected to be significantly less than corresponding ramshead loads; i.e., load reductions from 20% to 80% are most probable. For air discharge loads, the Mark II T-quencher design is thus a promising one. In view of the experimental background that has been developed, we believe that a T-quencher design for Mark II applications can be developed that will meet or substantially better the air discharge load specification proposed for Mark II ramshead discharges. Although the ramshead load methodology has been confirmed for only a limited range of plant-specific parameters (See Section III.C.2.b.1), the extrapolation of the ramshead predictions to Mark II plant conditions is considered acceptable for the limited purpose of providing an air discharge load specification for Mark II T-quenchers. We require, however, that the confirmatory program outlined in Section III.C.2.b.3 provide final confirmation of the load specification.

3. Loads on Quencher and Quencher Supports

When a safety/relief valve is actuated, the high pressure steam released from the primary system through the SRV compresses and accelerates the air and water column in the SRV line. Once the water column is cleared, the air volume injects into the suppression pool and forms high pressure bubbles. As a result, the air bubbles generate oscillatory loads on the containment structures and components. Our evaluation of these loads has been discussed in Section III.C.2. In this section, however, we will discuss the loads on the quencher device and its supports.

A generic methodology for calculating loads on the four-arm quencher is provided in the DFFR, Revision 2. Our evaluation is, therefore, based on this generic approach. For each individual plant, however, the applicant is required to provide justification for the applicability of this generic approach for his plant specific design.

For the T-quencher device, the applicants have not proposed a methodology to predict support loads. However, we believe that the general approach for the four-arm quencher can be adapted to the T-quencher device and would be acceptable.

a. Loads on Quencher Device

Loads on quencher devices are also referred to as quencher arm loads in the DFFR.[26] These loads are developed during the air clearing transient. The DFFR methodology postulates the following:

1) For single SRV actuation, each quencher arm is assumed to be exposed to the maximum positive bubble pressure on one side and the maximum negative bubble pressure on the other side. The difference between these two opposite sign loads will result in maximum lateral loads on the quencher arm.

2) The quencher device will also experience lateral loads from adjacent quenchers during multiple valve operations. Again, the assumption of simultaneous occurrence of positive and negative bubble pressure across the quencher arm is applied. In addition, a minimum distance of 2.3 feet between adjacent quenchers is assumed.

The quencher arm loads calculated using the assumptions indicated above are conservative. We find this calculational method acceptable.

b. Loads on Quencher Supports

Two types of loads, vertical and lateral, are imposed on the quencher tie-down structure during water and air clearing. For example, the lateral loads discussed in the previous section are transmitted to the basemat via the quencher tie-down. Vertical thrust loads on the basemat result when water or air passes from the SRV line into the quencher arm making a ninety degree turn. The vertical and lateral moments are also produced by lateral and vertical forces acting on each quencher arm. Results of our review of these forces and moments are discussed below in detail.

1) Vertical Loads

The vertical thrust load during SRV water leg clearing is the primary vertical load on the quencher supports. The air clearing transient also produces a vertical thrust load, but results in only about 10% of the thrust due to water clearing.[5] Both vertical thrusts, water clearing and air clearing, are calculated by classical momentum balance methods, which we find acceptable as discussed in Section III.C.2.b.1.

There are also several secondary vertical loads on the quencher supports. These include loads due to asymmetric discharge of air or water, transient wave loads, and inlet line loads. The proposed method of calculation is conservative. For example, the vertical loads resulting from asymmetric discharge of air or water is based on the assumption that the entire field of holes either above or below the horizontal plant of symmetry on a single quencher arm is blocked. Thus, the total air or water discharge is assumed to occur through only half of the available perforations. In reality, this phenomena is not expected to occur. However, it does lead to bounding loads.

Based on the inherent conservatism of the assumptions, we find the evaluation method for vertical loads on quencher supports acceptable.

2) <u>Lateral Loads</u>

The lateral loads considered for quencher supports include the lateral loads during the air clearing transient and water clearing transients. For air clearing, the same methodology used for calculating quencher arm loads is also used for quencher supports. However, the proposed methodology further specifies that the resultant of air clearing loads on two adjacent arms which are 120 degrees apart should be used as the total lateral load on the quencher supports. This load specification represents the maximum lateral load which could possibly be developed on the quencher supports. We, therefore, find this load specification acceptable.

With respect to water clearing transients, the methodology postulates that lateral loads could be developed due to potential uneven water discharge among the quencher arms. Test results[5] indicate that this load is insignificant. However, the methodology uses 30% of the maximum air clearing loads to account for this load. As a result of our evaluation, we find this assumption conservative and therefore, acceptable.

3) Vertical Moments

Vertical moments are produced by lateral forces occurring during air or water clearing. The DFFR methodology suggests that the vertical moments are calculated by the force imposed on a single quencher arm over a moment arm from the mid-point of the field of the holes to the center of the quencher. The resulting moments are the sum of the moments on all the four arms assuming all the moments to be in the same direction at the same time.

For multiple valve actuations, the moment is computed using the method developed for a single quencher arm. However, since the forces generated from the adjacent quencher will be in the same direction and equal in magnitude, no net moment will be produced. For conservatism, the methodology assumes that the resulting torque produced is equal to that for one arm.

All the lateral forces in computing the vertical moments have been found acceptable in Section III.C.3.b.2 for their conservatism and the moment arm used is also acceptable. We, therefore, conclude that the procedure for calculating vertical moments is acceptable.

4) Lateral Moments

Lateral moments are produced by the vertical forces discussed in Section III.C.3.b.1. Instead of assuming that the vertical loads on all four arms act in the same direction, the assumption made in calculating the lateral moments is that the vertical loads on two adjacent arms act in the upward direction and a downward direction on the other two arms. The moment arm is taken from the midpoint of the field of holes to the center of the quencher. No moments will be produced by the vertical thrust and transient wave loads because they are symmetric loads. The proposed calculation procedure for lateral moments is conservative and, therefore, acceptable.

The design values for the quencher arm and tie-down loads are the linear summation of all the loads due to SRV hydrodynamic loads, LOCA induced loads, and seismic loads. No credit has been taken for the fact that the water and air clearing loads do not occur at the same instant of time. We, therefore, conclude that the methodology to compute the quencher arm loads, quencher tie-down loads and moments is conservative, and acceptable.

C. Summary

As discussed in the previous sections, the assumptions used in the DFFR methodology for calculating loads on the four-arm quencher are conservative. In fact, some of the assumptions such as bubbles oscillating completely out of phase across the quencher arm result in bounding loads in the affected areas.

Since the T-quencher device has similar performance characteristics to the four-arm quencher, the general design guidance described above is applicable for the T-quencher device. As a result, we conclude that the DFFR methodology for calculating loads on quencher devices and quencher supports is acceptable.

D. LOCA/SRV Submerged Structure Loads

The expulsion of the water, air and subsequently steam, following a postulated LOCA or SRV actuation, induces a flow velocity and acceleration field within the suppression pool. Structures either initially submerged within the pool, or located sufficiently close to the pool surface, will experience various loads due to this induced motion. These loads can be conveniently divided into three categories: (1) jet loads due to the expulsion of the water; (2) bubble loads due to the air clearing phase; and (3) condensation loads due to oscillatory steam condensation and chugging.

Because of the large number of plant unique features associated with submerged structure loads, numerical loading values are not proposed by the Mark II Owners Group. Instead a load calculation methodology based on analytical models is presented. The methodology for the calculation of jet loads and air bubble loads during LOCA and SRV actuation for a ramshead device is described in an application memorandum.[55] This methodology is justified by supporting analytical models.[11, 56, 57] A description and critical review of these methods and analyses is presented in Appendix C.2. Additional justification for this methodology and analysis will be provided by the Mark II Owners Group during the LTP on the basis of the results from a series of sub-scale experimental programs (Appendix E).

A methodology for the calculation of submerged structure loads due to steam condensation and chugging has not been proposed by the Mark II Owners Group. This is also the case for the quencher device for all three categories. These loads will be addressed in a supplement to this Lead Plant Program LER.

In the following sub-sections, the staff's evaluation of the Mark II Owners Group methodology for each of the categories listed above is presented. Various modifications to the methodology are discussed. The full basis for these modifications is detailed in Appendix C.2. Specific methods that the NRC would find acceptable are outlined in Appendix D.

1. <u>LOCA/SRV Jet Loads</u>

   During the LOCA or SRV vent clearing transient, water is discharged rapidly into the suppression pool. For either the LOCA vents or the ramshead discharge device, the water discharges in the form of a narrow jet whose transverse dimension remains approximately the size of the exit diameter. For the quencher device, the expelled water takes a form that is three dimensional in character, and the use of the word jet may be misleading.

a. LOCA Jet Loads

The NRC has been informed that no structures are being placed in the path of vent jets in Mark II containments. However, a methodology for computing loads on such structures has been presented in Reference 55. The prescribed procedure is based on a one-dimensional jet model formulated in Reference 56. This model adequately bounds the loads on structures partially or entirely within the jet sufficiently removed from the jet front. The model is inadequate for describing the jet engulfing process at the jet front or for describing the velocities induced in the pool by the jet. The procedure in Reference 55, if it is to be applied to structures below the vents, is acceptable subject to the following constraints and modifications (See Appendix C.2 for details):

(1) Standard drag at the time the jet first encounters the structure must be multiplied by the factor

$$1 + \frac{6 V_a}{C_D A_X R_i} \; ;$$

where: V_a, C_D, A_X are the acceleration volume, drag coefficient, and projected area of the structure as defined in Reference 55 and R_i is the vent exit radius.

(2) Forces in the vicinity of the jet front shall be computed on the basis of an acceleration and standard drag (Formula 2-12 and 2-13 of Reference 55). The local velocity, U_∞, and acceleration, $\dot{U}_\infty$ are to be computed conservatively by the methods of Reference 57 from the potential function:

$$\phi = \frac{-3}{8\pi} U_j V_w \frac{\cos}{r^2} ;$$

where r and θ are the spherical coordinates from the jet front center with θ measured from the jet direction, U_j is the jet front velocity from Reference 55 and V_w is the initial volume of water in the vent.

(3) The jet cannot be assumed to dissipate when the last jet particles have reached the jet front. After the last fluid particle has reached the jet front a spherical vortex continues propagating. The drag on structures in its vicinity can be bounded by using the flow field from the formula for ϕ above with U_j as the jet front velocity from Reference 55 at time $t = t_f$.

(4) Experimental confirmation of the loads calculational methodology through scaled tests is required from the LTP.

b. <u>SRV Jet Loads - Ramshead Device</u>

The methodology treats jet loads for SRV ramshead operation in a fashion identical to that for LOCA, except that two jets are expelled and the geometry of the ramshead is approximated by an equivalent water leg length and an area ratio modification of the exit velocity. These are standard and acceptable approximations. The methodology for SRV ramshead jet loads is, therefore, acceptable subject to the constraints listed in Section III.D.1.a.

c. <u>SRV Jet Loads - Quencher Device</u>

No procedure for calculation of the submerged structure loads during expulsion of water through a quencher has been submitted.

Quencher jet loads are expected to be small. This load may be neglected for those structures located outside of a sphere circumscribed about the quencher arms. If there are holes in the end cap on the quencher, the radius of this sphere should be increased by 10 hole diameters. Confirmation of this assumption must be provided in conjunction with the Long Term Program.

2. Air Bubble Loads

After the water is discharged, air clearing begins. An air bubble forms at the pipe exit and grows due to the continued mass addition. In a postulated LOCA, the bubbles from adjacent vents eventually coalesce into a "blanket" of air which leads to the pool swell phenomenon. For SRV operation the bubble separates from the device and rises to the surface while undergoing dynamic oscillations.

a. LOCA Air Bubble Loads

The methodology for computation of submerged structure loads during the air clearing phase of a postulated LOCA is based on an analytical model of the bubble charging process and drag calculations of Reference 57 until the bubbles coalesce. After bubble contact the PSAM[11] together with the drag computation procedure of Reference 57 are used. Subsequent to the maximum rise of the pool, pool fallback loads are computed based on the velocity and acceleration conditions resulting from gravity effects alone. The staff considers the methodology to be basically sound and finds it to be acceptable subject to the following constraints and modifications:

(1) A conservative estimate of asymmetry should be added by increasing acceleration and velocities computed in step 12 of section 2.2 of Reference 55 by 10%. If the alternative steps 5A, 12A and 13A are used, the acceleration drag shall be directly increased by 10% while the standard drag shall be increased by 20%.

(2) The drag coefficients C_D for the standard drag contribution in steps 13, or 13A, 15 of section of 2.2 and step 3 of section 2.3 of Reference 55 may not be taken directly from the steady state coefficients of Table 2-3. Modified coefficients C'_D from accelerating flow as presented in References 58 and 59 shall be used with transverse forces included, or an upper bound of a factor of three times the standard drag coefficients shall be used for structures with no sharp corners or with streamwise dimensions at least twice the width. (See Appendix C.2 Section C.2.b.2).

(3) The equivalent uniform flow velocity and acceleration for any structure or structural segment should be taken as the maximum values "seen" by that structure not necessarily the value at the geometric center.

(4) The computation of drag forces on submerged structures independent of each other (as presented in Reference 55) is adequate for structures sufficiently far from each other so that interference effects are negligible. Interference effects can be expected to be insignificant when two structures are separated by more than three characteristic dimensions of the larger one. For structures closer together than this separation, either detailed analysis of interference effects shall be performed or a conservative multiplication of both the acceleration and drag forces by four shall be performed.

(5) A specific example of interference which must be accounted for is the blockage presented to the motion of the water slug during pool swell due to the presence of downcomer bracing systems. If significant blockage relative to the net pool area exists, we require that the standard drag coefficients be modified for this effect by conventional methods.[60]

(6) Formula 2-23 of Reference 55 shall be modified by replacing M_H by $\rho_{FB} V_A$ where V_A is obtained from Tables 2-1 and 2-2. This is then consistent with the analysis of Reference 57.

(7) Experimental confirmation of the load calculation methodology through scaled tests is required as part of the LTP.

b. SRV - Ramshead Bubble Loads

The methodology for computation of submerged structure loads during the air clearing phase of a ramshead SRV actuation is based on an analytical model of the bubble charging process and the subsequent bubble rise and oscillation as described in Reference 57. The drag computations are based on acceleration drag only using the method described in Reference 57 while standard drag is neglected. The procedures for including synchronous multiple bubbles are also described.

The staff finds the methodology to be basically sound and acceptable, subject to the following constraints and modifications;

(1) Standard drag should not be neglected without first estimating its order of magnitude. The importance of standard relative to acceleration drag depends on the size of the structure, size of the bubble and the distance from the bubble (See Appendix C.2).

The importance of standard drag can be estimated using the equation:

$$\frac{F_{S_M}}{F_{A_M}} = f\left(\frac{P_{max}}{P_\infty}\right) \frac{C'_D}{\pi} \frac{R_{min}}{d} \left(\frac{R_{min}}{r}\right)^2$$

where:

F_{S_M} = maximum standard drag;

F_{A_M} = maximum acceleration drag;

C'_D = cycle-averaged effective drag coefficient;

d = diameter of a cylindrical structure;

R_{min} = minimum bubble radius;

r = distance from bubble center to the structure; and

$f\left(\frac{P_{max}}{P_\infty}\right) \simeq 8/3$ for $\frac{P_{max}}{P_\infty} \leq 30.$

(2) The constraints and modifications outlined in Section III.D.2.a for LOCA air bubbles shall also be applied to the calculation of ramshead air bubble loads as appropriate.

c. SRV - Quencher Bubble Loads

The Mark II Owners Group have not provided any procedures or analyses for predicting air clearing loads on submerged structures for quencher SRV operation. These loads may be computed by the same basic methodology as used for the ramshead device subject to the modification of the source strength as substantiated by experimental data. On an interim basis, submerged structure loads due to the four-arm quenchers shall be determined using the source strength derived from bubble pressures calculated by the methods of Reference 26 for a four arm quencher.

Associated loads for the T-quencher may be computed on the basis of the ramshead methodology and bubble pressure described in Reference 26. However, the bubble shall be assumed to be located at the center of the quencher device with bubble radius equal to the radius of the quencher. The source strength of the quencher bubble may be assumed to be 25% of the source strength calculated by the ramshead methodology, to compensate for the difference in bubble growth rate for these two devices.

3. Steam Condensation Loads

The initial steam condensation associated with the postulated LOCA takes place in a steady fashion. Under those conditions, no pool motion is induced and no appreciable drag forces are produced on the submerged structures. When steam mass flow has been reduced, condensation takes place first in a sinusoidal oscillatory manner, and finally in a highly stochastic unsteady process referred to as chugging. These transient phenomena produce an effective unsteady source at the vent exit analogous to the air bubble source, and can also be expected to produce acceleration and standard drag forces on submerged structures. The Mark II Owners Group has not proposed any procedures or analyses for determining the loads on submerged structures during these phases of steam condensation. The methodolgy for specifying these loads will be reviewed by the NRC staff on a plant unique basis in the DARs.

E. Secondary Loads

The emphasis of the Mark II owner's supporting program was to perform tests and analyses to provide a strong technical basis for establishing the load methodology for the more significant of pool dynamic loads. A number of other pool dynamic loads can occur which are considered secondary by virture of their low magnitude when compared with the primary loads or by virture of the small response of the containment and related equipment to these loads. The Mark II owner's treatment of the secondary loads falls into one of the following three categories: 1) a generic load methodology was developed using well established methods along with conservative assumptions; 2) the load was excluded from the generic program, however, the load will be addressed on a plant unique basis within the individual DAR; or 3) the load was ignored since it results in a negligible loading on the containment. Each of the secondary loads is identified below including a description of its categorization

1. Sonic Waves

Immediately following the postulated instantaneous rupture of a large primary system pipe, a sonic wave front is created at the break location and propagates through the drywell to the vent system.

This loading condition was excluded on the basis that the finite opening time of a real break in conjunction with the rapid attenuation of the load with distance and the short load duration would result in a negligible loading on the structures. The staff concurs with this assessment and concludes that the sonic wave load may be considered negligible.

2. Compression Waves

The compression of the air in the drywell and vent system causes a compression wave to be generated in the downcomer water legs. This compression wave then propagates through the pool and causes a differential pressure loading on submerged structures and on the wetwell wall.

This loading condition was excluded because a finite break opening time and an approximate 50 psi per second pressurization rate in the drywell is not sufficient to cause a significant compression wave. This wave would subsequently attenuate as it travels along the downcomer water leg and out through the pool. In addition, this type of loading phenomenon has never been observed in any of the pressure suppression testing conducted to date. Based on these considerations, the staff concludes that compression waves may be considered negligible.

3. <u>Post Pool Swell Waves</u>

Following the pool swell process, continued flow through the vent system generates random pool motion. This pool motion creates waves which may impinge upon the wetwell wall and internal components. The methodology for establishing loads resulting from post pool swell waves will be evaluated on a plant unique basis in our review of the individual plant DARs.

4. <u>Seismic Slosh</u>

Seismically induced vibrations of the wetwell will generate pool slosh (i.e., wave) loads on the wetwell wall and internal components. The methodology for establishing loads resulting from seismic slosh will be evaluated on a plant unique basis in our review of the individual plant DARs.

5. <u>Fallback Loads on Submerged Boundary</u>

The staff has examined the potential for "water hammer" type loads during fallback of the suppression pool. Such loads could occur if the water slug remained intact during this phase. The available experimental evidence suggests that the fallback process consists of a relatively gradual settling of the pool water to its initial level as the air bubble "percolates" upward. This assessment is based on direct visual observations during the EPRI tests[10] as well as indirect evidence provided by a careful examination of pool bottom pressure forces from the 4T, EPRI, foreign licensee and Marviken tests. In no case were large overpressures observed during the fallback phase. In all cases the maximum net downward force occured at vent clearing. The staff concludes that fallback loads on the submerged boundary are small and need not be considered during the structural evaluation of the containment.

6. Thrust Loads

Thrust loads are associated with the rapid venting of air and/or steam through the downcomers. For prototypical Mark II containment conditions, the maximum values occur during steam blowdowns and are conservatively estimated to be on the order of 1000 KIPS (total vent area$\sim$300 ft^2). Because the downcomers in Mark II plants are of constant area, essentially none of this thrust is experienced as a load by the downcomers. A consistent application of momentum balance for the control volume consisting of the drywell, diaphragm floor and vents indicates that the thrust force is manifested as a reduction in the downward pressure differential on the diaphragm. Either this has already been accounted for in the FSAR calculations or if neglected, represents a conservatism insofar as diaphragm downward load is concerned. The staff has reviewed the methodology proposed by the Mark II Owners Group to compute thrust loads on the downcomer in the DFFR and finds it acceptable.

7. Friction Drag Loads

Friction drag loads are experienced internally by the downcomers during the venting of air and/or steam and externally due to the upward motion of the suppression pool. The Mark II Owners Group, using standard techniques, conservatively estimates these loads as 0.6 and 0.3 KIPS per downcomer, respectively. The staff finds these values acceptable and concludes that friction drag loads need not be considered during the structural evaluation of the containment.

8. Vent Clearing Loads on the Downcomers

The expulsion of the water leg in the downcomers at vent clearing creates a transient water jet in the suppression pool. In general, this jet formation may occur asymmetrically leading to lateral reaction loads on the downcomer.

The Mark II Owners Group has not identified a lateral load on the downcomers during vent clearing. This is based on direct observation during the 4T tests (Appendix B.1) which indicate that no signficant vent clearing loads are encountered.[16, 17] Vent clearing loads as high as 3.5 KIPS were observed during foreign licensee tests (Appendix B.5.a). However, the test configuration in these experiments was not prototypical of the 4T facility, in particular, and Mark II plants, in general. Specifically, no drywell volume other than that represented by the vent line volume existed (see Figure B-5 of Appendix B). The staff concludes that the vent clearing loads observed during the foreign licensee tests are unique to the test setup and inappropriate for Mark II application. In any case, the loading specification during the chugging phase of the blowdown (Section III.B.4.a) will be design controlling. The staff concludes that vent clearing lateral loads on the downcomers are negligible and need not be considered during structural evaluation.

IV. Conclusion

The Mark II Owners Group has identified two major elements for this generic program. These elements include the Lead Plant Program and the Long Term Program.

We find that the LPP provides an adequate data base, through the program's experimental and analytical tasks, to enable conservative loads to be established for the evaluation of the lead Mark II plants. It should be noted that we found some of the loads proposed by the Mark II owners unacceptable. However, for these loads we have developed acceptance criteria that we find acceptable. These criteria were developed, based on a conservative interpretation of the existing data base to assure conservative loads for the lead plants.

We have also reviewed the proposed Long Term Program and find it acceptable in concept. The LTP includes a number of analytical and experimental tasks. We conclude that sufficient planning has gone into these tasks to assure that an adequate data base will be available at the LTP conclusion. We believe that the LTP tasks will provide sufficient understanding of the phenomena to support a significant reduction in a number of the curent LPP loads. In addition, they will provide additional confirmation of the conservative LPP loads.

A summary of our conclusions related to the Lead Plant and Long Term Programs is provided in the following sections.

A. Lead Plant Program

A summary of our review status for each of the pool dynamic loads is presented in Table IV-1. This table provides a description of each load or phenomenon, the Mark II Owners Group load specification and reference, the NRC review status, and the section of this report containing our evaluation. As shown in this Table, in a number of instances we find the loads as proposed by the Mark II Owners Group acceptable. In a number of other cases we find the loads provisionally acceptable, subject to certain constraints and modifications specified by the NRC. Each of the loads that have been found acceptable on a provisional basis is noted in the Table. The load modifications and constraints have been put in the form of acceptance criteria (See Appendix D) and have been sent to each of the applicants for the lead Mark II plants.

The Mark II owners have not provided an adequate response to our concerns regarding load conservatism in a number of areas. As a result the NRC staff and its consultants have reviewed the information from the lead plant supporting program tasks and developed certain load acceptance criteria to assure that conservative loads are used in the evaluation of the lead plants.

We have required that the applicants for each of the lead plants identify any deviation from these criteria. Loads that differ from the acceptance criteria shall not be

Table IV-1

Mark II Pool Dynamic Load Summary Table

Load or Phenomenon	Mark II Owners Group Load Specification	Reference	NRC Review Status	LER Section
I. LOCA-Related Hydrodynamic Loads				
A. Submerged Bourdary Loads During Vent Clearing	33 psi over-pressure added to local hydrostatic below vent exit (walls and basemat) - linear attenuation to pool surface	DFFR - Rev. 2	Acceptable	III.B.2
B. Pool Swell Loads				
1. Pool Swell Analytical Model				
a) Air Bubble Pressure	Calculated by the Pool Swell Analytical Model(PSAM) Used in calculation of submerged boundary loads.	DFFR - Rev. 2 NEDE-21544-P	Acceptable	III.B.3.a.1
b) Pool Swell Elevation	1.5 x submergence	DFFR - Rev. 2	NRC Criteria I.A.1	III.B.3.a.2
c) Pool Swell Velocity	Velocity history vs. pool elevation predicted by the PSAM used to compute impact loading on small structures and drag on gratings between initial pool surface and maximum pool elevation and steady state drag between vent exit and maximum pool elevation. Analytical velocity variation used up to maximum velocity. Maximum velocity applies thereafter up to maximum pool swell.	DFFR - Rev. 2 NEDE-21544-P	NRC Criteria I.A.2	III.B.3.a.3
d) Pool Swell Acceleration	Acceleration predicted by the PSAM. Pool acceleration is utilized in the calculation of acceleration drag loads on submerged components during pool swell.	DFFR - Rev. 2 NEDE-21544-P	Acceptable	III.B.3.a.4
e) Wetwell Air Compression	Wetwell air compression is calculated by the PSAM. Defines the pressure loading on the wetwell boundary above the pool surface during pool swell.	DFFR - Rev. 2 NEDE-21544-P	Acceptable	III.B.3.a.5
f) Drywell Pressure History	Plant unique. Utilized in PSAM to calculate pool swell loads.	Plant Unique FSAR NEDM-10320	Acceptable if based on NEDM-10320. Otherwise plant unique reviews required.	III.B.3.a.6

Mark II Pool Dynamic Load Summary Table

Load or Phenomenon	Mark II Owners Group Load Specification	Reference	NRC Review Status	LER Section
2. Loads on Submerged Boundaries	Maximum bubble pressure predicted by the PSAM added uniformly to local hydrostatic below vent exit (wells and basemat) linear attenuation to pool surface. Applied to walls up to maximum pool swell elevation.	DFFR - Rev. 2 NEDE-21544-P	Acceptable	III.B.3.b
3. Impact Loads				
a) Small Structures	1.5 x Pressure-Velocity correlation for pipes and I beams. Constant duration pulse	DFFR - Rev. 2	NRC criteria I.A.6	III.B.3.c.1
b) Large Structures	None - Plant unique load where applicable	FSAR	Plant unique review where applicable	III.B.3.c.2
c) Grating	No impact load specified. P_{drag} vs. open area correlation and velocity vs. elevation history from the PSAM.	DFFR - Rev. 2	NRC Criteria I.A.3	III.B.3.c.3
4. Wetwell Air Compression				
a) Wall Loads	Direct application of the PSAM calculated pressure due to wetwell compression.	DFFR - Rev. 2 NEDE-21544-P	Acceptable	III.B.3.d.1
b) Diaphragm Upward Loads	2.5 psid	DFFR - Rev. 2	NRC Criteria I.A.4	III.B.3.d.2
5. Asymmetric Load	None	DFFR - Rev. 2	NRC Criteria I.A.5	III.B.3.e
C. Steam Condensation and Chugging Loads				
1. Downcomer Lateral Loads				
a) Single Vent Loads	8.8 KIP static	DFFR - Rev. 2	NRC Criteria I.B.1	III.B.4.a.1
b) Multiple Vent Loads	Prescribes variation of load per downcomer vs. number of downcomers	DFFR - Rev. 2	NRC Criteria I.B.2	III.B.4.a.2

Mark II Pool Dynamic Load Summary Table

Load or Phenomenon	Mark II Owners Group Load Specification	Reference	NRC Review Status	LER Section
2. Submerged Boundary Loads				
a) High Steam Flux Loads	Sinusoidal pressure fluctuation added to local hydrostatic. Amplitude uniform below vent exit-linear attenuation to pool surface. 4.4 psi peak-to-peak amplitude. 2-7 Hz frequencies.	January, 1977 Application memorandum	Acceptable	III.B.4.b.2
b) Medium Steam Flux Loads	Sinusoidal pressure fluctuation added to local hydrostatic. Amplitude uniform below vent exit-linear attenuation to pool surface. 7.5 psi peak-to-peak amplitude. 2-7 Hz frequencies.	January, 1977 Application memorandum	Acceptable	III.B.4.b.3
c) Chugging Loads	Representative pressure fluctuation taken from 4T test added to local hydrostatic.	January, 1977 Application memorandum	Acceptable pending resolution of FSI concerns.	III.B.4.b.4
- uniform loading condition	Maximum amplitude uniform below vent exit-linear attenuation to pool surface. +4.8 psi maximum overpressure, -4.0 psi maximum under pressure, 20-30 Hz frequency.	" "	" "	
- asymmetric loading condition	Maximum amplitude uniform below vent exit-linear attenuation to pool surface. 20 psi maximum overpressure, -14 psi maximum underpressure, 20-30 Hz frequency, peripheral variation of amplitude follows observed statistical distribution with maximum and minimum diametrically opposed.	" "	" "	

Mark II Pool Dynamic Load Summary Table

Load or Phenomenon	Mark II Owners Group Load Specification	Reference	NRC Review Status	LER Section
II. SRV-Related Hydrodynamic Loads				
A. Pool Temperature Limits for KWU and GE four arm quencher	No temperature limit	DFFR Revision 2	NRC Criteria II.1 and II.3	III.C.1
B. Quencher Air Clearing Loads	Mark II plants utilizing the KWU quencher use an interim load specification consisting of the ramshead calculational procedure. Mark II plants utilizing the four arm quencher use quencher load methodology described in DFFR.	DFFR Revision 2	NRC Criteria II.2	III.C.2.b III.C.2.c
C. Quencher Tie-Down Loads				
1. Quencher Arm Loads				
(a) Four Arm Quencher	Vertical and lateral arm loads developed on the basis of bounding assumptions for air/water discharge from the quencher and conservative combinations of maximum/minimum bubble pressure acting on the quencher.	DFFR Revision 2	Acceptable	III.C.2.e.1
(b) KWU T Quencher	KWU "T" quencher not included in Mark II O.G. Program. T quencher arm loads not specified at this time.	N/A	Review Continuing	

Mark II Pool Dynamic Load Summary Table

Load or Phenomenon	Mark II Owners Group Load Specification	Reference	NRC Review Status	LER Section
2. Quencher Tie-Down Loads				
(a) Four-Arm Quencher	Includes vertical and lateral arm load transmitted to the basemat via the tie downs. See II.C.1.a above plus vertical transient wave and thrust loads. Thrust load calculated using a standard momentum balance. Vertical and lateral moments for air or water clearing are calculated based on conservative clearing assumptions.	DFFR Revision 2	Acceptable	III.C.2.e.2
(b) KWU "T" Quencher	KWU "T" quencher not included in Mark II O.G. program. T quencher tie-down loads not specified at this time	N/A	Review Continuing	

Mark II Pool Dynamic Load Summary Table

Load or Phenomenon	Mark II Owners Group Load Specification	Reference	NRC Review Status	LER Section
III. LOCA/SRV Submerged Structure Loads				
A. LOCA/SRV Jet Loads				
1. LOCA/Ramshead SRV Jet Loads	Methodology based on a quasi-one-dimensional model.	NEDE-21730 NEDE-21471	NRC Criteria III.A.1	III.D.1.a III.D.1.b
2. SRV-Quencher Jet Loads	No loads specified for lead plants. Model under development in Long Term Program.	N/A	NRC Criteria III.A.2	III.D.1.c
B. LOCA/SRV Air Bubble Drag Loads				
1. LOCA Air Bubble Loads	The methodology follows the LOCA air carryover phase from bubble charging, bubble contact, pool rise and pool fallback. The drag calculations include standard and acceleration drag compenents.	DFFR - Revision 2 NEDE-21471 NEDE-21730	NRC Criteria III.B.1.	III.D.2.a
2. SRV-Ramshead Air Bubble Loads	The methodology is based on an analytical model of the bubble charging process including bubble rise and oscillation. Acceleration drag alone is considered.	NEDE-21471	NRC Criteria III.B.2	III.D.2.b
3. SRV-Quencher Air Bubble Loads	No quencher drag model provided for lead plants. Lead plants propose interim use of ramshead model (See III.B.2 above). Model will be developed in long term program.	N/A	NRC Criteria III.B.3.	III.D.2.c
C. Steam Condensation Drag Loads	No generic load methodology provided. Generic model under development in long term program.	N/A	Lead plant load specification and NRC review will be conducted on a plant unique basis with confirmation in long term program using generic model.	III.D.3

Mark II Pool Dynamic Load Summary Table

Load or Phenomenon	Mark II Owners Group Load Specification	Reference	NRC Review Status	LER Section
IV. Secondary Loads				
A. Sonic Wave Load	Negligible Load - none specified	DFFR - Revision 2	Acceptable	III.E.1
B. Compressive Wave Load	Negligible Load - none specified	DFFR - Revision 2	Acceptable	III.E.2
C. Post Swell Wave Load	No generic load provided.	N/A	Plant unique load specification and NRC review	III.E.3
D. Seismic Slosh Load	No generic load provided.	N/A	Plant unique load specification and NRC review.	III.E.4
E. Fallback load on Submerged Boundary	Negligible load - none specified	DFFR - Revision 2	Acceptable	III.E.5
F. Thrust Loads	Momentum balance	DFFR - Revision 2	Acceptable	III.E.6
G. Friction Drag Loads on Vents	Standard friction drag calculations	DFFR - Revision 2	Acceptable	III.E.7
H. Vent Clearing Loads	Negligible Load - none specified	DFFR - Revision 2	Acceptable	III.E.8

used in the evaluation of Mark II cc inments and related systems unless additional information is provided to justify these differences and until such information has b reviewed and found acceptable by the NRC staff.

In a few areas, we have required that additional information be provided prior to operation of the first Mark II plant. The most significant of these areas relates to the SRV quencher devices. Testing programs to establish loads for both the cross quencher and T quencher are not complete and final load specifications for these devices have not been provided to the NRC. Interim acceptance criteria for these devices are provided in our acceptance criteria, wherein ramshead loads may be used for T quenchers and the DFFR, Revision 2 cross quencher loads may be used for the cross quencher. A substantial body of currently available test data supports the conservatism of the SRV loads specified in our acceptance criteria. Nevertheless, we shall require completion of large scale tests in support of these devices prior to plant operation of the first plant that uses them.

Several pool dynamic related loads were not included in the Mark II owner's generic program. These will be reviewed on a plant unique basis as a part of the staff's review of the individual DARs.

B. Long Term Program

The Mark II Owners Group is sponsoring a number of Tasks that extend beyond the Lead Plant Program. These tasks are listed in Table E-1 of Appendix E. They include a number of analytical and experimental programs relating to most of the significant pool dynamic loads. The primary purpose of the tasks in this extended program is to provide a basis for the reduction of selected design basis loads. These tasks will provide additional confirmation for a number of the loads utilized in the LPP.

The staff has reviewed the description of the tasks included in the proposed Long Term Program as described in References 7 and 61. We find the proposed program suitably designed for meeting the stated objectives and conclude that it is acceptable. We will continue to monitor the progress of the LTP to assure conservatism in the current lead plant load specifications. In addition, we will review any proposed load reductions in light of the results of the LTP tasks. Our evaluations will be provided in a revision to this report.

The NRC staff will also monitor the results of other related domestic and foreign programs currently underway.

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Appendix A. Mark II Containment Program Chronology

A chronology of the significant events related to the Mark II containment system reevaluation program is presented in Table A-1.

Table A-1
Mark II Containment Program
Chronology of Events

1.	1958 - 1962	Humboldt Bay and Bodega Bay Testing on the viability of the pressure-suppression pool concept.
2.	1972 - 1974	Pressure Suppression Test Facility (PSTF) testing on the viability of the Mark III containment concept.
3.	April, 1975	Pennsylvania Power and Light Company stops work on portions of the Susquehanna Nuclear Plant because of concerns about ability of the suppression chamber to withstand pool dynamic forces.
4.	April 17, 1975	NRC issues standard letter to licensees concerning primary system pressure relief valve loads for plants with Mark II type containments.
5.	April 18, 1975	NRC issues standard letter to licensees concerning pool dynamic LOCA loads for plants with Mark II type containments.
6.	May, 1975	Formation of Mark II Owners Group.
7.	June 30, 1975	Meeting: Mark II Owners Group and NRC Staff. Mark II owners present a generic program and schedule to establish Mark II LOCA and Safety Relief Valve Loads.
8.	October 24, 1975	Mark II owners submit Rev. 0 of the Mark II Containment Dynamic Forcing Function Information Report.
9.	December 1975 - June, 1976	Mark II owners submit plant unique Design Assessment Reports.
10.	April, 1975 - June, 1976	Mark II owners notify regional NRC offices of design deficiencies associated with the identification of pool dynamic loads in accordance with 10 CFR 50.55 (e).
11.	April 29, 1976	Meeting: Mark II owners, GE, NRC Staff. Discussion to detemrine if a common basis for Mark II loads included in the DFFR is feasible. Discuss status of the Mark II owner's supporting program.

12.	May 20, 1976	Meeting: Mark II owners, NRC Staff, Discussion of DFFR revisions required to make the forcing function report useful as a generic loads report.
13.	May - June, 1976	Mark II owners submit the 4T phase 1 test report and applications memorandum.
14.	June 23, 1976	NRC issue questions to the Mark II owners dealing with the Mark II containment dynamic forcing function information report.
15.	September, 1976	Mark II owners submit schedules for revisions to the plant unique Design Assessment Reports.
16.	October 27, 28, 1976	Meeting: Mark II owners, NRC Staff. Discussion of draft NRC questions for several Mark II supporting program tasks. NRC specifies need for pool swell air tests and multivent steam tests.
17.	December 2, 1976	Meeting: Mark II owners, NRC Staff. Discussion of the method of combining loads for structural design and the status of the Mark II supporting program.
18.	January 12, 1977	NRC issues questions to Mark II owners related to several Mark II supporting program tasks.
19.	February 16, 17, 1977	Meeting: Mark II owners, NRC Staff. Discussion of Mark II supporting program. Discussions of EPRI tests, chugging program, fluid structure interaction program, role of Japanese multivent tests in Mark II program and preliminary results of Monticello SRV tests.
20.	May 10, 1977	Meeting: Mark II owners' Executive Committee, NRC Management. Discussion of large scale multivent steam tests. Mark II owners propose a two part pool dynamic load program consisting of a Lead Plant Program and a Long Term Program.

21. May 18, 19, 1977 — Meeting: Mark II owners, NRC. Discussion of Mark II owners chugging program to show that single vent 4T steam loads are bounding. NRC agrees with Mark II owners that full scale multivent steam tests are probably not required. NRC highlights difficulties in the use of scaled multivent steam tests to refine single vent bounding chugging loads.

22. May 26, 1977 — Meeting: Mark II owners and NRC Staff. Discussion of draft questions for Mark II Supporting Program Tasks. NRC highlights deficiencies in several DFFR load specifications.

23. June 6, 1977 — NRC sends GE a letter indicating the need to establish SRV loads associated with leaking valves.

24. June 15, 1977 — Meeting: General Electric and NRC. Discussion of adequacy of several DFFR load specifications including the vent lateral load, maximum pool swell height and wetwell - drywell pressure differentical.

25. July 7-8, 1977 — Meeting: ACRS Subcommittee, Mark II Owners and NRC Staff. Discussion of Mark II research programs related to pool dynamics issues.

26. July 29, 1977 — Meeting: Mark II owners and NRC. Discuss structure and schedule for the Mark II owners Lead Plant Program and Long Term Program.

27. August 11, 1977 — Mark II owners submit lead plant topical report to justify proposed pool boundary and main vent chugging loads.

28. August 31 - September 1, 1977 — Meeting: Mark II owners and NRC, Discussion of status of several Mark II supporting program tasks.

29. September 14, 1977 — NRC generic program to review Mark II pool dynamic loads approved for implementation.

30. October 11, 1977 — GE sends NRC a Part 21 Notification related to a new SRV load combination case dealing with multiple actuation of Safety Relief Valves.

31.	November 1 - 3, 1977	Meeting: Mark II owners, NRC, KWU and TUV. Discussed German testing work and design load bases related to LOCA condensation loads and SRV loads.
32.	December 16, 1977	Meeting: Mark II owners and NRC. Discussion of fluid structure interaction concerns related to the establishment of vent lateral loads and chugging boundary loads. Discussed status of Mark II multivent steam tests and submerged structure drag load tasks. NRC presents positions for several LOCA related pool dynamic loads.
33.	January 3, 1978	Mark II owners submit preliminary program plan for multivent steam tests.
34.	January 18, 19, 1978	Meeting: NRC and Mark II Owners Group, Discussion of methods for load predictions for various SRV devices. Mark II owners discuss plans for an Intermediate Program to justify a reduction in the Lead Plant Program loads prior to completion of the Long Term Program.
35.	January - February, 1978	Mark II owners submit reports describing models for calculating submerged structure drag and jet loads for ramshead and LOCA air discharge.
36.	March 1, 1978	Meeting: General Electric and NRC. Discussion of GE's methods and schedule to evaluate BWR NSSS Equipment Adequacy considering additional hydrodynamic containment response loads.
37.	March 3, 1978	Meeting: Mark II owners and NRC. Discussion of fluid structure interactions associated with LOCA steam and SRV loads. NRC indicates need for meeting with Livermore to resolve differences in GE and Livermore SRV fluid structure interaction studies for the Monticello tests.

38.	April 5, 6, 1978	Meeting: Mark II owners and NRC. Discussion of GE and Livermore studies of the role of fluid structure interactions in the Monticello SRV load measurements. Also discussed the BNL evaluation of the DFFR pool swell impact loads. NRC discusses proposed position that all Mark II plants utilize a quencher SRV device.
39.	April, 1978	Mark II owners submit a major revision to the Mark II containment Supporting Program Report.
40.	April 28, 1978	NRC issue questions and positions related to the Lead Plant Program supporting program.
41.	May 16, 17, 1978	Meeting: Mark II owners and NRC. The Mark II owners present their plans for an Intermediate Plant Licensing Program to justify a reduction in the Lead Plant Program loads in time for use by the next group of Mark II plants to be licensed. Mark II owners discussed status of their efforts to establish an impulse chugging wall load.
42.	May 23, 1978	Meeting: ACRS Subcommittee and NRC Staff. Discuss status of the staff generic review related to Mark II LOCA and SRV pool dynamic loads.
43.	May - July, 1978	Lead Mark II plants notify NRC of intention to switch from ramshead to quencher SRV devices.
44.	August 15, 1978	Meeting: Mark II owners and NRC. The Mark II owners present a status report on several of their Long Term Program tasks including the multivent test program, improved chug load definition and the CAORSO tests. Also discussed NRC concerns related to submerged structure drag loads, pool temperature limits and the 4T chug impulse study.
45.	September 11, 1978	NRC acceptance criteria for Mark II pool dynamic loads sent to the lead Mark II plants.

APPENDIX B

TEST PROGRAMS

1. <u>Mark II Pressure Suppression Tests (4T)</u>

The primary data base for LOCA - related hydrodynamic loads was obtained at the 4T test facility constructed by General Electric in support of the Mark II Owners Group Containment Program. This facility simulates a single cell of a full-scale Mark II suppression pool. The objective of the test programs conducted at this test facility was to evaluate pool dynamic effects on the Mark II containment geometry due to a LOCA.

The test facility consisted of a steam generator, a tank to simulate the drywell volume, a single downcomer, and a vessel simulating a single cell of a prototypical wetwell. A schematic diagram of the 4T facility is shown in Figure B-1. Pertinent dimensions and other design parameters are listed in Table B-1.

The test program consisted of approximately 60 blowdowns,[1, 2] all but two being saturated vapor blowdowns. The two exceptions employed saturated liquid blowdowns. All blowdowns were initiated by rupturing a disc located between the steam generator and the drywell. This disc was located downstream of a flow restrictor (calibrated venturi) which controlled blowdown flow rates. Four different blowdown flow rates were employed corresponding to two venturi sizes (2-1/2, 3 in.

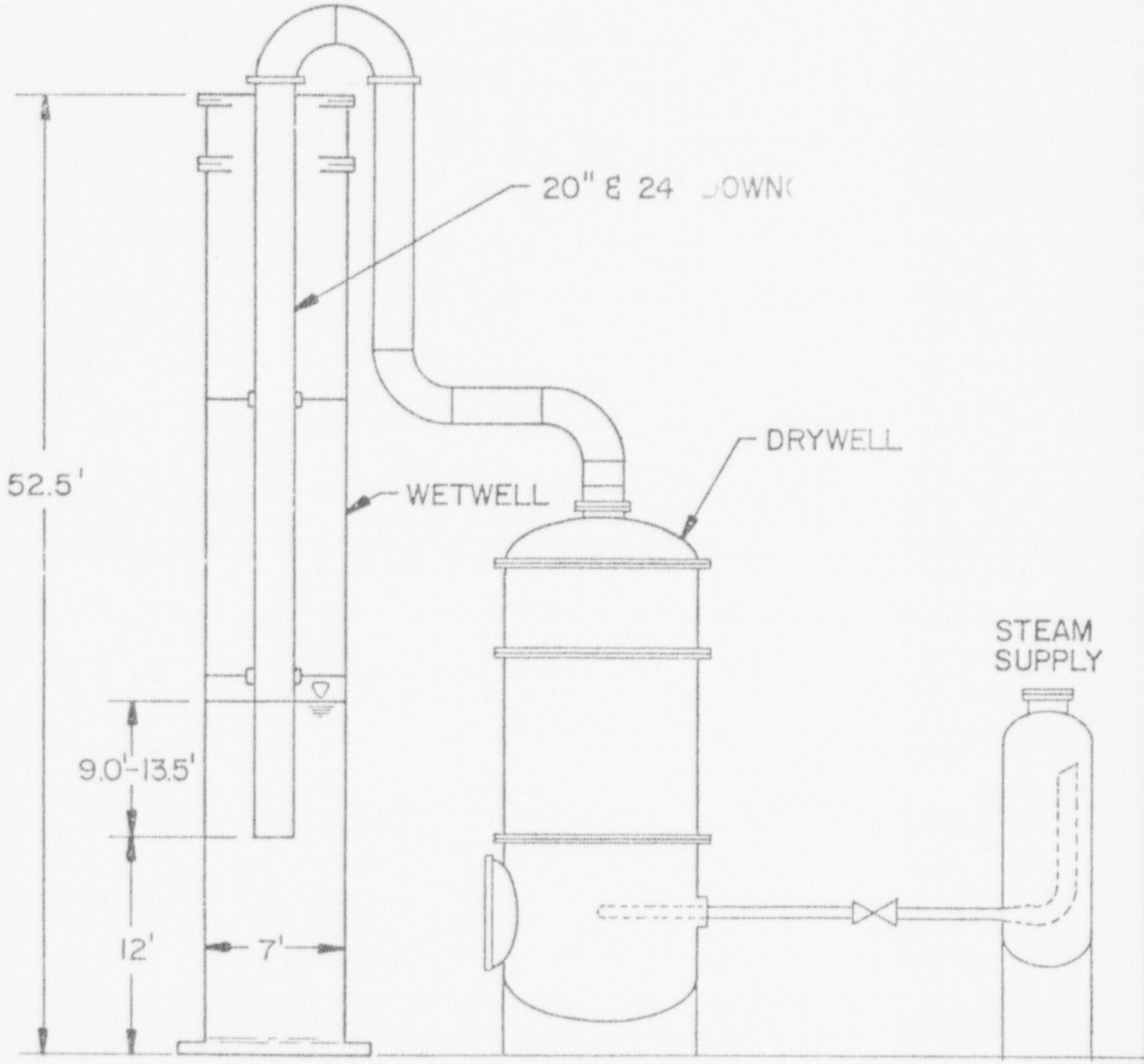


FIGURE B-1 SCHEMATIC OF THE 4T TEST FACILITY

Table B-1

COMPARISON OF MODIFIED PSTF AND MARK II CONTAINMENTS

Scaling Parameter	Modified PSTF 24-in. Downcomer 2-1/2-in. Venturi	Modified PSTF 24-in. Downcomer 3-in. Venturi	Modified PSTF 20-in. Downcomer 2-1/2-in. Venturi	Modified PSTF 20-in. Downcomer 3-in. Venturi	Susquehanna Plant	Mark II Range
Break Area/Drywell Free Volume (ft^{-1})	1.80×10^{-5}	2.60×10^{-5}	1.80×10^{-5}	2.60×10^{-5}	1.66×10^{-5}	N/A
Break Area per Vent (ft^2)	0.0341	0.0491	0.0341	0.0491	0.0448	N/A
Break Area/Vent Area	0.0116	0.0167	0.0169	0.0243	0.0152	N/A
Break Area/Pool Area[b]	9.97×10^{-4}	14.35×10^{-4}	9.70×10^{-4}	13.96×10^{-4}	7.82×10^{-4}	N/A
Vent Diameter, in. (Nominal)	24	24	20	20	24	24 – 28[a]
Drywell Volume per Vent (ft^3)	1,892	1,892	1,892	1,892	2,700	1,800 – 2,700
Drywell Volume/Vent Area (ft)	642	642	936	936	914	575 — 914
Pool Area[b] per Vent (ft^2)	34.21	34.21	35.17	35.17	57.45	36.8 – 60.0
Pool Area[b]/Vent Area	11.60	11.60	17.40	17.40	19.49	12 – 20
Clearance, Downcomer to Pool Bottom (ft)	← 12.0 →				12.00	8.3 – 18.0
Vent Submergence (ft)	← 9.0, 11.0, and 13.5 →				11.0	8.8 – 13.5
Clearance, Pool Surface to Ceiling (ft)	← 31.5, 29.5, and 27.0 →				29	22.7 – 37.0
Overall Height (ft)	← 52.5 →				52	45.4 – 62.0

[a] 28-inch downcomers have 10-inch relief valve piping located concentrically within the downcomer.
[b] Pool area is a net value which excludes areas of downcomers, support columns, and pedestal.
[c] All units and scaling ratios in this table are English units.

diam.) at the saturated vapor and liquid conditions. Additional parameters which were varied included downcomer submergence, diameter, and bracing configuration, wetwell pool temperature and drywell initial temperature. In addition, the blowdowns were conducted with and without venting of the wetwell airspace to atmosphere.

The measurements which were taken yielded data on pool swell hydrodynamic loads and pool swell behavior, froth and breakthrough. In addition, data were obtained on downcomer lateral loads and pressure fluctuation loads on the submerged wetwell during steam condensation and chugging.

The data from these tests form the basis for qualification of the pool swell analytical model[3] and specification of diaphragm loads and submerged wetwell pressure loads during steam condensation.[4, 5]

2. Pressure Suppression Test Facility (PSTF)

The Pressure Suppression Test Facility consists of three major components: 1) a 1/3-scale model of an 8-degree sector of the Mark III containment pool; 2) a volumetrically scaled Mark III drywell; and 3) an electrically heated steam generator. A schematic diagram of the PSTF is shown in Figure B-2. During simulated LOCA in the PSTF, air, which is initially in the drywell,

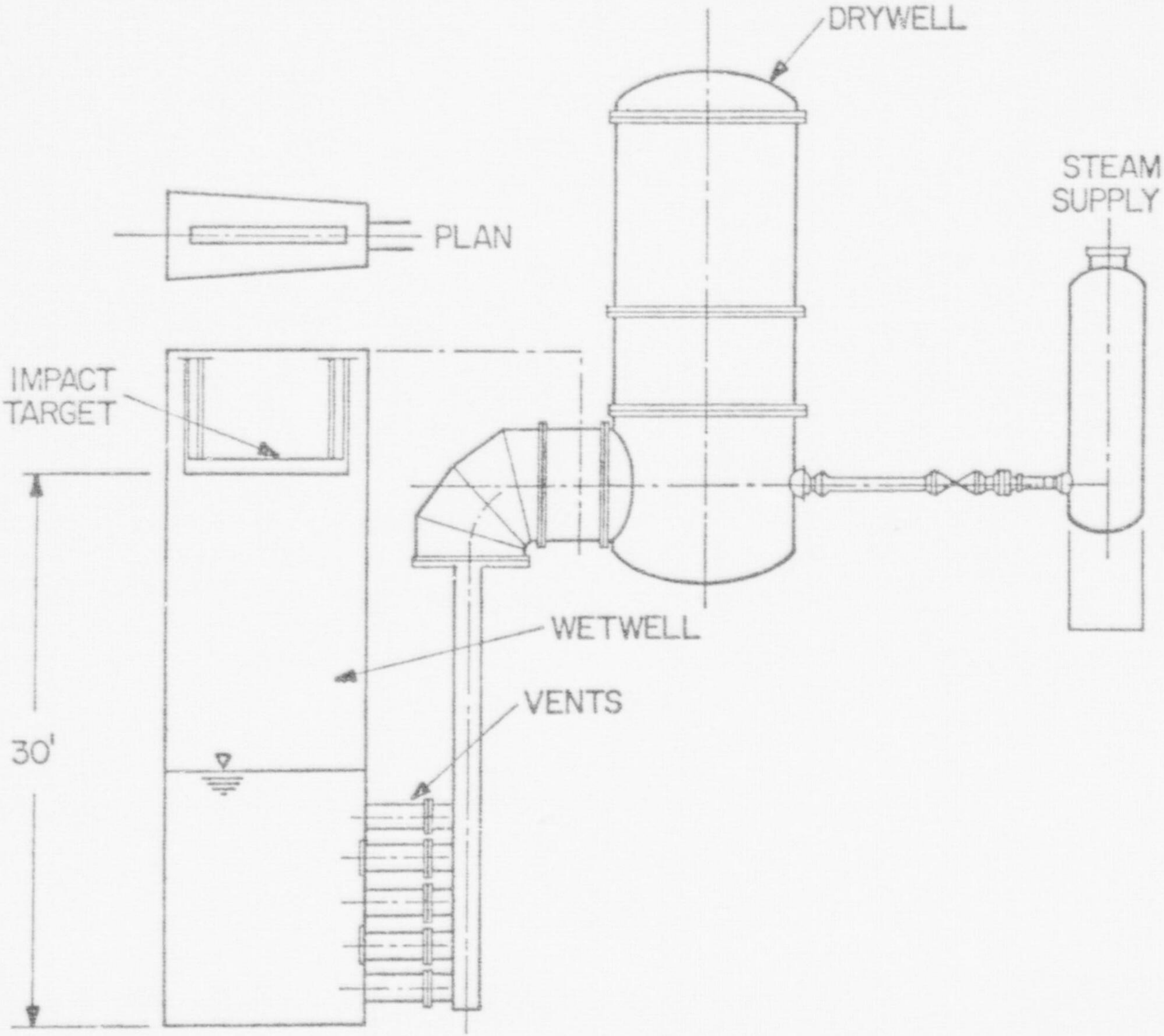


FIGURE B-2 SCHEMATIC OF THE PSTF TEST FACILITY is forced into the pool through horizontal vents. As a consequence, the pool surface rises to accommodate the expanding air bubbles.

The particular PSTF tests of interest for this SER are the Test Series 5805.[6]

In these tests, specific emphasis was placed on the effects of pool impact on structures located above the suppression pool. Targets of various sizes and geometries (cylinders, I-beams, grating) were mounted above the pool where they would be impacted by the rising water. These targets were selected to be representative of actual structures that might be present above Mark III pools. The targets were oriented parallel as well as perpendicular to the line of the vents. A total of 51 test runs were performed. The pool surface velocity and slug thickness at impact were controlled by variations in the initial pool depth and the charging rate of air into the pool. During impact, the total force on the targets, as well as local pressures at specific locations on the impacted surface, were recorded as functions of time. These data provided the information needed to determine: maximum force, pulse duration, impulse and bending moment.

The results of the PSTF indicate that the total impulse correlates well with the measured velocity and geometry of the impacted structure. The maximum pressure and pulse duration, on the other hand, exhibited considerable variation in the data. This effect was ascribed, primarily, to the differences in pool surface shape from run to run.

3. Electric Power Research Institute 1/13 Scale Tests

The Mark II 1/13 scale tests[7] were performed by the Electric Power Research Institute (EPRI) to examine the potential for three-dimensional effects during the pool swell phase of a LOCA blowdown. Thus, the objective was to demonstrate the validity of the single cell approach and, in general, to provide additional data to assess the adequacy of the General Electric pool swell analytical model.[3]

The test facility simulated a 90° sector of a typical Mark II containment. Strict geometric similarity was maintained for the wetwell including the geometry and layout of downcomers, columns and pedestal. This is shown in Figure B-3. The drywell volume was sized to be consistent with the Moody scaling laws[8] and orifice flow restrictors were installed in the vents to correctly simulate enthalpy fluxes.

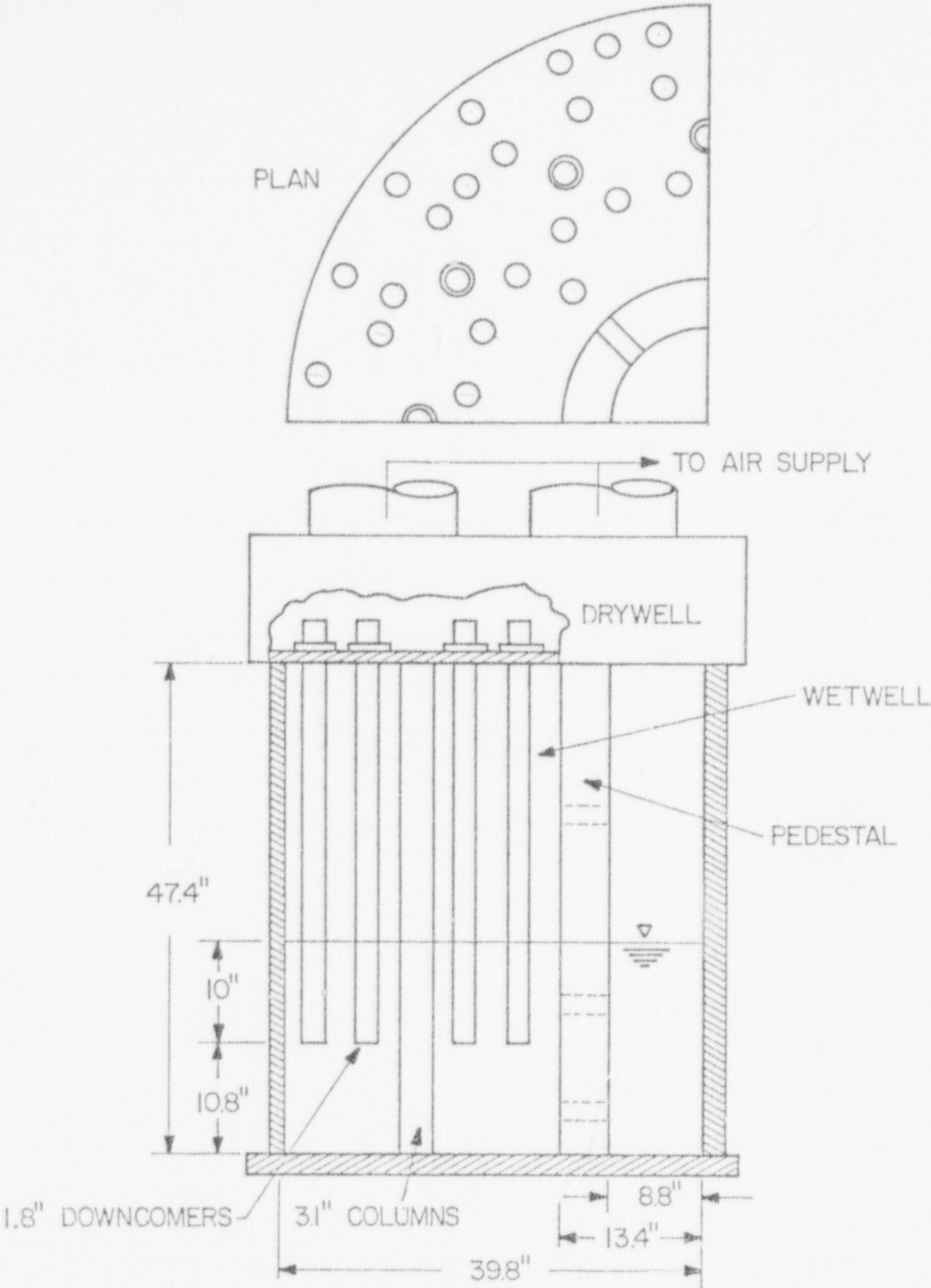


FIGURE B-3 SCHEMATIC OF THE EPRI SUB-SCALE TEST FACILITY

The LOCA blowdowns were simulated by charging of the drywell with air from a supply tank. The flow rates were controlled by an orifice located between the supply tank and drywell and the supply tank initial pressure. The flow rates were selected to simulate the nominal drywell pressure response given in the plant FSAR (Susquehanna Plant).

A total of seventeen blowdowns were conducted, three of which were at the nominal conditions corresponding to the prototypical Mark II containment. The remaining blowdowns represented perturbations from the nominal and included variations in submergence, drywell charging rate and total pool water volume. Some parametric studies of vent orifice size and initial drywell overpressure were also conducted. The effect of plugging the pedestal flow holes was also examined.

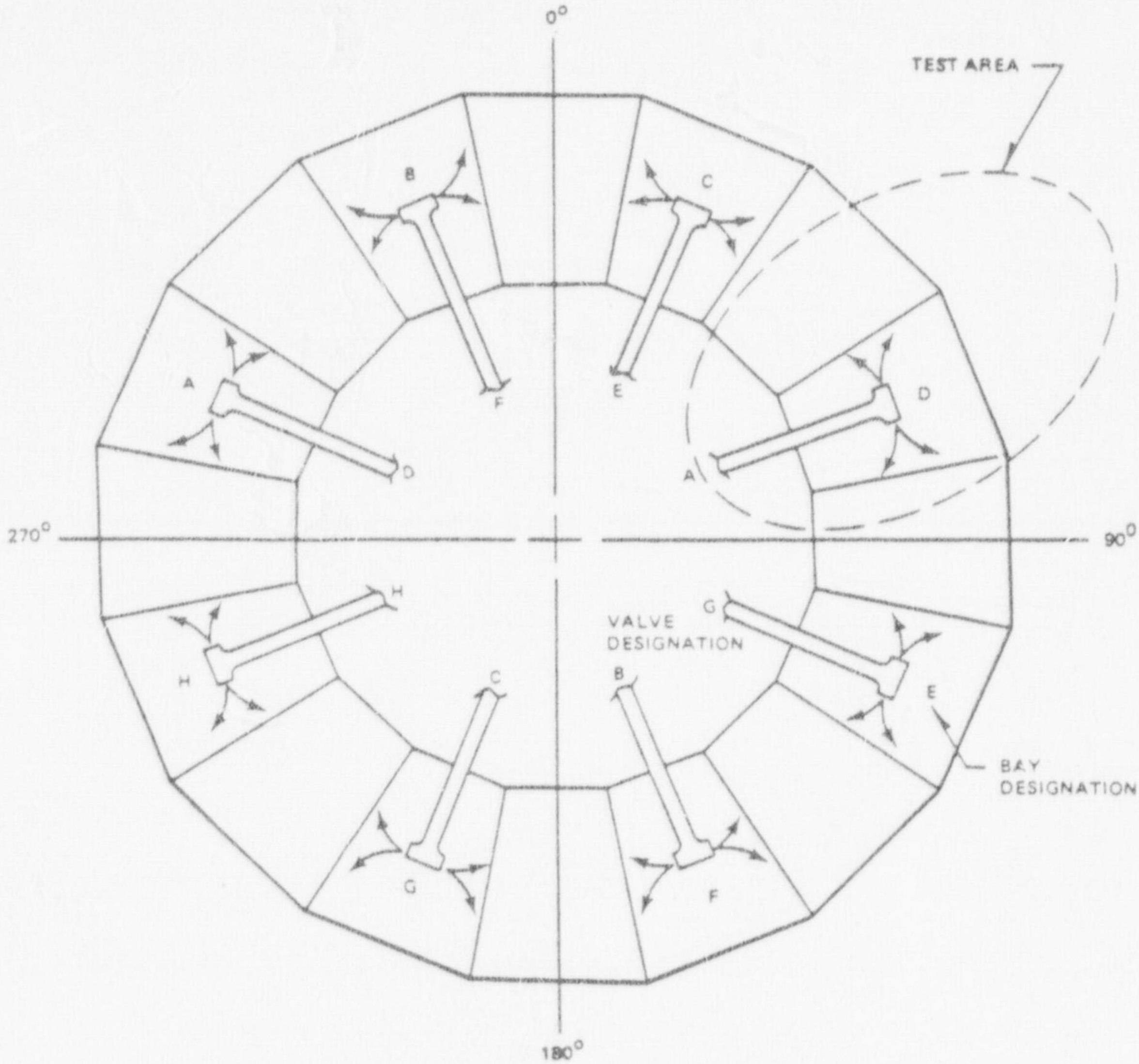
The facility was constructed of clear plastic to permit visual and photographic observation of the pool swell phenomenon. The supply tank, drywell and wetwell airspace pressures were measured continuously using fast response transducers. The timewise variation of the pool swell motion was determined from these pressures by applying appropriate conservation laws via a computer code specifically developed for this purpose.

The results of these studies indicated that three-dimensional effects are small during pool swell in a Mark II containment. It was also found that the pool swell motion is accurately predicted by the General Electric analytical model.[3]

4. Monticello Tests

Containment pressure loads due to ramshead SRV discharges were measured in-plant at the Monticello Mark I BWR plant of the Northern State Power Company.[9] These tests were conducted during the plant's refueling outage in the September - October 1975 period. The objective of this test program was to provide the necessary data base for pressures and temperatures in the torus and SRV piping associated with single, consecutive and multiple valve actuations. These data were used to verify the analytical model[10] employed by the Mark II Owners Group to specify boundary loads during SRV discharges.

A schematic diagram showing the location of SRV valves within the Monticello torus is shown in Figure (B-4). There were 37 tests performed; 22 single valve actuations, 5 two-valve actuations, 4 three-valve actuations and 6 consecutive actuations. All valves were actuated manually and closed after a predetermined time. The



BAY	S/R VALVE DESIGNATION	CATEGORY	AZIMUTH (DEGREE)
A	RV2-71D		292-1/2
B	RV2-71F		337-1/2
C	RV2-71E		22-1/2
D	RV2-71A	ADS*	67-1/2
E	RV2-71G		112-1/2
F	RV2-71B	ADS	157-1/2
G	RV2-71C	ADS	202-1/2
H	RV2-71H		247-1/2

* ADS = AUTOMATIC DEPRESSURIZATION SYSTEM

Figure B-4 Orientation of Safety/Relief Valve Discharges Within Monticello Torus consecutive actuations were performed by reopening the valves after initial closure for designated durations and then reclosed. The initial power level for all the tests was chosen to be about 70% of full reactor power and the pool temperature maintained between 75° to 95°F. A total of seven tests were conducted after an SRV developed steam leaks into the discharge pipe. A quantification of the magnitude of the leak was not made.

The sensor group for hydrodynamic data consisted of 21 pressure transducers, 15 temperature detectors, 12 water level/void probes, and 1 vacuum breaker flow indicator. The output of these sensors provided pressure loading histories within the torus pool, on the torus shell, and within the SRV discharge line. Also measured were SRV line temperatures, torus pool temperature, the opening and closing of the SRV line vacuum breaker valve, and water reflood into the SRV line following SRV closure.

The major findings from the test data are summarized as follows.

1. Single Valve Actuation - The highest recorded values of peak positive and negative pressures on the torus shell due to air bubble oscillation were below predictions based on the original DFFR methodology (modified slightly for application to the Mark I configuration).

2. Consecutive Valve Actuation - The worst case consecutive actuation test showed an increase of approximately 85% in peak positive pressure over the mean single valve actuation valve and a thirty percent increase in peak negative pressure. These results led to modifications in the proposed DFFR methodology for consecutive valve actuations in Mark II plants.

3. Multiple Valve Actuation - For tests run with 3 adjacent valves the peak positive pressure recorded showed a load increase (50%) over a single valve actuation results. The negative peak pressure increased by 30%.

4. The leaky valve tests demonstrated that discharge loads can increase somewhat by this type of SRV malfunction, although the observed increases were less than those that occurred during the more severe consecutive valve actuation tests.

5. The maximum SRV discharge line pressure recorded was about 80% of the value predicted by the DFFR (Mark II) methodology. The peak line pressure in consecutive valve or leaky valve tests were slightly higher than those for single valve actuations.

5. LOCA - Related Foreign Licensee Tests

a. GKM I Tests

Lateral loads generated at the end of the vertical downcomers during chugging were investigated at this foreign test facility.(11) The primary objective of this program was to quantify lateral downcomer loads over a range of prototypical steam blowdown conditions.

The test facility consisted of a wetwell, simulated by a cylindrical tank, 10 feet in diameter and 60 feet high, and a single downcomer, two feet in diameter with ten feet of submergence into the wetwell pool (Figure B-5). The lateral loads were determined from measurements with strain gauges and linear displacement transducers located on the downcomer between the suppression pool surface and the lateral support struts. Steam flow rate through the vent system, mass fraction of air in the flow and pool temperature were varied parametrically in the tests. Computation of energy deposition in the pool permitted evaluation of lateral loads as a function of pool temperature. All tests were performed with the wetwell vented to atmosphere.

The major finding from this test program was that maximum lateral loads occur at the later stages of a LOCA blowdown; i.e., under conditions of low steam flux rates, high pool temperature and pure

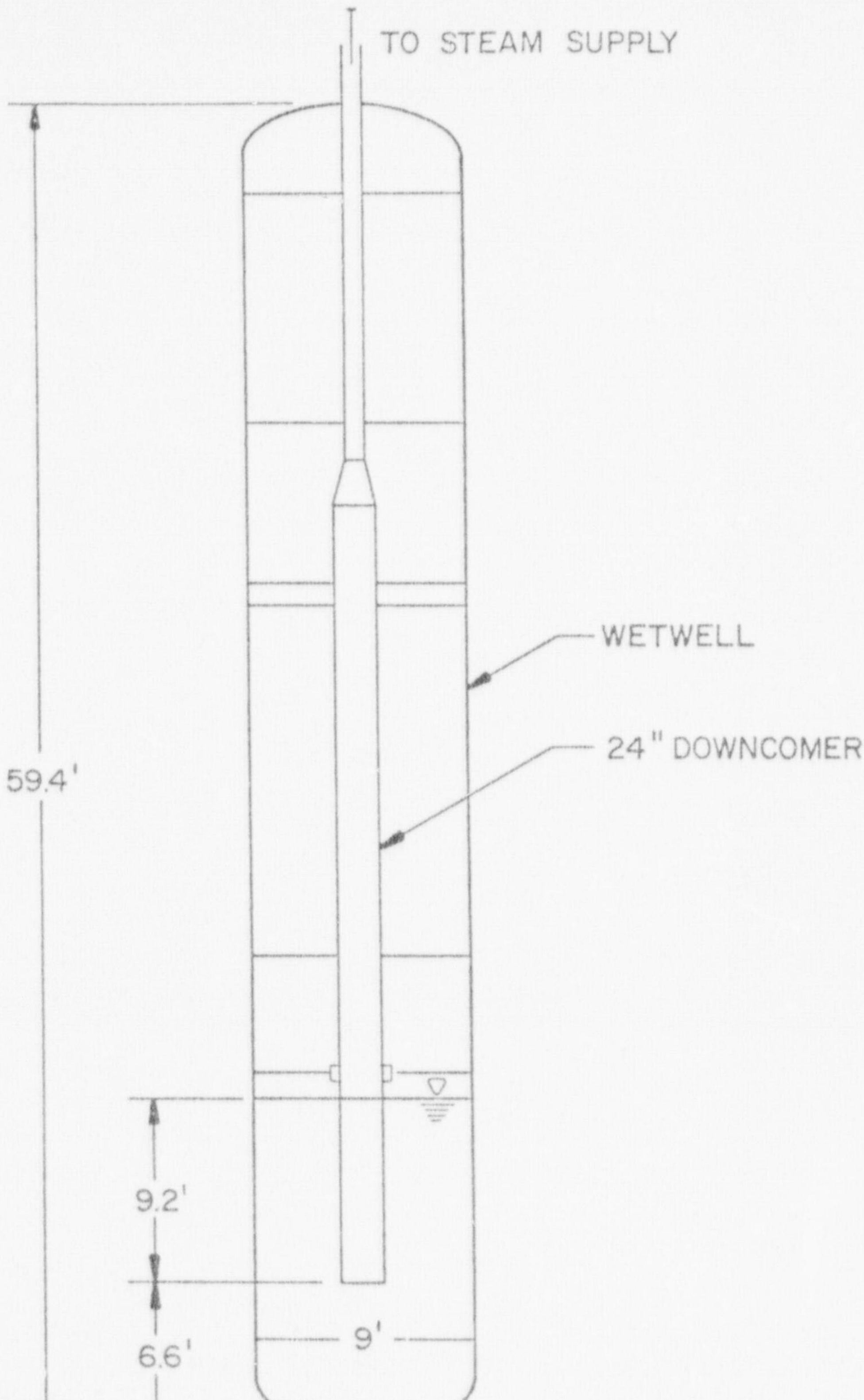


FIGURE B-5 SCHEMATIC OF THE GKM I TEST FACILITY steam flow. It was also found that the force magnitudes are stochastically distributed, that the force direction is random and that the time interval between consecutive chugs varies over a small range.

The results of these tests form the basis for the Mark II Owners Group specification of downcomer lateral load on a single vent. The data is also used to develop loads for groups of downcomers according to probabilistic methods.

b. GKM II Tests

The pressure loads experienced by the submerged pool boundaries during the steam blowdown phase of the LOCA event were investigated at this foreign test facility.[12] The primary purpose of these tests was to establish a full-scale single vent data base for conditions representative of the KWU BWR containment design.

The facility consisted of a steam generator, drywell, wetwell and a 24 inch diameter vent (Figure B-6). Transient blowdowns were performed for two submergences (6.6 and 9.2 feet), a maximum vent flux of about 10 $lb/ft^2/sec$ and initial pool temperature ranging from 85°F to 140°F. The effect of wetwell pressurization was also examined (2.6 - 2.9 bar). Pressure measurements were

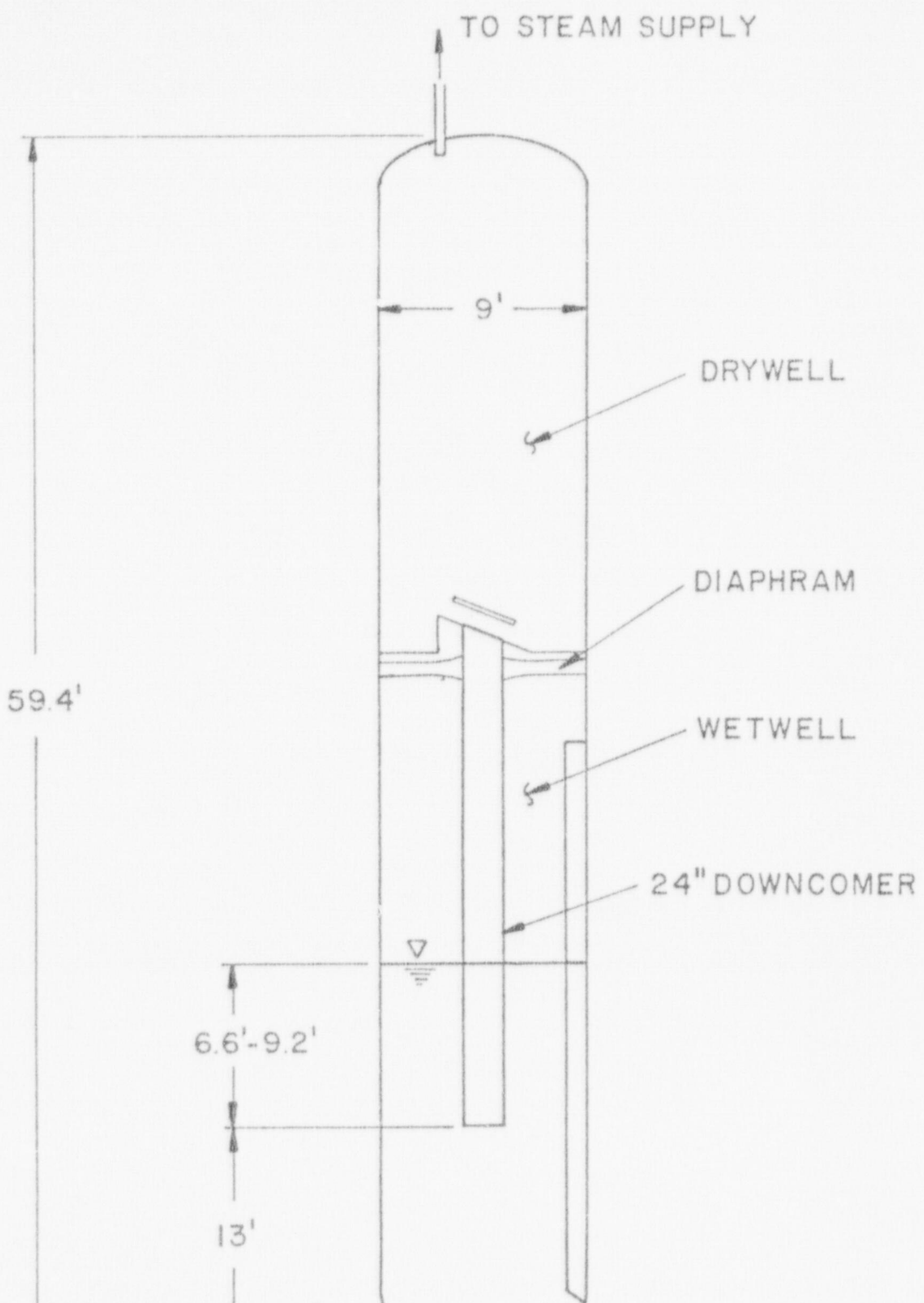


FIGURE B-6 SCHEMATIC OF THE GKM II TEST FACILITY taken at various points on the suppression pool boundaries including the pool bottom and on a flexible plate installed in the suppression pool to simulate the stiffness of the KWU prototype containment.

The relevant findings from these tests for the Mark II Owners Group load evaluation was that pool temperature had a negligible effect on the overpressures experienced at the boundaries during chugging.

c. KWU-Karlstein Tests

Downcomer lateral loads and submerged boundary pressure loads during chugging in multi-vent configuration were studied in these foreign test facilities.[13] The primary objective of these tests was to evaluate the influence of multiple vents on the hydrodynamic loads encountered by BWR containment structures during steam blowdown, condensation and chugging.

The test facilities included 1 and 2 vent tests with 24" downcomers (Figure B-7), 1, 3, 4 and 6 vent tests with 12" downcomers (Figure B-8) 1, 2, 3, 6, 8, 12 vent tests with 3" downcomers (Figure B-9) and 3 vent tests with 6" downcomers (Figure B-10). All tests

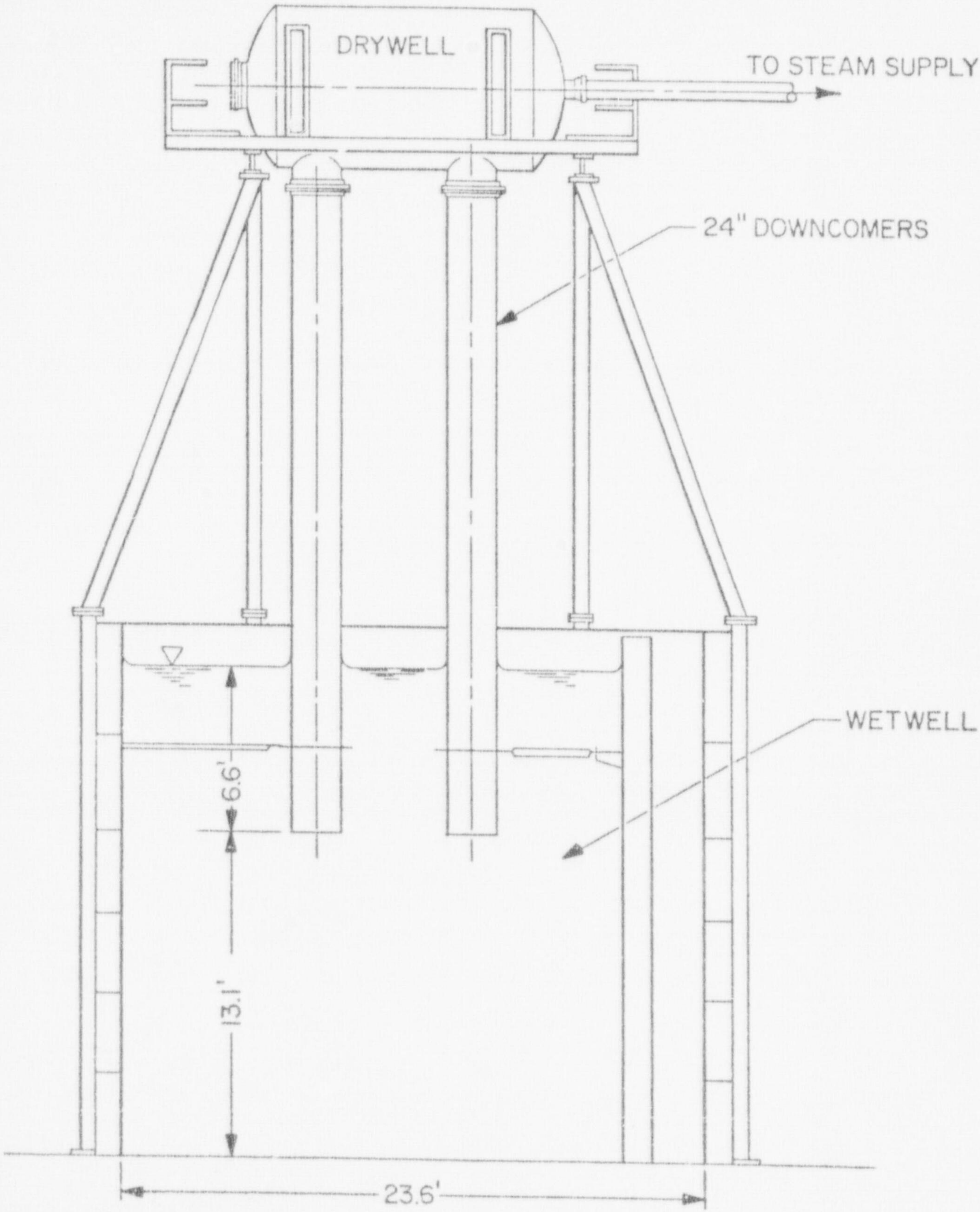


FIGURE B-7 SCHEMATIC OF THE 24" DOWNCOMER
KWU-KARLSTEIN TEST FACILITY

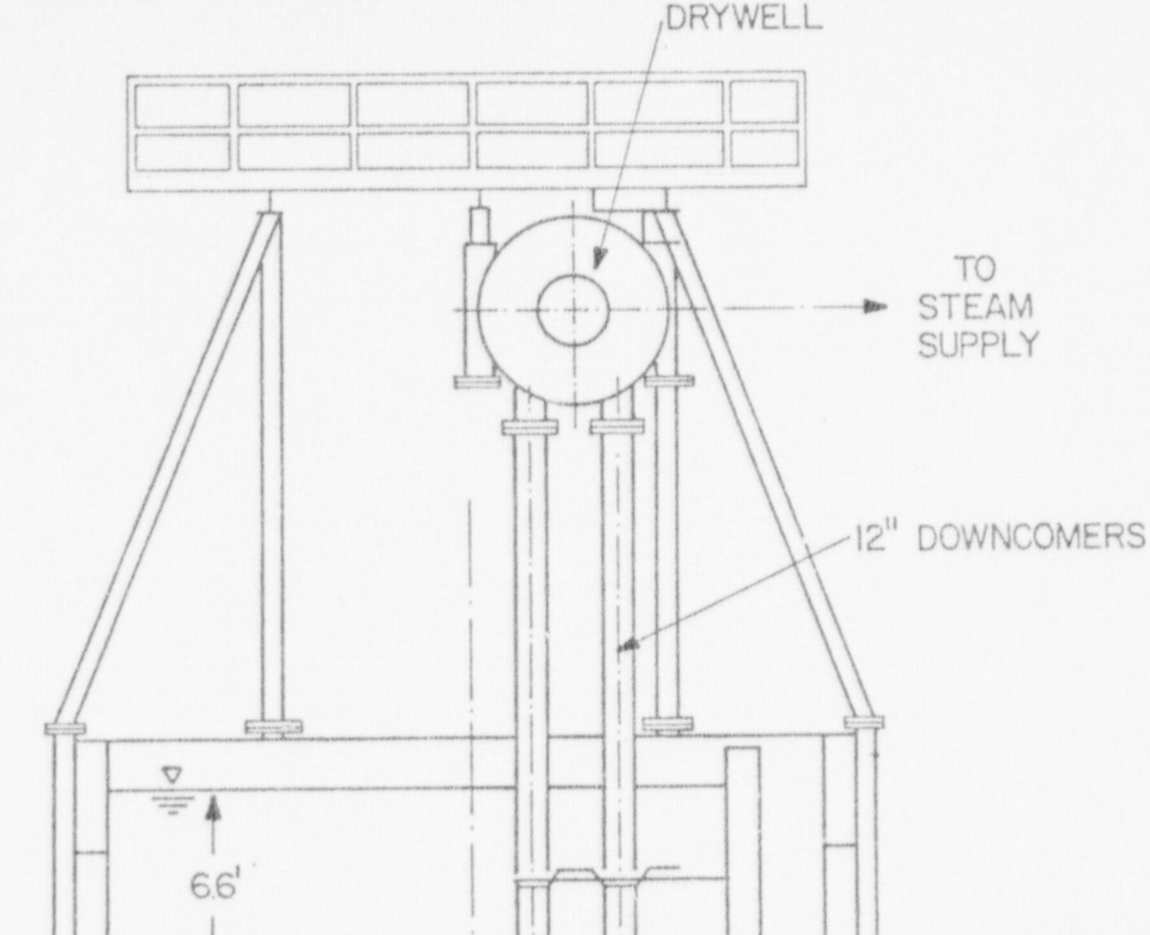


FIGURE B-8 SCHEMATIC OF THE 12" DOWNCOMER
KWU-KARLSTEIN TEST FACILITY

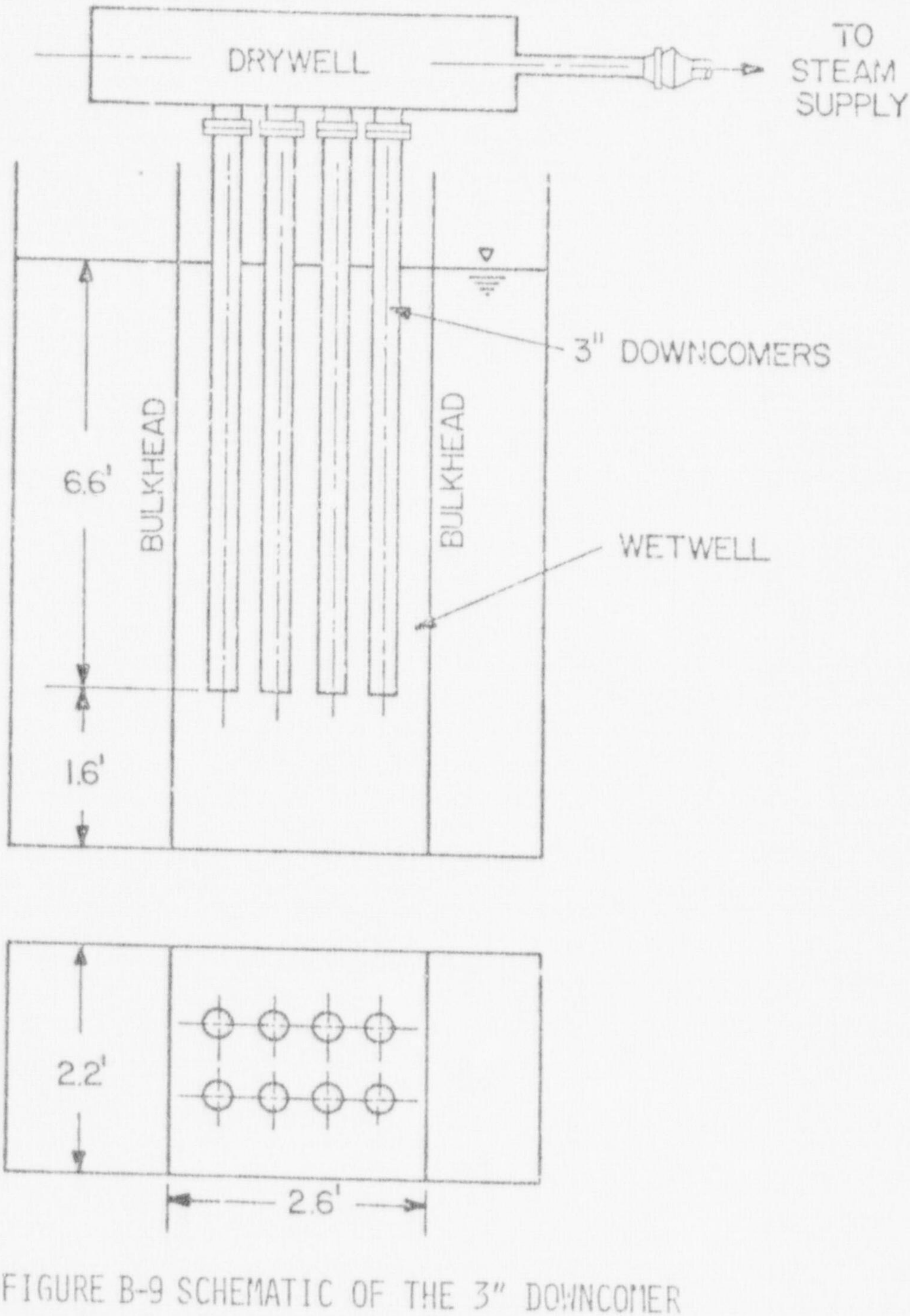


FIGURE B-9 SCHEMATIC OF THE 3" DOWNCOMER
KWU-KARLSTEIN TEST FACILITY

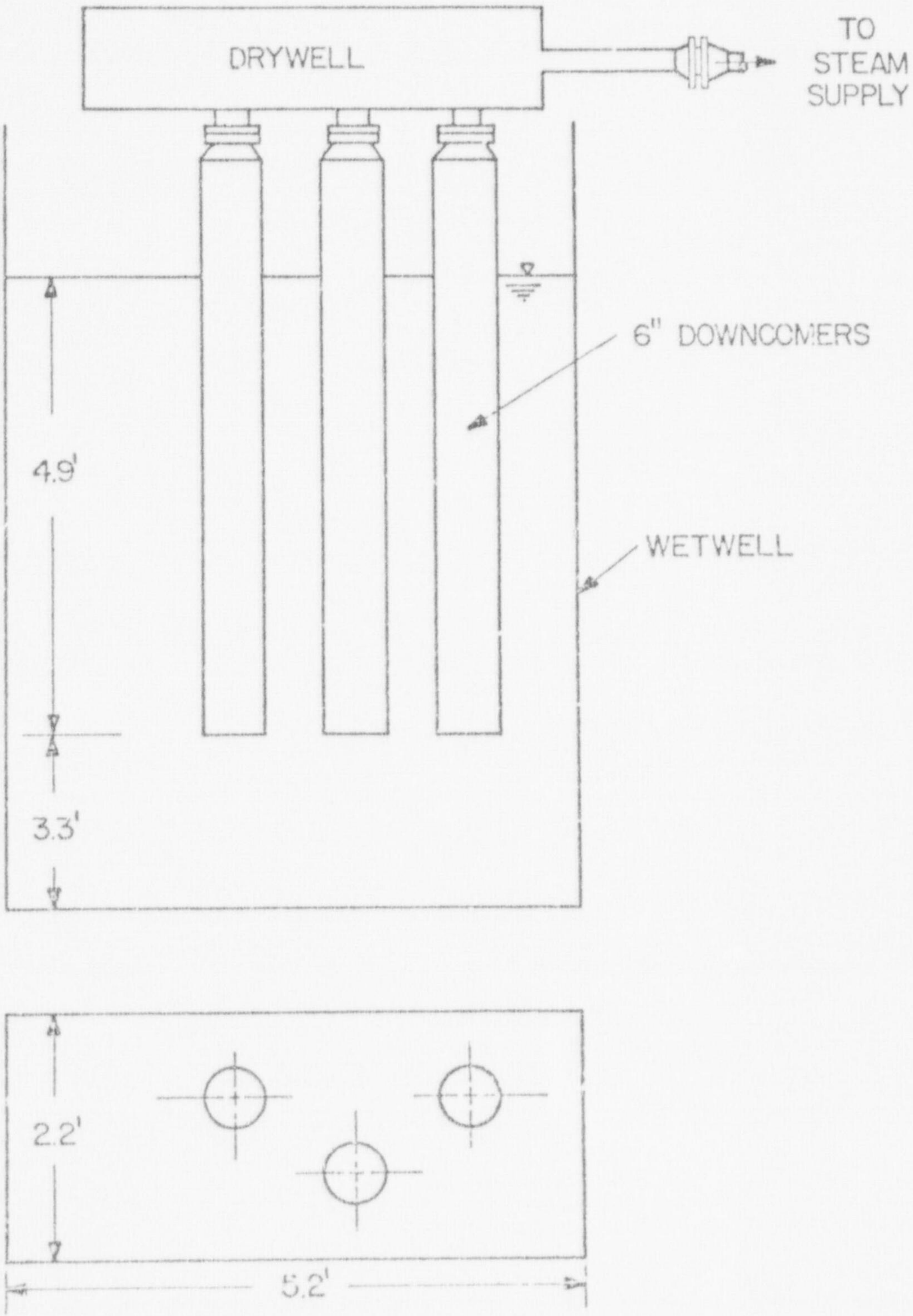


FIGURE B-10 - SCHEMATIC OF THE 6" DOWNCOMER
KWU-KARLSTEIN TEST FACILITY

were conducted with an unpressurized wetwell. Also the large scale tests (12" and 24" diameter vents) were conducted with constant suppression pool area. The influence of steam flux rate, air addition, suppression pool temperature vessel wall stiffness and strut arrangement was examined. Also the effect of transverse flow in the pool was investigated. Measurements included pressures at various points on the tank boundaries and strain in the vent bracing struts.

The relevant findings for the Mark II Program from these experiments are that the magnitudes and directions of lateral loads on individual downcomers in a multi-vent system are random and statistically independent and that the vents do not chug exactly in phase. Also, statistical analysis of the wall pressures indicates that pressure loads on the boundaries would decrease with increasing number of vents for constant pool to vent area ratio.

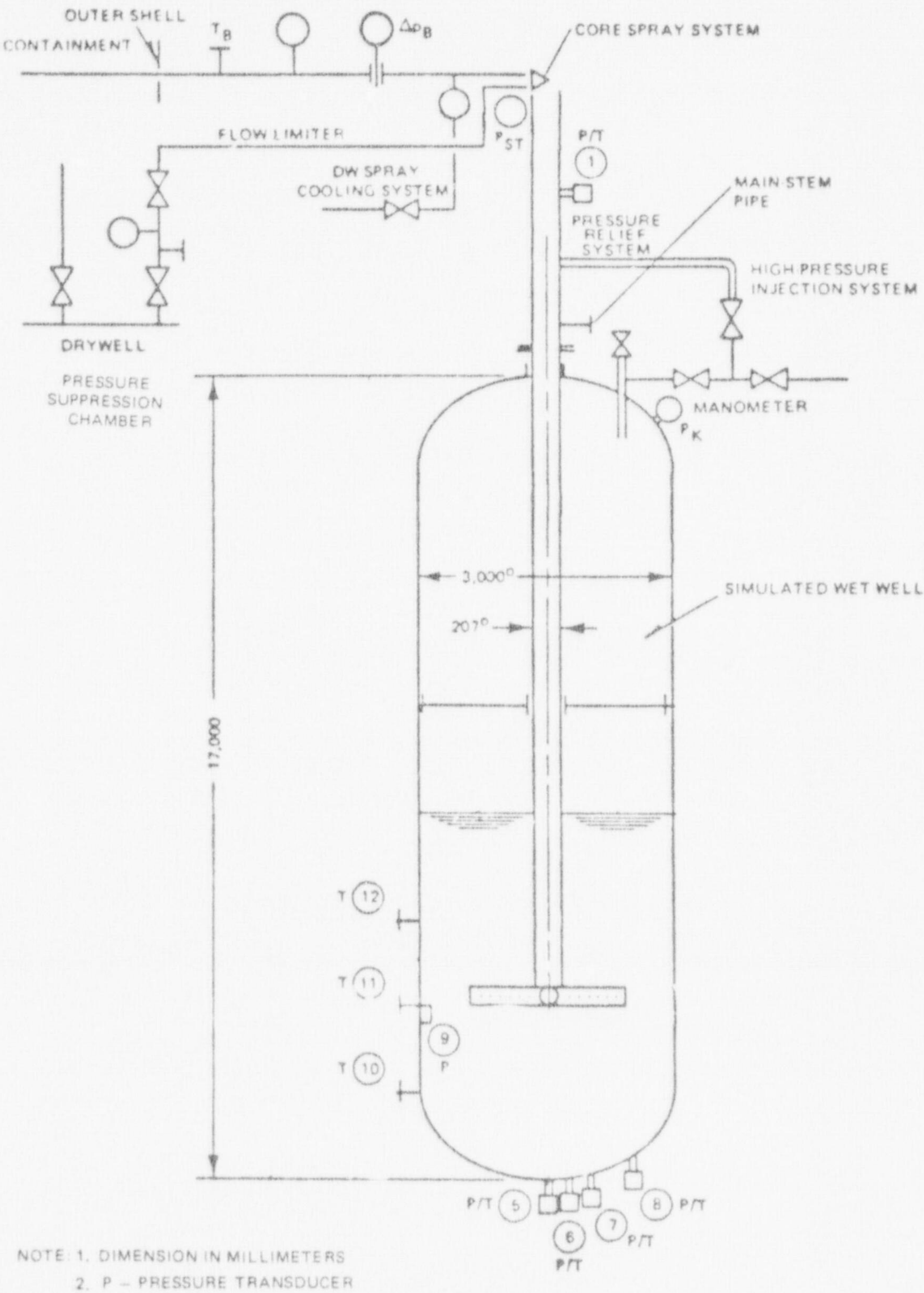
6. SRV - Related Foreign Licensee Tests

A four-arm quencher discharge device very similar to the GE four arm quencher was tested extensively at several foreign test facilities.[14] The primary objective of this program was to provide a data base to quantify the performance characteristics of a four arm quencher. The statistical model used to predict air clearing loads for the GE quencher is based on this data base. The foreign tests involved three series of experiments conducted in mini-scale, small-scale, and large-scale facilities. The large scale test facility was an actual BWR plant with installed four arm quenchers. In addition, numerous small-scale experiments were conducted to supplement the quantitative test information.

a. Small-Scale Test Observations

The test facility in which the small-scale experiments were performed was a cylindrical tank 3 meters (9.84 ft) in diameter and 17 meters (55.8 ft) high with hemispherical ends. The maximum steam flow rate was 120 tons/hour. Figure B-11 illustrates the small-scale test facility. The dimensions of the test system are full scale in all flow wise directions, but of reduced scale in dimensions perpendicular to the flow.

Pressure and temperature measuring devices were installed at appropriate positions in the discharge line and pool. Movable or temporary measurement devices were also installed at strategic locations to investigate various features of special tests. Of the thirty independent parameters



NOTE: 1. DIMENSION IN MILLIMETERS
2. P – PRESSURE TRANSDUCER
3. T – TEMPERATURE MEASUREMENT

Figure B-11 Small Scale Test Facility

originally considered to be of possible importance in determining air discharge loads, four were identified (by the small-scale test data) as dominant. These are: 1) the ratio of the initial pipe air volume to the quencher area; 2) the submergence of the quencher; 3) the pool temperature; 4) the ratio of the pool surface per quencher to the quencher area. The sensitivity of the air discharge loads to the first three variables was systematically tested in a total of 70 firings in the small scale test facility. The summary of the results follows.

1) Effect of Air Volume

When the air initially in the SRV discharge line is expelled, the water above the quencher is forced into motion in the direction of the water surface. The air bubble emerging from the quencher is distributed over the quencher cross-section delivering an impulse to the water mass above the bubble. The ratio of the initial air volume to the cross-section of the bubble gives a measure of the thickness of the pancake-shaped bubble ultimately formed. An increase of the expelled air volume thus tends to involve the acceleration of the water above the bubble, and hence increase the magnitude of the pressure fluctuation in the pool. As expected, peak positive pressures were observed to increase with increasing air line volume. However, at sufficiently large air volumes, the trend towards increases in pool pressures associated with the formation of a large bubble is more than offset by the reduction in bubble pressure resulting from the more gradual air clearing from the discharge line.

2) Effect of Submergence

The effect of quencher submergence on the peak positive pressures for submergences in the range of 2 to 6 meters was quantified. A fairly sharp increase in pressure with submergence in the 2-4 in submergence range was found followed by a more gradual increase in the 4-6 m range.

3) Effect of Pool Temperature

Increasing pool temperature was found to slightly increase the pool boundary loads. Measurements also showed a decrease in bubble frequency, with increasing pool temperature.

b. Mini-Scale Test Observations

The effect of the sensitivity parameter pool-surface-to-quencher-area ratio was quantified in a series of "mini-scale" tests. The test facility consisted of a model tank of rectangular cross-section 1.6 m (5.25 ft) x 1.65 m (5.4 ft) and a height of 3 m (9.84 ft). Submergence was 1 m (3.28 ft), with 0.3 m (0.98 ft) between the quencher and the floor of the tank [Figure B.12]. To obtain a variable free water surface area, cylindrical pipes with variable cross-section were placed around the quencher.

Results of these tests show that as the free surface per quencher is reduced, the maximum boundary pressure increases. Increasing the number of quenchers in a pool would have the effect of reducing the free surface area per quencher.

c. Large-Scale Test Observations

The large-scale test facility was an actual BWR suppression pool. Figure B-13 shows a cross-section through the reactor suppression pool and the relative location of the quencher device tested. Pressure and temperature measurements were made at selected points on the suppression pool walls to provide pressure loads for comparison with predictions made from the reduced scale test results. Data measurements were taken in the cross-sectional plane shown in Figure B-13 as well as in the circumferential direction (i.e., around the suppression pool).

The large-scale tests provided information about the effects of steam flow rate and consecutive actuations on the maximum bubble pressure. A total of 37 single and 10 consecutive actuations

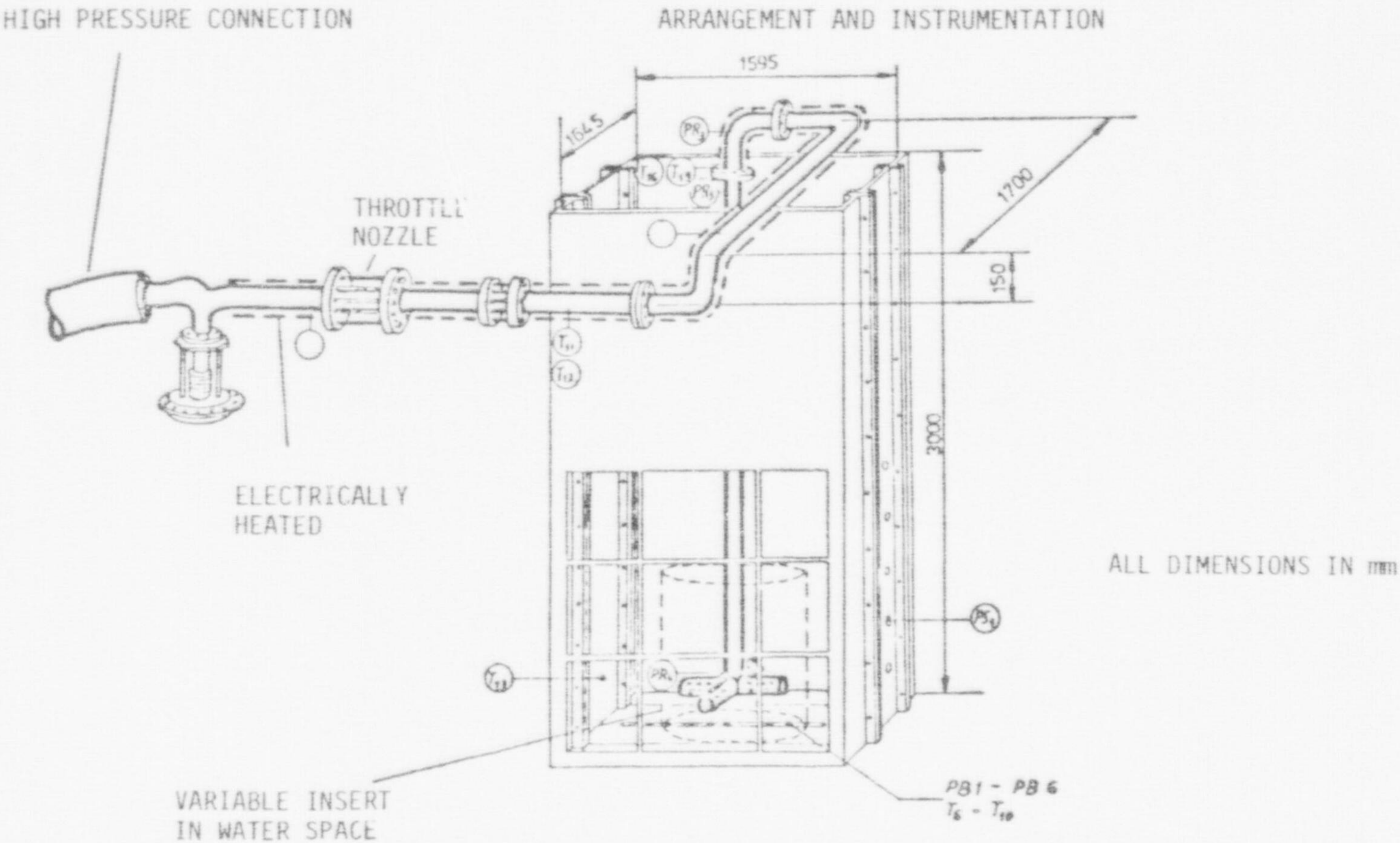


Figure B-12 Mini-Scale SRV Test Facility

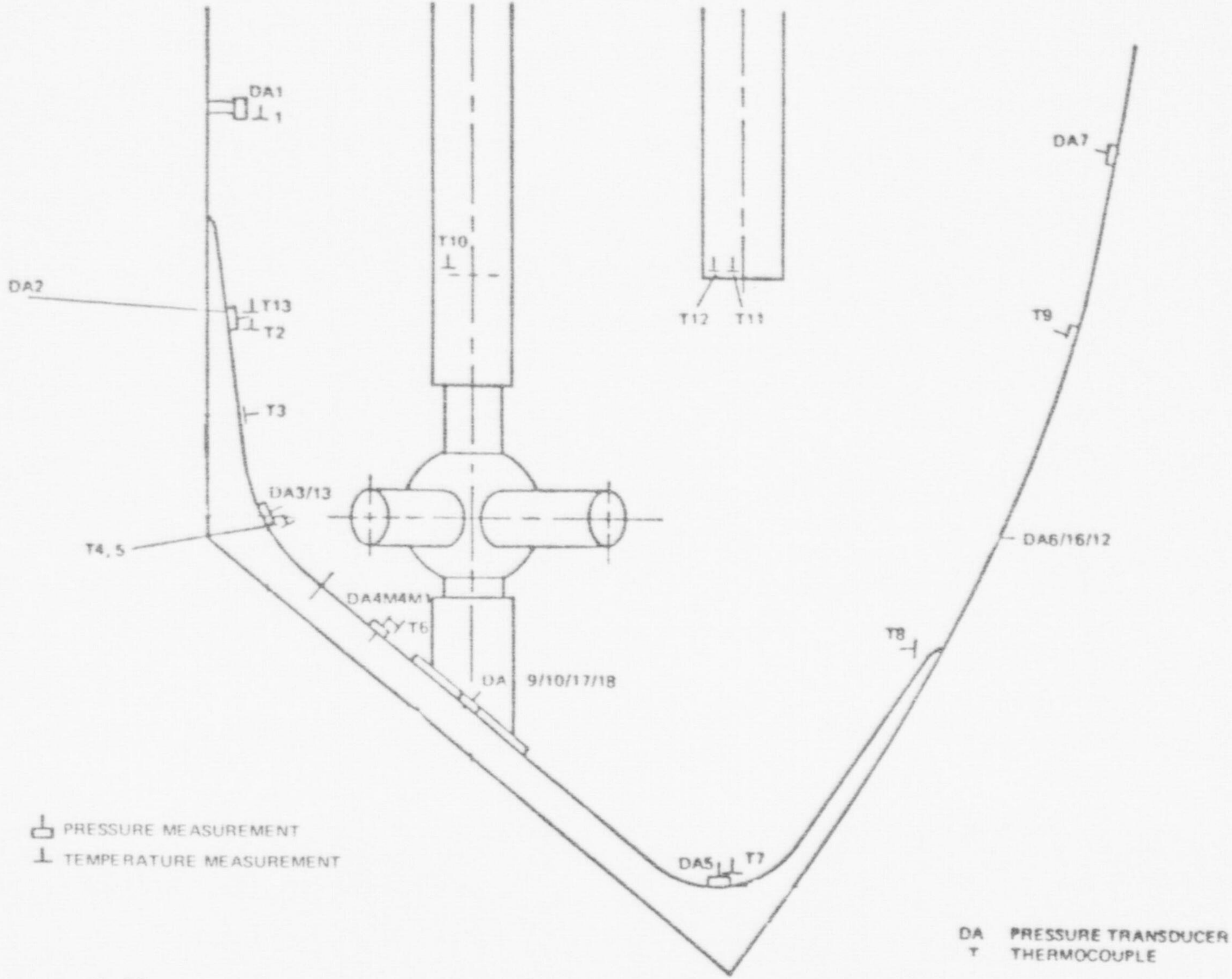


Figure B-13 Large Scale Relief Valve Tests - Pressure and Temperature Measuring Points were performed. The single valve actuation data show that the bubble pressure increases as steam flow rate increases. The consecutive actuation showed;

(1) Repeated actuation of the safety/relief valve performed within a minute or two caused the bubble pressure to first increase with each consecutive actuation and then decrease. The maximum bubble pressure occurred on either the second or third "subsequent" actuation (i.e., third or fourth when the first actuation is counted).

(2) On the average, the maximum bubble pressure observed in a subsequent actuation was higher than the value for the first actuation.

Multiple valve actuation tests in the large scale facility yielded data on pressure altenuation and superposition effects that form the bases for the multiple valve actuation scheme outlined in the DFFR.

7. Marviken Power Station Tests - Second Series

These tests consisted of 9 blowdowns (17-25) in a converted power station (Figure B-14) using multiple downcomers of 1 foot diameter.[15] The primary variables studied were blowdown flow rate, submergence and number of vents. High response pressure instrumentation was installed for this test series to allow for detailed resolution of the pressure fluctuations occurring in the vents and on the submerged boundaries. The data were analyzed to establish the extent of coherence between vents, vents and drywell, and vents and submerged boundaries. The results indicate that exact synchronization is a very low probability event. The general characteristics of the pressure fluctuations observed were quite similar to those measured in other facilities including the 4T. However, the amplitude of the pressure fluctuations both in terms of mean and maximum values were generally lower than those encountered during the 4T tests.

8. GE Tests for Independent BWR

A series of six blowdowns with a 24" downcomer and 9 foot submergence were conducted in the 4T facility in support of independent work for BWR systems.[16] The primary objective of these tests was to

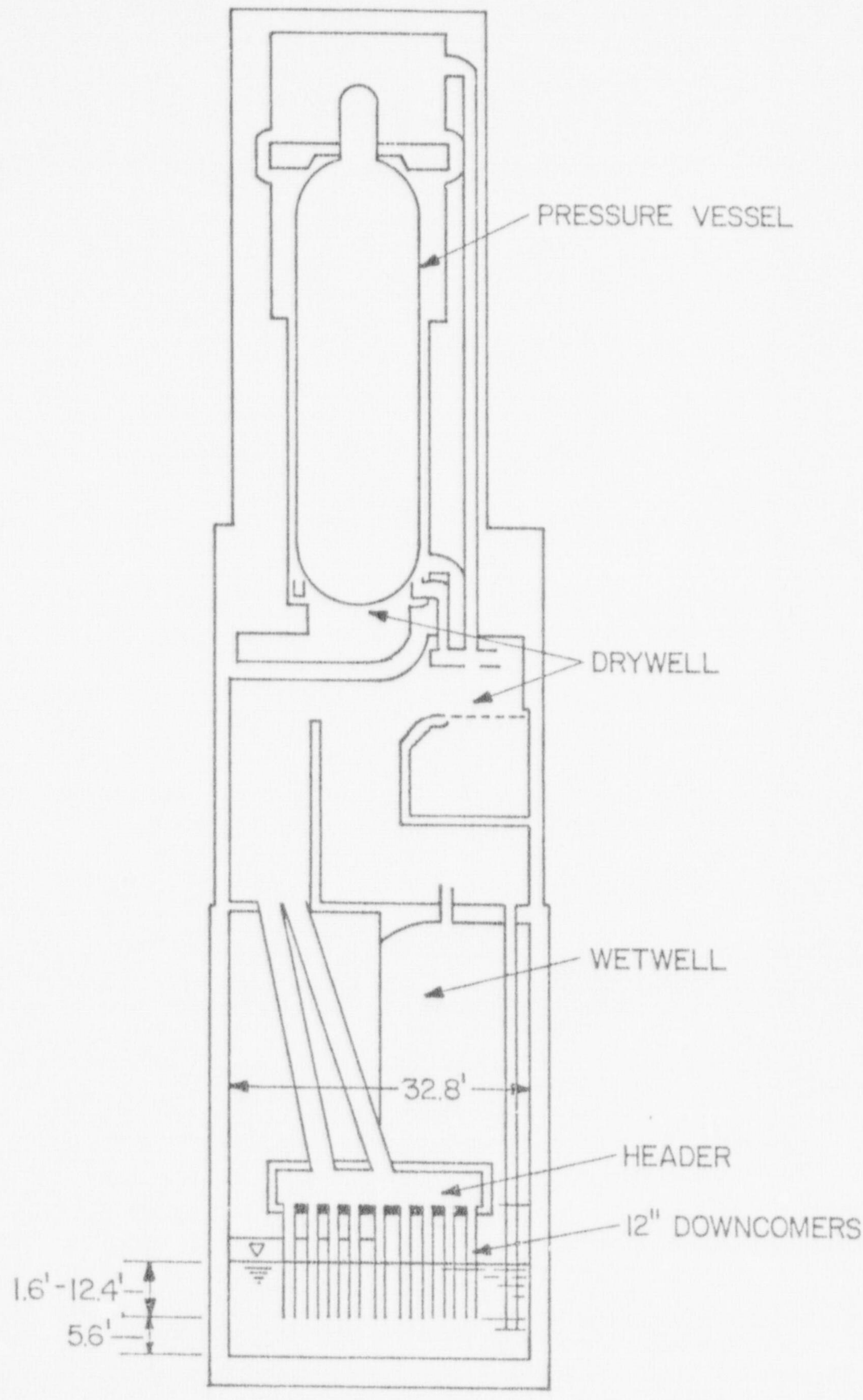


FIGURE B-14 SCHEMATIC OF THE MARVIKEN POWER STATION CONTAINMENT investigate the effect of wetwell pressure and pool temperature on chugging wall pressure loads. The tests were conducted with initial pool temperatures ranging from 70°F to 150°F and with a closed wetwell resulting in a wetwell pressure during steam blowdown of about 2.8 bars. It was concluded from these tests that neither the mean or maximum amplitudes of the chugging pressure fluctuations are influenced by pool temperature.

9. Anamet 4T FSI Study

A modal survey of the GE 4T facility was conducted by the Anamet Laboratories, Inc. The primary objective of these experiments was to determine the extent to which the inherent characteristics of the 4T vessel and geometry had influenced the pressures measured in the 4T test facility. The modal survey was used to establish the natural frequencies of the 4T system. A second phase of the study was to examine the response of the 4T system to a simulated chug. This simulation was accomplished by the fracturing of an evacuated bell jar situated at the downcomer exit. The interpretation of the results of these experiments are used by the Mark II Owners Group to demonstrate that 4T FSI effects did not adversely modify the measured pressures in terms of their applicability for Mark II Plants. A detailed description of this study is provided in Reference 17.

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16. "Influence of Back Pressure During Chugging in Simulated LOCA's at the G.E. Testing Facility," NEDE-24017-P, June 1977, (G. E. Proprietary).

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APPENDIX C

ANALYTICAL PROGRAMS

1. <u>General Electric Pool Swell Analytical Model (PSAM)</u>

The analytical model used by the Mark II Owners Group to describe the response of the suppression pool to a LOCA has been developed by General Electric. A detailed description of the model is provided in Reference 1.

The essential assumption used in the analysis is that the phenomenon is strictly one dimensional. Specifically, a water slug of fixed mass and constant thickness is accelerated upward by the pressure force exerted by an expanding air bubble. The upward motion is resisted by gravity and the pressure force exerted by the air entrapped in the free volume above the water slug. The describing equations include the equation of motion of the water slug, an adiabatic compression of the wetwell air, conservation of mass in the bubble and conservation of mass and momentum for the vent flow. Additional assumptions employed by the model include all-air, adiabatic vent flow, and bubble temperature equal to the instantaneous drywell temperature. The latter is determined by assuming adiabatic compression of the drywell air according to the instantaneous value of drywell pressure. Drywell pressure variation must be input and is taken from each Mark II plant's FSAR. The model applies from vent clearing to bubble "breakthrough." The initial values of pool velocity and bubble pressure are obtained from other analyses of the vent clearing process. Breakthrough criterion is taken as occurring when bubble pressure is equal to wetwell pressure. Standard loss coefficients are used to determine the pressure drop across the vent. Drag forces on the water slug are neglected.

The model provides the time history of pool elevation, velocity and acceleration, and bubble and wetwell pressure.

2. Submerged Structure Loads Analytical Models

A discussion of the analytical models (References 2 and 3) which provide the basis for the applications memorandum (Reference 4) is presented below to justify the constraints and modifications stated in Section III.D.

a Jet Loads

The sudden pressure increase within the vent pipe resulting from either LOCA or SRV actuation accelerates the water slug initially within the vent into the pool. While a large body of literature exists reporting both experimental and theoretical work on steady jets, there has been little research interest in the phenomena of unsteady initial jet formation that would be relevant to the current problem. While order of magnitude analysis can show that the spreading of jets due to viscous forces or turbulence cannot be substantial during the short lifetime of the vent clearing water jet, the details are intimately connected to the local effects of viscosity at the sharp edge of the pipe exit. This is analogous, to some extent, to the creation of the Kutta condition in airfoil theory. Since no purely theoretical analysis of this problem exists, nor is it likely to be easily obtainable, somewhat cruder modelling of the phenomena must be attempted.

1) Analytical Model of the Jet

Reference 2 models the jet on the basis of a quasi one-dimensional analysis. In addition, the pressure within the jet, once it leaves the pipe, is assumed to be equal to the hydrostatic value. This implies that no induced flow field is produced by the jet within the pool and that pressure is instantaneously adjusted within the jet. This leads to the conclusion that, within the jet, fluid particles travel at the velocity they attain at the pipe exit. For accelerating flow, this leads to jet divergence and an obvious difficulty at the jet front where fluid particles catch-up to the previously released ones. Moody considers this region at the jet front as an analog to a shock wave but does not discuss the phenomena in detail.

A fruitful way to evaluate the conservatism inherent in this model is to look at the expulsion of a slug of water out of the pipe as the firing of a "bullet" from a gun. The bullet in this case, however, is highly deformable and changes in shape due to its interaction with the surrounding fluid. Some distance from the leading edge of the water slug, where the velocities within the water adjacent to the jet boundary are small, Moody's assumptions appear reasonable. Near the jet front, where fluid particles that were initially just outside the pipe exit must be accelerated out of the way, pressure gradients must be created to induce the necessary flowfield within the pool, and the jet boundary must substantially deform. Since the pressure gradients induced within this flowfield are certainly going to be small compared to the pressure change within the pipe, exit velocity will not be affected. The computation of the initial arrival of the jet front and the nearby flowfield as well as the subsequent velocity history at a fixed point that is engulfed by the jet may, however, be in error. An upper bound on the velocities, can be determined by considering the water slug to have negligible deformation and thus have the velocity everywhere within the jet equal to the instantaneous pipe exit velocity, while external to the jet the flowfield is equal to that produced by a "bullet" travelling at the current speed of the water jet. Alternatively the procedure of Reference 2 can be utilized for the major portion of the jet away from the jet front, but the jet front is modelled by a spherical vortex as done in Reference 5 until the entire jet rolls up into the vortex. For times subsequent to this roll-up time the spherical vortex is expected to propagate in the pool with its velocity slowed only by the drag acting on it. A conservative estimate would predict a vortex ring moving at the constant velocity of the jet front at time of roll-up.

2) Jet Loads on Submerged Structures

Reference 2 states that loads on structures are of the standard drag type and acceleration drag is not important. Once a structure is immersed wholly or in part within the jet such an assumption is fully consistent with the model of the jet described in the report.

Until the structure becomes intercepted by the jet, however, it must see an induced flowfield arising from the advancing jet front. During this stage the structure will be in a time changing flowfield and the acceleration drag can be appreciable. An estimate of the magnitude of this term can be made by modelling the jet front region as a moving body. An approximate estimate of the drag history including the acceleration term for a structure lying directly on the path of the jet is deduced below.

The flowfield induced by the jet front in the pool can be modelled by the inviscid flow produced by a moving sphere. Within a coordinate system fixed in the pool the potential due to a moving sphere is:

$$\phi = -\frac{U}{2} a^3 \frac{\cos\theta}{r^2} \tag{1}$$

where U is the velocity and a is the radius of the spherical jet front and r, θ describe a spherical coordinate system fixed at the center of the jet front. The radius a must be larger than R_p the pipe radius and smaller than the radius of a sphere containing the entire volume of water initially in the vent.

$$R_p \leq a \leq R_p \left(\frac{3}{4} \frac{L}{R_p}\right)^{1/3} \tag{2}$$

where L is the length of the water leg in the pipe.

The maximum value of acceleration drag on the structure occurs just as the jet front arrives ($r \sim a$) and is given by the relation

$$F_A = \frac{\rho}{g_c} V_A \left[\frac{\partial u}{\partial t}\right] \approx \frac{3\rho}{g_c} U^2 \frac{V_A}{a} \tag{3}$$

where $\frac{\rho}{g_c}$ is the density of the water and V_A the acceleration volume.

Note than since the standard drag is given by the relation

$$F_S = C_D \frac{\rho}{g_c} \frac{U^2}{2} A_p \tag{4}$$

where C_D is the drag coefficient and A_p is the projected area of the structure, the ratio of acceleration to standard drag can be estimated conservatively by approximating $a \approx R_p$:

$$R_{A/S} \approx 6 \frac{V_A}{C_D R_p A_p} \tag{5}$$

This number can be substantially larger than unity when the structure is comparable in size to the pipe exit diameter. While the actual shape of the jet front may be substantially different the magnitude of the induced flowfield seen by the structure must be comparable to that given by equation (1) and thus the acceleration drag must be of the order of that given by equation (3). The drag on the structure in the path of a jet is therefore expected to rise rapidly to a value $1 + R_{A/S}$ times the standard drag as the jet approaches, drop rapidly within the jet to the standard value and fall as the end of the jet reaches the structure. The drag computation in Reference 2 is only relevant to that portion of the load history when the structure is within the jet. The use of the momentum theorem for structures only partially within the jet, or totally deflecting the jet is certainly appropriate for this computation of the steady drag.

b Air Bubble Loads

Once the water has cleared the vent during LOCA or SRV operation, compressed air is forced into the pool, forming a bubble which for SRV operation separates from the pipe exit and rises while oscillating. Both the bubble charging process and the subsequent motion induce unsteady flowfields within the pool. This fluid motion in turn produces substantial loads on submerged structures. The methodology for computing these loads, proposed in Reference 4, is based primarily on analysis presented in Reference 3. A review of the basic model and its analysis is presented in two parts:

1. Analysis of the bubble induced flowfield
2. Analysis of the drag forces.

Subsequently, the methodology presented in the Reference 4 is evaluated in light of this review.

1) Bubble Induced Flowfield

The analysis presented in Reference 3 is based on hydrodynamic theory using the potential flow resulting from a point source to model the real flowfield. The source strength and position is determined from bubble dynamics using the Rayleigh equation coupled with a mass and energy balance for the bubble. The use of incompressible potential flow theory can be justified by the fact that characteristic flow times are short compared to viscous diffusion times while long compared to any acoustic transit times. The modelling of the flow by that due to a point source, while rigourously true for distances far from the bubble, may be in error at distances of only a few bubble radii.

The potential flowfield induced by any object can always be expanded in terms of spherical harmonics within the far field. While the leading term is always the point source solution with the strength arising from volume changes of the object, the next higher order term (the dipole) can be significant at a distance of a few bubble radii when the bubble is asymmetric or moves at a velocity comparable to the bubble expansion or contraction rate. The magnitude of this additional term can be approximated as

$$\phi \approx B \left[\frac{R}{r}\right]^2 \cos\theta \tag{6}$$

where R is the current bubble radius and r and θ are spherical coordinates from the bubble center. B is a constant resulting from both bubble asymmetry and directed motion. For asymmetry, B is of the order of the first Fourier coefficient describing the bubble boundary. For directed motion

$$B = -\frac{1}{2} UR \tag{7}$$

where U is the bubble center velocity. Because of the $(R/r)^2$ decay of this contribution compared to the (R/r) decay of the source term, this term is always negligible sufficiently far away from the bubble. For distances of four bubble radii and a bubble which approaches a hemisphere in shape the contribution due to asymmetry could be of the order of 10%. At a similar distance upstream of the bubble, the induced velocity due to directed motion is of the order $\frac{1}{12}\sqrt{gR}$ and the induced acceleration is about g/4. These latter effects are expected to be small compared to the source contribution.

The effects of solid boundaries and free surfaces as well as multiple bubbles are represented by image sources and sinks that allow for the appropriate boundary conditions. This is a standard procedure in potential flow theory and thus requires no further justification, beyond the assurance that a sufficient number of images is included to provide numerical accuracy.

2) Drag Force Computation

Reference 3 proposed that drag computations for submerged structures be based upon:

1. an equivalent locally uniform flowfield at the location of the geometric center of the structure,
2. a linear combination of acceleration and standard drag forces,
3. and no consideration of interference effects between adjacent structures,
4. the structures which experience the loads are rigid.

The justification for using a locally uniform flowfield can be easily provided for structures that are small in all dimensions compared to the distance from the bubble. For long thin structures at an angle with respect to the flow, the locally uniform assumption will be conservative provided the normal flow velocity is taken as the maximum that is encountered by the structure, not necessarily that which exits at the geometric center.

The decomposition of drag forces into acceleration and "standard" drag can be easily justified provided one properly interprets those two contributions. There is a force contribution directly proportional to the acceleration of the fluid, and another contribution which accounts for the remaining effects. The meaning of the decomposition is therefore only determined by specifying how each contribution will be computed. Since potential flow theory produces only a contribution proportional to acceleration it is most useful to identify acceleration drag as the potential flow drag as is done in Reference 3. Reference 3 further proposes to take the standard steady flow drag at the instantaneous velocity as the remaining contribution to the load on a structure. The use of this standard drag coefficient from steady flow measurements cannot be fully justified as the departures from potential theory which are all lumped into this coefficient are very sensitive to local acceleration and the resultant separation of the boundary layer.

The "standard" drag coefficient should be obtained from experiments in which not only the Reynolds number but also a nondimensional acceleration parameter (Strouhal number $S = \dot{u}D/u^2$) are comparable to the conditions encountered in the bubble induced flowfield. Moody[6] proposes essentially that approach. Because of the limited data available M ody proposes using data from oscillating flows where a cycle-averaged effective drag coefficient C_D' can be deduced. This can increase the "standard" drag result by a factor as high as about six for rapidly oscillating flows and should certainly produce more conservative results than the use of steady flow standard drag coefficients.

Figure C-1 shows the ratio of a modified "drag" coefficient to the standard steady drag coefficient deduced from the same data base [7,8] used by Moody[6] versus the acceleration parameter (U_m T/D). The quite considerable side force has, however, been added vectorially and the drag coefficient matching the peak load rather than the cycle average has been used. The vertical line with a right pointing arrow for cylinders and plates shows the region of the parameter $\left(\frac{U_m T}{D}\right)$ where the maximum "standard" drag exceeds the maximum acceleration drag. A similar line is shown as an estimate for rectangular beams of depth at least twice the width, based on the assumption that the drag coefficient is not drastically altered by the streamwise depth of the object while the acceleration drag is increased by the additional acceleration volume. Note that for cylinders and rectangles limited by the constraint above the drag coefficient can be assumed to be three times the standard steady drag coefficient whenever it dominates the acceleration drag thus allowing a conservative estimate of $C_D' = 3\ C_D$ for such structures to be used in computing the peak load.

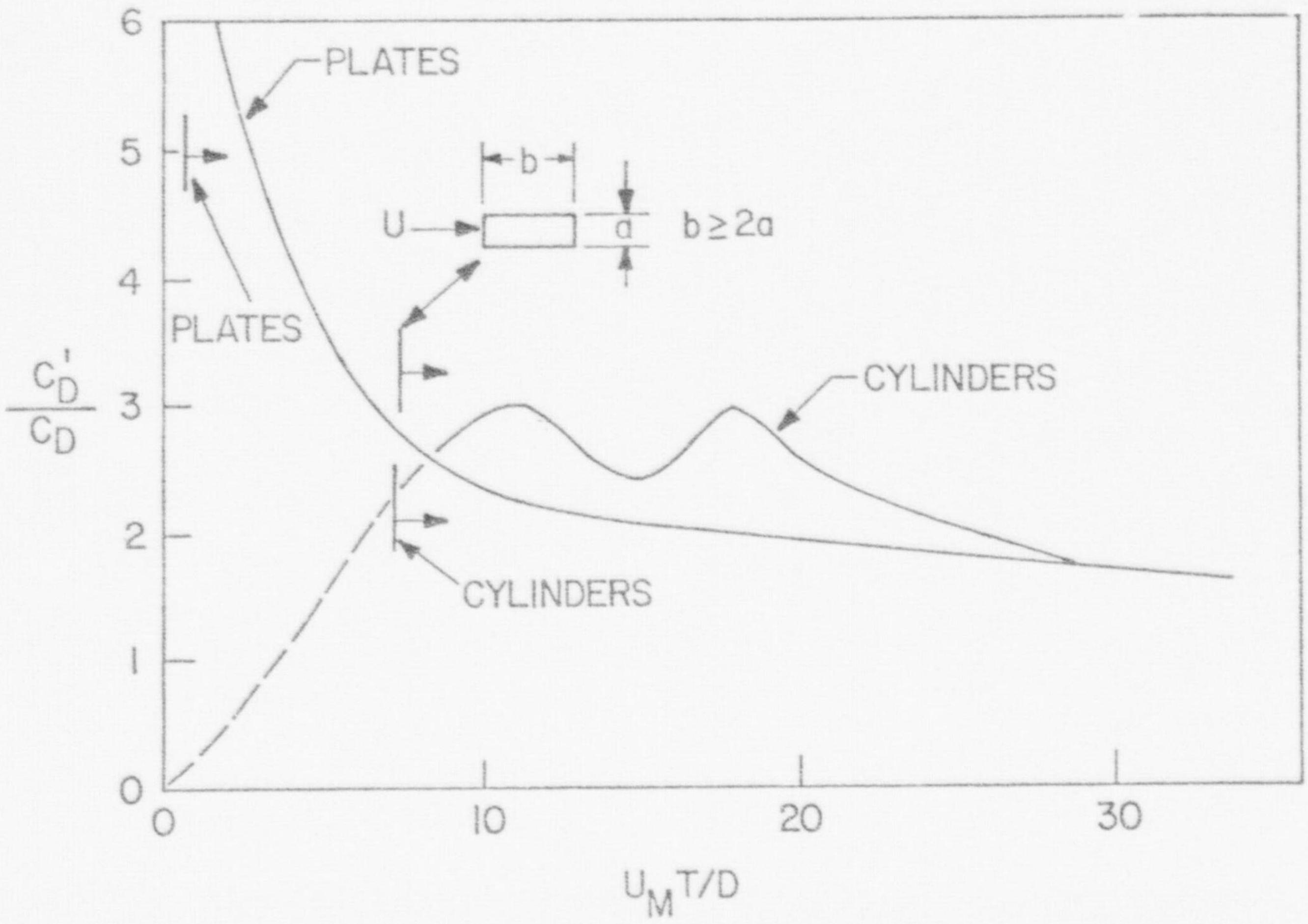


FIGURE C-1 ACCELERATION CORRECTION FOR STANDARD DRAG COEFFICIENT

Reference 4 ignores the standard drag on submerged structures due to the air bubbles produced by a Ramshead SRV device. While this is a good approximation for "typical" structures and "typical" locations the ratio of maximum standard drag to the maximum acceleration drag is strongly dependent on both the size of the structure and the distance from the bubble center relative to the bubble radius. Using the theory of Reference 3 or the Figures 3-2 and 3-3 of Reference 4 one can show that the ratio of the maximum standard drag

$$F_{s_M} = C_D' \frac{A_x}{2} \left(\frac{R_{min}}{r}\right)^4 \rho_\infty \left(R^{\star 2} \dot{R}^\star\right)^2_{max} \tag{8}$$

to the maximum acceleration drag

$$F_{A_M} = V_A\, p_\infty \frac{R_{min}}{r^2}\left(R^{*2}\ddot{R}^* + 2R^*(\dot{R}^*)^2\right)_{max} \tag{9}$$

can be written as

$$\frac{F_{S_M}}{F_{A_M}} = f\left(\frac{p_{max}}{p_\infty}\right)\left(\frac{C_D'}{\pi}\right)\left(\frac{R_{min}}{d}\right)\left(\frac{R_{min}}{r}\right)^2 \tag{10}$$

where $f(\frac{p_{max}}{p_\infty})$ is a result of the bubble dynamics model, d is the diameter of a cylindrical structure, R_{min} is the minimum bubble radius and r the distance from the bubble center to the structure. For $p_{max}/p_\infty = 30$, $f \approx 8/3$ and thus the standard drag will only be small compared to the acceleration drag when $(\frac{R^3_{min}}{dr^2})$ is a small number, i.e. far from the bubble or for a large structure.

Both Reference 3 and Reference 4 imply that loads on each structure can be computed independently. It is a well known fact that structures in sufficient proximity to each other will produce interference effects. C. Dalton and R. A. Helfinstine(9) analyze this interference effect in two-dimensional flow while M. M. Zdravkovich (10) presents a review of experimental data. While it may not be possible to generalize their conclusions to other flow situations directly, it is clear from their results that when two structures are closer together than about four characteristic dimensions of the larger one, appreciable interference effects will exist. These interference effects can increase the acceleration loads by very substantial factors as well as dramatically alter the direction of the load. When the structures are asymmetric and contain sharp corners this phenomena may be even more pronounced and effect dramatically the direction of the "standard" drag as well, since the departure from potential flow will be strongly effected by the induced velocities. The proposed neglect of interference effects should therefore be limited to situations where the gap between adjacent structures is more than three "effective diameters" of the larger structure. A conservative estimate of a four-fold increase in the acceleration drag can be deduced from Reference 9. While Reference 10 is limited to cylinders of equal radius, for a small structure in the vicinity of a large cylinder one would expect the local velocity to be as much as twice as large as the free stream. This produces a four-fold increase in the estimated standard drag as well.

Reference 3 does not provide explicit justification for assuming that the submerged structures are rigid. However, the temporal variations of drag load magnitudes can be expected to have a characteristic period which is substantially larger than the natural periods of prototypical structural components. Accordingly, static application of drag loads for determination of structural response is deemed acceptable.

3. Basis for Single Downcomer Lateral Load Specification

In Section III.B.4.a.1 an acceptable specification for lateral loads on a single downcomer due to chugging was presented. This specification differs from that proposed by the Mark II Owners Group. The basis for these modifications is as follows.

In addition to the 8.8 KIP figure observed during the GKM I tests (Appendix B.5.a), lateral loads were also reported from the 4T tests (Appendix B.1) and the KWU-Karlstein tests (Appendix B.5.c). The 4T tests gave a highest-measured lateral static equivalent load of 2.4 KIPS (10.7 kN), considerably less than that at GKM I, and the Karlstein tests gave 31.5 KIPS (140 kN), considerably higher. The highest lateral loads were found to occur at pool temperatures in the range 70-80°C (158-176°F) and steam flow rates in the range of 16-20 kg/m^2 sec.

In order to understand the various reported data and how they relate to the accepted load specification, it is necessary, first of all, to understand how the downcomer's natural frequency of oscillation affects the static equivalent load. This can be seen in terms of a simple model where the downcomer is viewed as a spring-mass system excited from rest by an impulse $I = \int_0^\infty F\,dt$. The impulse is assumed to result from an arbitrary dynamic force history F(t) which lasts only over an interval short compared with the system's quarter-period of natural oscillation.

(Typically, the dynamic lateral force on a downcomer is exerted over 1-10 msec, whereas the dominant period of oscillation is of order 100 msec and the loading interval is of order 2 sec). A simple calculation shows that the static equivalent load, that is, the hypothetical load which would give the actual maximum deflection which corresponds to this excitation, is

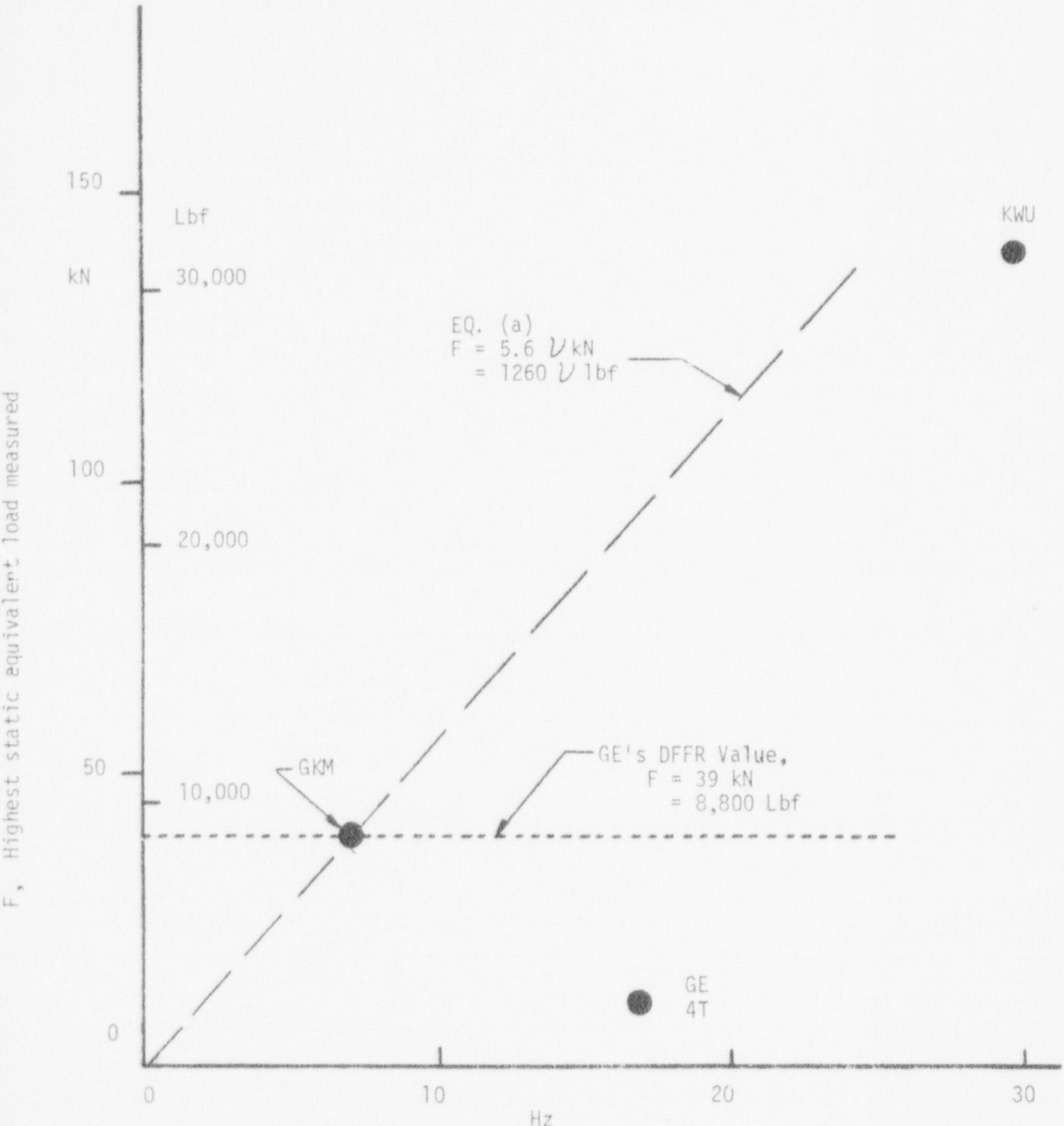
$$F_{st.eq.} = 2\pi\nu I, \tag{a}$$

where ν is the system's natural frequency of oscillation (Hz). For the chugging problem, ν is identified with the dominant natural frequency of the lateral oscillation of the submerged downcomer, and I is identified with the lateral impulse associated with a chug.

Equation (a) shows that the static equivalent load depends on a part, I, that is controlled by the system geometry and the hydrodynamics of the chug (i.e., the actual dynamic load history), and a part, ν, that depends on the downcomer's structural stiffness.

The three test facilities referred to above differed among themselves and relative to Mark II prototypes both with respect to their geometrical hydrodynamic conditions and the structural stiffness of their downcomers. However, if we accept the experimental indications that: (a) submergence is not a very important parameter for lateral loads; (b) the ratio of pool area to vent area is not an important parameter, since chugging is a local phenomenon which does not involve bulk pool swell; and (c) lateral loads are not significantly dependent on whether or not the wetwell is pressurized, then we can conclude that, except for the downcomers' natural frequencies, the GKM I and Karlstein test conditions were at least approximately similar, and also representative of Mark II conditions with 24" downcomers. The hydrodynamic equivalence of the GKM I and Karlstein test conditions appears to be confirmed by the fact that both data points satisfy Eqn. (a) with about the same value of hydrodynamic impulse, I = 200 lbf-sec. (Figure C-2). The different values for the static equivalent loads thus appear to be explained in terms of the different natural frequencies of the downcomers.

The 4T highest-measured load of 2.4 KIPS derives from measurements with a 20" downcomer rather than 24", and refers to a measured load component, not the total vector magnitudes as in the foreign licensee test data. In addition, the pool temperatures in the 4T tests did not reach the values where the foreign licensee results indicated peak lateral loads. All these differences would tend to make the indicated highest-measured hydrodynamic load in the 4T test facility less than in the GKM I and Karlstein tests as well as less than the highest possible value in a Mark II plant with a 24" downcomer operating through the 70-80°C pool temperature range. (The fact that the 4T wetwell was realistically pressurized, unlike the case in the GKM I and Karlstein facilities, appears not to have an important effect, as was mentioned earlier). This is reflected in the fact that the 4T data point falls well below the line in Fig. C-2 which represents Eqn. (a) with an impulse corresponding to the GKM I and Karlstein highest-measured loads, I = 200 lbf-sec. Based on the above-mentioned differences and the experimental information available, it can be argued that the 4T data point should be boosted by a factor of 2, and possibly a factor of 3, to bring it to the same basis as the other data points and Mark II conditions. This would bring the 4T maximum load value into line with the GKM I figure of 8.8 KIPS, but it would still fall considerably below the value obtained from Eqn. (a) with the same I as that which matches the GKM I data point (Fig. C-2). This remaining discrepancy is further reduced, however, if the comparison is based on the mean load measured in each test facility, rather on the highest-observed values.



ν, Natural lateral oscillation frequency of Submerged downcomer.

Summary of data on static equivalent lateral force

Figure C-2

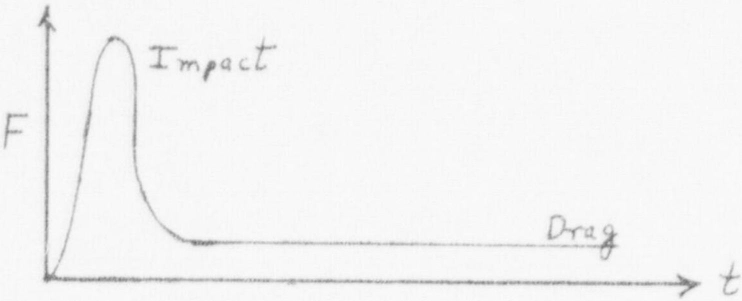
The staff's licensing specification is based first of all on the fact that Eqn. (a) with I = 200 lbf-sec bounds the highest loads ever measured in all the test facilities. In the 4T tests, for example, the highest load ever measured was about a factor of five higher than the average of a set that was already pre-selected for their high loads. In the KWU-Karlstein tests the factor was more like seven. The specification is also supported by some simple hydrodynamic models which our consultants have developed for the lateral loads.[11] These predict "conservative" loads more in line with the measured average loads than with the much higher-observed loads adopted in the specification.

Taking the load as 8.8 KIPS for $\nu < 7$ Hz, rather than the value given by Eqn. (a) with I = 200 lbf-sec, is a conservatism, as are the other caveats in the specification. The period of the dynamic force history to be used in item (c) of the specification (see Section III.B.4.a.1) is based on 4T test results while the impulse to be associated with it is not the highest value observed in the 4T tests, but the much higher value which bounds the highest-observed values in all three test facilities.

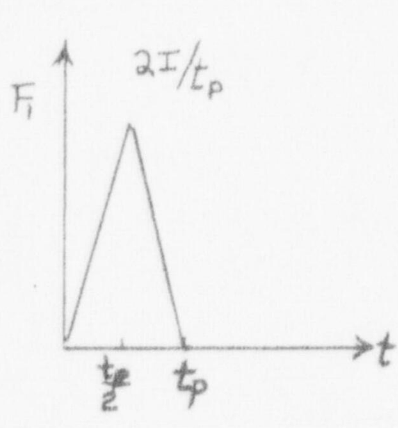
4. Dynamic Load Factor Due to Impact and Steady Drag Loads

During pool swell, certain structures in the wetwell are subjected to impact loads when the pool surface intercepts the structure, followed by a period of steady drag resulting from the velocity of the pool water flowing past the structure. A typical history of the applied force is shown in Figure C-3a.

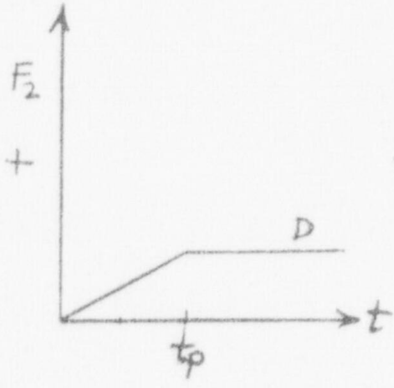
The conventional procedure for dealing with dynamic loading on a structure is to expand the solution in terms of normal modes. The time function multiplying the mode shape function is called the dynamic load factor (DLF) and can be interpreted as the time response of an equivalent single degree of freedom system having the natural frequency of the relevant normal mode. Generally only the lowest mode or modes contribute significantly to the loads. Usually the quantities of interest are the maximum values of the dynamic load factor (DLF_{max}) for each relevant mode. This quantity can be viewed as the ratio of the static equivalent load F_{SE} to the peak applied load. The maximum dynamic load factors for common time functions (such as a step function, ramp, triangle) are available in the literature [12]. Clearly the key parameter for these results



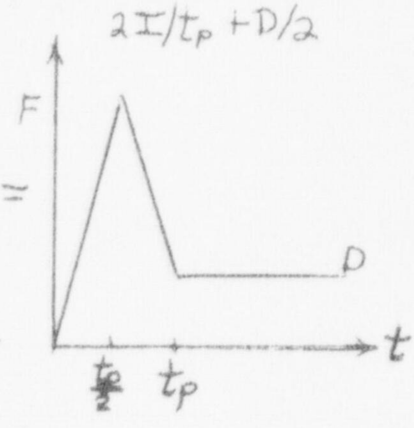
a. Typical Force History



b. Idealized Triangular Impulse Load



c. Idealized Steady Drag Load



d. Combined Idealized Impulse and Steady Drag Load

Figure C-3 Impact and Steady Drag Load

is a ratio of some characteristic time constant of the load application to the natural period of the mode. In the limit of an instantly applied steady load (step function) the $DLF_{max} = 2$. Consider an impulsive load (modelled as a triangle) of total impulse I and duration t_p, acting on a structure of natural frequency f with a peak applied force $\frac{2I}{t_p}$. The maximum dynamic load factor is

$$DLF_{max} = \frac{F_{SE}}{\left(\frac{2I}{t_p}\right)} = \pi f t_p \qquad (1)$$

for short durations ($ft_p \ll 1$), and reaches a peak $DLF_{max} = 1.51$ near $ft_p \approx 1$. Clearly the relative importance of the impact load and the steady drag load on a particular structure will depend both on the ratio of the peak impact force to the steady drag force, and on the duration of the impact times the natural frequency.

The typical force history shown in Figure C-3a can be modelled by a combination of a triangular "impulsive" load of duration t_p and peak amplitude $\left(\frac{2I}{t_p}\right)$, combined with a ramp of slope D/t_p, followed by a steady drag D for $t > t_p$. This is shown in Figure C-3b through C-3d.

The analysis for the resultant driving force can be performed in a straight forward manner and leads to the result:

$$DLF = 1 + \sqrt{\left(\frac{\sin x}{x}\right)^2 + \emptyset^2 \left(\frac{1 - \cos x}{x}\right)^2} \sin (T - x - x_o); \quad T \geq 2x \qquad (3)$$

$$x_o = \tan^{-1} \left(\frac{\sin x}{\emptyset(1 - \cos x)}\right) \qquad (4)$$

where $x = \pi t_p f$, $T = 2\pi t f$ and $\emptyset = 4I/t_p D$. (5)

Note that Equation 3 and the subsequent analysis the DLF is normalized with respect to the drag force; i.e., DLF is defined as F_{SE}/D.

The maximum occurs at $T_m = x + x_o + \frac{\pi}{2}$ as long as $T_m \geq 2x$ and is given by the relation

$$DLF_{max} = \sqrt{\left(\frac{\sin x}{x}\right)^2 + \emptyset^2 \left(\frac{1 - \cos x}{x}\right)^2} + 1; \; x \leq \frac{\pi}{2} + \frac{1}{\emptyset + 1} \tag{6}$$

For $x > \frac{\pi}{2} + \frac{1}{\emptyset + 1}$ the maximum DLF occurs for T between x and 2x and must be obtained by finding the maximum of the following expression

$$DLF = \frac{T - \sin T}{2x} + \frac{\emptyset}{2x} (2x - T + 2 \sin (T - x) - \sin T) \tag{7}$$

While this is complicated for arbitrary values of Ø, when Ø >> 1 the formula for DLF_{max} reduces to the result for the triangular impulse (Figure 2.8 p. 47 Reference 12) plus a number near 1. A bound can therefore be easily established as:

$$DLF_{max} \leq 1 + 1.51\, \emptyset/2 ; \quad x > \frac{\pi}{2} + \frac{1}{\emptyset + 1} \tag{8}$$

In order to apply the above results (Eqn. 6 and 8) to structures subjected to combined impact and drag loads we must find the values of the parameters Ø and x for such structures. The key parameter appearing in both Ø and x is the time t_p which measures the duration of the impact load. This parameter depends strongly on the shape of the pool swell and has already been discussed in Reference 13 for the case of pure impact load. Clearly the shortest duration for a fixed impulse leads to a conservative load. Fortunately the importance of the steady drag arises only when $t_p f \ll 1$. Under those circumstances the result for DLF_{max} from Eqn. 6 can be easily simplified to

$$DLF_{max} = 1 + \sqrt{1 + \left(\frac{\emptyset x}{2}\right)^2}\,; \quad x < .7 \tag{9}$$

The parameter $\frac{\emptyset x}{2}$ can be written as

$$\frac{\emptyset x}{2} = 2\pi f\left(\frac{I}{D}\right) \tag{10}$$

and does not depend on the time constant t_p. Using data from Reference 3 and standard drag coefficients one can deduce the general result

$$\frac{\emptyset x}{2} = \frac{4\pi C_I}{C_D}\ \frac{Wf}{V} \tag{11}$$

where W is the width of the structure impacted and V the velocity of the water, C_D the standard drag coefficient and $C_I = \frac{M_H}{\rho AW}$ from Reference 14. For flat structures $C_I = .49$ and for cylinders $C_I = .156$ from Reference 14. The corresponding drag for flat structures is $C_D \approx 2$ while for cylinders C_D depends strongly on both aspect ratio and Reynolds number ($.33 \leq C_D \leq 1.2$). The formula

$$\frac{\emptyset x}{2} = 3.14\left(\frac{Wf}{V}\right) \tag{12}$$

is correct for flat structures and bounds the results for cylinders longer than one diameter provided the Reynolds number is less than 5×10^5. For a velocity of 40 ft/sec this is satisfied by all cylinders less than 25 inches in diameter.

Once $x > .7$ the dynamic force is dominated by the impact load and to a high degree of accuracy the results of Reference 13 can be directly combined with the steady drag. Within the present formalism for flat pools and a velocity of 40 ft/sec the parameter $\emptyset \approx 37$ regardless whether the structure is flat or cylindrical and

$$x = .000345\ Wf \tag{13}$$

where W is the width in inches and f is the natural frequency in Hz. For gratings the drag coefficient increases as the percentage open area decreases while the hydrodynamic mass coefficient C_I varies little. The value of $\frac{\emptyset x}{2}$ and $\emptyset$, therefore, is conservative by taking the results above which apply exactly in the limit as the grating becomes 100% open. Of course the width W is taken as the width of the individual bars in the grating.

Figure C-4 shows the exact results (Egn. 6, 7, 8), the approximation for small x (Eqn. 11), and the pure impact load, and the instantaneously applied drag versus the non-dimensional time parameter x as well as the parameter Wf (Eqn. 13) for the case $\emptyset = 37$. Note that for this model of the force history the DLF_{max} can be approximated conservatively by using Equation 9 with the result of Equation 12 for sufficiently rapid application of the force, (x small) until the result DLF_{max} exceeds the maximum DLF_{max} obtained from pure impact load calculations by one. For this triangular impact load the bounding formula is:

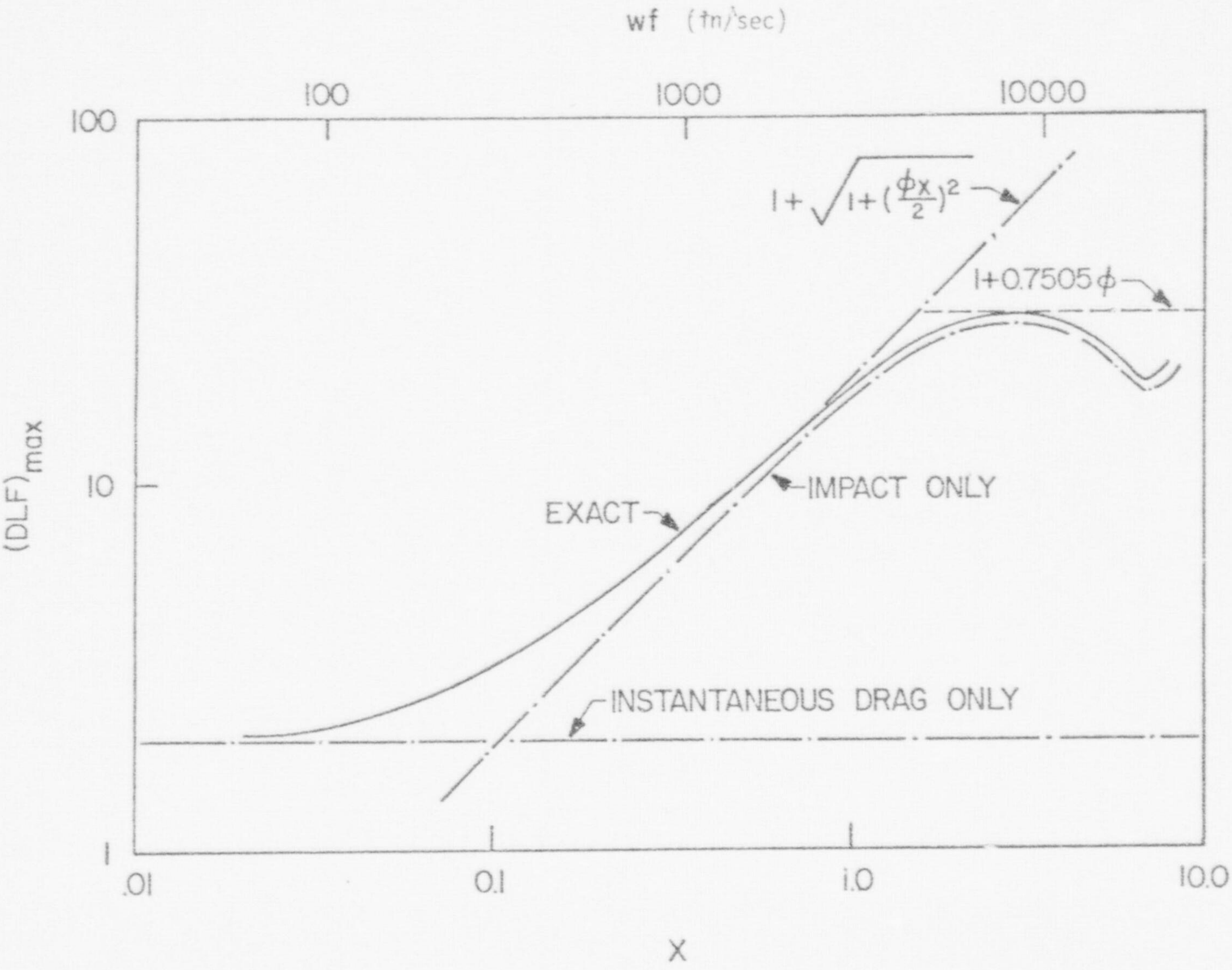


FIGURE C-4 DYNAMIC LOAD FACTOR (Ø = 37)

$$DLF_{max} \leq 1 + \sqrt{1 + (\emptyset x/2)^2} \qquad x < 1.51$$

$$DLF_{max} \leq 1 + 1.51\ \emptyset/2; \qquad x \geq 1.51 \tag{14}$$

For other shaped pulses the coefficient 1.51 will be changed, i.e., for the versed sine pulse 1.51 would be replaced by 1.70, but the results for small x (Eqn. 9) would remain unchanged. In dimensional terms Eqn. 14 can be evaluated for $\emptyset$ = 37 as:

$$DLF_{max} \leq 1 + \sqrt{1 + (.0064\ Wf)^2} \qquad Wf \leq 4372 \text{ in/sec}$$

$$\leq 29 \qquad Wf \geq 4372 \text{ in/sec} \tag{15}$$

If this formula is used to multiply the load produced by steady drag for each mode of the structure, the resultant dynamic load will always remain conservative regardless of the relative importance of impact to drag and/or the value of pool swell curvature. Alternatively it can be observed from Figure C-4 that for values of Wf > 2000 the dynamic load factor arises almost entirely from the impact load. Therefore, the multiplier given on Page III-32

$$F_{SE}/D = 1 + \sqrt{1 + (.0064\ Wf)^2} \tag{16}$$

can be used for all Wf valves, while for Wf > 2000 the conventional procedure for pure impact loads is an acceptable alternative.

5. Basis for Diaphragm Upward Load Specification

In Section III.B.3.d.2 an acceptable specification for Mark II diaphragm upward loads due to pool swell was presented. This specification differs from that proposed by the Mark II Owners Group. The basis for this modification is as follows.

One of the parameters recorded during the 4T tests [15, 16] was the "maximum" pressure differential between the wetwell air space and the drywell during the pool swell transient. Although no diaphragm or drywell floor, as such, actually exists in the 4T facility (See Figure B-1), this pressure differential has been interpreted as representative of the upload that may be experienced by an actual Mark II prototype diaphragm.

The magnitude of this pressure differential exhibited considerable variation, depending on the test configuration. The overall range which was observed varied from -9.4 to +2.2 psid ($\pm$.25 psi) so that, in some cases, no net upward load actually occurred. No effort to establish trends of this parameter with the various test variables was undertaken by the Mark II Owners Group. Rather a "direct application" of the test results was used to develop a load specification for Mark II application. [17] An "upper bound" value of +2.5 psid was specified based on the notion that this was in excess of the maximum observed during all of the 4T tests in which the test parameters were varied over a range covering that of interest in all Mark II plants. The maximum upward pressure differential was also determined during the EPRI multi-vent subscale tests.[18] In this case, an actual drywell floor formed an integral part of the test configuration (See Figure B-3). In these tests also a considerable variation in this parameter was observed. For those test configurations which are pertinent for the present discussion the observed range was 0 to +3.2 psid ($\pm$1.9 psid) in terms of scaled-up full scale values. Thus, despite the fact that the EPRI tests were made on a scaled replica of a representative Mark II plant (the Susquehanna plant) uploads substantially higher than those encountered during the 4T test series were observed.

Since the EPRI results were not incorporated into the development of the Mark II Owners Group load specification, an NRC consultant undertook a regression analysis whose aim was to correlate all of the available data. The details of this analysis are given in Reference 19. Briefly, the analysis first demonstrated that the trends of upward pressure differential observed during the 4T tests with break area (AB), pool area (AP), vent area (AV) and initial wetwell air space volume (VS) could be correlated according to the relation

$$\Delta PUP = 1.9 - 44\ F\ (\pm 1.2)\ \text{psid} \qquad \text{(a)}$$

where $F = (AB)\ (AP)\ (VS)\ (VD)^{-1}\ (AV)^{-2}$

and VD is the drywell volume.

From the heated pool tests (Phase III) and from the single unheated drywell test (Test No. 33) conducted during the 4T tests, influence coefficients were established to conservatively account for these effects. The resulting correlation then took the form

$$\Delta PUP = 4.2 - 44\ F\ (\pm 1.2)\ \text{psid} \qquad \text{(b)}$$

for prototypical values of initial wetwell pool and drywell temperature.

A comparison of the EPRI results with these correlations showed higher values of upward pressure differential. Possible reasons for this discrepancy were examined. It was concluded that the most probable mechanism was the use of a non-condensible (air) for drywell charging in the subscale tests. Although this is not representative of prototypical con[illegible] an influence coefficient to conservatively bound this effect was also developed. The correlation then takes the form

$$\Delta PUP = 7 - 44\ F\ (\pm 1.2)\ \text{psid} \qquad \text{(c)}$$

The recommended specification given in Section III.B.3.J.2 follows directly from Equation (c) by including the maximum positive uncertainty (+1.2) as part of the specification, while maintaining the upward pressure differential at a minimum of +2.5 psid.

6. Probabalistic Considerations for Vent Lateral Loads

The effects of downcomer lateral loads on a structural element is evaluated in Reference 20 by choosing the directions of the resultant forces on groups of downcomers in order to maximize the appropriate parameter used in the assessment of that element. The magnitudes of the forces are selected from figure 4.10a of Reference 20 which is intended to represent the force magnitude for which the exceedance probability of at least one event in 265 is 10^{-4}. The procedure used to obtain the curve in figure 4-10a of Reference 20, however, is based on histograms which represent a probability distribution for a component of the resultant force in a preselected, though random, direction rather than the magnitude of the force subject to the constraint that the direction of action of the force has already been selected. These two probability distributions, while related, are not identical

The relation between these two probability distributions can be formally evaluated by first considering the probability that a group of n downcomers chugging simultaneously produce a resultant force vector n defined as $P_n(\vec{F}) = f_n(\vec{F})\, dA_F$ where dA_F represents an differential area element in the two-dimensional force space. The function $f_n(\vec{F})$ represents, therefore, a probability density in this two-dimensional space and its histogram representation could in principle be generated by Monte Carlo calculations. Specific probability of the force lying on a certain sub region of the force space can be obtained by appropriate integration. For instance, the probability that in one chug event the magnitude of the force due to n downcomers exceeds (nF_0) can be represented as

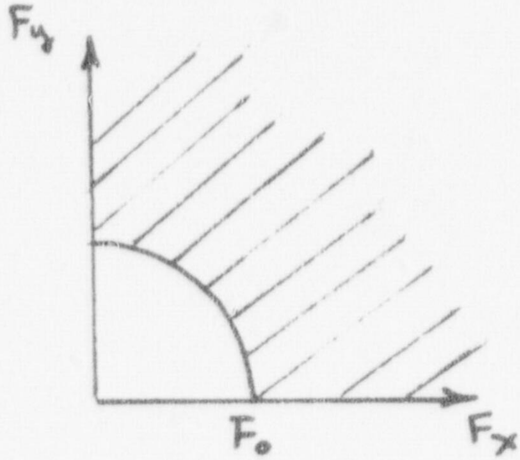
$$p_1 (nF > nF_0) = \int_{F_0}^{\infty} \int_0^{2\pi} f_n (\vec{F}) \, d\theta F dF = 2\pi \int_{F_0}^{\infty} f_n (F) \, F dF \tag{1}$$

where the randomness of the direction of the force allows us to assume $f_n(\vec{F}) = f_n(F)$ where F is the magnitude of $\vec{F}$. The probability that the x component of the force (nF_x) exceeds (nF_0) which is used to generate figure 4-10a can be obtained from f_n as

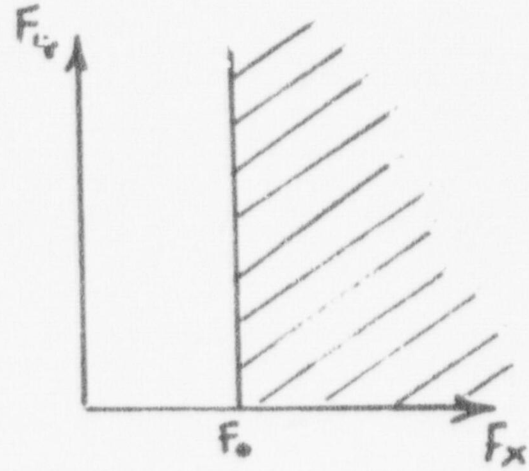
$$p_{x1} (n|F_x| > nF_0) = 2\int_{F_0}^{\infty} \int_{-\infty}^{\infty} f_n \left(\sqrt{F_x^2 + F_y^2}\right) dF_y \, dF_x \tag{2}$$

The difference between these two probabilities can be best represented graphically by showing the regions of integration where only a quarter of the domain $(F_x \geq 0, F_y \geq 0)$ is shown because of the symmetry properties of $f_n(F)$. The shaded region in Figure C-5a shows the range of integration to obtain p_1 while Figure C-5b shows the region for p_{x1}.

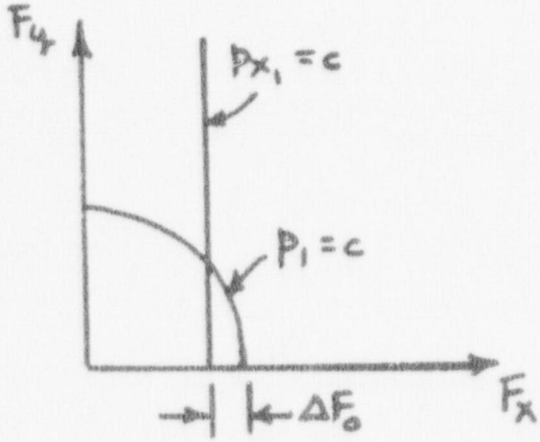
The desired result is the value F_0 for a given value of probability of at least one exceedance of F_0 in N independent events. This probability can be straightforwardly translated into a probability in a single event. Indeed the question of correspondence between p_1 and p_{x1} can be determined by evaluating the difference in F_0 that is required to make $p_{x1} = p_1$. Figure C-5c shows schematically lines of constant values of p_{x1} and p_1 and the difference in force ΔF_0.



a. Probability Domain for Resultant Force



b. Probability Domain for Component Force



c. Comparison of Probability Domains for Resultant and Component Forces

Figure C-5 Probability Domains for Resultant and Component Forces

The numerical value of F_o cannot be evaluated in general without knowing the distribution function $f_n(F)$, and for moderate values of probability is expected to be quite large for the probability level of 10^{-4} of one exceedance in 265 chugs f_n (F) can be adequately fitted by a normal distribution with the standard deviation for a specific number n obtained by matching the probability curves in Figure 4-10 of Reference 20 near $P = 10^{-4}$. The resultant changes in F_o are always positive and are listed below normalized by the F_o of figure 4-10a.

number of downcomers (n)	% change in F_o $\frac{100\ \Delta F_o}{F_o}$
50	7.0
10	7.5
20	7.7
40	8.3
100	8.5

Because of the very low value of probability (10^{-4}) the increase in the lateral load is therefore small for any number of downcomers. Because of substantial conservatism contained in the remaining aspects of the methodology, the loads in figure 4-10a, while deduced incorrectly, can be assumed to conservatively bound the downcomer lateral loads subject to the other constraints listed in Section III.B.4.a.2 of this LER.

7. Basis for Impact Load Specification on "Small" Structures

In Section III.B.3.c.1 it is stated that the impact load specification in the DFFR is not acceptable to the staff and a revised specification, which is acceptable, is presented. This appendix outlines the reasons why the DFFR methodology is deficient and also the basis for the staff's specification. The last part of this appendix addresses the question of fluid-structure interaction during impact, and presents the method for accounting for this effect in the structural analysis.

a. DFFR Specification

The Mark II Owners Group methodology for impact loads on "small" structures, as described in the DFFR, is based on data from the Pressure Suppression Test Facility (PSTF) (Appendix B.2). To calculate stresses in structures under dynamic conditions requires the specification of impact force as a function of time. The DFFR defines a normalized force history, somewhat similar to a versed-sine shape, for impact on all targets. This shape was selected because it was "found to be the most representative of the many profiles that were obtained during PSTF impact tests." With the pulse shape defined, there are then two parameters that are needed to completely specify the force history: 1) the maximum force or pressure and 2) the pulse duration. The DFFR presents the maximum pressure (force/projected area of target) as a linear function of pool velocity. These correlations are obtained directly from the PSTF data. One correlation is presented for cylindrical targets ranging from 0 to 20 inches in diameter. Two different correlations are presented for flat targets: one for 0 to 5 inches in width, the other 5 to 20 inches. For conservatism, the DFFR further specifies that the loads be increased by a factor of 1.5. For load prediction, the velocity to be used with these correlations is obtained from the pool swell model (III.B.3.a.3). For the pulse duration, the DFFR specifies a constant value of 7 milliseconds regardless of target geometry or impact velocity. This was done on the grounds that it is "most representative."

b. Critique of DFFR Specification

The staff has reviewed the impact load specification in the DFFR and has concluded that it is incomplete. It felt that neither the maximum pressure correlations nor the constant pulse duration of 7 msec can be considered applicable to all Mark II structures without further qualifications. The maximum pressures measured during impact in the PSTF were found to be sensitive to slight variations in the curvature of the pool surface (maximum pressures increased with increasing "flatness" of the pool). It is not apparent that maximum pressures measured in the PSTF are bounding for all structures above Mark II pools, without addressing the question of pool curvature. The pulse duration, likewise, depends on pool curvature. In addition, the pulse duration is a function of target geometry, size and pool approach velocity. A constant duration of 7 msec for all situations, as specified in the DFFR, obviously does not account for these variations. (The overall range of pulse duration that was actually recorded in PSTF varied from 2 to 38 msecs).

c. Basis for NRC Load Specification

The impact load specification formulated by the staff addresses the concerns stated in the previous paragraph. First of all, note that the total impulse (or hydrodynamic mass) correlates well with the target geometry and pool velocity (Figures 5-24 and 5-25, Reference 21). A similar correlation does not exist for peak force or peak pressure. The reason is that, for a fixed impulse, the peak force is approximately inversely proportional to the duration of the pulse, and the latter is very sensitive to slight variations in curvature of the pool surface. The staff's specification, therefore, is based on the PSTF impulse data rather than the maximum pressure data.

In general, the impulse values alone are not sufficient to perform a stress analysis under dynamic conditions. Unless the pulse duration is very short (less than 10% of the important natural periods of the structure), one requires the actual force or pressure history, $p = f(t)$. Since the impulse or pressure integral $I_p = \int_0^T p\,dt$ is known (from PSTF), the question in formulating the load specification becomes one of distributing the pulse in time in such a way that is both conservative and realistic. If one selects a non-dimensionalized pulse shape, then a pulse duration automatically specifies the maximum amplitude or vice versa. The former approach will be used for the current specification.

The actual pulse shapes for impact are rather complex and depend on the details of fluid flow around the immersed portion of the structure. The DFFR defines a pulse similar to a versed-sine shape. The staff finds the versed-sine shape an acceptable approximation since it is a shape that leads to higher stresses than rectangular, triangular or half-sine pulse of the same total imuplse.

As was pointed out earlier, the pulse durations are greatly affected by small changes in pool curvature. In the absence of data on pool curvature it is prudent to assume flat pool impact, since this results in the shortest impact duration and the greatest stresses. Another way to state this is that one cannot see the basis for excluding the possibility of flat pool impact for Mark II targets. The fact that shorter pulses lead to greater response (stress) may be inferred from Figure 8.18 of Reference 22 where the response of a single degree-of-freedom system is plotted as a function of pulse duration τ for various pulse shapes. It is seen that, regardless of the pulse shape, the deflection increases as the pulse shape is narrowed. Eventually, as the pulse duration becomes equal to about 1/10 the natural period of the system, the deflection becomes a constant and no longer depends on the duration (or shape) of the pulse; i.e., pure impulsive loading.

The pulse durations are very different for cylindrical and for flat targets. For cylindrical targets, one can estimate the pulse duration from the experimental observation that the impact pressure is experienced by a cylinder only over an angle of 25° on either side of the stagnation line.[23] This leads to the following equation for pulse duration:

$$\tau = \frac{D}{2}\ (1 - \cos 25°)/V$$

or

$$\tau = 0.0468\ \frac{D}{V}$$

For flat targets the situation is more complex. An experimental investigation by Chuang[24] indicates that if the flat target is perfectly horizontal, a cushion of air is trapped between the target and pool and the pulse duration is spread out in time. Chuang presents an approximate analytical method for calculating the pulse duration in the presence of this air cushion. In addition, he tested some wedges with small deadrise angles (1, 3, 6, 10 and 15°) and noted that even a slight inclination in the target surface, with respect to the pool, was sufficient for the air to be pushed out of the way. Specifically, he noted that at 1° some air was still present but at 3° the air had been pushed aside. Since the shortest pulse durations lead to the largest stresses, one is interested, for sake of conservatism, in identifying the shortest reasonable pulse duration for the load specification. To do this, the pulse durations were determined for two different situations: (1) perfectly horizontal targets with air cushions, and (2) targets inclined at 1° without air cushions.* (Since the air cushion disappears somewhere between 1° and 3° inclination, the more conservative approach is to assume it happens at 1°). The pulse durations with the air cushion were calculated by the analytical method suggested by Chuang.[24] For the 1° inclination, the pulse durations were determined in the following manner: From examination of the pressure traces for the individual transducers on wedge surfaces (Figure 8 Reference 24), it is apparent that,

*It seems reasonable to assume that the flat structures above a Mark II pool may be inclined by 1° from the horizontal.

during impact, a high pressure wave traverses the wedge from the keel to the edge. From these same pressure traces, one can calculate the (approximately constant) speed at which this wave travels, or, conversely, the time required to traverse a target of a certain width. This traverse time is essentially the pulse duration for impact. It was observed that, for small wedge angles, the traverse time was directly proportional to the wedge angle. Thus, one could readily establish the pulse durations for a 1° inclination at the particular velocity of impact (5.7 ft/sec) tested by Chuang. To generalize the pulse duration for higher velocities, it is noted from the theoretical treatment of this problem (e.g. Reference 25, pp. 360-363) that the pulse travel times are inversely proportional to the pool velocity. Thus, one obtains the equation

$$\tau = 0.011 \frac{W}{V}$$

where the factor of 0.011 was established from Chuang's experiments.

When the pulse durations, using the above equation, were compared to corresponding pulse durations for a horizontal target with an air cushion, it was found that, for pool velocities greater than

7 ft/sec, the pulse durations for the 1° inclined target were shorter. Therefore, the equation shown above is used in the load specification for impact velocities greater than 7 ft/sec. At impact velocities less than 7 ft/sec., the analysis for the air cushion leads to pulse durations approximated by the equation

$$\tau = 0.0016\ W$$

With the impulse data from PSTF, the assumed versed-sine shape for the pulse, and the pulse durations as specified in the previous paragraph, the impact specification is complete. The remaining item, maximum amplitude of the pulse p_{max}, is automatically specified by the information above.

$$I_p = \int_0^{\tau} p(t)\, dt = \frac{1}{2} p_{max} \tau$$

or

$$p_{max} = 2I_p / \tau$$

d. Comparison of the Two Specifications

A comparison was made of the two loading specifications: (1) the DFFR specification and (2) the staff specification. It is noted that, unless the pulse durations are very brief (impulsive loading) or very long (static loading), it is not possible to compare a certain combination of force and pulse duration without considering the dynamic response of the structure that is impacted. The comparison was therefore conducted by calculating dynamic bending and shear stresses in pipes and beams by the two different methods. It was found that, depending on the natural frequency of the targets, the DFFR can predict stresses (bending and shear) considerably lower than one obtains using the staff's criteria; i.e., PSTF impulse data and flat pool impact. For the range of conditions considered in this study (V = 15 to 35 ft/sec, D = 5 to 20 inches, f = 10 - 1000 Hz), the flat pool stresses in pipes were as much as 5.6 times greater than predicted using DFFR methodology. The corresponding ratios for flat targets were even higher. Consequently, the DFFR load specification is not conservative, unless it is applied with restrictions which have not, as yet, been rigorously defined.

e. Fluid/Structure Interaction

The last item to be addressed is the question of fluid-structure interaction during impact. The pressure pulses, defined by the load acceptance criteria, correspond to impact on rigid structures. This is due to the fact that the experimental data [23, 24] which were used as the basis to characterize pulse durations, were obtained with very rigid models. The real structures above the Mark II pools may be more flexible, with the result that the pressure pulse, during impact, will be modified by the motion of the target. Following the approaches outlined in References 24 and 26, this effect can be accounted for as indicated below.

The motion of a slender uniformly loaded beam is given by the following equation [12]

$$m\ddot{y} + EI \frac{d^4 y}{dx^4} = p(t)$$

where

m = mass of beam per unit length

y = deflection from unloaded position

p = force per unit length of beam (p was used for pressure earlier in this appendix).

Consider the total force p as composed of the rigid body impact force p_r and a perturbation on p_r due to the fact that the body is deformable, p_i. Thus

$$p = p_r + p_i$$

If one neglects the damping and compressibility of water, the interaction force is simply equal to

$$p_i = -m_H \ddot{y}$$

where

m_H = hydrodynamic mass of impact.

The minus sign comes from the fact that as the interface moves in the positive direction (away from the water) the total force is reduced.

Combining the last three equations, we have

$$m\ddot{y} + EI \frac{d^4y}{dx^4} = p_r(t) - m_H \ddot{y}$$

or

$$(m + m_H)\ddot{y} + EI \frac{d^4y}{dx^4} = p_r(t)$$

It is seen that the motion (and stresses) of a flexible beam can be calculated by driving it with a rigid beam forcing function. The mass of the beam, however, must be increased by the hydrodynamic mass of impact.

The validity of this approach has been verified experimentally by Chuang.[24] As part of his test program, he dropped a horizontal flexible plate into a pool of water from different heights. He then compared the motion of this flexible plate with the analytical prediction using the rigid body driving function. He showed good agreement between the analytical prediction and tests.

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APPENDIX D

NRC Acceptance Criteria
Mark II Pool Dynamic Loads
for Lead Plants

I. LOCA-Related Hydrodynamic Loads

A. Pool Swell Load

1. Pool Swell Elevation

The maximum pool swell elevation specification shall consist of that predicted by the pool swell analytical model described in NEDE-21544-P using a polytropic exponent of 1.2 for wetwell air compression.

2. Pool Swell Velocity

The pool swell velocity used to determine impact and drag loads on wetwell components shall consist of the velocity predicted by the pool swell analytical model described in NEDE-21544-P multiplied by a factor of 1.1.

3. Impact/Drag Loads on Grating

The static drag load, Fss, on grating in the pool swell zone of the wetwell shall be calculated for grating with open area greater than or equal to 60% by forming the product of pressure differential as given in Figure 4-40 of NEDO-21060, Revision 2 and the total area of the grating. To account for the dynamic nature of the initial loading, the load shall be increased by a multiplier given by

$$F_{SE}/D = 1 + \sqrt{1 + (0.0064\ Wf)^2}\ ; \text{ for } Wf < 2000 \text{ in/sec},$$

where:

F_{SE} = static equivalent load

W = width of grating bars, in

f = natural frequency of lowest mode, Hz

D = static drag load

4. Diaphragm Upward Load

The maximum upward load, ΔPUP, on the diaphragm shall be calculated by the correlation:

$$\Delta PUP = 8.2 - 44\ F\ (psi) \qquad 0 \leq F \leq 0.13$$

$$\Delta PUP = 2.5\ (psi) \qquad F > 0.13$$

$$F = \frac{AB \cdot AP \cdot VS}{VD \cdot (AV)^2}$$

where

AB = break area

AP = net pool area

AV = total vent area

VS = initial wetwell air space volume

VD = drywell volume

5. Asymmetric Bubble Load

A bounding asymmetric bubble pressure load at the time of vent clearing on the wetwell wall shall be based on the assumption that all air is vented on one-half of the drywell periphery and steam is vented on the other half. The pool swell analytical model described in NEDE-21544-P shall be the basis for the specification of the maximum asymmetric load. A maximum increase of the air bubble pressure predicted by this model and a minimum increase of zero shall be applied to the wetwell wall in a worst case distribution for this asymmetric bubble load condition.

6. Impact Loads on Small Structures

The hydrodynamic loading function that characterizes pool impact on small* horizontal structures shall have the versed sine shape

$$p(t) = P_{max} \frac{1}{2} (1 - \cos 2\pi \frac{t}{\tau})$$

where

p = pressure acting on the projected area of the structure, psi

P_{max} = the temporal maximum of pressure acting on the projected area of the structure, psi

t = time, sec

τ = duration of impact, sec

For both cylindrical and flat structures, the maximum pressure P_{max} and pulse duration τ will be determined as follows:

(a) The hydrodynamic mass per unit area for impact loading will be obtained from the appropriate correlation for a cylindrical or flat target in Figure 6-8 of Reference 4.

(b) The impulse will be calculated using the equation

$$I_p = \frac{M_H}{A} V \times \frac{1}{(32.2)\ (144)}$$

*Small structures, in the present context, are defined as pipes, I-beams and other similar structures having one dimension less than or equal to 20 inches. The acceptance criteria, given below, are not applicable to the determination of ovaling stresses in cylindrical pipes.

I_p = impulse per unit area, psi-sec

$\frac{M_H}{A}$ = hydrodynamic mass per unit area, lbm/ft^2, from (a) above.

V = impact velocity, ft/sec, determined according to Section I.A.2

(c) The pulse duration will be obtained from the equation

$$\tau = \frac{0.0463\ D}{V} \text{ (cylindrical target)} \qquad \tau = \frac{0.011\ W}{V} \quad V > 7 \text{ ft/sec (flat target)}$$

$$\tau = 0.0016\ W \quad V < 7 \text{ ft/sec}$$

where:

τ = pulse duration, sec

D = diameter of cylindrical pipe, feet

W = width of the flat structure, feet

V = impact velocity, ft/sec

(d) The value of P_{max} will be obtained using the following equation:

$$P_{max} = 2I_p/\tau$$

For both cylindrical and flat structures, a margin of 35% will be added to the P_{max} values (as specified above) to obtain conservative design loads.

The load acceptance criteria, as specified above, corresponds to impact on rigid structures. The effect of finite flexibility of real structures will be accounted for in the following manner. When performing the structural dynamic analysis, the "rigid body" impact loads will be applied; however, the masses of the impacted structures will be adjusted by adding on the hydrodynamic masses of impact. The numerical values of hydrodynamic masses will be obtained from the appropriate correlations for cylindrical and flat structures in Figure 6-8 of Reference 4.

B. Steam Condensation and Chugging Loads

1. Single Vent Lateral Loads

The following single vent load specification will be used.

(a) A static equivalent load of 8.8 KIPS shall be used provided that: (i) the downcomer is 24 inches in diameter; (ii) the downcomer has a dominant natural frequency of 7Hz or less in its submerged state; and (iii) the downcomer is either unbraced or braced at approximately 8 feet or more above the downcomer exit.

(b) A static equivalent load of 8.8 KIPS multiplied by the ratio of the downcomer natural frequency and 7 Hz shall be used for downcomers with natural frequency greater than 7 Hz but less than or equal to 14 Hz. This specification may be used only if the other restrictions outlined in item (a) above are satisfied.

(c) If the natural frequency of the downcomer is above 14 Hz, or if the downcomer is braced at a point closer than 8 feet above the vent exit, a dynamic structural calculation of the downcomer response shall be performed on a plant specific basis. For such a calculation, the dynamic load shall be defined by the equation:

$$F(t) = F_0 \sin \frac{\pi t}{T}; \; 0 < t < T$$

$$= 0; \text{ for } t < 0 \text{ and } t > T$$

where 2 msec $< T <$ 10 msec and the impulse $I = F_o(T/\pi) \times 2$ is 200 lbf-sec. This specification is also subject to restriction (i) listed in item (a), above. Analyses of downcomer dynamic response to lateral chugging loads shall be performed for all plants during the Mark II Long Term Program to provide additional confirmation that the static load specification is conservative.

2. Multiple Vent Lateral Loads

The multiple vent load specification shall consist of the load specified in Figure 4-10b of NEDE 21061-P, Revision 2 multiplied by a factor of 1.26. For downcomers with natural frequency greater than 7 Hz, an additional multiplier equal to the ratio of this frequency and 7 Hz shall also be applied.

II. SRV-Related Hydrodynamic Loads and Pool Temperature Limit

1. Discharge Device

The applicants for all Mark II facilities will be required to commit to the use of quencher type devices.

2. Interim Load Specification

(a) Methodology for Bubble Load Prediction

Those applicants that have committed to the use of the KWU "T" quencher device, shall use the SRV air clearing loads based on the predictions of loads for ramshead air discharges extrapolated to Mark II conditions. The methodology for predicting ramshead loads is described in Section 3.2 of NEDO-21061 and NEDE-21061P Revision 2.

The applicants for all plants using the four-arm quencher device shall use the methodology for predicting the quencher discharge loads described in Section 3.3 of NEDO-21061 and NEDE-21061P Revision 2.

A final load specification for the quencher devices is under development and shall be based on results from large scale tests which must be complete before plant operation. In plant tests are desirable. However, the need for these inplant tests will be determined as a part of NRC staff's review of the applicant's quencher supporting program.

(b) SRV Discharge Load Cases

The following load cases shall be considered for design evaluation of containment structures and equipment inside the containment:

1. Single valve discharge for first and consecutive actuation;
2. ADS valves discharge;
3. Two adjacent valves discharge;
4. All valves discharge sequentially by setpoint group;
5. All valves discharge simultaneously and assuming all bubbles oscillating in-phase.

The number of valves actuated consecutively in the multiple valves cases (Cases 2, 4 and 5 above) shall be determined by plant unique analyses.

(c) Bubble Frequency

The forcing function used to evaluate the SRV discharge load cases described in item (b) above shall include as a minimum a range of bubble frequency of 4 to 12 Hz for both discharge devices. For the T-quencher, the range shall be increased, if required, to include the frequency predicted by the ramshead methodology together with a ± 50% margin.

3. Pool Temperature Specification

(a) Pool Temperature Limit

The suppression pool local temperature shall not exceed 200°F for all plant transients involving SRV operations. The applicants are required to provide an in-plant test data base for establishing the difference between local and bulk pool temperature. The definition of local and bulk pool temperature is provided in the following section. The applicants are also required to provide plant unique analyses for pool temperature responses to transients involving SRV operations to demonstrate that the plants will operate within the limit.

(b) Local and Bulk Temperature

Local temperature is defined as the water temperature in the vicinity of the quencher device. For practical purpose, measurement from the temperature sensors, which are located on the containment wall in the sector containing the discharge device and at the same elevation of the discharge device, can be used as local temperature.

Bulk temperature, on the other hand, is a calculated temperature which assumes the pool as a uniform heat sink. Bulk temperature is calculated on the basis of energy and mass released from the primary system through the safety/relief valves following the plant transients.

4. Suppression Pool Temperature Monitor System

The suppression pool temperature monitoring system is required to ensure that the plant is always operated within the technical specification limits. It is our position that the applicants should meet the following general requirements for the design of this system:

1. Redundant sensors shall be provided at each monitoring location.

2. The total number of monitoring locations shall not be less than eight. Monitoring locations shall be distributed evenly around the pool.

3. Sensors shall be installed sufficiently below the minimum technical specification water level to assure that the sensor properly monitors pool temperature.

4. Pool temperature shall be monitored on recorders in the control room. Two sensors from each sensor group shall be recorded. The difference between measurement reading and actual local temperature shall be within $\pm$ 2°F.

5. Instrument set points for alarm shall be established so that the primary system can be shutdown and depressurized to less than 200 psia before the suppression pool temperature reaches the temperature limit as speciifed in 3(a).

6. All sensors shall be designed to seismic Category I, quality group B, and energized from onsite emergency power supplies.

III. LOCA/SRV - Submerged Structure Loads

A. LOCA/SRV Jet Loads

1. LOCA/SRV - Ramshead Jet Loads

LOCA related vent jet loads and ramshead jet loads shall be calculated based on the methods described in NEDE-21730, subject the following constraints and modifications:

(a) Standard drag at the time the jet first encounters the structure must be multiplied by the factor

$$1 + \frac{6\ V_a}{C_D\ A_X\ R_i},$$

where V_a, C_D, A_X are the acceleration volume, drag coefficient and projected area of the structure as defined in NEDE-21730 and R_i is the vent exit radius.

(b) Forces in the vicinity of the jet front shall be computed on the basis of an acceleration and standard drag (Formula 2-12 and 2-13 of NEDE-21730). The local velocity, U_∞, and acceleration, $\dot{U}_\infty$ are to be computed conservatively by the methods of NEDE-21471 from the potential function:

$$\phi = \frac{-3}{8\pi}\ U_j\ V_w\ \frac{\cos\theta}{r^2};$$

where r and θ are the spherical coordinates from the jet front center with θ measured from the jet direction, U_j is the jet front velocity from NEDE-21730 and V_w is the initial volume of water in the vent.

(c) After the last fluid particle has reached the jet front a spherical vortex continues propagating. The drag on structures in its vicinity can be bounded by using the flow field from the formula for ϕ above with U_j as the jet front velocity from NEDE-21730 at time $t = t_f$.

2. SRV Quencher - SRV Jet Loads

Quencher jet loads are expected to be small. This load may be neglected for those structures located outside of a sphere circumscribed around the quencher arms. If there are holes in the end cap on the quencher, the radius of this sphere should be increased by 10 hole diameters. Confirmation of this assumption must be provided in the Long Term Program.

B. LOCA/SRV Air Bubble Loads

1. LOCA Air Bubble Loads

The methodology for computation of submerged structure loads during the air clearing phase of a postulated LOCA shall be based on an analytical model of the bubble charging process and drag calculations of NEDE-21471 until the bubbles coalesce. After bubble contact, the pool swell analytical model, together with the drag computation procedure of NEDE-21471 shall be used. Use of this methodology shall be subject to the following constraints and modifications:

(a) A conservative estimate of bubble asymmetry shall be added by increasing accelerations and velocities computed in step 12 of Section 2.2 of NEDE-21730 by 10%. If the alternative steps 5A, 12A and 13A are used, the acceleration drag shall be directly increased by 10% while the standard drag shall be increased by 20%.

(b) The drag coefficients C_D for the standard drag contribution in steps 13, or 13A, 15 of Section 2.2 and step 3 of Section 2.3 of NEDE-21730 may not be taken directly from the steady state coefficients of Table 2-3. Modified coefficients C_D' from accelerating flows as presented in References 1 and 2 shall be used with transverse forces included, or an upper bound of a factor of three times the standard drag coefficients shall be used for structures with no sharp corners or with streamwise dimensions at least twice the width.

(c) The equivalent uniform flow velocity and acceleration for any structure or structural segment shall be taken as the maximum values "seen" by that structure, not the value at the geometric center.

(d) Drag forces on submerged structures may be computed independently of each other (as presented in NEDE-21730) for structures sufficiently far from each other so that interference effects are negligible. Interference effects are expected to be insignificant when two structures are separated by more than three characteristic dimensions of the larger one. For structures that are closer together, either detailed analysis of interference effects must be performed or a conservative multiplication of both the acceleration and drag forces by a factor of four must be performed.

(e) A specific example of interference which must be accounted for is the blockage presented to the motion of the water slug during pool swell due to the presence of downcomer bracing systems. If significant blockage relative to the net pool area exists, the standard drag coefficients shall be modified for this effect by conventional methods (See Reference 3).

(f) Formula 2-23 of NEDE-21730 shall be modified by replacing M_H by $\rho_{FB} V_A$ where V_A is obtained from Tables 2-1 and 2-2.

2. Ramshead Air Bubble Loads

The methodology for computation of submerged structure loads during the air clearing phase of a ramshead as described in NEDE-21471 shall be used, subject to the following constraints and modifications:

(a) Standard drag shall not be neglected without first estimating its order of magnitude. The relative importance of standard to acceleration drag depends on the size of the structure, size of the bubble and the distance from the bubble. The importance of standard drag can be estimated using the equation:

$$\frac{F_{S_M}}{F_{A_M}} = f\left(\frac{P_{max}}{P_\infty}\right) \frac{C'_D}{\pi} \frac{R_{min}}{d} \left(\frac{R_{min}}{r}\right)^2$$

where:

F_{S_M} = maximum standard drag;

F_{A_M} = maximum acceleration drag;

C'_D = cycle-averaged effective drag coefficient;

d = diameter of a cylindrical structure;

R_{min} = minimum bubble radius;

r = distance from bubble center to the structure; and

$f\left(\frac{P_{max}}{P_\infty}\right) \approx 8/3$ for $\frac{P_{max}}{P_\infty} \geq 30$.

(b) The constraints and modifications described above in Section III.B.1, above for LOCA air bubbles shall also be applied to the calculation of ramshead air bubble loads as appropriate.

3. Quencher Air Bubble Loads

No procedure or analyses for air clearing loads on submerged structures for quencher SRV operation has been presented by the Mark II Owners Group. These loads may be computed by the same basic methodology as used for the ramshead device subject to the modification of the source strength as substantiated by experimental data. An interim submerged structure load due to four-arm quenchers shall be determined using the source strength derived from bubble pressures calculated by the methods of NEDO-21061 Revision 2 and NEDE-21061-P for a four arm quencher.

Associated loads for the T-quencher may be computed on the basis of the ramshead methodology and bubble pressure described in NEDO-21061, Revision 2 and NEDE-21061-P. However, the bubble shall be assumed to be located at the center of the quencher device with bubble radius equal to the radius of the quencher. The bubble pressure of the quencher may be assumed to be 25% of the bubble pressure calculated by the ramshead methodology.

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Appendix E. Mark II Containment Supporting Program

The methodology proposed by the Mark II owners for specifying the generic hydrodynamic loading on the Mark II containments is provided in the Dynamic Forcing Function Information Report, DFFR.[1] These loads are based on a number of experimental and analytical tasks comprising the Mark II containment supporting program. These tasks are sponsored and directed by the Mark II Owners Group. The supporting program was first described in outline form to the NRC in August 1975, and was followed by Revision 0 of the supporting program report[2] in May 1976. A more current description of the program is provided in the March 1978 Revision 1 issue of this report. Modifications to both the DFFR and the supporting program have and will continue to be made as information from the supporting program continues.

The program structure consists of three series of tasks which are categorized as LOCA-related (Series A) tasks, SRV-related (Series B) tasks, and other miscellaneous (Series C) tasks. The tasks are further categorized as Lead Plant, Intermediate Program or Long Term based on the role of the specific task as a basis for loads in the respective programs. Documentation for the tasks associated with the LPP is complete. The tasks associated with the proposed IP were approximately 25% complete as of August 1978.

Within the LOCA and SRV-related task series, one or more of the tasks are associated with the evaluation of each of the primary hydrodynamic loads. Figure E-1 provides an overview of the total program organized by the primary loads. Table E-1 provides a list of all the tasks comprising the Mark II Owners Program. This table includes information related to the task activity, activity type, target completion date, form of documentation and documentation date and task category. The task categories include Lead Plant (LP SER) and Intermediate Plant (IP). Those tasks with no category specified are Long Term Program Tasks.

A number of programs related to Mark II pool dynamic loads are being conducted outside the scope of the Mark II Owners Program. The most significant of these activities relates to the quencher qualification program. The lead Mark II plants have committed to the use of "T" quencher SRV discharge devices. The supporting programs for these devices consist of a combination of individual utility sponsored programs and programs sponsored by subgroups of the Mark II owners.

A summary description of the load related primary tasks included in the Lead Plant Program is provided in Appendices B and C of this report. A brief description of several of the more important load related tasks in the remainder of the program is provided below.

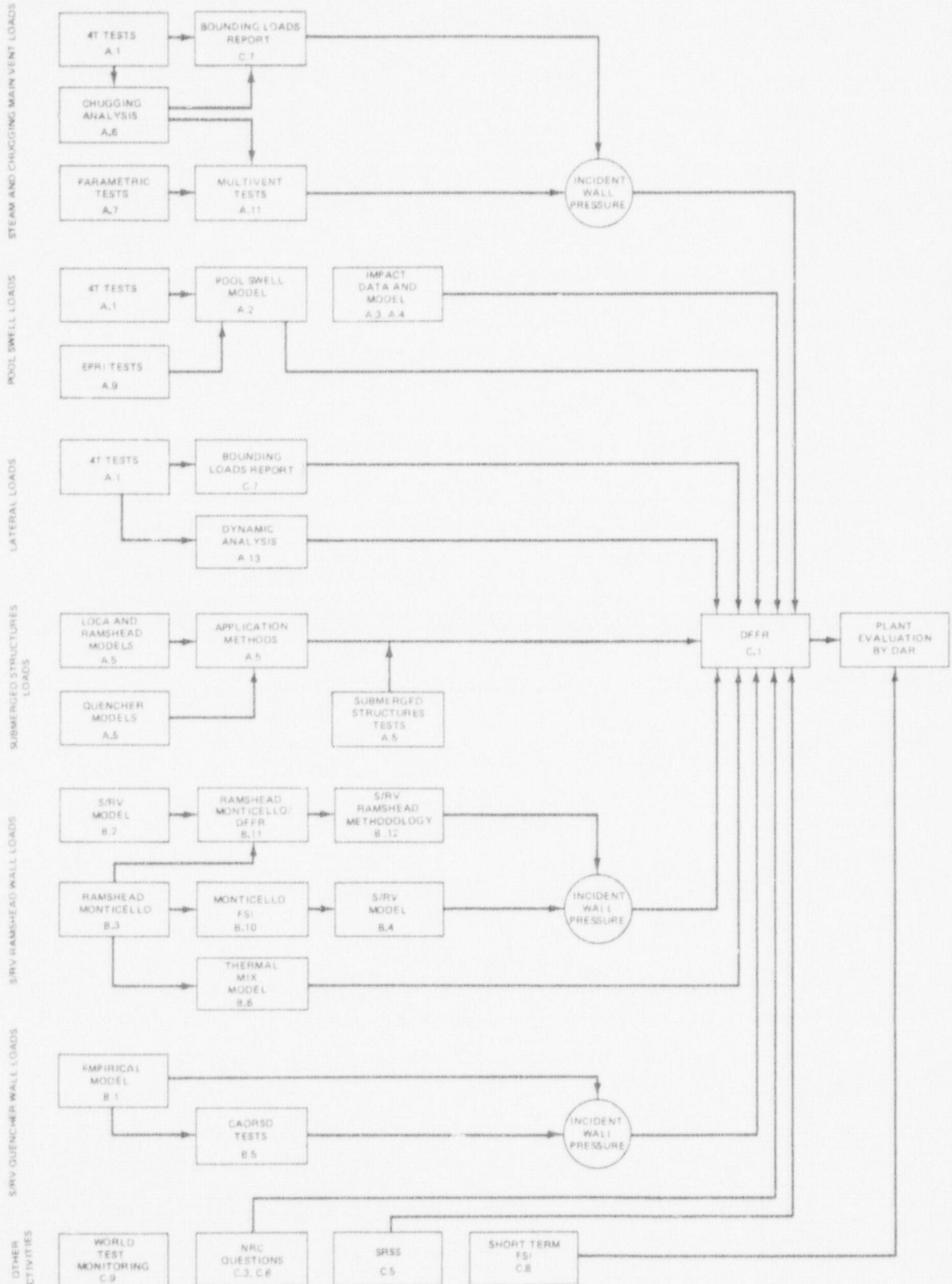

Figure E-1 Mark II Containment Supporting Program

Table E-1

MARK II CONTAINMENT - SUPPORTING PROGRAM
LOCA - RELATED TASKS

TASK NUMBER	ACTIVITY	ACTIVITY TYPE	TARGET COMPLETION	DOCUMENTATION	DATE DOC/SUBM	LEAD PLANT SER/ INTERMED PLANT
A.1	"4T" TEST PROGRAM	Phase I Test Report	Completed	NEDO/NEDE 13442-P-01	5/76 - 5/76	LP SER/IP
		Phase I Appl Memo	Completed	Application Memo	6/76 - 6/76	LP SER/IP
		Phase II & III Test Rpt	Completed	NEDO/NEDE 13468-P	12/76 - 1/77	LP SER/IP
		Application Memorandum	Completed	NEDO/NEDE 23678-P	1/77 - 2/77	LP SER/IP
A.2	POOL SWELL MODEL REPORT	Model Report	Completed	NEDO/NEDE 21544-P	12/76 - 2/77	LP SER/IP
A.3	IMPACT TESTS	PSTF 1/3 Scale Tests	Completed	NEDO/NEDE 13426-P	8/75 - 9/75	LP SER/IP
		Mark I 1/12 Scale Tests	Completed	NEDO/NEDC 20989-2P	9/75 - 11/75	LP SER/IP
A.4	IMPACT MODEL	PSTF 1/3 Scale Tests	Completed	NEDO/NEDE 13426P	8/75 - 9/75	LP SER/IP
		Mark I 1/12 Scale Tests	Completed	NEDO/NEDC 20989-2P	9/75 - 11/75	LP SER/IP
A.5	LOADS ON SUBMERGED STRUCTURES	LOCA/RH Air Bubble Model	Completed	NEDO 21471	9/77 - 1/78	LP SER/IP
		LOCA/RH Water Jet Model	Completed	NEDO/NEDE 21472	9/77 - 1/78	LP SER/IP
		Applications Methods	Completed	NEDO/NEDE 21730	12/77 - 1/78	LP SER/IP
		Quenc. Air Bubble Model	3Q 78	NEDO 21471 Supplement		IP
		Quenc. Water Jet Model	3Q 78	NEDE 23539-P		IP
		Steam Condensation Model	4Q 78	NEDE 23610-P		IP
		Appl. Memo. Supp.	3Q 78	NEDE 21730 Supplements		IP
		Simple Geometry Tests	3Q 78	Report		
		1/4 Scaling Tests	3Q 78	Report		
		Model/Data Eval.	4Q 78	Report		
A.6	CHUGGING ANALYSIS AND TESTING	Single Cell Report	Completed	NEDO/NEDE 23703-P	9/77 - 11/77	LP SER
		Multivent Model	Completed	NEDO/NEDE 21669-P	2/78 - 3/78	IP
		4T FSI Report	Completed	NEDE 23710-P	4/78 - 3/78	LP SER
A.7	CHUGGING SINGLE VENT	CREARE Report	Completed	NEDE 21691-P	6/78 - 7/78	
A.9	ERPI TEST EVALUATION	EPRI-4T Comparison	Completed	NEDO 21667	8/77 - 9/77	LP SER*
-	EPRI 1/13 SCALE TESTS	3D Tests	Completed	EPRI NP-441	4/77 - --	LP SER*
-	EPRI SINGLE CELL TESTS	Unit Cell Tests	4Q 78	Report		
A.11	MULTIVENT SUBSCALE TESTING AND ANALYSIS	Preliminary MV Prog Plan	Completed	NEDO 23697	12/77 - 1/78	LP SER/IP
		Phase I Scaling Anal.	3Q 78	Report		IP
		Phase I Scoping Tests	2Q 79	Report		
		Phase II MV Test & Final Prog. Plan	2Q 79	Report		
		Phase II MV Test Rept.	1Q 80	Report		
		MV Tests - Final Rept.	3Q 80	Report		
A.13	SINGLE VENT LATERAL LOADS	Dynamic Analysis	Completed	NEDE 24106-P	3/78 - 7/78	IP
		Summary Report	3Q 78	NEDE 23806-P		
A.16	CHUGGING LOADS IMPROVEMENT	Impulse Evaluation	Completed	Letter Report	6/78 - 7/78	LP SER
		Ringout Removal Analysis	1Q 79	Report		IP

LP SER: Zimmer, LaSalle, Shoreham

MARK II CONTAINMENT - SUPPORTING PROGRAM
SRV - RELATED TASKS

TASK NUMBER	ACTIVITY	ACTIVITY TYPE	TARGET COMPLETION	DOCUMENTATION	DATE DOC/SUBM	LEAD PLANT SER/ INTERMED PLANT
B.1	QUENCHER EMPIRICAL MODEL	DFFR Model	Completed	NEDO/NEDE 21061-P	9/76 - 9/76	IP
		Supporting Data	Completed	NEDO/NEDE 21078P	5/75 - 7/75	IP
B.2	RAMSHEAD MODEL	DFFR Model	Completed	NEDO/NEDE 21061-P	9/76 - 9/76	LP SER
		Supporting Data	Completed	NEDO/NEDE 21062-P	7/75 - 10/75	
		Analysis	Completed	NEDO/NEDE 20942-P	5/75 - 7/75	
B.3	MONTICELLO IN-PLANT S/RV TESTS	Preliminary Test Rpt.	Completed	NEDO/NEDC 21465-P	12/76 - 1/77	LP SER
		Hydrodynamic Report	Completed	NEDO/NEDC 21581-P	8/77 - 8/77	
B.4	CONSECUTIVE ACTUATION TRANSIENT ANALYSIS	Analytical Models	-	Report		IP
B.5	S/RV QUENCHER IN-PLANT CAORSO TESTS	Test Plan	Completed	NEDM 20988 Rev. 2	12/76 - 3/77	IP
		Test Plan Addendum 1	Completed	NEDM 20988 Rev. 2, Add 1	10/77 - 3/78	IP
		Test Plan Addendum 2	Completed	NEDM 20988 Rev. 2, Add 2	4/78 - 7/78	IP
		Prelim. Test Report	4Q 78	Report		IP
		Final Report	2Q 79	Report		IP
B.6	THERMAL MIXING MODEL	Analytical Model	Completed	NEDO/NEDC 23689-P	3/78 - 3/78	
B.7	SRV LINE CLEARING	Analytical Model	1Q 79	NEDE 23749-P		IP
B.10	MONTICELLO FSI	Analysis of FSI	Completed	NEDO 23834	6/78 - 7/78	LP SER
B.11	DFFR RAMSHEAD MODEL TO MONTICELLO DATA	Data/Model Comparison	Completed	NSC-GEN 0394	9/77 - 10/77	
B.12	RAMSHEAD SRV METHODOLOGY SUMMARY	Analytical Methods	Completed	NEDO 24070	10/77 - 11/77	LP SER
B.14	QUENCHER EMPIRICAL MODEL UPDATE	Model Confirmation	2Q 79	Report		
B.15	QUENCHER MULTIVALVE MODEL	Statistical Model	1Q 79	Report		IP
B.17	Q ANALYTICAL MODEL	Model Development	1Q 79	Report		IP
B.18	Q FORCING FUNCTION	Data Evaluation	1Q 79	DFFR Rev.		IP
B.20	Q ATTENUATION	Data Evaluation	1Q 79	DFFR Rev.		IP
B.21	Q EMPIRICAL MODEL IMPROVEMENT	Statistical Evaluation	1Q 79	DFFR Rev.		IP

MARK II CONTAINMENT - SUPPORTING PROGRAM
MISCELLANEOUS TASKS

TASK NUMBER	ACTIVITY	ACTIVITY TYPE	TARGET COMPLETION	DOCUMENTATION	DATE DOC/SUBM	LEAD PLANT SER/ INTERMED PLANT
C.0	SUPPORTING PROGRAM	Supp Prog Rpt	Completed	NEDO 21297	5/76 - 6/76	
		Supp Prog Rpt Rev.	Completed	NEDO 21297 - Rev. 1	4/78 - 4/78	
C.1	DFFR REVISIONS	Revision 1	Completed	NEDO/NEDE 21061-P Rev. 1	9/75 - 4/76	
		Revision 2	Completed	NEDO/NEDE 21061-P Rev. 2	9/76 - 9/76	
		Revision 3	Completed	NEDO/NEDE 21061-P Rev. 3	6/78 - 6/78	IP
C.3	NRC ROUND 1 QUESTIONS	DFFR Amendment 1	Completed	NEDO/NEDE 21061-P Amend. 1	12/76 - 2/77	LP SER*/IP
		Revision 3, Appendix A	3Q 78	NEDO/NEDE 21061-P Appendix A		LP SER*/IP
C.5	SRSS JUSTIFICATION	Interim Report	Completed	(NEDE 24010)	4/77 - 3/77	
		SRSS Report	Completed	NEDO/NEDE 24010-P	7/77 - 8/77	LP SER*/IP
		SRSS Exec. Report	Completed	Summary Report	4/78 - 5/78	LP SER*/IP
C.6	NRC ROUND 2 QUESTIONS	DFFR Amendment 2	Completed	NEDO/NEDE 21061-P Amend. 2	6/77 - 7/77	LP SER*/IP
		DFFR Amend 2, Suppl 1	Completed	NEDO/NEDE 21061-P Amend. 2 Suppl. 1	8/77 - 9/77	LP SER*/IP
		DFFR Amend 2, Suppl 2	Completed	NEDO/NEDE 21061-P Amend. 2 Suppl. 2	9/77 - 11/77	LP SER*/IP
		Revision 3, Appendix A	3Q 78	NEDO/NEDE 21061-P, Appendix A		LP SER*/IP
C.7	JUSTIFICATION OF "4T" BOUNDING LOADS	Chugging Loads Justification	Complete	NEDO/NEDE 23617-P	7/77 - 8/77	
			Complete	NEDO/NEDE 24013-P	6/77 - 8/77	
			Complete	NEDO/NEDE 24014-P	6/77 - 8/77	
			Complete	NEDO/NEDE 24015-P	6/77 - 8/77	LP SER
			Complete	NEDO/NEDE 24016-P	6/77 - 8/77	
			Complete	NEDO/NEDE 24017-P	6/77 - 8/77	
			Complete	NEDO/NEDE 23627-P	6/77 - 8/77	
C.8	S/RV AND CHUGGING FSI	Prestressed Concrete Reinforced Concrete Steel	Completed	NEDE 21936-P	7/78-7/78	LP SER/IP
C.9	MONITOR WORLD TESTS	Monitor Tests	End of Program	None		
C.13	LOAD COMBINATIONS & FUNCTIONAL CAPABILITY	Criteria Justification	4Q 78	Report		IP
C.14	NRC ROUND 3 QUESTIONS	Revision 3, Appendix A	3Q 78	NEDO/NEDE 21061-P Appendix A	(6/78)	LP SER*/IP

1. LOCA-Related Subscale Steam Tests

These tests consist of parametric and multivent steam tests for the evaluation of various effects on the steam chugging phenomena. The parametric tests are included to evaluate the effects of downcomer parameters and scaling variations on chugging. The multivent tests provide data to confirm the assumptions used in the multivent hydrodynamic model for calculating pool boundary loads and provide confirmation of the bounding chugging loads utilized in the Lead Plant Program. The multivent tests include tests at several scales and with a varying number of vents. The proposed multivent tests are described in the preliminary scaled multivent test program plan.[3]

2. Single Vent Chugging Models

Two analytical models of the chugging event for a single vent have been developed. These models are "first principles" models based on the application of laws of hydrodynamics, heat transfer, and thermodynamics. These models are referred to as the Heat Transfer Chugging Model and the Vent Pressure Chugging Model. These models are used to provide a qualitative description of the chugging event particularly with regard to the frequency content of the wall pressures, the chugging interval and the dependence of these variables on parameters such as drywell volume, vent diameter and length. This model will be used in conjunction with the multivent model to establish more realistic chugging loads on the Mark II containment boundary.

3. Multivent Hydrodynamic Model

This model, in parallel with the single vent model, is developed for the prediction of multiple vent loads on the containment boundary during chugging. The model utilizes potential flow theory and simple assumptions to permit super-position of pressure derived from existing single vent test data. This model will use data from the subscale multivent tests for model qualification.

4. Improved Chugging Load Definition

The Lead Plant chugging load specification for the containment boundary consists of a conservative pressure measurement observed at the walls of the 4T test facility. The chugging signal includes the ring-out of the test tank walls. Studies performed in the Lead Plant program indicate this is a significant conservatism. The objective of this task is to establish a more realistic chug definition by removing the ring-out of the test facility.

5. Dynamic Lateral Load on Main Vents

The lead plant vent lateral load specification consists of a bounding static equivalent load. The objective of this task is to develop a model which provides a dynamic forcing function for the vent lateral load. Test data from the 4T test facility is used in an analytical model developed using the Euler-Bernoulli equation which represents a continuous method for describing the dynamic vibratory response of a beam having distributed mass, elasticity and damping.

6. EPRI Single Cell Subscale Tests

The central tests for the establishment of Mark II pool swell loads are the single cell tests conducted in the 4T facility. The 4T tests are steam driven tests. The NRC required that air tests be conducted to assure conservative pool swell loads. We also required that 3D pool swell tests be conducted to investigate potential 3D pool swell related loading phenomena. Data from the subscale EPRI 3D air test were submitted to the NRC to satisfy our concerns. The EPRI single cell subscale tests should provide a mechanism by which subscale, air and 3D effects can be separated.

7. In-Plant Tests of the Cross-Quencher

The objective of this task is to measure air clearing loads imposed on the suppression pool and containment structures as a consequence of SRV discharge through a cross-quencher. These tests will be performed in the CAORSO plant which is the first BWR plant with a Mark II containment that is equipped with quenchers. The CAORSO plant is located in Italy. Tests will be conducted to obtain data bases regarding the following: pipe clearing transients, containment loads, quencher structural response, containment response and containment liner and downcomer vent response. Single and multiple SRV tests will be conducted.

8. Improved Cross Quencher Load Methodology

Preliminary results from the cross quencher testing program indicate that the methodology currently specified in the DFFR Revision 2 yields results that are unrealistically high. The objective of this task is to develop improvements in the methodology to justify a significant reduction in loads. Proposed improvements include the following air clearing SRV methodologies for the cross quencher: amplitude, pressure-time attenuation, pressure-distance attenuation and an alternate approach to the DFFR bubble dynamics model. A revision has also been proposed for the multiple-valves structural response methodology to remove conservatisms in the current methodology which assumes that all the quencher discharge bubbles are in phase and have the same frequency.

9. Loads on Submerged Structures

This task involves the development of theoretical models to predict both the velocity and acceleration components of postulated LOCA and SRV loads due to submerged jets and air clearing loads. It also includes tests to be conducted to verify these loads. A number of activities associated with this task are complete and serve as the basis for the associated LPP loads. Significant activities to be conducted in the remainder of the program include: the cross-quencher air bubble and water jet model, the steam condensation model, tests in the PSTF 1/3 scale test facility, tests in the 1/4 scale pool swell facility and a comprehensive evaluation of test data and theoretical models.

10. References for Appendix E

1. "Mark II Containment Dynamic Forcing Functions Information Report," General Electric Company and Sargent Lundy Engineers, NEDO-21061-P, Revision 2 class I, September 1976.

2. "Mark II Containment Supporting Program Report," General Electric Company, NEDO 21297-Revision 1, May 1976.

3. "Preliminary Scaled Multivent Test Program Plan," General Electric Company, NEDO-23697, December 1977.

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15. SUPPLEMENTARY NOTES

16. ABSTRACT (200 words or less)

The staff and our consultants, Brookhaven National Laboratory, have conducted an evaluation of the hydrodynamic loads associated with the design of the Mark II pressure suppression containment that were not explicitly considered in their original design. These loads may result from the actuation of safety relief valves or a postulated loss-of-coolant accident.

The report describes generic load methodologies acceptable to the staff for the evaluation of the lead Mark II plants. The report provides the staff's load acceptance criteria, the bases for our evaluations, and a summary description of supporting analytical and experimental programs. The emphasis on establishing loads acceptable for the lead plants has resulted in the assignment of bounding load specifications for selected loads, to assure that conservative loads are utilized in the evaluation of the lead plants.

17. KEY WORDS AND DOCUMENT ANALYSIS

17a. DESCRIPTORS

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