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**ANALYSIS OF THE BABCOCK & WILCOX
FULL-SIZE STEAM GENERATOR
FOR A
SODIUM-WATER REACTION**

AEC Contract No. AT(11-1)-1280, Subcontract No. 1

Liquid Metal Cooled Reactors Branch

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NOVEMBER 30, 1965

**ATOMIC POWER
DEVELOPMENT ASSOCIATES, INC.**

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ABSTRACT

An analysis of the effects of a sodium-water reaction in The Babcock & Wilcox Full-Size Steam Generator was conducted. A leak in a tube of the unit would cause a sodium-water reaction which would result in the rapid generation of quantities of reaction products, including hydrogen and high temperatures.

The comprehensive mathematical models and supporting experimental tests developed to analyze a sodium-water reaction are discussed and data are presented on the effect of (1) leak size, (2) paths for reaction relief, and (3) leak location on hydrogen pressure. The location and operating conditions for the reaction that would result in the most severe pressure transient from the complete severance of one tube is discussed and illustrated in detail with curves of the reaction data.

The phenomena relating to leak propagation, i. e. , mechanical damage, temperature softening of the tubes, and wastage, are discussed in terms of the mathematical models developed and the experimental tests conducted. The results from these analyses, coupled with estimates of leak detection, isolation, and dump times, are used to analyze the accidents which would result from initiating leaks of various sizes in the steam generator.

Recommendations are presented for (1) improving the method of analysis of a sodium-water reaction in the Full-Size Steam Generator and (2) changing the design to improve the ability of the unit to withstand such a reaction.

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1. PREFACE

1-1. SCOPE

The scope of the work, as specified in AEC Contract No. AT(11-1)-1280 Subcontract No. 1 with The Babcock & Wilcox Co. (B&W), is as follows:

- (a) APDA will conduct a literature search for information pertaining to sodium-water reactions. Following this literature search, APDA will utilize this information to develop relationships used in evaluating the capability of steam generators to withstand water leaks.
- (b) APDA will undertake a preliminary review of the B&W Full-Size Steam Generator based on previous experience and a limited amount of mathematical analysis. This review is to determine obvious problem areas from the standpoint of sodium-water reactions.
- (c) APDA will establish analytical procedures and determine areas where evaluations are necessary.
- (d) Also, APDA will scope, design, and conduct the tests necessary to give a reasonable degree of assurance that the assumptions to be used in the detailed analyses are conservative.
- (e) Concurrent with the test program (Item 4), APDA will carry out an extensive analysis of the Full-Size Steam Generator to ascertain, with the best information available, whether the integrity of the shell of this steam generator will be maintained during a sodium-water reaction. As part of this analysis, APDA will conduct a study to determine the amount and rate of water which might flow into the sodium, prior to release of the relief diaphragms, as a result of an initiating leak in a tube at a rate not to exceed the flow from one complete tube rupture. During the analysis, APDA will determine the forces, temperatures, and pressures and supply these to B&W who will conduct the structural analysis.
- (f) APDA will undertake a preliminary review of the B&W 30-Mw(t) Prototype Steam Generator based on previous experience and a limited amount of mathematical analysis. This review is to determine obvious problem areas from the standpoint of sodium-water reactions.

(g) Concurrent with the test program (Item 4), APDA will carry out an extensive analysis of the 30-Mw(t) Prototype Steam Generator to ascertain, with the best information available, whether the integrity of the shell of this steam generator will be maintained during a sodium-water reaction. As part of this analysis, APDA will conduct a study to determine the amount and rate of water which might flow into the sodium, prior to release of the relief diaphragm(s), as a result of an initiating leak in a tube at a rate not to exceed the flow from one complete tube rupture. During the analysis, APDA will determine the forces, temperatures, and pressures and supply these to B&W who will conduct the structural analysis.

(h) Once, prior to the completion of the work, APDA will up-date the information furnished in the light of any new sodium-water reaction data that has become available.

It should be emphasized that APDA was not to do any stress analyses or any work on rupture strengths. All work of a stress-analysis nature was specifically reserved by B&W to be done by B&W. APDA was to provide only the pressure-temperature-time history for various postulated tube rupture conditions. Therefore, for a complete story on sodium-water reactions and their effects, the reader should read:

"Literature Review of Sodium-Water Reactions," APDA-167, March 15, 1965.

"Preliminary Design Report Full-Size Steam Generator," B&W 67-2

"Addendum to Preliminary Design Report Full-Size Steam Generator," B&W 67-2(a)

The work covered by this report consisted of three phases directed toward the analysis of the B&W Full-Size Steam Generator. These phases were:

The analytical effort comprising the two mathematical models, the credible rupture propagation, and the analytical determination of temperatures and pressures.

Small leak sodium-water tests to assist in analysis of leak propagation. These were "quick and dirty" pot tests aimed at obtaining some data quickly.

Air-water tests to assist in evaluating one of the mathematical models -- the finite-difference model.

In addition to the work reported, a description of the B&W Full-Size Steam Generator is included to assist in understanding the report.

1-2. RELATIONSHIP TO OTHER PROJECTS

This study is conducted in direct support of the Sodium-Heated Steam Generator Development Contract awarded to The Babcock & Wilcox Company (Contract No. AT(11-1)-1280).

2. SUMMARY

An analysis of the effects of a sodium-water reaction in The Babcock & Wilcox Full-Size Steam Generator was conducted. The Full-Size Steam Generator, shown in Figure 4.1, is a once-through-type unit with sodium on the shell side, and water and steam in the helically coiled tube bundles. Two 20-inch rupture disk assemblies and one 4-inch relief valve are provided at the top of the unit for relief of the products of a sodium-water reaction.

A leak in a tube in the Full-Size Steam Generator would cause a sodium-water reaction which in turn would result in the rapid generation of large amounts of hydrogen and high temperatures. In order to analyze this accident, several mathematical models were developed which would predict the hydrogen bubble pressure. These models account for the inertial effects of the sodium above the hydrogen bubble, compression of the gas in the gas space, the static head of sodium above the leak, the frictional drag on the sodium flowing away from the leak, and the pressure behavior of the bubble. One model, designated as the piping model (BUG 2), was based on the assumption that the liquid was pushed above the gas bubble as a solid slug with no penetration of the gas into the liquid. A more refined finite-difference model (BUG 3) was also developed to predict the bubble pressure which took into consideration the three-dimensional growth of the bubble. The development of this model was supported by a program of air-water model experiments in which high-pressure gas was injected into a column of water. During the traverse of the bubble through the fluid column, pressure measurements and high-speed movies were made. The results of the piping model agreed well with those of the finite-difference model; therefore, the simpler piping model was used for most of the analyses in the Full-Size Steam Generator. All the assumptions made in the derivation and application of the model were conservative with the exception of two. These two exceptions were the assumptions of (1) a bubble temperature of 1800 F and (2) an instantaneous reaction between sodium and water. A bubble temperature of 1800 F, although an average of experimental data, is not necessarily conservative; however, the effect of a higher temperature on the bubble pressure is not appreciable, i. e., an increase of ~25% in bubble pressure is realized if a maximum adiabatic temperature of 3050 F is used. The assumption of an instantaneous reaction between the water and sodium does not agree with experimental data; however, further information on reaction kinetics is necessary before this can be refined.

In order to determine the location and operating condition for the reaction that would result in the most severe pressure transient, the piping mathematical model was used to evaluate the pressure effects of a reaction

from the complete severance of one tube at various locations in the steam generator. A leak in the boiler downcomer tubes halfway up the boiler downcomer annulus during flooded conditions resulted in the highest pressure (Case XXVd). The relief paths for this reaction were up and down the boiler downcomer annulus and up through the boiler and superheater tube bundles. No relief was provided up the center pipe since it was assumed, based on analyses by B&W, that the center pipe would collapse and possibly not be available for reaction relief. The reaction pressure, bubble volume, sodium velocity, and other reaction parameters versus time are presented for this case.

Experience with operating steam generators and sodium-water reaction experiments has shown that a leak in a sodium-heated steam generator can propagate a series of tube failures due to mechanical damage, temperature softening of the tubes, and wastage of the tube material from the effects of the initiating reaction. Thus a complete analysis of reactions in the Full-Size Steam Generator must include these phenomena. Sodium-Water Tube Thinning Tests were conducted to obtain information on wastage and temperature softening. Although the results from these and other experiments were contradictory and nonreproducible, wastage rates and the extent of damage from a leak were estimated from the limited data. Since almost no experimental data were available on temperature softening, several models of the reaction bubble were postulated in order to calculate the variables which affect this propagation mechanism. This information, coupled with estimates of leak detection, isolation, and dump times, was used to analyze the accidents which would result from initiating leaks of various size in the steam generator. The case of an initiating leak of 1 lb/sec in the film boiling section of the boiler bundle during flooded conditions resulted in the highest total leak rate of water into sodium. This case resulted in a maximum total leak rate of 44.4 lb/sec which would be equivalent to slightly less than the full severance of three tubes. A total of four tubes failed as a result of the initiating leak; two developed small holes as a result of localized wastage and two failed completely from fairly uniform wastage. The isolation and dump valves were completely positioned 21-1/2 seconds after the initial leak, dropping the water pressure to 135 psia; the unit was completely dumped at the end of 4-1/2 minutes. This analysis was based on many assumptions since experimental data applicable to this problem were sparse; therefore, it was difficult to assess how well these results represent what actually might occur in a steam generator.

Recommendations for improving the analyses and the design of the Full-Size Steam Generator for a reaction are presented, including the need for large scale sodium-water reaction tests and various structural changes that might be made to improve the steam generator from a reaction standpoint. Suggestions are presented with regard to special problems resulting from a reaction that should be explored in the stress analysis of the shell of the unit. This analysis is being performed by B&W based on the pressures

determined from the data presented in this report and is reported in BW 67-2 and its supplements.

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3. INTRODUCTION

The reaction between sodium and water results in the generation of large volumes of hydrogen, the production of highly corrosive reaction products, and the liberation of heat. Of these, the pressure transient caused by the rapid generation of hydrogen is potentially the most destructive effect. To determine the magnitude of the pressures exerted on the steam generator during a reaction, the analysis was conducted stepwise as follows:

- (a) Mathematical models were developed to predict the hydrodynamic behavior of the hydrogen bubble and the surrounding sodium.
- (b) Air-Water Model Tests were conducted to verify certain aspects of the mathematical models.
- (c) The mathematical models were then used to predict the operating conditions and reaction locations which would result in the greatest pressures. Assuming constant leak rates, the effects of leak size and paths chosen for reaction relief were determined.
- (d) The possible mechanisms of leak propagation from tube to tube were explored as follows:

Mechanical Damage - Information on the extent of damage based on the results from (c) were to be obtained from B&W.

Wastage - The rate, distribution, and extent of material loss from the corrosion and erosion effects of a leak were estimated from previous sodium-water reaction tests and the results of tests run in conjunction with this analysis.

Temperature Softening - Although this mode of failure has not been experienced in any tests to date, it was considered in the analysis. Data from previous sodium-water reaction tests and the current wastage tests were examined to shed light on the heat transfer coefficient and possible temperatures propagated during a reaction. Several models of the reaction were postulated which would enable one to calculate the reaction thermodynamics and heat transfer characteristics and to study their effect on the steam generator tubes.

- (e) Methods of detecting the reaction, and isolating and dumping the water side of the steam generator were established to enable a realistic analysis of a complete accident resulting from a leak.

- (f) Using (1) the mathematical models for the hydrodynamic effects from (a), (2) the reaction location and operating conditions of the maximum accident determined from (c), (3) the leak propagation data from (d), and (4) the reaction detection and termination methods from (e), analyses of the accidents resulting from initiating leaks of various sizes not exceeding the complete severance of one tube were conducted.
- (g) Based on these analyses, recommendations concerning the steam generator design and the analysis were made.

4. DESCRIPTION OF THE FULL-SIZE STEAM GENERATOR

A description of the Babcock & Wilcox Full-Size Steam Generator can be found in the preliminary design report for the unit, BW 67-2. The Full-Size Steam Generator, like the Prototype, is a once-through-type unit with the boiler and superheater sections in the same shell. The arrangement of the two sections is illustrated in Figure 4.1, which shows the sodium flowing over the helically coiled tubes of the superheater section and then passing over the boiler section, which also is composed of helically coiled tubes. The superheater and boiler regions are connected outside the unit by a manifold; a steam drum is not incorporated between the regions. Dimensions and operating information are presented in Table 4.1.

Several aspects of the unit are important from the standpoint of a sodium-water reaction analysis. Single-walled tubes are used to separate the high-pressure water and steam from the sodium. A leak in a tube, therefore, allows water or steam to flow directly into the sodium resulting in a reaction. Two 20-inch rupture disk assemblies and one 4-inch relief valve are provided at the top of the unit for relief of the products of a sodium-water reaction. A 3-inch gas annulus is maintained between the shell of the unit and a liner which fits inside the shell and extends from the sodium outlet at the bottom to one foot above the sodium level. A 30-inch-OD cylinder is in the center of the unit and is shown as the center pipe in Figure 4.1. As indicated in the figure, this pipe contains stagnant sodium.

Vertical, bar-type tube supports are used in both the boiler and superheater tube bundles. The bars are placed in a manner that divides the helical bundles into eight pie-shaped sections, allowing essentially no sodium flow circumferentially between the sections. The boiler downcomer and riser tubes and the superheater downcomer tubes are housed in annular-shaped regions separated by cylindrical shrouds. The tubes are distributed fairly uniformly around the circumference of these regions. These annuli contain horizontal, plate-type tube supports and convection baffles staggered along their length. Sodium forced through these regions by a sodium-water reaction, therefore, behaves much the same as shell-side flow through a segmental baffled heat exchanger with a correspondingly high pressure drop.

TABLE 4.1

PHYSICAL AND OPERATING DATA FOR THE FULL-SIZE STEAM GENERATORSIZE AND WEIGHT

Over-all Length	73 ft
Inside Diameter	13 ft
Weight, empty	1,064,391 lb
Weight, filled with sodium to operating level	1,300,000 lb

BOILER SECTION TUBES

Material	Croloy 2-1/4, T-22
Number	676
Transverse Spacing	1.5 in
Parallel Spacing	1.375 in
Size and Length, OD x wall thickness x length	
Downcomer Region	5/8 in x 0.165 in x 53 ft
Economizer and Nucleate Boiling Region	1 in x 0.120 in x 60 ft
Film Boiling Region	1 in x 0.145 in x 53 ft
Superheat Region	1 in x 0.165 in x 18.4 ft
Riser Region	1 in x 0.165 in x 32 ft

SUPERHEATER SECTION TUBES

Material	SA-213, TP-316SS
Number	1220
Transverse Spacing	1.25 in
Parallel Spacing	1.25 in
Outside Diameter	7/8 in
Wall Thickness	0.120 in
Length, effective	40.6 ft

GAS SPACE VOLUME2250 ft³WATER INVENTORY, Including Headers and Interconnecting Piping

Volume	602 ft ³
Weight, 350 F Isothermal	33,800 lb
Weight, 100% load	9322 lb

OPERATING CONDITIONS, 100% Load

Sodium Flow	15.59 x 10 ⁶ lb/hr
Water and Steam Flow	2.403 x 10 ⁶ lb/hr

BOILER SECTION

Sodium Inlet Temperature	1013 F
Sodium Outlet Temperature	644 F
Feedwater Temperature	530 F
Steam Outlet Temperature	750 F
Feedwater Pressure	2665 psia
Steam Outlet Pressure	2565 psia
Over-all Heat Transfer Coefficient, based on OD	
Economizer Region	589 Btu/hr ft ² °F
Nucleate Boiling Region	709 Btu/hr ft ² °F
Film Boiling Region	464 Btu/hr ft ² °F
Superheat Region	473 Btu/hr ft ² °F

SUPERHEATER SECTION

Sodium Inlet Temperature	1140 F
Sodium Outlet Temperature	1013 F
Steam Inlet Temperature	750 F
Steam Outlet Temperature	1050 F
Steam Inlet Pressure	2565 psia
Steam Outlet Pressure	2465 psia
Over-all Heat Transfer Coefficient, based on OD	361.5 Btu/hr ft ² °F

FIG. 4.1 B&W FULL-SIZE STEAM GENERATOR

5. ANALYSIS OF REACTION PRESSURES

The most readily detectable and potentially destructive effect of a sodium-water reaction is the pressure transient caused by the rapid generation of hydrogen when a leak occurs. In order to predict this pressure transient as a function of the leak rate and location in the steam generator, several mathematical models were derived based on various assumptions concerning the behavior of the sodium and hydrogen during a reaction. The shock effects from a sodium-water reaction were not analyzed because data necessary for a quantitative evaluation were not available.

The first efforts to analyze a sodium-water reaction were made under the assumption that the hydrogen bubble immediately filled the entire cross section of the reaction region, and that the sodium above the reaction moved as a piston that retained its original cylindrical shape. The various flow paths and frictional losses were combined into a single path (one-path model). A second path was added so that the reaction products could take two paths from either side of the reaction to the gas space (two-path model). Both of these models led to relatively straightforward differential equations that were solved numerically using the generalized differential equation program called SWAG, Sodium-Water reaction Analysis in steam Generators.¹ The analysis of the B&W Prototype Steam Generator was done primarily with the SWAG Program.

In analyzing the Full-Size Steam Generator, two alternatives were explored which resulted in a more exact mathematical model. The first alternative was that the sodium did not act as a piston and the hydrogen bubble could grow in any direction. The sodium was divided into a series of small nodes and finite-difference techniques were used. This method was initially programmed in the BUG 1, Bubble Growth 1, computer code in order to compare the calculated predictions with the experimental results from air-water model tests.

For the second alternative, mockups of the various flow paths, e. g., boiler and superheater regions, downcomer annulus, center pipe, etc., were made which accounted for different frictional losses in each section. This analysis was made using a general transient piping code, BUG 2, still assuming one-dimensional flow.

The assumption of one-dimensional flow is valid if the reaction is in the boiler or superheater tube bundle since the baffles prevent radial or tangential spreading of the reaction. However, in a reaction in the downcomer region the hydrogen bubble will grow both axially and tangentially. The analysis for this case was done by combining the finite-difference

and general piping models (BUG 1 and BUG 2). The finite-difference models were used in the annulus, and the piping models for the remainder of the steam generator; this was programmed in the BUG 3 computer code.

A detailed description of these models and a brief description of the computer codes used for their solution is presented in Appendix A.

5-1. DESCRIPTION OF THE MATHEMATICAL MODELS

5-1.1. BUG 1

Earlier analyses of the effects of a sodium-water reaction in a steam generator assumed that the hydrogen bubble filled the entire cross section and drove the sodium before it as a piston; flow was one dimensional.

In order to obtain a more accurate representation of the bubble growth process in which the bubble would be permitted to grow in all directions from a point source, a three-dimensional mathematical model of the flow process was developed using the same basic assumptions. The many assumptions made in the derivation and application of the mathematical model are listed and discussed below.

- (a) The leak rate of water and steam was instantaneous and constant and not a function of the reaction back pressure.
- (b) The reaction was instantaneous according to the equation:
$$2 \text{ Na} + \text{H}_2\text{O} \rightarrow \text{Na}_2\text{O} + \text{H}_2$$
- (c) The hydrogen was generated at a temperature of 1800 F.
- (d) The hydrogen bubble expanded isothermally.
- (e) A rupture disk bursting pressure and relief valve setting of 60 psig were used, and it was assumed that the reaction relief system beyond the rupture disk was capable of receiving the reaction products resulting from the rupture of the number of tubes being considered with a pressure drop less than 60 psi.

Assumption (a)

Leak rate was instantaneous - This assumption is conservative since it leads to an infinite bubble pressure at time zero. In the actual case of a tube rupture it will take time for the leak to build up to the maximum rate.

Leak rate was constant and not a function of the reaction back pressure - This assumption is also conservative since leak rate is dependent on the differential pressure between the water-and steam-side headers and the hydrogen bubble. As the bubble pressure rises, this differential should become much less than the initial ΔP of 2590 psi from which the leak rate was calculated. Also, for the cases considered where all tubes rupture, it is quite possible that the pressure in the header will not be maintained since the total leak rate of water from all the tubes greatly exceeds the normal water flow through the unit. Thus, the leak rate could be reduced by the inability of the feedwater supply system to supply the greater water rate at the high pressure.

Assumption (b)

Reaction was instantaneous - This assumption is not representative of the actual case of a reaction since sodium-water reaction experiments are characterized by sporadic pressure spikes soon after water injection indicating that a significant amount of water may be injected into the sodium before reacting. This assumption, therefore, is probably not conservative. Assumption (a) may make up for this nonconservatism since the results are pressure decays from infinity which somewhat simulate the high initial pressure peaks experienced in the experiments. Also, since the times involved for these peaks to occur are in terms of milliseconds, energy apparently becomes the controlling parameter for vessel strength considerations. The combination of Assumptions (a) and (b) yields the maximum possible integrated mechanical energy release at any time from a reaction consistent with Assumption (c). It does not necessarily, however, give the maximum possible rate of energy release since this is a function of the reaction kinetics.

Reaction was according to the equation $2 \text{ Na} + \text{H}_2\text{O} \rightarrow \text{Na}_2\text{O} + \text{H}_2$ - This equation is assumed since it represents the maximum hydrogen generation rate from a sodium-water reaction.

Assumption (c)

The hydrogen was generated at a temperature of 1800 F - This temperature was chosen as the average expected in the bubble from experimental data for sodium-water reactions.² Since no experiments have been conducted in large quantities of sodium and/or at significant depths, the effect of these variables on temperature has never been determined. It is possible, however, that the temperatures may be greater due to the higher saturation temperature of sodium at the higher pressures developed. If the theoretical maximum hydrogen temperature were used, the volumetric hydrogen generation rate would be increased by a factor of 1.55 and the bubble pressure increased by a factor of approximately 1.25.

Assumption (d)

Hydrogen bubble expanded isothermally - This gives the maximum pressure consistent with Assumption (c).

Assumption (e)

Relief system parameters - These values were assumed because a design of the relief system for this steam generator was not available.

Additional assumptions which are related to the finite-difference aspects of the model are:

Assumption (f)

The hydrogen and sodium phases were assumed to remain discrete (no mixing).

Assumption (g)

The static pressure of the gas was assumed to act on the gas-liquid interface.

Assumption (h)

The liquid was assumed to be noncompressible.

Assumption (i)

The liquid was assumed to be nonviscous. Frictional energy losses were introduced as a later modification of the derivation (Refer to BUG 3).

Assumption (j)

Body (gravitational) forces were neglected.

A series of tests, described in Appendix C, were run in which helium or air was injected into water, in order to determine how well the mathematical model could predict the bubble growth. The preliminary experimental model consisted of a cylinder partially filled with water into which gas was injected through a pipe at the bottom by means of a quick-opening solenoid valve. The experimentally determined static pressure trace as a function of time, measured near the bottom of the cylinder, was used as the input to the computer code. The bubble pressure was adjusted to make the calculated pressure at the wall match the experimental value.

A comparison between the experimental data and the computer predictions (Figures C.3, C.6, and C.10) showed that the mathematical model led to conservative results, e. g., for a given gas generation rate, the model predicted higher pressures and less penetration of the liquid by the bubble than was observed experimentally. The possible reasons for the disagreement are discussed in Appendix C.

The advanced experimental model was similar to the preliminary model except that it contained a series of disk-and doughnut-type baffles. The experimental data obtained from these tests are shown in Appendix C, Figures C.12 through C.15.

5-1.2. BUG 2

The BUG 2 model, or piping model, assumed one-dimensional flow and was essentially the same as the transient solution to a piping network. Since the flow was one dimensional, the hydrogen bubble forced the sodium away from the reaction as a solid slug or piston. The program was quite flexible and provided for various arrangements of relief channels which coincided with the relief paths for reactions at different locations in the unit. The frictional and inertial effects of the sodium as it moved through each channel were determined. The derivation of the equations and the method of solution are given in Appendix A. Assumptions (a), (b), (c), (d), and (e) listed for the BUG 1 model also apply to the BUG 2 model. In addition, the following assumptions were made.

Assumption (f)

The hydrogen bubble instantaneously assumed the shape of the cross section of the region or regions being studied and forced the liquid above it to rise as a solid slug - This assumption is not realistic, especially in regions with a large horizontal dimension such as the boiler downcomer annulus. Initially, the bubble probably approximates a sphere rather than a thin wafer. Also, penetration of the bubble through the liquid is possible; however, this cannot be predicted with the piston model.

A finite-difference model was developed to account for these weaknesses. Initial calculations using this model applied to the boiler downcomer region of the Full-Size Steam Generator indicated that the bubble pressure was not significantly different from the piston model during the early stages of bubble development. Also, as time progressed, bubble penetration through the liquid was predicted thus lowering the bubble pressure.

Assumption (g)

The full pressure drop of the sodium as it moved through each channel was distributed uniformly along the length of the sections considered - The effect of this assumption on the bubble pressure is negligible.

5-1.3. BUG 3

This model combined the features of the finite-difference model, BUG 1, with those of the piping model, BUG 2, described above. For a leak in one of the annular regions, three-dimensional bubble growth was permitted with flow along all of the available reaction relief paths in the steam generator.

Since the center pipe and segments of the tube bundles had large vertical dimensions compared to their horizontal dimensions, the one-dimensional (BUG 2) model was used for these regions. The annular regions (boiler downcomer and riser and superheater downcomer annuli), were divided into eight circumferential segments and eight 71-inch-high vertical regions. A more detailed description of the model and its method of solution are provided in Appendix A.

5-2. APPLICATION OF THE PIPING AND COMBINED MODELS TO THE FULL-SIZE STEAM GENERATOR FOR CONSTANT LEAK RATES

In order to make an initial determination of the effect of leak location, leak size, and steam generator operating conditions on the hydrogen bubble pressure for a reaction, the effects of leak propagation were ignored. Thus many cases were run using the piping and combined mathematical models for water leak rates which were constant with time.

5-2.1. STEAM GENERATOR OPERATING CONDITIONS

The various operating modes of the unit were studied to determine which would result in the most severe reaction. The conditions prior to power operation of the unit were found to result in the maximum water leak rate for a tube rupture at any location in the boiler section. At this time, the boiler section was flooded with water at 350 F and pressurized to 2625 psig. The total water leak rate from both ends of a completely severed boiler tube as a function of location of the break is presented in Figure 5.1. It was found that for the flooded condition, the water stayed as a liquid throughout the length of the tube and did not flash into steam until it left the broken end of the tube.

Since operation of the steam generator at load could result in the most severe accident if leak propagation were considered, leak rates

for a one-tube rupture in the boiler section at full load were also determined. The full-load leak rate as a function of the location of the break is shown in Figure 5.2. The leak rate for flooded conditions was approximately 2-1/2 times the leak rate for full-load operation. The leak rate calculation for full load was based on the method presented in Reference 3 which accounted for the change in phase of the water as it flowed from the pipe.

As outlined in BW 67-2, the superheater section of the Full-Size Steam Generator will be empty during start up and will contain superheated steam during normal operation. It was assumed for this analysis, therefore, that the superheater section would never contain pressurized liquid water with sodium on the shell side. The total water leak rate from both ends of a completely severed tube during full-load operation is presented in Figure 5.3 for the superheater section.

5-2.2. SIZE OF ACCIDENT

The effects of the complete, simultaneous rupture of from one to all the tubes in the region under consideration were determined. Although the scope of the contract called for the analysis of an accident initiated by a leak equal to the complete rupture of one tube or less, larger, simultaneous leaks were considered to illustrate the effect of leak size on the reaction pressure. The simultaneous rupture of all 676 tubes in the boiler section or 1220 tubes in the superheater section was also included as an indication of the maximum possible accident that could occur in the unit even though such an occurrence is improbable and has no real significance. Leaks of various magnitudes between these two extremes were also considered to determine the effect of leak size on the reaction pressure. The use of the mathematical model for leak rates below about 7 lb/sec is questionable since bubbling rather than slug flow may begin to be the mechanism by which the generated hydrogen is dissipated. The rate of 7 lb/sec was determined from tests run by The Detroit Edison Co. (DECo) using the ratio of the cross-sectional areas according to the iso-velocity scaling laws. The minimum leak rate for which the slug or piston flow exists is a function of the size and location of the reaction.

5-2.3. LOCATIONS CHOSEN FOR ANALYSIS

The six locations chosen for analysis are shown in Figure 5.4. Reactions in these areas were expected to yield greater bubble pressures than reactions in other regions of the steam generator. Initially, the hydrogen bubble pressure was primarily dependent upon the inertial effects of accelerating the sodium between the reaction zone and gas space. The magnitude of the pressure, then, was dependent on the area and length of the channels available for relief. Locations (A) and (E) were near the bottom of the boiler and superheater regions, respectively; therefore, the channel lengths should be greatest for these rupture positions. Positions

(A), (B), (E), and (F) were in regions of restricted cross section; therefore, higher pressures were needed to accelerate the sodium in these areas.

After the sodium had been accelerated to significant velocities, frictional effects also exerted a significant effect on the bubble pressure. Regions with small cross-sectional areas and impediments to flow, such as the downcomer and riser annuli which contained tube supports and convection baffles, resulted in high bubble pressures when frictional effects became controlling. Locations (A), (B), (E), and (F) were in these high pressure drop regions.

Since most of the tubing in the boiler section was in the tube bundle, reactions at two locations, (C) and (D), were considered. Also, it was expected that leak propagation would be most severe in this area because of the high temperatures and low internal heat transfer coefficients present in some areas of the bundle. Reactions in these areas were assumed to be confined by the vertical, bar-type tube supports to a vertical segment one-eighth the total area of the tube bundle.

The paths chosen for relief of the reaction were those which offered the least resistance to the flow of sodium away from the reaction zone. All the cases run for the Full-Size Steam Generator using constant leak rates are summarized in Table 5.1 and include the location of the rupture, the paths chosen for reaction relief, the number of tubes ruptured, and the water leak rate.

5-3. RESULTS OF THE MATHEMATICAL MODELS FOR CONSTANT LEAK RATES

The results of the constant leak rate cases described in Table 5.1 are presented in this section. From these results, the effects of leak size, paths chosen for relief, and leak locations were determined. In addition, the reaction location that resulted in maximum bubble pressures during both flooded and full-load operation was ascertained. The results of the two mathematical models used were compared.

5-3.1. LEAK SIZE

The effect of various leak sizes on the hydrogen bubble pressure for leaks halfway up the boiler tube bundle (Location (C)) and near the bottom of the superheater downcomer annulus (Location (E)) is illustrated in Figures 5.5 and 5.6.

The smallest time interval shown on the curves, 0.1 millisecond, was chosen only as a convenient point for starting the graph. The formal solution of the mathematical model predicted an infinite bubble pressure at time zero. It can be seen from the curves in both figures that,

at any particular time, the ratio of the bubble pressures at two different leak rates is approximately equal to the square root of the ratio of the leak rates. The curves also indicate that the bubbles originating from higher leak rates penetrate the gas space more rapidly. The time of penetration can be approximated for different leak rates by the following:

$$\left(\frac{T_1}{T_0} \right)_{\text{penetration}} = \left(\frac{\text{Leak Rate}_0}{\text{Leak Rate}_1} \right)^{1/2}$$

5-3.2. PATHS FOR REACTION RELIEF

The accuracy of the pressure analysis for reactions in a steam generator is no better than the techniques used in determining the paths through which the sodium will be forced by the reaction products. The choice of paths for a reaction at any location markedly affects the bubble pressure as is illustrated in Figures 5.7 and 5.8.

Figure 5.7 shows the effect of three different choices for a single-tube rupture halfway up the boiler bundle (Location ©). The curve for Case XXI represents the bubble pressure for the case where the relief paths were chosen as (1) up through one segment of the boiler bundle and then through the superheater bundle and (2) down through one segment of the boiler bundle and up the center pipe. These paths are shown in Table 5.1. The path down through one segment of the boiler bundle, up through the remaining seven segments, and then through the superheater bundle was not considered, even though its resistance was low, because of model limitations.

Since the possibility arose of the center pipe being pressurized with gas to within a short distance of its base to reduce the sodium inventory and improve the reaction relief, a case was run using the same paths as in Case XXI except that only one foot of sodium was present in the center pipe. Thus, the inertial and frictional resistance to sodium flow up the center pipe was small and the curve for this case, XXId, was significantly lower than that for Case XXI.

The Babcock & Wilcox Co. has concluded from analyzing the pressures predicted by these analyses that the center pipe may collapse locally from reactions in the boiler section and thus not be available as a path for relief of the reaction. Case XXVIII was run to determine the effect on the bubble pressure of a blocked center pipe. The path up through the other seven segments of the boiler tube bundle was added for this case. Nevertheless, the absence of the center pipe for relief of the reaction increased the bubble pressure significantly as illustrated in Figure 5.7.

Figure 5.8 presents a similar comparison for leaks 3-1/2 feet above the bottom of the boiler downcomer annulus. The curve for Case XXII represents the bubble pressure where reaction relief was up through the center pipe and the boiler downcomer annulus as shown in Table 5.1. Again, the path up through the eight sectors of the boiler and superheater bundles was not considered because of limitations in the model. When B&W concluded that the center pipe might fail, a case was run substituting the path up through the tube bundles for the path up the center pipe. The bubble pressure for this case, XXIX, was significantly lower than the case where the center pipe was used for relief. This indicated that the path up through the center pipe was not as effective for relieving the pressure from a reaction in the boiler downcomer annulus as the path through the boiler and superheater tube bundles. Ignoring the path through the tube bundles resulted in bubble pressures which were much too high. The cases which included relief through the tube bundles, based on the assumption that the center pipe would collapse, were considered to be the most valid for reactions in these areas.

5-3.3. COMPARISON OF THE RESULTS FOR THE BUG 2 AND BUG 3 MODELS

Four runs were made with the combined piping and finite-difference model (BUG 3). Two of these runs were made using conditions identical to those used in cases run with the piping model (BUG 2). The results of these two runs are compared in Figure 5.9. The initial oscillations in the BUG 3 runs were due to inexact choices of initial conditions, and may be ignored. The curves for the BUG 3 cases were quite close to the curves for the BUG 2 cases during the first 0.3 to 0.5 second. During this time, the bubble pressure predicted from the BUG 3 model was approximately 15% lower than that from the BUG 2 model. This indicated that using the assumption in the BUG 2 model that the bubble immediately took the shape of the region in which it was located did not result in a significant error for reactions in the boiler downcomer annulus.

The development of the bubble in the boiler downcomer annulus as predicted by the BUG 3 model is also of interest. The bubble reached the bottom of the annulus in 0.4 to 0.45 second, at which time the bubble had progressed only 45% of the way around the circumference of the annulus. The top of the bubble left the top of the annulus at a reaction time of about 0.77 second at which time the bubble was only 52% of the way around the circumference of the annulus. This indicated that the bubble did not occupy the full cross section of the annulus during a reaction.

As expected, the BUG 3 cases predicted penetration of the bubble into the gas space significantly sooner than the BUG 2 model. For the one-tube rupture, the BUG 3 model predicted penetration in approximately one-quarter of the time predicted by BUG 2 model.

Since (1) the BUG 3 results were closely approximated by the BUG 2 results and (2) the BUG 3 model was quite expensive to run on the computer, only four runs were made using this model.

5-3.4. LEAK LOCATION

In order to determine the location in the steam generator that yielded the maximum reaction pressures, cases involving a single-tube rupture of one tube at various locations in the unit were compared. One case, representing the most valid choice of paths for reaction relief, was chosen for each location. The pressures obtained from single-tube ruptures under flooded conditions are presented in Figure 5.10 and those obtained from single-tube ruptures for full-load operation are given in Figure 5.11.

A reaction halfway up the boiler downcomer annulus (Location B) resulted in the greatest bubble pressure for a single-tube rupture during both flooded and full-load conditions. The two cases at this location, XXVd and XXVe, were chosen as maximum for reactions at flooded and full-load conditions, respectively. Figure 5.10 shows that the bubble pressure for reactions at all the locations did not deviate from the maximum case by more than approximately 10% during the first 10 milliseconds. Reactions halfway up the boiler tube bundle (Location C) were quite close to Cases XXVd and XXVe during the first 15 milliseconds, dropping off significantly after the initial time period.

The curves did not change appreciably relative to each other from flooded to full-load conditions as illustrated in Figure 5.11. A reaction in the superheater section yielded the lowest bubble pressure for the full-load cases since reactions in this region were closest to the surface.

5-3.5. MAXIMUM PRESSURE CASE

Location B, halfway up the boiler downcomer annulus, was chosen as yielding the highest bubble pressure for a given leak size. The relief for a reaction at this location was up the boiler downcomer annulus, and down the boiler downcomer annulus and then up through the boiler and superheater bundles. No relief up the center pipe was provided since it was assumed that the center pipe would collapse blocking flow. The bubble pressure and bubble volume for a single-tube rupture at B during flooded conditions (Case XXVd) is presented in Figure 5.12. Since this case was selected as representing the maximum bubble pressure for a single-tube rupture in the steam generator, additional information is presented in Figures 5.13 through 5.16 which detail the progress of the hydrogen bubble through the steam generator.

A schematic of the channels selected for relief of a reaction at (B) is shown in Figure 5.12. The four channels shown in Figure 5.12 are detailed in Figures 5.13 through 5.16 where the fraction of sodium in the channel, the sodium volume, the sodium velocity, the frictional pressure drop, and the total pressure at the inlet and exit of the channel are shown. As can be seen from the figures, Channel a emptied before Channels b, c, and d could empty.

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3. Fauske, H. K., "Contribution to the Theory of Two-Phase, One-Component Critical Flow," ANL-6633, October 1962.
4. "Reply to Division of Reactor Licensing Request for Additional Information Dated May 27, 1964," Power Reactor Development Company, Enrico Fermi Atomic Power Plant, Docket 50-16, Addendum No. 2, Attachment No. 1, September 8, 1964.

TABLE 5.1 DESCRIPTION OF PRESSURE ANALYSES IN THE FULL-SIZE STEAM GENERATOR

LOCATION (A)
REACTIONS 3-1/2 FEET ABOVE THE BOTTOM
OF THE BOILER DOWNCOMER ANNULUS

Case	No. of Tubes Ruptured	Water Leak Rate, lb/sec
XXII	1	18.5
XXIIa	10	185
XXIIb	100	1850
XXIIc	676	12,506

XXIX	1	18.5 ^a
XXIXa*	1	7.6 ^a

^a Center pipe collapses blocking flow

LOCATION (B)
REACTIONS HALFWAY UP THE
BOILER DOWNCOMER ANNULUS

Case	No. of Tubes Ruptured	Water Leak Rate, lb/sec
XXV	1	19.5
XXVa	5	97.5
XXVc	676	13,182

XXVd	1	19.5 ^a
XXVe*	1	8.4 ^a

^a Center pipe collapses blocking flow

BUG3, Case I**	1	19.5
BUG3, Case II	5	97.5
BUG3, Case III	1	19.5 ^a

^a Center pipe empty to within two feet of bottom

LOCATION (C)
REACTION HALFWAY UP
THE BOILER TUBE BUNDLE

Case	No. of Tubes Ruptured	Water Leak Rate, lb/sec
XXI	1	18.2
XXIa	10	182
XXIb	100	1820
XXIc	676	12,303.2
XXId	1	18.2 ^a
XXIe*	676	4732

^a Center pipe empty to within two feet of bottom

XXVIII	1	18.2 ^a
XXVIIIa*	1	6.75 ^a

^a Center pipe collapses blocking flow

LOCATION (D)
REACTIONS NEAR THE TOP OF THE
BOILER TUBE BUNDLE

Case	No. of Tubes Ruptured	Water Leak Rate, lb/sec
XXVI	1	24
XXVIa	10	240
XXVIc	676	16,224

XXVId	1	24 ^a
XXVIe*	1	7 ^a

^a Center pipe collapses blocking flow

LOCATION (E)
REACTIONS NEAR THE BOTTOM OF THE
SUPERHEATER DOWNCOMER ANNULUS

Case	No. of Tubes Ruptured	Water Leak Rate, lb/sec
XXXVIII*	1	7.6
XXXVIIIa*	10	76
XXXVIIIb*	100	760
XXXVIIIc*	1220	9272

LOCATION (F)
REACTION 6 FEET ABOVE THE BOTTOM
OF THE BOILER DOWNCOMER ANNULUS

Case	No. of Tubes Ruptured	Water Leak Rate, lb/sec
BUG3, Case IV	1	18.5

*Full-load conditions, all other cases are for flooded conditions.

** BUG 3 is the designation for the finite-difference mathematical model; all other cases are the BUG 2, or piping model.

FIG. 5.1 WATER LEAK RATE FROM BOTH ENDS OF A SEVERED TUBE IN THE BOILER SECTION - FLOODED CONDITIONS

FIG. 5.2 WATER LEAK RATE FROM BOTH ENDS OF A SEVERED TUBE IN THE BOILER SECTION - FULL-LOAD CONDITIONS

FIG. 5.3 WATER LEAK RATE FROM BOTH ENDS OF A SEVERED TUBE IN THE SUPERHEATER SECTION - FULL-LOAD CONDITIONS

FIG. 5.4 LOCATION OF SODIUM-WATER REACTIONS IN THE B&W FULL-SIZE STEAM GENERATOR

FIG. 5.5 EFFECT OF LEAK SIZE ON HYDROGEN BUBBLE PRESSURE FOR REACTIONS HALFWAY UP THE BOILER TUBE BUNDLE

FIG. 5.6 EFFECT OF LEAK SIZE ON HYDROGEN BUBBLE PRESSURE FOR REACTIONS NEAR THE BOTTOM OF THE SUPERHEATER DOWNCOMER ANNULUS

FIG. 5.7 EFFECT OF PATHS FOR REACTION RELIEF ON HYDROGEN BUBBLE PRESSURE FOR A SINGLE-TUBE SEVERANCE HALFWAY UP THE BOILER TUBE BUNDLE

FIG. 5.8 EFFECT OF PATHS FOR REACTION RELIEF ON HYDROGEN BUBBLE PRESSURE FOR A SINGLE-TUBE SEVERANCE 3.5 FEET ABOVE THE BOTTOM OF THE BOILER DOWNCOMER ANNULUS

FIG. 5.9 COMPARISON OF THE RESULTS FROM THE BUG 3 MODEL WITH THOSE FROM BUG 2 MODEL

FIG. 5.10 EFFECT OF LEAK LOCATION ON HYDROGEN BUBBLE PRESSURE FOR A SINGLE-TUBE SEVERANCE - FLOODED CONDITIONS

FIG. 5.11 EFFECT OF LEAK LOCATION ON HYDROGEN BUBBLE PRESSURE FOR A SINGLE-TUBE SEVERANCE - FULL-LOAD CONDITIONS

FIG. 5.12 BUBBLE PRESSURE AND VOLUME FOR A SINGLE-TUBE SEVERANCE
HALFWAY UP THE BOILER DOWNCOMER ANNULUS - CASE XXVd

FIG. 5.13 REACTION PARAMETERS FOR CHANNEL α - CASE XXVd

FIG. 5.14 REACTION PARAMETERS FOR CHANNEL b - CASE XXVd

FIG. 5.15 REACTION PARAMETERS FOR CHANNEL c - CASE XXVd

FIG. 5.16 REACTION PARAMETERS FOR CHANNEL d - CASE XXVd

6. ANALYSIS OF LEAK PROPAGATION

In a steam generator the size of the B&W full-size unit, it is neither practical nor desirable to assume, for purposes of analysis, that all the tubes fail simultaneously. To design for this assumption would be expensive and difficult, if not impossible. For this reason, the propagation of leaks to nearby tubes from an initiating source or leak due to some mechanism of the reaction must be considered. For the purpose of this analysis, it was assumed that the initiating leak was no greater than the full rupture of one tube in the unit.

6-1. MECHANISMS OF LEAK PROPAGATION

The three mechanisms that could cause leak propagation are mechanical damage, temperature softening, and wastage.

6-1.1. MECHANICAL DAMAGE

With the large pressures and forces developed in the steam generator during a reaction, there existed the possibility of propagating a leak through mechanical damage to tubes by the movement or failure of various tube supports and internals in the unit. Reaction pressure data was submitted to B&W for evaluation to determine whether additional failures would occur from mechanical damage. No further studies were made in this area since B&W, as of this writing, had not determined whether any tubes might fail due to this mechanism.

6-1.2. TEMPERATURE SOFTENING

A large amount of heat is generated when water reacts with sodium. If this heat were confined to the reaction products, adiabatic temperatures of ~3 000 F would be attained. Thus it may be possible to heat adjacent tubes in the reaction zone sufficiently to cause the tubes to burst from internal pressure. No tube failure have occurred to date as a result of temperature softening; however, some overheating of tubes has been experienced.

(a) BACKGROUND

(1) Fermi Experience¹ - Metallographic examination of the Croloy tubing in the vicinity of the reaction which occurred in the No. 1 steam generator of the Enrico Fermi Atomic Power Plant showed overheating of many tubes, some to temperatures higher than 1650 F for several minutes.

(2) APDA Na-H₂O Tests^{2,3} - Four sodium-water reaction tests were conducted in which the water leak rate was about 16 lb/sec for a duration of 4-1/2 to 6 seconds. The leakage was from a center bayonet tube into a seven-tube heat exchanger. Most of the sodium was blown out of the unit and maximum temperatures of 1950 F were measured in the reaction zone with average temperatures that ranged between 1600 and 1800 F. Examination of the tubes from the bundle showed that a small section of one tube, (~1/3 the thickness covering an area of ~1/3 the circumference) was heated above 1500 F.

(3) APDA NaK-H₂O Tests^{4,5} - In tests where a quick-opening valve was used between a water and NaK loop, temperatures in excess of 2500 F were measured during several of the tests. Pressures during all the tests were quite high (~1500 psig). In additional tests, where water was injected into a seven-tube, bayonet-type heat exchanger, maximum temperatures of 1700 F were measured with average temperatures of about 1400 F.

(4) MSA NaK-H₂O Tests⁶ - In 13 tests, 1-1/2 to 3 gpm of water was injected into a NaK loop for durations of approximately one minute. Temperatures as high as 1670 F were measured. The pressure in these tests averaged about 230 psig with peaks greater than 3000 psig.

(5) Detroit Edison Sodium-Water Tests^{7,8} - Water was injected at a bank of tubes in a pot of sodium at rates up to 1-1/4 lb/sec. It was noted that during many of the tests, the tubes were cooled by the water or steam impinging on their surface; however, a maximum temperature of 1420 F was measured during one test.

(b) SODIUM-WATER TUBE THINNING TESTS

Additional information on the temperature softening problem was obtained from the nine sodium-water tube thinning tests. Thermocouples connected to high-speed recording instruments were installed in the apparatus described in Appendix D and provisions were made to pressurize two tubes to determine the tube rupture time from both thinning and temperature softening. For a detailed description of the Sodium-Water Tube Thinning Tests and their results, refer to Appendix D.

As indicated from the results described in Appendix D, the maximum reaction zone temperature varied from 413 F during Test 7 to 2014 F during Test 4. The three tests that resulted in the highest reaction zone temperatures were those in which the highest water leak rate was used. During these tests, in which the water was injected at a rate of 1 lb/sec for 4 seconds, much of the sodium in the vessel was blown into the relief system indicating the presence of transient high pressures. No correlation for temperature could be found for the other two leak rates used.

During most of the tests, large fluctuations in the reaction zone temperature were recorded. During Test 9 a rise in temperature of 1300 F in less than 3/4 of a second was noted and fluctuations of 400 F were common in many of the other tests. During two of the tests, the reaction zone temperature dropped to 212 F for approximately a second at some time during the reaction indicating cooling by unreacted steam.

The results from the thermocouple attached to the inside surface of a tube adjacent to the target tube were inconclusive. During three of the tests in which this thermocouple was included, the tube temperature dropped before starting to rise indicating initial cooling by the water being injected. A temperature rise of 26.5 F/sec was recorded during Test 3. Using this rate and the average reaction zone temperature during the rise, transient heat transfer calculations were made to determine the heat transfer coefficient on the outside of the tube: the calculated value was less than 200 Btu/hr ft² °F. Although the reaction zone thermocouple was installed where it would indicate the temperature near the outside of the thermocouple-containing tube, the temperature data from both thermocouples did not correspond during many of the tests. Due to this discrepancy, this method was not used to determine the outside heat transfer coefficient.

All the target tubes were examined for temperature effects by microscopic examination of the structure. When 2-1/4 Cr - 1 Mo is subjected to temperatures above 1450 F, a transformation occurs from the spheroidized to the austenitic state. The extent to which the transformation occurs is dependent on the length of time that the material is above 1450 F and the temperature in excess of 1450 F. Only one target tube showed significant transformation. Extensive transformation was observed on a tube from Test 2 at the point of impingement with decreasing effect around the circumference of the tube on either side of the impinged point. A Tukon hardness traverse at intervals of 10 mils through the thickness and 10° around the tube indicated that the transformation occurred through the thickness of the tube at the impinged point and was confined to within 30° of either side of this point. The temperature effect, therefore, appeared to be confined to a small area at the point of impingement.

(c) ANALYSIS

(1) Critical Outside Heat Transfer Coefficient - In order to determine the areas and operating conditions in the boiler section which would be most susceptible to temperature softening during a reaction, calculations were made to determine the heat transfer coefficient (critical h) on the outside of the tube that would cause the tube to be on the verge of failing under steady-state conditions. This calculation was made for all five regions of the boiler section at all projected steady-state operating modes of the steam generator. A reaction temperature of 2200 F was chosen for the analysis and values of the ultimate strength of the 2-1/4 Cr - 1 Mo

furnished by B&W were used. The reference temperature of 2200 F was selected as an average between the majority of the data (1800 F) and the maximum temperature recorded (2600 F). The values of critical h , presented in Table 6.1, ranged from 916 to 4675 Btu/hr ft² °F. The lowest value, 916 Btu/hr ft² °F occurred in the film boiling region of the boiler bundle at 20% load conditions.

The critical outside heat transfer coefficient as a function of reaction temperature was calculated for this location at 20% load condition and is shown in Figure 6.1. As can be seen from the figure, if a high reaction temperature occurred heat transfer coefficients greater than about 400 Btu/hr ft² °F could cause the tube to fail if enough time to establish steady-state conditions were available.

According to BW-67-2, its supplements, and personal communication with B&W, the steam generator will not be pressurized with static water or steam during its operation. If a zero flow condition should exist with sodium in the unit, the susceptibility of all regions to temperature softening failures would be greatly increased. If, therefore, operation of the full-size unit at zero flow is envisioned at sometime in the future, additional analysis of the temperature softening problem for this case will be necessary.

(2) Determination of Outside Heat Transfer Coefficients in the Reaction Zone - Unfortunately, no reliable estimate of the outside heat transfer coefficient can be made from the existing test data for sodium-water reactions. In order to gain some insight into the magnitude of the coefficients that might be expected to exist in the reaction zone, several simplified models of the hydrogen bubble, described in Appendix B, were examined.

Model 1 - The first model postulated that the bubble was made up of pure hydrogen flowing across the tubes at the same velocity as the bubble front. A heat transfer coefficient obtained for this case using typical bubble front velocities for a one-tube rupture was about 110 Btu/hr ft² °F and was essentially independent of reaction temperature. Based on the analysis in Section 6-1.2(c) and using a reaction temperature of 3050 F, no tube failures would occur due to temperature softening if an h of this magnitude existed during the reaction.

Model 2 - The second model postulated that some sodium was entrained in the hydrogen bubble. Using spray cooling data for sodium, the heat transfer coefficient as a function of volume fraction of sodium, droplet velocity, and droplet size was determined. The droplet was assumed to be traveling at the velocity of the bubble front. All the heat of reaction was assumed to be in the reaction products and the entrained sodium, thus the reaction temperature could be found as a function of the amount of sodium entrainment.

The diameter of the sodium droplets that might be entrained during a reaction was unknown; therefore, to yield conservative results, the diameter was optimized as a function of velocity from target efficiency and heat transfer considerations for maximum heat transfer. Figure 6.2 shows the outside heat transfer coefficient versus reaction pressure, temperature, and velocity of the bubble front. The reaction temperature curves were terminated at the reaction pressure corresponding to the saturation pressure of sodium at that temperature.

The main assumptions implicit in this model were:

- (a) Sodium droplets were entrained in the gas bubble.
- (b) The droplets traveled at the same velocity as the bubble front.
- (c) The droplet diameter was not known and, therefore, was optimized to give the best heat transfer.
- (d) The reaction proceeded according to the equation:
$$2 \text{ Na} + \text{H}_2\text{O} \rightarrow \text{Na}_2\text{O} + \text{H}_2$$
- (e) The reactants were at an initial temperature of 750 F.
- (f) All the heat generated in the reaction was confined to the reaction products and entrained sodium and the mix was at a uniform temperature.

Assumptions (a), (c), (d), and (f) are considered conservative and Assumptions (b) and (e), although not necessarily conservative, are reasonable.

Model 3 - Model 2 assumed that all the heat of reaction was confined to the reaction products. This assumption, although conservative, could introduce a significant error in the heat transfer calculations for reaction times on the order of seconds since heat transfer to the bulk fluid and water tubes could lower the reaction temperature significantly. A third model was postulated, therefore, which included the effect of heat loss to the tubes of the steam generator. Time becomes a variable and complicates the heat balance equations as shown in the detailed description of this model in Appendix B. Due to this complication, the equation could not be solved to give the temperature versus outside heat transfer coefficient information as a function of time. The equations were solved, however, to give the instantaneous equilibrium reaction zone temperature versus volume fraction of entrained sodium at any particular time, assuming that the gas generated at this instant of time did not mix with that generated earlier.

The results of these calculations are presented in Figures 6.3 through 6.7 for reaction times of 10^{-4} , 10^{-2} , 10^{-1} , 1, and 3 seconds, respectively. These curves used in conjunction with the left side of Figure 6.2 give values of the outside heat transfer coefficient versus reaction temperature for various reaction times. For comparison, both Models 2 and 3 were applied to a one-tube rupture in the center of the boiler bundle during full load (Case XXVIIIa); the results are shown in Figure 6.8. The curves are terminated at the saturation temperature of sodium corresponding to the pressure existing at the time under consideration. As expected, Model 2 predicts lower heat transfer coefficients, especially for longer time periods and higher reaction zone temperatures.

This model was not used in the extensive leak propagation analysis since one assumption, Assumption (f), was not conservative.

- (a) Sodium droplets were entrained in the gas bubble.
- (b) The droplets traveled at the same velocity as the bubble interface.
- (c) The droplet diameter was not known and, therefore, was optimized to give the best heat transfer.
- (d) The reaction proceeded according to the equation:

$$2 \text{ Na} + \text{H}_2\text{O} \rightarrow \text{Na}_2\text{O} + \text{H}_2.$$
- (e) The reactants were at an initial temperature of 750 F.
- (f) The gas generated and sodium entrained at a particular time were at a uniform temperature; however, no mixing took place from one instant of time to the next.
- (g) Heat was lost from the bubble to the steam generator tubes only.
- (h) An over-all heat transfer coefficient of 143 Btu/hr ft² °F and a steam temperature of 663 F, representative of the film boiling region, was chosen for the heat loss calculations. These were assumed constant and not a function of the quantity of heat transferred.

- (i) A heat transfer area to bubble volume ratio of $5 \text{ ft}^2/\text{ft}^3$ was used for the calculation. This corresponds to the boiler tube bundle.

6-1.3. WASTAGE

(a) BACKGROUND

Rapid thinning of tubes in a sodium-water reaction environment has been experienced in one of the Enrico Fermi Atomic Power Plant steam generators.¹ This effect has also been reproduced in sodium-water reaction tests conducted by DECo.² The absence of any wastage-type damage from any sodium-water or NaK-water reaction tests where the water leak rates were high enough to cause ejection of the sodium from the reaction vessel indicates that wastage is a phenomenon associated only with low rates. Similar evidence of this was noted in the DECo tests when a leak rate of 1.25 lb/sec, which resulted in the expulsion of most of the sodium from the reaction zone, yielded no damage due to wastage. Thus, the mechanism of leak propagation appears to be self-limiting.

Although most of the rapid wastage experienced in the DECo tests was highly localized, being confined to a small section of one tube, some evidence of more uniform corrosion was found. Also, examination of the tubes surrounding the reaction in the Fermi unit showed uniform thinning spread over a large number of tubes. The exact mechanism of tube wastage is as yet unknown; however, the possibility of the presence of sodium hydroxide and water along with high temperatures in the reaction zone leads one to suspect high-temperature corrosion caused by aqueous sodium hydroxide.

(b) SODIUM-WATER TUBE THINNING TESTS

Since the water pressure, the temperature, and the tube size and spacings simulated in the DECo tests did not coincide with the B&W unit, the existing data on tube wastage was not directly applicable. In order to obtain more applicable wastage data and to gain insight into the wastage problem, nine Sodium-Water Tube Thinning Tests, described in Appendix D, were conducted at APDA. Only five of the nine tests resulted in significant amounts of thinning (losses of less than 3 mils were not considered significant since they could occur from removal of scale by the sodium). Most of the tests did not run to completion due to plugging of the injection orifice by reaction products. The detailed wastage and weight-loss data for the tests are given in Table 6.2.

(c) ANALYSIS

In order to use the available wastage data in an analysis, it was necessary to develop a correlation between wastage rate and leak rate and to determine the region of influence and the extent of wastage. Since various types of wastage were observed and measured on different tubes, wastage was arbitrarily divided into three categories which would best represent the existing data as shown in Table 6.3. As an additional category, the following correlation was used:

$$\text{Wastage Rate, mils/sec} = K \times \text{Leak Rate, lb/sec}$$

Combining the categories, the following classification was used to describe the wastage that occurred to a tube during a sodium-water reaction.

<u>Category</u>	<u>Type of Damage</u>	<u>No. of Tubes Affected</u>	<u>K, Mils/lb</u>
Primary	Localized pitting or holes, characterized by very rapid rate	1	2 to 16
Secondary	Less localized pitting, characterized by rapid rate	1 to 2	2 to 6
Uniform	Uniform rather than localized pitting or holes, characterized by relatively slow rate	1 to 5	0.5 to 1.5

Distribution of the wastage varied significantly from test to test; several tests were not of sufficient duration to produce measurable thinning. Total wastage values of less than 3 mils were not considered when drawing up Table 6.3 since cleaning the scale from the tubes could account for losses of this magnitude.

6-2. REACTION DETECTION AND TERMINATION

In order to conduct a realistic analysis that would include leak propagation, some provision had to be made for the termination of the reaction. If this provision were not made, the leak would continue until all the tubes failed due to the mechanisms of leak propagation. To accomplish this, various assumptions were made regarding the sodium and steam systems connected to the steam generator and the leak detection and isolation equipment used during operation.

6-2.1. LEAK DETECTION

The leak detection times determined for the Full-Size Steam Generator were scaled up from information gained from the steam generators at the Enrico Fermi Atomic Power Plant. The analysis was based on the following assumptions:

Total sodium inventory in loop, 5.85×10^5 lb

Total cover gas volume in loop, 2500 ft³

Sodium cycle time, 3.0 min

The leak was considered to be detected when a 5 psi rise in cover gas pressure occurred

A plot of the leak detection time versus water leak rate is shown in Figure 6.9.

6-2.2. ISOLATION AND DUMP

For the purpose of analysis, it was assumed that initiation of the isolation and dump of the water side of the steam generator occurred at the time that the leak was detected. The isolation and dump information for the Full-Size Steam Generator was obtained from The Babcock & Wilcox Company.

The dump time is composed of the time required to open the dump valves (~15 sec) and the time required for the pressure to decay to a value equivalent to the pressure on the sodium side.

B&W computed the water pressure versus time for a dump from the boiler inlet header for the two conditions listed below; the resulting curves are shown in Figure 6.10.

The steam generator was filled with water at 350 F and 2500 psia. The pressure decreased almost instantaneously

to a saturation pressure of 135 psia, and then decayed slowly to atmospheric pressure. Expansion of water through the valve was isentropic.

The steam generator was filled with water and steam at 20% load conditions. Subcooled water was dumped first, followed by a steam-water mixture. Expansion of the water and steam-water mixture was isentropic.

6-3. ANALYSIS OF ACCIDENTS THAT INCLUDE LEAK PROPAGATION

Although the information discussed previously was not sufficiently complete to provide the basis of a comprehensive analysis of an accident that involved leak propagation, several accidents were postulated and analyzed using the available data. In areas where information was lacking, assumptions which were conservative, based on the existing data, were made.

6-3.1. POSTULATION OF ACCIDENTS

Six accidents, located in the film boiling section of the boiler bundle, were proposed. This region was chosen for its vulnerability to temperature softening failures as discussed in Section 6-1.2. Examination of the strength properties of the tubing in the superheater section indicated that even though this section operated at higher temperatures, the film boiling region of the boiler section would be more susceptible to temperature softening failures.

The two operating modes chosen, as indicated in the tabulation below, were the flooded and 20% load conditions.

<u>Initiating Leak Rate, lb/sec</u>	<u>Operating Conditions</u>	<u>Location</u>		
0.03	Flooded	Film Boiling Region		
0.03	20% Load	"	"	"
1.0	Flooded	"	"	"
1.0	20% Load	"	"	"
18.2	Flooded	"	"	"
7.0	20% Load	"	"	"

The flooded condition was chosen since a leak at these conditions yielded the highest leak rate. During the 20% load condition, the tubes in the film boiling region were most susceptible to temperature softening failures; there-

fore, this condition was also chosen. As indicated in Section 6-1.2, operation of the unit at no flow while pressurized with water and/or steam was not considered in this analysis.

Three basic leak rates, which covered the range of possible initiating accidents, were chosen as initiating leaks. A leak rate of 0.03 lb/sec was chosen as the smallest since it was the lowest rate at which wastage data were available. An intermediate rate of 1.0 lb/sec was chosen since it was the highest leak rate for which wastage was measured. The complete severance of one tube resulting in leak rates of 18.2 and 7.0 lb/sec for flooded and 20% load conditions, respectively, was selected as the maximum leak rate.

6-3.2. ANALYSIS

A running account of the following parameters was kept in order to chart the progress of the accident and the extent of leak propagation.

- Reaction zone temperature
- Reaction zone pressure
- Heat transfer coefficient on outside of tubes
- Tube temperatures
- Amounts of wastage on affected tubes
- Number of leaking tubes
- Total leak rate
- Total water leaked

The times of the following events were also noted:

- Time of each tube failure
- Time at which leak was detected and isolation and dump was initiated
- Time at which the gas bubble penetrated gas space
- Time at which the rupture disk burst
- Time at which the isolation and dump valves were positioned
- Time at which the unit was completely dumped

The wastage criteria used in the analysis were based on the data in Section 6-1.3. The three categories of wastage described in that section were used, along with the following wastage correlation.

$$\text{Wastage Rate, mils/sec} = K \times \text{Leak Rate, lb/sec}$$

The wastage correlation constant, the number of tubes affected, and the size of the resultant failure for each type of wastage is shown below:

<u>Type of Wastage</u>	<u>K (mils/lb)</u>	<u>No. of Tubes</u>	<u>Size of Hole</u>
Primary	20	1	3/16 in. dia.
Secondary	5	1	Complete Tube
Uniform	2	7	Complete Tube

The values of K were picked to give rates which approximated the rates shown in Table 6.3 for the particular type of wastage under consideration. It was assumed that for each leaking tube, there would be nine tubes affected: one by primary wastage, one by secondary wastage, and seven by the slow, uniform type of wastage. It was also assumed that when a tube being wasted failed, the remaining eight tubes would continue to be wasted by the original leak and that the new leak began to waste nine previously unaffected tubes. The diameter of the hole from the primary wastage was selected as 3/16 inch since this was, except for one case, the minimum value in Table 6.3. The minimum value was chosen since high leak rates from large holes would be less conservative because wastage would cease and penetration of the bubble into the gas space would occur rapidly. However, the pressure peaks resulting from larger holes may be greater. It was also assumed that all wastage ceases when the total leak rate exceeds a value large enough to push the sodium out of the reaction zone. This leak rate is estimated at 7 lb/sec in the film boiling region. This was scaled from DECo tests using the ratio of the cross-sectional areas according to the isovelocity scaling laws. The size of the wasted area on the tubes was estimated from data given in Section 6-1.3.

Model 2, discussed in Section 6-1.2 and Appendix B was used to predict the outside heat transfer coefficient versus reaction temperature. This model was used, rather than Model 3, because the solution obtained for Model 3 was based on the assumption that there was no mixing of the gas and entrained sodium from one instant of time to the next; this did not give a conservative result. Model 2 was based on sodium being entrained in a gas bubble with no heat loss to the surroundings.

The reaction temperature used in the analysis was the saturation temperature of sodium at the particular reaction zone pressure present at the time. This temperature was chosen to be consistent with Model 2 since temperatures higher than the saturation temperature would turn the sodium to vapor. If such were the case, the resultant heat transfer coefficient would be much lower than predicted since the gas and vapor phase coefficient is much lower than that for entrained liquid. In addition, much of the heat of reaction would be used to vaporize the entrained sodium. It was also found that if temperatures below the saturation temperature were used, lower tube temperatures were predicted despite the fact that a higher coefficient resulted from such a choice.

The tube failures were a function of both temperature and wastage. A critical outside tube temperature exists for each amount of wastage at which the tube will fail due to the internal pressure. If, during a reaction, steady state heat transfer calculations indicated that the outside tube temperature would be above the critical value, a Schmidt plot was made to determine when the tube would fail.

The leakage from a 3/16-inch hole was found to be 1 and 3.5 lb/sec for 20% load and flooded conditions, respectively. The leakage from both ends of a severed tube in the film boiling region for 20% load and flooded conditions was 7 and 18.2 lb/sec, respectively. The times required for leak detection, dump valve operation, isolation, and dump were taken from the information given in Section 6-2. Pressure analyses of the accidents were made using the BUG 2 Model with reaction relief paths the same as those shown for Case XXVIII. This model did predict when the bubble reached the gas space. The rupture disk setting for this analysis was 60 psig.

6-3.3. RESULTS

The results of the analyses are presented in the form of curves and tables in Figures 6.11 through 6.16 and Tables 6.4 through 6.9. In the most severe case considered, the maximum total leak rate never exceeded 44.4 lb/sec.

For the cases where the initiating leak was the complete severance of one tube, wastage was not considered since the leak was large enough to force the sodium out of the reaction zone. The results for these two cases are presented in Figures 6.11 and 6.12 and Tables 6.4 and 6.5. It can be seen from the figures that the reaction zone temperature was above the critical temperature for the tubes until the rupture disk burst. For the flooded condition, the rupture disk burst before the tubes had heated sufficiently to reach the temperature at which they would fail. It was assumed that the water in the tube would go into nucleate boiling and then film boiling as the temperature on the inside of the tube rose. In the case of the 20% load condition, the outside heat transfer coefficient caused a sizeable temperature drop between the reaction zone temperature and tube temperature preventing the tubes from reaching the critical temperature. Once the rupture disk burst, the pressure in the gas space fell low enough that the reaction zone temperature was well below the critical temperature of the tubes. Isolation and dumping of the unit occurred long after the reaction zone temperature dropped, eliminating the threat of temperature softening, even though no flow conditions could exist during the dumping process. Wastage could occur during the dumping process if sodium flows back into the leak area; however, this was not analyzed since the water pressure had dropped enough to eliminate the possibility of large amounts of water entering the steam generator from many leaks.

The results of the cases with an initiating leak of 1 lb/sec are given in Figures 6.13 and 6.14 and Tables 6.6 and 6.7. The case for the 1 lb/sec initiating leak during flooded conditions resulted in the largest leak rate and largest total amount of water leaked into the unit before isolation was accomplished. Even though steady-state calculations indicated that the rest of the tubes should fail from temperature effects just before the rupture disk burst, a Schmidt plot of these tubes indicated that they did not reach the critical temperature by the time the rupture disk burst. After the rupture disk burst, the reaction zone temperature dropped below the critical temperature of the tubes due to the drop in pressure and corresponding drop in the sodium saturation temperature. It should be noted that for both 1 lb/sec cases, no full-tube ruptures occurred until after the gas bubble had reached the gas space. Thus no large pressure excursions resulted from these failures. No tube temperatures are included in Figures 6.13 and 6.14 since the temperature of each tube was dependent upon how long that tube had been subjected to the reaction and how much wastage it had experienced.

Figures 6.15 and 6.16 and Tables 6.8 and 6.9 are the results for the cases where the initiating leak rate was 0.03 lb/sec. This initial leak rate resulted in the least severe accident of all the cases considered. The maximum leak rates during the accidents were 7 and 10 lb/sec for cases during flooded conditions and 20% load conditions, respectively. In both cases, no tubes failed after the bubble reached the gas space because the leak rate was low enough to reduce the outside heat transfer coefficient significantly. Thus, even though the reaction zone temperature was higher than the critical temperature for the tube, the outside of the tube never reached this temperature due to the resistance of the film.

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TABLE 6.1

CRITICAL OUTSIDE HEAT TRANSFER COEFFICIENT FOR THE BOILER SECTION

LOCATION	LOAD, %	TUBE DIMENSIONS		WATER OR STEAM CONDITIONS			CRITICAL OUTSIDE HEAT TRANSFER COEFFICIENT Btu/hr ft ² °F
		OD, in.	Wall, in.	Flow Rate, 10 ⁶ lb/hr.	Temp, F	Press., psig	
Boiler Downcomer	Flooded	0.625	0.076	0.458	350	2625	2640
	20	0.625	0.076	0.458	370	2410	3080
	40	0.625	0.076	0.888	430	2440	4675
	60	0.625	0.076	1.338	475	2490	4450
	80	0.625	0.076	1.838	505	2560	4720
	100	0.625	0.076	2.403	530	2650	4830
Economizer	Flooded	1.00	0.120	0.458	350	2625	1750
	20	1.00	0.120	0.458	663	2415	1272
	40	1.00	0.120	0.888	665	2435	1773
	60	1.00	0.120	1.338	668	2485	2100
	80	1.00	0.120	1.838	671	2539	2260
	100	1.00	0.120	2.403	676	2618	2370
Nucleate Boiling	20	1.00	0.120	0.450	663	2415	3870
	40	1.00	0.120	0.888	665	2435	3840
	60	1.00	0.120	1.338	668	2485	3745
	80	1.00	0.120	1.838	671	2538	3640
	100	1.00	0.120	2.403	676	2617	3510
Film Boiling	Flooded	1.00	0.145	0.458	350	2625	1607
	20	1.00	0.145	0.458	663	2403	916
	40	1.00	0.145	0.888	664	2430	1345
	60	1.00	0.145	1.338	667	2467	1730
	80	1.00	0.145	1.838	670	2519	1940
	100	1.00	0.145	2.403	674	2585	2110
Superheat	Flooded	1.00	0.165	0.458	350	2625	4440
	20	1.00	0.165	0.458	715	2421	1265
	40	1.00	0.165	0.888	723	2437	1890
	60	1.00	0.165	1.338	730	2464	2380
	80	1.00	0.165	1.838	739	2502	2680
	100	1.00	0.165	2.403	750	2550	2880

TABLE 6.2

SODIUM-WATER TUBE THINNING TEST DATA

RUN NO. DATE	1 6-4-65	2 6-14-65	3 6-22-65	4 6-25-65	5 7-15-65	6 7-21-65	7 7-24-65	8 7-28-65	9 8-2-65
Orifice Diameter, in.	0.016	3/32	0.016	3/32	0.016	0.025	0.025	0.025	3/32
Planned Rate of H ₂ O Injection, lb/sec	0.04	1.0	0.04	1.0	0.04	0.1	0.1	0.1	1.0
Amount of H ₂ O Injected, lb	1 - 2 (Est.)	4.0	--	4.0	1.0	--	--	4.0	4.0
Duration of Injection, sec	~ 180	--	~ 16	3.7	~ 15	~ 1.5	~ 2.4	~ 49	3.7
Initial Temp of Na & H ₂ O, F	350	350	350	350	350	350	350	350	350
Initial Press. of H ₂ O, psig	3000	--	2950	2950	2950	2950	2950	2950	2950
Tube Size, in.	5/8	5/8	5/8	5/8	1.0	5/8	1.0	1.0	1.0
Maximum Wastage, mils									
Tube No. 11	0.6	0.5	1.3	0	0.3	0.1	2.3	4.5	1.6
12	0.9	0.2	1.7	0.2	2.7	0.2	1.9	2.0	2.6
22	4.9	3.3	1.9	0.4	1.8	0	--	1.3	0.7
23	23.7	28.4	2.6	0.8	1.7	0.4	0	0	3.4
24	6.1	1.0	1.5	0.3	0.3	0.4	0.4	2.2	1.8
32	0.9	0.8	1.6	0.1	0.3	0.1	1.1	3.5	0.5
33	2.6	0.2	2.1	0.1	3.5	0.1	0.2	0.9	0.5
34	0.5	0.3	1.5	0.1	1.1	0.9	1.7	0.3	1.6
Maximum Weight Loss, grams									
Tube No. 11	0.0656	0.0602	0.0599	0.1008	1.2	0.0224	0.3	0.2	0.1
12	0.0978	0.0686	0.0543	0.1323	0.5	0.0334	0.2	-0.1	0
22	0.3226	--	--	0.0756	--	--	--	--	--
23	1.2721	0.3776	0.0334	0.0920	--	0.0597	1.2	0	0.2
24	0.4098	0.4229	0.1742	0.1271	--	0.0823	0.6	0.2	0
32	0.0707	0.0900	0.0465	0.0807	0.4	0.0209	0.4	1.1	0.1
33	0.0669	0.0905	0.0381	0.0703	0.1	0.0156	0.1	0.6	-0.1
34	0.0395	0.0608	0.0333	--	0.4	0.0213	0.2	-0.2	0.5
Maximum Temperature in Reaction Zone, F	1000	1057	1286	2014	640	530	413	1261	1551
Inside Adjacent Tube, No. 22	--	426	456	--	450	384	No Ch.	710	510

TABLE 6.3

SUMMARY OF AVAILABLE WASTAGE DATA

Tests	Test Designation	Leak Rate, lb/sec	Water Injected, lb	Duration of Test, sec	TYPE OF WASTAGE														
					PRIMARY					SECONDARY					UNIFORM				
					Rate, mls/sec	Total, mls	K, mls/lb	No. of Tubes Affected	Type of Damage	Rate, mls/sec	Total, mls	K, mls/lb	No. of Tubes Affected	Type of Damage	Rate, mls/sec	Total mls	K, mls/lb	No. of Tubes Affected	Type of Damage
APDA Na-H ₂ O Tube Thinning Tests	1	~0.04	1-2 est.	~180	0.13	23.5	12 to 23.5	1	Valley	~0.03	4 9&6.1	~3.3 to 4	2	Somewhat Uniform	--	--	--	--	--
	2	~1	4.0	--	--	28.4	7.1	1	5/16-in. diameter	--	--	--	--	--	--	3.3	0.825	1	Uniform
	3	~0.04	--	~16	NO SIGNIFICANT WASTAGE														
	4	~1.0	4.0	~3.7	NO SIGNIFICANT WASTAGE														
	5	~0.04	~1.0	~15	--	--	--	--	--	0.233	3.5	~3.5	1	Somewhat Uniform	--	--	--	--	--
	6	~0.1	--	~1.5	NO SIGNIFICANT WASTAGE														
	7	~0.1	--	~2.4	NO SIGNIFICANT WASTAGE														
	8	--	4.0	~49	--	--	--	--	--	--	--	--	--	--	0.07 to 0.09	3.5&4.5	0.875 to 1.25	2	Uniform
	9	~1.0	4.0	~3.7	--	--	--	--	--	--	--	--	--	--	0.92	3.4	0.85	1	Uniform
Detroit Edison Tube Thinning Tests ^{7,8}	Ib	0.033	4.0	120	0.35+	42+	10.5+	1	1/4x3/8-in. oval hole	0.1833	22	5.5	1	Valley	~0.033	3 to 5	0.75 to 1.25	5	Uniform
	III	0.038	1.0	26	--	--	--	--	--	~0.192	3 to 5	3 to 5	2	Somewhat Uniform	--	--	--	--	--
	IV	0.43	2.0	4.6	1.74	8	4	1	pit, ~3/16-in. dia	~1.1	5	2.5	1	Somewhat Uniform	~0.65	~3	~1.5	4	Uniform
	VI	0.042	4.0	95.0	0.0737	7	1.75	1	--	--	--	--	--	--	~0.0527	4 to 5	1 to 1.25	5	Uniform
	VII	0.46	4.0	8.7	4.25	37	9.25	1	pit, ~3/16-in. dia	--	--	--	--	--	~0.345	2 to 3	0.5 to 0.75	5	Uniform
Fermi ⁸	Tube 92-2 93-2 89-4 93-3								Hole equiv to 0.25 -in. dia 0.2645-in. dia 0.3455-in. dia 0.774 -in. dia										

TABLE 6.4
INITIAL LEAK OF ONE TUBE IN THE FILM BOILING SECTION DURING FLOODED CONDITIONS

EVENT DESIGNATION	TIME, sec	EVENT DESCRIPTION	NO. OF FAILED TUBES	LEAK RATE, PER TUBE, lb/sec	TOTAL LEAK RATE, lb/sec	TOTAL WATER DISCHARGED, lb
0	0	Leak is initiated, no wastage since rate is approximately 18.2 lb/sec	1	18.2	18.2	0
1	0.7	Leak is detected by a 5 psi rise in cover gas pressure, isolation is automatically initiated	1	18.2	18.2	12.74
2	1.8	Gas bubble penetrates into the gas space, pressure is reduced to the pressure drop of the hydrogen flowing through the steam generator and gas space buildup	1	18.2	18.2	21.5
3	2.72	Rupture disk bursts, pressure reduces to 21 psia	1	18.2	18.2	49.5
4	15.7	Isolation valves are positioned, pressure in water side decays to 135 psia instantaneously	1	0.75	0.75	455
5	247	Unit is completely dumped	1	0	0	621

TABLE 6.5

INITIAL LEAK OF ONE TUBE IN THE FILM BOILING SECTION DURING 20% LOAD CONDITIONS

EVENT DESIGNATION	TIME, sec	EVENT DESCRIPTION	NO. OF FAILED TUBES	LEAK RATE PER TUBE lb/sec	TOTAL LEAK RATE, lb/sec	TOTAL WATER DISCHARGED, lb
0	0	Leak is initiated, no wastage since rate is approximately 7 lb/sec	1	7	7	0
1	1.27	Leak is detected by a 5 psi rise in cover gas pressure, isolation is automatically initiated	1	7	7	8.9
2	2.55	Gas bubble penetrates into the gas space, pressure is reduced to the pressure drop of the hydrogen and the gas space buildup	1	7	7	17.8
3	7.06	Rupture disk bursts, pressure drops to 21 psia	1	7	7	49.5
4	16.27	Isolation valves are positioned, pressure in water side begins decay	1	< 7	< 7	184
5	63.27	Unit is completely dumped	1	0	0	~ 419

TABLE 6.6

INITIAL LEAK OF 1 LB/SEC IN THE FILM BOILING SECTION DURING FLOODED CONDITIONS

EVENT DESIGNATION	TIME, sec	EVENT DESCRIPTION	INITIAL WASTAGE						RESULTANT WASTAGE						NO. OF FAILED TUBES	LEAK RATE PER TUBE, lb sec	TOTAL LEAK RATE, lb sec	TOTAL WATER DISCHARGED, lb
			PRIMARY k 20 Rate 20		SECONDARY k 5 Rate 5		UNIFORM k 2 Rate-2		PRIMARY k 20 Rate 20		SECONDARY k 5 Rate 17.5		UNIFORM k 2 Rate 7					
			No. of Tubes	Wastage, Mils	No. of Tubes	Wastage Mils	No. of Tubes	Wastage, Mils	No. of Tubes	Wastage, Mils	No. of Tubes	Wastage, Mils	No. of Tubes	Wastage Mils				
0	0	Initiating leak	1	0	1	0	7	0	--	--	--	--	--	--	1	1	1	0
1	4.9	First tube fails	<u>1</u>	98 *	1	24.5	7	9.8	1	0	1	0	7	0	<u>1</u> 1	<u>1</u> 3.5	4.5	4.9
2	6.5	Leak detected by 1.5 psi pressure rise, isolation and dump initiated	--	--	1	32.5	7	13.0	1	45.5	1	11.38	7	4.6	<u>1</u> 1	<u>1</u> 3.5	4.5	7.5
3	6.57	Second tube fails, wastage ceases	--	--	1	32.9	7	13.2	<u>1</u>	<u>117</u> *	1	29.2	7	11.7	<u>1</u> 2	<u>1</u> 3.5	8.0	12.4
4	7	Gas bubble penetrates into gas space	--	--	1	32.9	7	13.2	--	--	1	29.2	7	11.7	<u>1</u> 2	<u>1</u> 3.5	8.0	15.8
5	10.25	Third tube fails	--	--	<u>1</u>	<u>32.9</u> *	7	13.2	--	--	1	29.2	7	11.7	<u>1</u> 2 1	<u>1</u> 3.5 18.2	26.2	41.8
6	10.4	Fourth tube fails	--	--	--	--	7	13.2	--	--	<u>1</u>	<u>29.2</u> *	7	11.7	<u>1</u> 2 2	<u>1</u> 3.5 18.2	44.4	45.7
7	10.5	Rupture disk bursts	--	--	--	--	7	13.2	--	--	--	--	7	11.7	<u>1</u> 2 2	<u>1</u> 3.5 18.2	44.4	49.5
8	21.5	Isolation and dump valves fully positioned	--	--	--	--	7	13.2	--	--	--	--	7	11.7	<u>1</u> 2 2	<u>0.025</u> <u>0.15</u> <u>0.75</u>	1.825	538.5
9	268.5	Unit is completely dumped	--	--	--	--	7	13.2	--	--	--	--	7	11.7	<u>1</u> 3	<u>0</u> <u>0</u>	0	990

TABLE 6.7

INITIAL LEAK OF 1 LB/SEC IN THE FILM BOILING SECTION DURING 20% LOAD CONDITIONS

EVENT DESIGNATION	TIME, sec	EVENT DESCRIPTION	INITIAL WASTAGE						RESULTANT WASTAGE						NO. OF FAILED TUBES	LEAK RATE PER TUBE, lb/sec	TOTAL LEAK RATE, lb/sec	TOTAL WATER DISCHARGED, lb
			PRIMARY k=20 Rate=20		SECONDARY k=5 Rate=5		UNIFORM k=2 Rate=2		PRIMARY k=20 Rate=20		SECONDARY k=5 Rate=5		UNIFORM k=2 Rate=2					
			No. of Tubes	Wastage, Mils	No. of Tubes	Wastage, Mils	No. of Tubes	Wastage, Mils	No. of Tubes	Wastage, Mils	No. of Tubes	Wastage, Mils	No. of Tubes	Wastage, Mils				
0	0	Initiating leak	1	0	1	0	7	0	--	--	--	--	--	--	1	1	1	0
1	3.3	One tube fails	<u>1</u>	<u>65</u> *	1	16.5	7	6.6	1	0	1	0	7	0	2	1	2	3.25
2	6.8	Second tube fails	--	--	1	34.0	7	13.6	$\frac{1}{1}$	$\frac{70}{0}$ *	1 1	17.5 0	7 7	7 0	3	1	3	10.25
3	7.0	Leak detected, isolation and dump initiated	--	--	1	35.0	7	14.0	1	4	1 1	18.5 1	7 7	7.4 0.4	3	1	3	10.85
4	9.0	Bubble penetrates into gas space	--	--	1	45.0	7	18.0	1	44	1	28.5 11.0	7 7	11.4 4.4	3	1	3	16.85
5	10.0	Third tube fails	--	--	1	50.0	7	20.0	$\frac{1}{1}$	$\frac{60}{0}$ *	1 1 1	33.5 16.0 0	7 7 7	13.4 6.4 0	4	1	4	19.85
6	11.0	Fourth tube fails, wastage ceases	--	--	<u>1</u>	<u>55.0</u> *	7	22.0	1	20	1 1 1 1	38.5 21.0 5.0 0	7 7 7 7	15.4 8.4 2 0	4 1	1 7	11	23.85
7	11.8	Fifth tube fails	--	--	--	--	7	22.0	1	20	1 1 1 1	38.5 * 21.0 5.0 0	7 7 7 7	15.4 8.4 2 0	4 2	1 7	18	32.65
8	13.0	Rupture disk bursts	--	--	--	--	7	22.0	1	20	1 1 1	21.0 5.0 0	7 7 7	15.4 8.4 2 0	4 2	< 1 < 7	< 18	49.5
9	20.0	Isolation and dump valves fully positioned	--	--	--	--	7	22.0	1	20	1 1 1	21.0 5.0 0	7 7 7	15.4 8.4 2 0	4 2	< 1 < 7	< 18	175.5
10	67.0	Unit is completely dumped	--	--	--	--	7	22.0	1	20	1 1 1	21.0 5.0 0	7 7 7	15.4 8.4 2 0	6	0	0	~ 480

* Tube Failure

TABLE 6.8

INITIAL LEAK OF 0.03 LB/SEC IN THE FILM BOILING SECTION DURING FLOODED CONDITIONS

EVENT DESIGNATION	TIME, sec	EVENT DESCRIPTION	INITIAL WASTAGE						RESULTANT WASTAGE						NO. OF FAILED TUBES	LEAK RATE PER TUBE, lb/sec	TOTAL LEAK RATE, lb/sec	TOTAL WATER DISCHARGED, lb
			PRIMARY k 20 Rate 0.6		SECONDARY k 5 Rate 0.15		UNIFORM k 2 Rate 0.06		PRIMARY k 20 Rate=70		SECONDARY k 5 Rate 17.5		UNIFORM k 2 Rate 7					
			No. of Tubes	Wastage, Mils	No. of Tubes	Wastage, Mils	No. of Tubes	Wastage, Mils	No. of Tubes	Wastage, Mils	No. of Tubes	Wastage, Mils	No. of Tubes	Wastage, Mils				
0	0	Initiating leak	1	0	1	0	7	0	--	--	--	--	--	--	1	0.03	0.03	0
1	150	First tube fails	<u>1</u>	<u>90</u> *	1	22.5	7	9	1	0	1	0	7	0	1 1	0.03 3.5	3.53	4.5
2	151.1	Second tube fails, wastage ceases	--	--	1	22.7	7	9.1	<u>1</u>	<u>77</u> *	1	19.25	7	7.7	1 2	0.03 3.5	7.03	8.4
3	152.9	Gas bubble penetrates into gas space	--	--	1	22.7	7	9.1	--	--	1	19.25	7	7.7	1 2	0.03 3.5	7.03	21.05
4	153.4	Leak detected by a 5 psi pressure rise, isolation and dump initiated	--	--	1	22.7	7	9.1	--	--	1	19.25	7	7.7	1 2	0.03 3.5	7.03	24.6
5	157.0	Rupture disk bursts	--	--	1	22.7	7	9.1	--	--	1	19.25	7	7.7	1 2	0.03 3.5	7.03	49.5
6	168.4	Isolation and dump valves fully positioned	--	--	1	22.7	7	9.1	--	--	1	19.25	7	7.7	1 2	0.01 0.14	0.29	129.6
7	415.4	Unit is completely dumped	--	--	1	22.7	7	9.1	--	--	1	19.25	7	7.7	3	0	0	201

TABLE 6.9

INITIAL LEAK OF 0.03 LB/SEC IN THE FILM BOILING SECTION DURING 20% LOAD CONDITIONS

EVENT DESIGNA- TION	TIME, sec	EVENT DESCRIPTION	INITIAL WASTAGE						RESULTANT WASTAGE						NO. OF FAILED TUBES	LEAK RATE PER TUBE, lb sec	TOTAL LEAK RATE, lb/sec	TOTAL WATER DISCHARGED, lb
			PRIMARY k-20 Rate 0.6		SECONDARY k 5 Rate 0.15		UNIFORM k 2 Rate 0.06		PRIMARY k-20 Rate 19		SECONDARY k 5 Rate 4.75		UNIFORM k 2 Rate-1.9					
			No. of Tubes	Wastage, Mils	No. of Tubes	Wastage, Mils	No. of Tubes	Wastage, Mils	No. of Tubes	Wastage, Mils	No of Tubes	Wastage, Mils	No of Tubes	Wastage, Mils				
0	0	Initiating leak in film boiling section	1	0	1	0	7	0	--	--	--	--	--	--	1	0.03	0.03	0
1	150	First primary tube failure	1	90 [*]	1	22.5	7	9	1	0	1	0	7	0	1 1	0.03 1.0	1.0	4.5
2	153.4	Second primary tube failure	--	--	1	23	7	9.2	$\frac{1}{1}$	$\frac{65}{0}$ [*]	1 1	16 0	7 7	6.5 0	1 2	0.03 1.0	2.0	7.8
3	155.8	Third primary tube failure	--	--	1	23.4	7	9.3	$\frac{1}{1}$	$\frac{46}{0}$ [*]	1 1 1	27.4 11.4 0	7 7 7	11.1 4.6 0	1 3	0.03 1.0	3.0	12.8
4	156	First secondary tube failure, wastage ceases	--	--	1	23.4	7	9.3	1 1	4 0	$\frac{1}{1}$ 1	$\frac{28}{12}$ [*] 0.6	7 7 7	11.5 5.0 0.4	1 3 1	0.03 1.0 7	10	13.3
5	156.5	Bubble penetrates into gas space	--	--	1	23.4	7	9.3	1 1	4 0	1 1	12 0.6	7 7 7	11.5 5.0 0.4	1 3 1	0.03 1.0 7	10	18.3
6	157	Leak is detected, isola- tion and dump initiated	--	--	1	23.4	7	9.3	1 1	4 0	1 1	12 0.6	7 7 7	11.5 5.0 0.4	1 3 1	0.03 1.0 7	10	23.3
7	159.6	Rupture disk bursts	--	--	1	23.4	7	9.3	1 1	4 0	1 1	12 0.6	7 7 7	11.5 5.0 0.4	1 3 1	0.03 1.0 7	10	49.3
8	172	Isolation and dump valves fully positioned	--	--	1	23.4	7	9.3	1 1	4 0	1 1	12 0.6	7 7 7	11.5 5.0 0.4	1 3 1	0.03 1.0 7	0.42	173.3
9	219	Unit is completely dumped	--	--	1	23.4	7	9.3	1 1	4 0	1 1	12 0.6	7 7 7	11.5 5.0 0.4	1 3 1	0.03 1.0 7	0	193

FIG. 6.1 CRITICAL OUTSIDE HEAT TRANSFER COEFFICIENT - FILM BOILING REGION - 20% LOAD CONDITIONS

FIG. 6.2 OUTSIDE HEAT TRANSFER COEFFICIENT VERSUS REACTION TEMPERATURE FOR VARIOUS REACTION PRESSURES AND BUBBLE FRONT VELOCITIES

FIG. 6.3 REACTION TEMPERATURE VERSUS SODIUM ENTRAINMENT -
REACTION TIME 10^{-4} SECOND

FIG. 6.4 REACTION TEMPERATURE VERSUS SODIUM ENTRAINMENT -
REACTION TIME 10^{-2} SECOND

**FIG. 6.5 REACTION TEMPERATURE VERSUS SODIUM ENTRAINMENT -
REACTION TIME 10^{-1} SECOND**

FIG. 6.6 REACTION TEMPERATURE VERSUS SODIUM ENTRAINMENT -
REACTION TIME 1 SECOND

FIG. 6.7 REACTION TEMPERATURE VERSUS SODIUM ENTRAINMENT -
REACTION TIME THREE SECONDS

FIG. 6.8 OUTSIDE HEAT TRANSFER COEFFICIENT VERSUS REACTION ZONE TEMPERATURE

FIG. 6.9 LEAK DETECTION TIME BY A 5 PSI PRESSURE RISE IN THE COVER GAS

FIG. 6.10 WATER PRESSURE DECAY DURING A DUMP OF THE BOILER SECTION

FIG. 6.11 REACTION ZONE PARAMETERS FOR AN INITIATING LEAK OF A SINGLE-TUBE SEVERANCE - FLOODED CONDITIONS

FIG. 6.12 REACTION ZONE PARAMETERS FOR AN INITIATING LEAK OF A SINGLE-TUBE SEVERANCE - 20% LOAD CONDITIONS

FIG. 6.13 REACTION ZONE PARAMETERS FOR AN INITIATING LEAK OF ONE POUND PER SECOND - FLOODED CONDITIONS

FIG. 6.14 REACTION ZONE PARAMETERS FOR AN INITIATING LEAK OF ONE POUND PER SECOND - 20% LOAD CONDITIONS

FIG 6.15 REACTION ZONE PARAMETERS FOR AN INITIATING LEAK OF 0.03 POUND PER SECOND - FLOODED CONDITIONS

7. DISCUSSION OF RESULTS

7-1. ANALYSIS

7-1.1. MATHEMATICAL MODELS

The results obtained with the BUG 3 model indicate that the additional simplifications made in the piston model lead to more conservative results (pressures up to 25% higher).

In the initial milliseconds of the reaction, the inertia of the sodium controls the pressure, and uncertainties in frictional pressure losses do not affect this high-pressure portion of the transient.

For a constant reaction rate, the bubble pressure initially varies inversely as the square root of the time approaching infinity at time zero. This should not be construed as having any physical significance. It results mathematically from the assumptions of a step change in hydrogen generation rate with an initial bubble volume of zero at time zero. For the case of a ramp increase in reaction rate, for example, the initial bubble pressure would remain constant (refer to Appendix A).

However, the pressure decay from infinity is not necessarily an unacceptable condition. Since the time involved is extremely short, the energy imparted to the vessel, rather than the instantaneous value of the pressure, apparently becomes the controlling parameter in determining the ability of the vessel to withstand the reaction.

The air-water test results indicate that the BUG 1 model is conservative. For a given gas generation rate, the pressures are lower and the penetration of the liquid by the bubble is greater than predicted by the mathematical model.

7-1.2. CONSTANT LEAK RATE RESULTS

The results of the cases run with a constant leak rate (Section 5.3), although not representative of a realistic accident, are useful as a reference for evaluation of the strength of the shell. As indicated, Case XXVd produced maximum pressures for the complete severance of one tube. This case, which was for a reaction halfway up the boiler downcomer annulus during flooded conditions, was based on the assumption that the center pipe collapsed and was not available for relief. The results for this case are presented in Figures 5.12 through 5.16.

Although the bubble volume shown in Figure 5.12 is quite small at the beginning of the reaction, the liner of the Full-Size Steam Generator will be subjected to a pressure approaching that of the full bubble pressure at a point opposite the reaction since it occurs close to the liner. The pressure on the liner further away from the reaction will be lower by the ratio of the distance between the bubble and the point in question to the length of the path from the reaction to the free surface. As the bubble becomes large enough to contact the surface of the liner, the liner will be exposed to the bubble pressure at this point and the pressure distribution elsewhere will change according to the ratio given above. There is the possibility that shock waves could be formed during the reaction. Although a quantitative evaluation of this was impossible because of a lack of reaction kinetic data, the formation of shock waves from a reaction could greatly change the bubble pressure and behavior.

Although most of the assumptions and techniques used in the analysis were conservative, several uncertainties were not resolved. As discussed previously, reaction kinetics were neglected by assuming that all the water leaking into the sodium reacted instantaneously as it left the tube. This resulted in the maximum integrated mechanical energy release at any time; however, it did not necessarily give the maximum rate at which the energy could be released. Also, the bubble temperature of 1800 F, used for the analysis, could be low; however, the effect of a higher temperature on the bubble pressure is not large (an increase of 25% if the maximum adiabatic temperature of 3050 F is used).

Conservative assumptions in the analysis were (1) the leak rate was constant and not a function of the back pressure, (2) the reaction was according to the equation $2 \text{ Na} + \text{H}_2\text{O} \rightarrow \text{Na}_2\text{O} + \text{H}_2$, and (3) the sodium remained as a solid slug with no penetration.

7-1.3. LEAK PROPAGATION ANALYSIS

Of the six cases chosen for analysis, the case for an initiating leak of 1 lb/sec in the film boiling section of the boiler tube bundle during flooded conditions was the most severe. A total leak rate equivalent to less than three complete tube severances occurred during this accident and a total of four tubes failed as a result of the initial leak. The reaction pressure rose to a peak of only 75 psia subsequent to the pressure decay caused by the small initiating leak. This pressure was caused by the buildup of gas in the gas space before the rupture disk burst. The water pressure dropped to 135 psia after 21-1/2 seconds and the unit was completely dumped in 4-1/2 minutes. The results for this case are presented in Figure 6.13 and Table 6.6.

It is difficult to assess how well these results represent what actually might occur in a steam generator. The wastage correlation used

for this analysis is based on a small number of tests in which it was difficult to obtain reproducible results. The experiments on which the wastage data were based consisted of tests in which a jet of water was directed at a point on the surface of a tube. It is possible that other patterns and rates of wastage would result from actual leaks since the type and direction of the jet may vary significantly from that produced in the experiments. No tests have been conducted in flowing sodium; therefore, the effects of this variable have not been determined. The limitation of wastage to leak rates below 7 lb/sec for reactions in the film boiling region has not been verified experimentally by tests in vessels of a similar size and geometry.

Subsequent to the analysis involving leak propagation, extensive wastage was reported for a test in which the water rate was relatively high. Although the results were not reproducible and exact data on the leak rate were not available, the results of this test indicate that the value of 7 lb/sec used for this analysis may be low. The size of the wasted areas, the arbitrary reduction of the mechanism into three different classifications, and the determination of the number of tubes affected were derived from the available data on wastage which were quite sparse and nonreproducible.

The temperature softening calculations were based on the model described in Section 5-1 and in Appendix A. This model is probably conservative; however, it may not duplicate what actually occurs in the unit during a reaction.

Realistic approaches to the methods of leak detection, isolation, and dump were assumed in the absence of any information on proposed systems for the Full-Size Steam Generator. The bursting pressure of the rupture disk was set at 60 psig for lack of a specified pressure for a particular application.

The extent and severity of the temperature softening effect during a reaction was found to be primarily dependent on the reaction pressure and the water leak rate. The reaction temperature was a direct function of the pressure and the heat transfer coefficient and, although somewhat influenced by the pressure, was mainly a function of leak rate. Since the pressure was a direct function of the leak rate, high leak rates resulted in high pressures and thus high temperatures. Penetration of the bubble into the gas space and bursting of the rupture disk occurred much more rapidly at high leak rates. Also, it was assumed that wastage did not occur at high leak rates, i. e., 7 lb/sec or above. Thus competing effects are present. In order to illustrate the dependence of these effects on leak rate the following tabulation is presented.

<u>Leak Rate</u>	<u>Factors Favoring Propagation of the Leak</u>	<u>Factors Against Propagation of the Leak</u>
HIGH, ~ >7 lb/sec	<p>High pressures are developed; therefore, high temperatures occur also.</p> <p>High heat transfer coefficients are present.</p>	<p>Rapid penetration of the bubble into the gas space followed by almost immediate bursting of the rupture disk causing the temperature to drop rapidly below the critical temperature of the tubes.</p> <p>No wastage at these leak rates.</p>
INTERMEDIATE, ~ 1 to 7 lb/sec	<p>Penetration of the bubble into the gas space occurs after the tubes are close to thermal equilibrium</p> <p>A significant time elapses before the rupture disk burst allowing further heat up of the tubes.</p> <p>Rapid wastage occurs.</p>	<p>The pressure decays rapidly; therefore, the temperature decays also.</p> <p>The heat transfer coefficient is less than for the high leak rates.</p>
LOW, ~ <1 lb/sec	<p>Penetration never occurs since a large, single bubble is never formed.</p> <p>Large numbers of tubes can be wasted significantly before a large increase in leak rate occurs.</p>	<p>Pressure does not rise above the normal fluid pressure at the leak location.</p> <p>Temperature softening does not occur since the tubes are washed by sodium.</p> <p>Wastage is not as rapid as that for the intermediate leak rates.</p> <p>Pressure buildup in the gas space is very slow.</p>

Since the BUG 2 mathematical model used in these analyses to predict the bubble pressure could not predict penetration of the gas into the sodium slug, the times used for arrival of the bubble at the gas space are probably conservative. No consideration, however, was given to the possibility of large quantities of sodium falling back into the reaction zone since this could not be predicted by the existing models.

Since time precluded the analysis of cases for leak rates other than those picked for the six cases, the 1 lb/sec initial rate may not result in the maximum accident. Leak rates below 0.03 lb/sec were not examined; (since no wastage experiments were run below 0.03 lb/sec) however, it is conceivable that rates below this value could cause a different pattern of wastage which could result in a more severe accident. Interpolation or extrapolation of the results of the six cases was impossible since too many variables entered into the analysis.

In reviewing the uncertainties in the various components of this analysis, it is estimated that a factor of confidence of ten might be applied to the results in order to balance these uncertainties. Thus a maximum resultant leak rate on the order of approximately 450 lb/sec might be used for evaluating the unit rather than the 44.4 lb/sec calculated.

7-2. REVIEW OF DESIGN FROM THE STANDPOINT OF A SODIUM-WATER REACTION

Any design of a sodium-heated steam generator must be a compromise between heat transfer efficiency, reliability, economics, and safety considerations. The B&W Full-Size Steam Generator is no exception and, therefore, is not ideally designed for a sodium-water reaction. The high pressures predicted by the analysis are the transient effects produced by the sudden buildup of gas under a large liquid head. To minimize these pressures, therefore, the head above any reaction area should be as small as possible and the area of the path available to the free surface should be large. Also, the frictional resistance between a potential reaction region and the surface should be minimized.

The greatest head of sodium above a potential reaction area in the Full-Size Steam Generator is 40 feet. This results in a large mass of sodium that must be accelerated during a reaction. Also, the steam generator is divided vertically by shrouds and tube supports into regions of relatively small cross-sectional area. The presence of tube supports, convection baffles, and tubes create a tortuous path through which the sodium must move for reactions in many regions.

B&W has found that the one, large, unrestricted path for reaction relief, the center pipe, will probably collapse inwardly from reactions in the steam generator. The collapse could be in such a way as to render the

center section unavailable as a relief path. This leaves the path through the boiler and superheater tube bundles as the only relief channel of any significance. This path, although of relatively large area, contains significant frictional resistance to flow.

From a leak propagation standpoint, the unit is not ideally designed since the helically coiled tube bundle enables a leak to be propagated to all the other tubes in the steam generator in a relatively small area. Thus the reaction products of a leak originating in one segment of a tube bundle will contact all the other tubes in the unit as they pass vertically upward through the segment. If the natural vertical path of the reaction products could be so designed as to encompass a limited number of tubes, the propagation of the leak could be immediately limited to the number of tubes present in the segment.

8. RECOMMENDATIONS

8-1. ANALYSIS

8-1.1. PRESSURE ANALYSIS

There are several aspects of the mathematical analysis which could be improved. In particular, the assumption that the water reacts with the sodium instantaneously should be refined. Sodium-water reaction experiments are characterized by several pressure spikes soon after water injection, indicating that a significant amount of water may be injected into the sodium before being reacted. Thus, if the kinetics of the reaction are to be resolved, an extensive experimental and analytical program would be necessary.

Other assumptions that could be refined by experimental programs are:

- (a) The reaction is according to the equation $2 \text{ Na} + \text{H}_2\text{O} \rightarrow \text{Na}_2\text{O} + \text{H}_2$
- (b) The leak rate of water and steam is not a function of the reaction pressure.

The results from the air-water tests indicated that the mathematical model was conservative. A continued experimental effort could be made to determine the scaling laws applicable to the bubble pressure and bubble penetration of the liquid to facilitate the use of the experimental data to modify the mathematical model and to permit modeling to be used as a technique in evaluating mechanical damage due to pressure.

8-1.2. LEAK PROPAGATION

To perform an accurate analysis for leak propagation, a much greater understanding of the various mechanisms of propagation must be acquired. Existing experimental data on wastage is quite sparse and the tests have not yielded results which are reproducible. Also, the wastage effects from leaks in large volumes of flowing sodium have not been duplicated. The existing Small Leak Test Program at APDA, (AEC Contract AT(11-1)-865, Project Agreement No. 10) will furnish additional data on wastage and the effect of various parameters. If a full understanding of wastage, as applied to the Full-Size Steam Generator, is to be acquired, some tests covering large leaks, and further tests covering small leaks in large volumes of sodium, should also be conducted. Data on the maximum and

minimum leak rates for which wastage will still occur and the distribution and region of influence of the wastage are necessary to conduct an accurate analysis of leak propagation.

Very little usable data on temperature softening is now available. The present analysis is based entirely on a hypothetical model of the reaction. Tests specifically designed to give temperature softening data such as reaction zone temperatures and outside heat transfer coefficients are necessary. The effect of leak rate, pressure, containment geometry, and sodium flow conditions must be determined in order to develop a realistic model of what occurs in the reaction zone.

Since penetration of the gas bubble into the gas space is a very important event in the pressure determination and leak propagation analysis, the use of the combined piping and finite-difference model to refine this prediction might be desirable.

Based on the leak propagation analysis, several additional recommendations are proposed in connection with the installation and operation of the unit. They are:

- (a) The bursting pressure of the rupture disk and the relief valve pressure should be set as low as possible without hampering normal operation of the unit.
- (b) The gas volume could be reduced to allow for more rapid detection of a leak. However, the volume should not be reduced to less than several hundred cubic feet.
- (c) A means of rapidly dumping one or both of the reactants from the steam generator when a leak is detected is necessary for safe operation of the unit.
- (d) A relief system should be provided for the unit which can relieve the products of a sodium-water reaction from a multiple-tube rupture with a minimum of pressure drop.
- (e) The unit should never be pressurized with water or steam at no-flow conditions.
- (f) The superheater section should never contain liquid water.

8-2. DESIGN

Several design recommendations were made to B&W as a result of a preliminary review of the Full-Size Steam Generator. The tube supports and convection baffles in the downcomer and riser regions have been changed by

B&W to a more uniform spacing as a result of these recommendations. B&W also has explored the recommendation of eliminating the sodium from the center pipe to reduce the inertial effects of reactions near the bottom of the unit. It was determined, however, that the center pipe would probably collapse during a reaction possibly eliminating this area as a reaction relief path. Further recommendations were that the width of the boiler downcomer annulus should be increased to provide a "chimney" for the relief of a reaction in this area, that full communication should be provided between sectors of the boiler tube bundle, that the lifting forces on the tube bundles should be checked to determine whether resulting movement causes further tubes to leak, and that the possibility of internal pieces of the unit penetrating the wall from movement induced by the reaction should be investigated.

As a result of the analyses presented in this report, the following recommendations are made:

- (a) Alternatives, as stated below, should be explored which will assure that the center pipe area will remain available for relief throughout the duration of a reaction:

The pipe could be reinforced so that it will not collapse from the pressure differential created by a reaction.

The pipe could be designed to rupture locally rather than deform to block the reaction relief.

The pipe could be designed to collapse completely throughout its entire length providing a large, open area for reaction relief.

- (b) Provision should be made to pressurize the center pipe with gas, providing this is consistent with any design change resulting from Recommendation (a).
- (c) A rupture disk should also be provided in the gas space at the top of the center pipe if the pipe is pressurized.
- (d) The reaction-induced movement of shrouds, tube supports, and other internals of the steam generator should be checked to determine whether tube failures will result.
- (e) In the stress analysis of the shell, consideration should be given to the following:

The possibility of fragments of various internal structures accelerated by the reaction pressure effects penetrating the shell wall.

The local force on the wall exerted by the supports between the liner and the wall.

The possibility of the movement of both tube bundles from the reaction pressures causing failure of the shell.

The effect of the sodium moving at a high velocity impinging on the upper head of the steam generator causing it to fail.

APPENDIX A. DERIVATION AND METHOD OF SOLUTION OF MATHEMATICAL MODELS FOR REACTION PRESSURE

A-1. BUG 1

A-1.1. DERIVATION OF THE MODEL

In BUG 1, the finite-difference mathematical model, the hydrogen bubble is allowed to grow in all directions from a point source. A discussion of the model, including a brief description and the assumptions used, is given in Section 5-1.

Additional assumptions are

The hydrogen and sodium phases remain discrete and there is no mixing.

The static pressure of the gas acts on the gas-liquid interface.

The liquid is noncompressible and nonviscous (frictional losses were introduced as a later modification of the derivation).

Body (gravitational) forces are neglected.

The resulting flow equation, shown below, is given also in Reference 1.

$$\frac{\partial \vec{V}}{\partial t} - \vec{V} \times \vec{W} = - \frac{g_c}{\rho} \nabla P_0 \quad (\text{A. 1})$$

where

g_c = conversion factor, $\text{lb}_m\text{-ft}/\text{lb}_f\text{-sec}^2$

P_0 = total pressure, $P + \frac{\rho V^2}{2g_c}$, lb/ft^2

V = velocity, ft/sec

t = time, sec

ρ = density, lb/ft^3

W = $\nabla \times \vec{V}$

Flow which is originally irrotational will remain irrotational for a nonviscous fluid, so that

$$\vec{W} \equiv \nabla \times \vec{V} = 0 \quad (\text{A. 2})$$

Equation (A. 1) reduces to

$$\frac{\partial \vec{V}}{\partial t} = - \frac{g_c}{\rho} \nabla P_0 \quad (\text{A. 3})$$

Equation (A. 3) may be expressed in Cartesian form:

$$\begin{aligned} \frac{\partial V_x}{\partial t} &= - \frac{g_c}{\rho} \frac{\partial P_0}{\partial x} \\ \frac{\partial V_y}{\partial t} &= - \frac{g_c}{\rho} \frac{\partial P_0}{\partial y} \\ \frac{\partial V_z}{\partial t} &= - \frac{g_c}{\rho} \frac{\partial P_0}{\partial z} \end{aligned} \quad (\text{A. 4})$$

where

$$\begin{aligned} x, y, z &= \text{distance along Cartesian coordinates, ft} \\ V_x, V_y, V_z &= \text{velocity component in x, y, and z directions, ft/sec} \\ P &= \text{pressure, lb/ft}^2 \end{aligned}$$

Differentiating the first equation in the set with respect to x, the second with respect to y, and the third with respect to z, and adding the results:

$$\frac{\partial^2 V_x}{\partial t \partial x} + \frac{\partial^2 V_y}{\partial t \partial y} + \frac{\partial^2 V_z}{\partial t \partial z} = - \frac{g_c}{\rho} \left(\frac{\partial^2 P_0}{\partial x^2} + \frac{\partial^2 P_0}{\partial y^2} + \frac{\partial^2 P_0}{\partial z^2} \right) \quad (\text{A. 5})$$

From continuity:

$$\frac{\partial V_x}{\partial x} + \frac{\partial V_y}{\partial y} + \frac{\partial V_z}{\partial z} = 0 \quad (\text{A. 6})$$

Differentiating Equation (A. 6) with respect to t:

$$\frac{\partial^2 V_x}{\partial x \partial t} + \frac{\partial^2 V_y}{\partial y \partial t} + \frac{\partial^2 V_z}{\partial z \partial t} = 0 \quad (\text{A. 7})$$

The terms in Equations (A.5) and (A.7) are exact differentials; therefore, the order of differentiation may be reversed. Combining these two equations, Equation A.8 results.

$$\frac{\partial^2 P_0}{\partial x^2} + \frac{\partial^2 P_0}{\partial y^2} + \frac{\partial^2 P_0}{\partial z^2} = 0 \quad (\text{A.8})$$

To calculate a numerical value for the flow, Equations (A.4) and (A.8) may be converted to finite-difference form. Using Equation (A.4), the velocity during a time interval can be calculated from the total pressure distribution. The new total pressure distribution at the start of the next time interval can be calculated from Equation (A.8).

A-1.2. METHOD OF SOLUTION

In setting up a computer program to apply these equations, Cartesian coordinates were used. Equal node dimensions were used in the x and y directions, and the gas-liquid interface for a boundary node was assumed to remain at the node center until the node emptied, at which time the boundary was shifted to the centers of the adjacent nodes.

To analyze the air-water tests described in Appendix C, the circular cylindrical test section was approximated by a square cylinder of equal dimensions. The node arrangement for the preliminary air-water model is shown in Figure A.1 and that for the advanced model is shown in Figure A.2. One-eighth of the cross section is shown. The gas is introduced at node 1.

Figure A.3 shows a simplified flow chart indicating the order in which the calculations are made in the three BUG programs. Although the order of calculations differs somewhat among the three programs, the flow chart serves as a general example.

In the BUG 1 program, which solves the three-dimensional, finite-difference problem, the initial bubble pressure is calculated from the input static bubble pressure versus time curve. Using atmospheric pressure for the pressure acting on the liquid level, the mesh is relaxed by the Leibmann iteration method to obtain the initial pressure distribution. This pressure distribution is printed out and the sequence of calculations for the first time step is begun.

At this point the program checks for termination and will terminate on any of the following conditions:

Maximum input time has been exceeded.

The pressures have not converged in a specified number of iterations.

The liquid level has exceeded the mesh spacing.

The bubble boundary has approached the liquid level.

If the program is to continue, the new velocities in the negative and positive directions are calculated based on the pressure distribution of the previous time step. Knowing these velocities, the growth of the bubble and new height of the liquid level can be calculated. The bubble growth and new liquid level are calculated from the changes in the volume of the nodes surrounding the bubble and the liquid level, respectively.

Due to the finite nature of the time step and mesh spacing, a node will never exactly empty or fill. The excess volume (negative in the case of bubble nodes over-emptying) is then distributed to its neighbor nodes in proportion to the flow between the emptied node and its neighbor.

After calculating the static bubble pressure from the input curve, the total pressure of the nodes on the bubble boundary and liquid level can be calculated. The mesh is then relaxed to determine the new pressure distribution, thus ending the calculations for the time step. The BUG 1 program assumes frictionless flow.

No rigorous analysis was done to determine the effect of the time step size on truncation error or stability. However, it was intuitively felt that the truncation error was tolerable and that the solution was stable if the time was such that no node could completely empty in one time step. As a check, similar problems were run with two different time steps. The solutions compared favorably.

A-2. BUG 2

A-2.1. DERIVATION OF THE MODEL

The BUG 2, a one-dimensional, transient piping model, is described briefly in Section 5-1.

The equations used in the BUG 2 program are derived below.

For a pipe of differential length:

$$\frac{\partial V}{\partial t} = - \frac{g_c}{\rho} \frac{\partial P'}{\partial x} \quad (A.9)$$

and for the entire length of pipe:

$$\frac{\partial V}{\partial t} = - \frac{g_c}{\rho} \frac{\Delta P'}{\Delta x} \quad (A.10)$$

Where V in Equations (A.9) and (A.10) is the average velocity in the pipe expressed by

$$V = \frac{Q}{A\epsilon} \quad (A.11)$$

Substituting (A.11) into (A.10), Equation (A.12) results:

$$\frac{\partial Q}{\partial t} = - \frac{g_c}{\rho} A\epsilon \frac{\Delta P'}{\Delta x} \quad (A.12)$$

From continuity at a junction of several pipes:

$$\Sigma Q = 0 \quad (A.13)$$

which when applied to Equation (A.12) yields

$$\Sigma A\epsilon \frac{\Delta P'}{\Delta x} = 0 \quad (A.14)$$

where $\Delta P'$ can be expressed as

$$\Delta P' = \Delta P + \frac{\rho}{2g_c} \Delta V^2 - \Delta P_f$$

Equation (A.14) gives the P' distribution at the start of any time step.

Equation (A.10), expressed in finite-difference form, gives the velocity change during the time step.

where

A	=	cross-sectional area of channel,	ft^2
g_c	=	acceleration of gravity,	ft/sec^2
P	=	static pressure,	lb/ft^2
ΔP_f	=	frictional pressure drop,	lb/ft^2
P'	=	total pressure,	lb/ft^2
Q	=	fluid volume flow rate,	ft^3/sec
t	=	time,	seconds
V	=	fluid velocity,	ft/sec

x = distance along channel, ft
 ϵ = fluid fraction in channel
 ρ = fluid density, lb/ft³

A-2.2. METHOD OF SOLUTION

Referring again to Figure A.3, the order of calculations for BUG 2 is much the same as that for BUG 1. The initial pressure distribution in the piping network is calculated knowing the input pressures of the bubble, gas space, and center pipe (if different than the gas space pressure).

The calculations for the first time step are begun by calculating the sodium velocities in each channel followed by the change in volume of the end channels (gas space, and those adjacent to the reaction). After calculating the static pressures in the gas space and bubble, the dynamic pressures next to these regions are calculated.

If any channels have over emptied, the excess volume is distributed, in proportion to the flow, to the connecting neighbor channels which flow away from the connection. If a connecting channel flows into the connection, that channel is taken out of the calculation, and its flow set equal to zero.

Now the friction pressure drops in each pipe are calculated and the piping network is relaxed to determine the pressure distribution.

Again there was no rigorous analysis done on stability or truncation error; however, a similar procedure to that of BUG 1 was used.

A-3. BUG 3

This model is a combination of BUG 1 and BUG 2 with the addition of a friction term in the finite-difference part of the model.

A-3.1. DERIVATION OF THE MODEL

The finite-difference flow equations developed for the BUG 1 model were for a frictionless system. Frictional losses are calculated for steady, one-dimensional flow in any direction from the following equation:

$$-\frac{\partial P_f}{\partial n} = f(V) \quad (A.15)$$

A new pressure term, P' , is defined as

$$P' = P_0 - P_f \quad (A.16)$$

Equations (A.4) and (A.8) of Section A-1 are rewritten with P' replacing P_0 in order to include frictional losses in the flow equations and Equation (A.17) results:

$$\frac{\partial V_x}{\partial t} = - \frac{g_c}{\rho} \frac{\partial P'}{\partial x} \quad (A.17)$$

Similar equations are written for the y and z directions:

$$\frac{\partial^2 P'}{\partial x^2} + \frac{\partial^2 P'}{\partial y^2} + \frac{\partial^2 P'}{\partial z^2} = 0 \quad (A.18)$$

Gravitational forces are neglected.

A-3.2. PROGRAMMING THE MODEL FOR THE FULL-SIZE UNIT

For a leak taking place in the boiler downcomer annulus, the Full-Size Steam Generator was divided into regions as shown in Figure A.4. The boiler downcomer annulus was divided into seven vertical regions (two half regions at top and bottom with six full regions between) and eight circumferential regions as shown. The bottom plenum, Region 8, was assumed to be bypassed by the flow and, therefore, the sodium flow or the bubble uniformly distributed to all the connecting regions so that the remaining regions were represented by a one-dimensional piping network. The gas space and center pipe pressures were equalized by holes in the top of the center pipe. In the absence of detailed information on the piping beyond the rupture disk in the center pipe, the center pipe was arbitrarily extended at the same diameter.

The free volumes of each of the regions are tabulated below:

<u>Region</u>	<u>Volume, ft³</u>
1	11.76
2	292
3	328
4	1030
5	542
6	320
7	186.8
7'	374
8	0 (Bubble can reach adjacent regions without first expelling sodium from Region 8)
9	1376

A sketch of Region 1 is shown in Figure A.5. The flow in the vertical direction was assumed to follow the path shown, making a 90° turn at each baffle. The frictional pressure drops for parallel flow along the tube bundle were calculated using the equivalent diameter and the pressure drop correlation for flow within tubes. The losses were found to be negligible compared with the cross flow and turn losses.

The pressure drops for cross flow past the tube bundle were calculated using the correlation for in-line tubes given on page 41 of Reference 2. The curve may be represented by the equation:

$$\Delta P = \frac{1.32 N (\rho V_{\max}^2 / 2g_c)}{(D_c V_{\max} \rho / \mu)^{0.2}} \quad (A.19)$$

The average clearance was used in the calculations. Since the spacing between tubes is less than the over-all average spacing, and a relatively large space exists between the inner most tube and the inner wall, flow would tend to bypass the tube bundle and the pressure drops would be expected to be lower than those calculated.

For horizontal flow, the cross flow correlation Equation (A.19) was also used. For flow past the triangular tube support braces, the flow area was assumed to be reduced to that at the brace for an average of 3-1/3-tube rows. For flow past the horizontal support bars, flow paths were assumed which took into account the flow blockage by the support bars.

Turn losses and pressure losses resulting from branching flow, which occurs whenever the over-all flow through the region is neither purely vertical nor purely horizontal, were calculated using Vazsonyi's correlation.³ The correlation is semi-empirical and has some internal inconsistencies. It was developed for branching ducts and its applicability to the present situation is open to question. However, it is believed to lead to conservative results because the flow around the baffles will tend to be smooth instead of following the series of 90° turns assumed in the calculations.

For 90° branches, Vazsonyi's correlation reduces to the following equations for the loss in total head:

$$\begin{array}{c} 2 \\ \uparrow \\ a) 0 \rightarrow \rightarrow 1 \end{array} \quad \Delta P_{01} = \frac{0.36}{2\rho g_c} \left(\frac{W_0}{A_0} - \frac{W_1}{A_1} \right)^2 \quad (A.20)$$

$$\Delta P_{02} = \frac{1}{2\rho g_c} \left(\frac{W_0^2}{A_0^2} + \frac{1.1 W_2^2}{A_2^2} - \frac{0.366 W_0 W_2}{A_0 A_2} \right) \quad (A.21)$$

b) $1 \rightarrow \overset{2}{\downarrow} \rightarrow 0$

$$\Delta P_{10} = \frac{1}{2\rho g_c} \left[\frac{0.95W_1^2}{A_1^2} + \frac{W_0^2}{A_0^2} - \frac{2}{A_0} \left(\frac{W_1^2}{A_1} + \frac{0.203W_2^2}{A_2} \right) \right] \quad (A.22)$$

$$\Delta P_{20} = \frac{1}{2\rho g_c} \left[\frac{0.60W_2^2}{A_2^2} + \frac{W_0^2}{A_0^2} - \frac{2}{A_0} \left(\frac{W_1^2}{A_1} + \frac{0.203W_2^2}{A_2} \right) \right] \quad (A.23)$$

c) $1 \rightarrow \overset{0}{\uparrow} \leftarrow 2$

$$\Delta P_{10} = \frac{1}{2\rho g_c} \left[\frac{0.60W_1^2}{A_1^2} + \frac{W_0^2}{A_0^2} - \frac{0.406}{A_0} \left(\frac{W_1^2}{A_1} + \frac{W_2^2}{A_2} \right) \right] \quad (A.24)$$

$$\Delta P_{12} = \frac{0.60}{2\rho g_c} \left(\frac{W_1^2}{A_1^2} - \frac{W_2^2}{A_2^2} \right) \quad (A.25)$$

When a flow direction was normal to the tubes, the area used in the above calculations was the free area in the space between tube rows rather than the restricted area at the tube row.

For the other annular regions, Regions 2 and 3, similar calculations were made, except that only vertical flow through the region was considered. In the absence of a horizontal flow component, the branch-loss equations become 90° turn-loss equations. One of the internal inconsistencies in Vazsonyi's correlation is the lack of agreement between Equations (A.21) and (A.23) when $W_1 = 0$ and the subscripts 0 and 2 in Equation (A.23) are interchanged. For the present calculations, Equation (A.21) was used which led to more conservative results. The frictional and turn losses were combined into a single term with the exponent on flow between 1.8 and 2.0.

For Regions 4 and 6, the boiler and superheater regions, only the cross flow pressure drop relation was used; while in Region 7, the center pipe, the frictional pressure drop equation for flow in a pipe was applicable.

The frictional pressure drop equations calculated for the various regions are tabulated below (pressures in psi, W in lb/sec):

Region

1: a) For $W_h > 2W_v$

$$\begin{aligned}\Delta P_{h,f} = & 2.389 \times 10^{-5} W_h^{1.8} + 7.167 \times 10^{-6} (W_h + 2W_v)^{1.8} \\ & + 7.167 \times 10^{-6} (W_h - 2W_v)^{1.8} + 1.722 \times 10^{-6} W_h^2 \\ & - 2.299 \times 10^{-6} W_v^2 + 3.059 \times 10^{-6} W_h W_v\end{aligned}\quad (A.26)$$

$$\begin{aligned}\Delta P_{v,f} = & 1.434 \times 10^{-5} (W_h + 2W_v)^{1.8} - 1.434 \times 10^{-5} (W_h - 2W_v)^{1.8} \\ & + 1.124 \times 10^{-5} W_v^2 + 6.910 \times 10^{-6} W_v W_h - 1.375 \times 10^{-6} W_h^2\end{aligned}\quad (A.27)$$

b) For $W_h < 2W_v$

$$\begin{aligned}\Delta P_{h,f} = & 2.389 \times 10^{-5} W_h^{1.8} + 7.167 \times 10^{-6} (2W_v + W_h)^{1.8} \\ & - 7.167 \times 10^{-6} (2W_v - W_h)^{1.8} + 1.562 \times 10^{-6} W_h^2 \\ & - 5.576 \times 10^{-7} W_v^2 + 3.104 \times 10^{-6} W_h W_v\end{aligned}\quad (A.28)$$

$$\begin{aligned}\Delta P_{v,f} = & 1.434 \times 10^{-5} (2W_v + W_h)^{1.8} + 1.434 \times 10^{-5} (2W_v - W_h)^{1.8} \\ & + 2.39 \times 10^{-5} W_v^2 + 1.708 \times 10^{-6} W_v W_h + 7.694 \times 10^{-7} W_h^2\end{aligned}\quad (A.29)$$

$$2: \quad \Delta P_f = 5.942 \times 10^{-6} W^{1.90} \quad (A.30)$$

$$3: \quad \Delta P_f = 1.008 \times 10^{-5} W^{1.863} \quad (A.31)$$

$$4: \quad \Delta P_f = 1.619 \times 10^{-7} W^{1.8} \quad (A.32)$$

5: Neglect friction.

$$6: \quad \Delta P_f = 6.003 \times 10^{-7} W^{1.8} \quad (A.33)$$

$$7: \quad \Delta P_f = 6.035 \times 10^{-8} W^{1.8} \quad (A.34)$$

$$7': \quad \Delta P_f = 1.207 \times 10^{-7} W^{1.8} \quad (\text{A.35})$$

9: Neglect friction.

The total pressure, P_0 , is calculated from

$$P_0 = P + \left(\frac{\rho V^2}{2g_c} \right)_{\text{vol ave}} \quad (\text{A.36})$$

$$= P + \frac{1}{2g_c \rho} \frac{\sum \left(\frac{W^2}{\epsilon^2 A^2} \right) (LA\epsilon)}{\sum (LA\epsilon)} \quad (\text{A.37})$$

where

A = total area in ft^2

ϵ = liquid fraction in the volume AL

The equation for the various regions are tabulated below (pressure in psi, W in lb/sec):

Region

$$1: \quad P_0 = P + 2.58 \times 10^{-7} W_h^2 + 7.80 \times 10^{-7} W_v^2 \quad (\text{A.38})$$

$$2: \quad P_0 = P + 1.974 \times 10^{-8} W^2 \quad (\text{A.39})$$

$$3: \quad P_0 = P + 1.348 \times 10^{-8} W^2 \quad (\text{A.40})$$

$$4: \quad P_0 = P + 5.610 \times 10^{-10} W^2 \quad (\text{A.41})$$

$$5: \quad P_0 = P + 2.268 \times 10^{-10} W^2 \quad (\text{A.42})$$

$$6: \quad P_0 = P + 2.593 \times 10^{-9} W^2 \quad (\text{A.43})$$

$$7: \quad P_0 = P + 8.736 \times 10^{-8} W^2 \quad (\text{A.44})$$

$$7': \quad P_0 = P + 8.736 \times 10^{-8} W^2 \quad (\text{A.45})$$

$$9: \quad P_0 = P + 1.296 \times 10^{-10} W^2 \quad (\text{A.46})$$

For each region, which is assumed to consist of several parallel paths, each consisting of a number of paths in series

$$\frac{\Delta W}{\Delta t} = -\Delta P' \sum \left[\frac{1}{\sum \left(\frac{L}{\epsilon A} \right)_{\text{series}}} \right]_{\text{parallel}} \quad (\text{A.47})$$

Values of P' are calculated from Equation (A.16) and the flow equation for each region is calculated from Equation (A.17). The flow equations for the various regions are tabulated below (pressures in psi; flow in lb/sec):

Region

$$1: \quad \frac{\Delta W_v}{\Delta t} = -498.24 \Delta P'_v \quad (\text{A.48})$$

$$\frac{\Delta W_h}{\Delta t} = -1944 \Delta P'_h \quad (\text{A.49})$$

$$2: \quad \frac{\Delta W}{\Delta t} = -1489.92 \Delta P' \quad (\text{A.50})$$

$$3: \quad \frac{\Delta W}{\Delta t} = -1920 \Delta P' \quad (\text{A.51})$$

$$4: \quad \frac{\Delta W}{\Delta t} = -15,206.4 \Delta P' \quad (\text{A.52})$$

$$5: \quad \frac{\Delta W}{\Delta t} = -71,769.6 \Delta P' \quad (\text{A.53})$$

$$6: \quad \frac{\Delta W}{\Delta t} = -10,598.4 \Delta P' \quad (\text{A.54})$$

$$7: \quad \frac{\Delta W}{\Delta t} = -541.44 \Delta P' \quad (\text{A.55})$$

$$7': \quad \frac{\Delta W}{\Delta t} = -270.7 \Delta P' \quad (\text{A.56})$$

$$9: \quad \frac{\Delta W}{\Delta t} = -29,600 \Delta P' \quad (\text{A.57})$$

where

A	= area, ft ³
D _c	= clearance between tubes, ft
g _c	= conversion factor, lb _m -ft/lb _f -sec ²
L	= distance in flow direction, ft
N	= number of rows of tubes
n	= distance, ft
P	= pressure, lb/ft ²
P _f	= frictional pressure, lb/ft ²
P ₀	= total pressure, $P + \frac{V^2}{2g_c}$, lb/ft ²
P'	= P ₀ - P _f , lb/ft ²
t	= time, sec
V	= velocity, ft/sec
V _{max}	= maximum velocity, ft/sec
V _x	= velocity component in x direction, ft/sec
W	= flow rate, lb/sec
x, y, z	= distance along Cartesian coordinates, ft
ε	= liquid fraction in the volume AL
μ	= viscosity, lb/ft-sec
ρ	= density, lb/ft ³

Subscripts

h	= horizontal
v	= vertical

A-3.3. METHOD OF SOLUTION

Since the BUG 3 program is a combination of BUG 1 and BUG 2, the program flow is much the same as that in the other BUG programs and is shown in Figure A.3. The exceptions are in the termination criteria and the fact that there are two sets of equations (general piping and finite-difference) to solve at every step. BUG 3 will terminate on the following conditions:

Maximum time has been exceeded.

Pressures have not converged after a specified number of iterations.

Any channel that was initially only partially full completely fills.

The hydrogen bubble has reached the top of the downcomer annulus.

If the hydrogen bubble reaches the bottom of the downcomer annulus, the program will not terminate; however, the finite-difference nodes below the original point of the reaction are taken out of the calculation. The bubble pressure then acts only on the nodes above the initial reaction point and on the piping network (Regions 4 and 7, Figure A.4) connected to the bottom of the finite-difference region.

Again, no rigorous analysis was done on stability or step size.

A-4. ANALYTICAL SOLUTIONS OF SHORT-TIME PRESSURE TRANSIENTS RESULTING FROM STEP AND RAMP CHANGES IN GAS GENERATION RATE

The equation for the single-path piston model is

$$\frac{m}{g_c A^2} \frac{d^2 V}{dt^2} + \frac{P_{og} (V_{og})^\eta}{(V_{og} - V)^\eta} + P_s = \frac{Q P_a}{V} t \quad (A.58)$$

It is similar to the parallel path equation given in Reference 4 except for the assumption of one path instead of two.

In order to simplify the equation sufficiently to permit an analytical solution, the last three terms on the left hand side of Equation (A.58) (pressure in overhead gas space, sodium head, and friction, respectively) will be neglected. This approximation should be satisfactory at high rates of hydrogen generation, where the inertial term will be much higher than the others. The simplified equation is

$$\frac{m}{g_c A^2} \frac{d^2 V}{dt^2} = \frac{Q P_a}{V} t \quad (A.59)$$

The boundary conditions are

$$V = 0 @ t = 0 \text{ (hydrogen volume initially zero)}$$

$$\frac{dV}{dt} = 0 @ t = 0 \text{ (velocity of sodium initially zero)} \quad (A.60)$$

It may be found by inspection that the above differential equation and boundary conditions are satisfied by

$$V = \left[\frac{4g_c Q P_a A^2}{3m} \right]^{1/2} t^{3/2} \quad (\text{A. 61})$$

Equation (A. 61) is for a step change in the rate of hydrogen generation, i.e., the instantaneous rupture of some number of tubes. For the case of a ramp change, tubes rupturing at a constant rate with the rate of hydrogen generation increasing uniformly, the rate of hydrogen generation at time t is

$$Q = \int_0^t Nq \, dt = Nqt \quad (\text{A. 62})$$

The total hydrogen generated is

$$\int_0^t Q \, dt = \int_0^t Nqt \, dt = 1/2 Nqt^2 \quad (\text{A. 63})$$

The hydrogen pressure is

$$P_h = \frac{P_a Nqt^2}{2V} \quad (\text{A. 64})$$

Neglecting the overhead gas pressure and the sodium head as before, the variation of hydrogen volume with time is

$$\frac{m}{g_c A^2} \frac{d^2 V}{dt^2} = \frac{P_a Nqt^2}{2V} \quad (\text{A. 65})$$

$$V = 0 \text{ @ } t = 0$$

$$\frac{dV}{dt} = 0 \text{ @ } t = 0 \quad (\text{A. 66})$$

Equations (A. 65) and (A. 66) are satisfied by

$$V = \left[\frac{g_c P_a Nq A^2}{4m} \right]^{1/2} t^2 \quad (\text{A. 67})$$

The hydrogen pressures calculated from Equations (A. 61), (A. 64) and (A. 67) are

For step change:

$$P_h = \frac{Q P_a}{V} t = \left[\frac{3m Q P_a}{4g_c A^2 t} \right]^{1/2} \quad (\text{A. 68})$$

For ramp change:

$$P_h = \frac{P_a N q t^2}{2V} = \left[\frac{m P_a N q}{g_c A^2} \right]^{1/2} \quad (\text{A.69})$$

By equating Equations (A.68) and (A.69), the two cases will give the same pressure at time,

$$t = \frac{3Q}{4Nq} \quad (\text{A.70})$$

at which time, the total number of tubes ruptured for the ramp change is three-quarters of the number for the step change. The bubble pressure varies as (time)^{-1/2} for a step change and remains constant during a ramp change.

where

- A = cross-sectional area of the sodium over which the pressure differential acts, ft²
- g_c = conversion factor, lb_m-ft/lb_f-sec²
- m = mass of sodium column, lb_m
- N = rate at which tubes fail, sec⁻¹
- P_a = atmospheric pressure, lb_f/ft² absolute
- P_f = frictional pressure term, lb_f/ft² absolute
- P_{og} = initial pressure in gas space, lb_f/ft² absolute
- P_h = hydrogen pressure at any time t, lb_f/ft² absolute
- P_s = sodium head, lb_f/ft²
- q = rate of generation of hydrogen at 1800 F and atmospheric pressure resulting from one tube failure, ft³/sec
- Q = rate of generation of hydrogen at 1800 F and atmospheric pressure, ft³/sec
- t = time, sec
- V = volume of sodium displaced at time t, ft³
- V_{og} = initial volume of gas in space, ft³
- η = ratio of specific heats for argon

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FIG. A.1 BUG 1 NODE ARRANGEMENT, AIR-WATER PRELIMINARY MODEL

FIG. A.2 BUG 1 NODE ARRANGEMENT, AIR-WATER ADVANCED MODEL

FIG. A.3 SIMPLIFIED FLOW CHART FOR THE THREE BUG PROGRAMS

<u>Region</u>	<u>No. of Circumferential Segments</u>
1. Boiler Downcomer Annulus	1 or 8*
2. Boiler Riser Annulus	1 or 8*
3. Superheater Downcomer Annulus	1 or 8*
4. Boiler Tube Bundle	1 or 8*
5. Plenum between Boiler and Superheater Tube Bundles	1
6. Superheater Tube Bundle	1 or 8*
7. Center Pipe	1
7'. Gas Space in Center Pipe	1
8. Sodium Outlet Plenum	1
9. Gas Space	1

*8 Segments were used for region in which leak occurred and 1 segment in remaining regions.

FIG. A.4 ARRANGEMENT OF REGIONS FOR BUG 3 ANALYSIS

FIG. A.5 DETAIL OF REGION 1 - BOILER DOWNCOMER ANNULUS - BUG 3 ANALYSIS

APPENDIX B. DERIVATION OF OUTSIDE HEAT TRANSFER COEFFICIENTS IN THE REACTION ZONE

A detailed derivation of the heat transfer coefficient on the outside of the tubes in the reaction area for the three different models of the hydrogen bubble are presented in this section.

B-1. MODEL 1

This model consisted of a bubble containing hydrogen only.

B-1.1. ASSUMPTIONS

The bubble contained only hydrogen.

The hydrogen flowed over the tubes at the velocity of the bubble interface.

B-1.2. DERIVATION OF THE MODEL

The heat transfer correlation used for this analysis was the Colburn dimensionless equation for gases flowing normal to banks of in-line tubes as shown below:¹

$$\frac{h_m D_o}{K_f} = 0.26 \left[\frac{C_p \mu}{K} \right]_f^{1/3} \left[\frac{D_o G_{\max}}{\mu_f} \right]^{0.6} \quad (B.1)$$

where

- C_p = specific heat of gas, Btu/lb°F
- D_o = tube outside diameter, ft
- $G_{\max}$ = maximum mass velocity through minimum cross section, lb/sec ft²
- h_m = heat transfer coefficient based on length mean temperature difference, Btu/hr ft²°F
- K = thermal conductivity of gas, Btu/hr ft²°F/ft
- μ = viscosity of gas, lb/hr ft

The conditions used for the analysis were

Tube array	boiler tube bundle
No. of tubes	1
Pressure	1500 psia
Velocity	35 ft/sec
Gas temperature	1900 F

The outside heat transfer coefficient calculated from the equation for these conditions was 109 Btu/hr ft² F. Since the pressure and volume in the gas bubble are related by the Ideal Gas Law, the heat transfer coefficient can be made independent of the pressure, volume, or velocity and can become just a function of the leak rate.

B-2. MODEL 2

This model consisted of a hydrogen bubble containing entrained droplets of sodium.

B-2.1. ASSUMPTIONS

Sodium droplets were entrained in the hydrogen bubble.

The droplets traveled at the same velocity as the bubble front.

The reaction proceeded according to the equation

All the heat generated in the reaction was confined to the reaction products and entrained sodium, and the mix was at a uniform temperature.

B-2.2. DERIVATION OF THE MODEL

The outside heat transfer coefficient was determined using a spray-cooling correlation developed for liquid metals at Rocketdyne by Nurick, Seader, and Coultas.² The correlation presented in Reference 2 is

$$\text{Nu} = 0.00334 (R_e C_o)^{0.407} \quad (\text{B.2})$$

where

$$\text{Nusselt Number, } Nu = \frac{h \bar{D}_{20}}{K}$$

$$\text{Reynolds Number, } R_e = \frac{V_{\text{impinging}} \bar{D}_{20} \rho}{\mu}$$

$$\text{Spray concentration or sodium entrainment, } C_o = \frac{\frac{\dot{w}}{A}}{\rho V_{\text{impinging}}}$$

and

A	= impingement area
$\bar{D}_{20}$	= area mean droplet size
h	= heat transfer coefficient
K	= liquid-metal thermal conductivity
$V_{\text{impinging}}$	= impingement velocity
$\dot{w}$	= impinging mass flow rate
μ	= liquid-metal viscosity
ρ	= liquid-metal density

This correlation can be used to determine the outside heat transfer coefficient directly as a function of the spray concentration and velocity if the droplet diameter is known. Since no estimate of this parameter could be made, the droplet diameter was optimized to give the best heat transfer. Target efficiency and heat transfer considerations were used for this optimization.

All the droplets in a gas stream which are flowing toward a cylindrical tube will not impinge on the tube because some will be carried around the tube by the deflected gas. The ratio of the droplets impinging on the tube to the droplets moving toward the tube is the target efficiency η and is a function of the separation number, N_{sep} . Values of η as a function of N_{sep} for cylinders were taken from Reference 3.

$$N_{\text{sep}} = U_t V_o / g_L D_b \quad (\text{B.3})$$

$$U_t = g_L (D_p')^2 (\rho_p - \rho) / 18 \mu \quad (\text{B.4})$$

where

- D_b = diameter of body being impinged, ft
- D_p = diameter of droplet, ft
- D_p = diameter of droplet, microns
- g_L = acceleration of gravity, ft/sec²
- U_t = terminal settling velocity of the particle under the force of gravity, ft/sec
- V_o = average velocity of gas, ft/sec
- μ = viscosity of gas, lb_m/ft sec
- ρ = density of gas, lb_m/ft³
- ρ_p = density of droplets, lb_m/ft³

since

$$\rho_p \gg \rho$$

then

$$U_t \approx g_L (D_p')^2 \rho_p / 18 \mu \quad (B.5)$$

Since μ does not change greatly with pressure nor ρ_p with temperature in the range of interest, U_t then is only a function of D_p . If a temperature of 2000 F and a pressure of 400 psia are used for determining μ and ρ_p , the following relationship is true:

$$U_t = 4.8 \times 10^{-5} D_p^2 \quad (B.6)$$

For impingement on one-inch tubes, therefore:

$$N_{sep} = 1.79 \times 10^{-5} D_p^2 V_o$$

and

$$\eta = f(1.79 \times 10^{-5} D_p^2 V_o)$$

since

$$h = C \frac{(C_{o_{eff}})^{0.407}}{(\bar{D}_{20})^{0.593}} \text{ and } C_{o_{eff}} = \eta C_o$$

$$\text{and } D_p = \bar{D}_{20}$$

$$h = C \frac{(\eta C_o)^{0.407}}{(D_p)^{0.593}}$$

Choosing a reference case where $D_p = 220$ microns and $\eta = 1$,

$$\frac{h}{h_o} = \frac{(220)^{0.593} (\eta)^{0.407}}{(D_p)^{0.593}} = 24.45 \frac{(\eta)^{0.407}}{(D_p)^{0.593}} \quad (B.7)$$

Plotting h/h_o against D_p for several velocities, the curves peaked at a maximum h/h_o for each velocity at a particular value of D_p . A plot of the optimum particle size and h/h_o versus velocity is shown in Figure B.1.

A temperature of 2000 F was used to determine the sodium properties for the reference case giving

$$h_o = 1,444 (V C_o)^{0.407}$$

or

$$h = \left(\frac{h}{h_o} \right) \times 1,444 (V C_o)^{0.407} \quad (B.8)$$

where

$$h/h_o = f(V) \text{ as defined in Figure B.1}$$

The heat transfer coefficient as a function of velocity V and spray concentration, C_o can thus be determined. A plot of h versus V and C_o is shown on the left hand side of Figure 6.2.

Since the reaction temperature is a function of the amount of entrained sodium, it is also a function of C_o .

Heat of Reaction = Heatup (Reaction Products + Sodium)

or, for the reaction $2 \text{ Na} + \text{H}_2\text{O} \rightarrow \text{Na}_2\text{O} + \text{H}_2$ and a starting temperature of 750 F,

$$-\Delta H = \int_{1210 \text{ R}}^T \left[C_p \text{ products} + \gamma C_p \text{ sodium} \right] dT \quad (\text{B.9})$$

where

ΔH = heat of reaction, Btu/lb-mole

C_p = heat capacity, Btu/lb-mole- $^{\circ}\text{R}$

T = temperature, R

γ = moles of entrained water/moles of water reacted

then

$$72,000 = \int_{1210 \text{ R}}^T \left[(22.22 + 7.5\gamma) + 3.435 \times 10^{-3} T \right] dT \quad (\text{B.10})$$

Since γ is not a function of T , integration yields:

$$T = \frac{-(22.22 + 7.5\gamma) + \sqrt{(22.22 + 7.5\gamma)^2 + 708 + 62.3\gamma}}{3.44 \times 10^{-3}} \quad (\text{B.11})$$

C_o can be found as a function of γ by

$$C_o = \frac{\gamma M_{\text{Na}} / \gamma_{\text{Na}}}{M_{\text{Na}_2\text{O}} / \rho_{\text{Na}_2\text{O}} + M_{\text{H}_2} / \rho_{\text{H}_2}} \gamma \quad (\text{B.12})$$

where

M = molecular weight, grams/g-mole

ρ = density, lb/ft³

ρ' = density at standard conditions, lb/ft³

Since

$$M_{\text{H}_2} / \rho_{\text{H}_2} \gg M_{\text{Na}_2\text{O}} / \rho_{\text{Na}_2\text{O}}$$

and

$$\rho_{\text{H}_2} = \rho'_{\text{H}_2} \frac{T_{\text{std}} P}{T P_{\text{std}}}$$

then

$$C_o = \frac{P\gamma}{28.9T - 3.725 \times 10^{-2} T^2} \quad (B.13)$$

or

$$\gamma = \frac{C_o}{P} (28.9T - 3.725 \times 10^{-2} T^2) \quad (B.14)$$

Since T is a function of γ , and γ is a function of C_o , P , and T , the reaction temperature was determined as a function of C_o and P and is plotted on the right hand side of Figure 6.2. Using both sides of Figure 6.2, therefore, the relationship of h versus T can be determined during a reaction using the values of reaction pressure and bubble front velocity at the time of interest.

B-3. MODEL 3

This model consisted of a hydrogen bubble containing entrained sodium with heat loss to the steam generator tubes.

B-3.1. ASSUMPTIONS

The assumptions for this model are listed in Section 6-1.

B-3.2. DERIVATION OF THE MODEL

If complete mixing in the bubble is assumed, the heat balance at any time θ is

$$\text{Heat Input} = \text{Heatup} + \text{Heat Loss}$$

$$\text{Heat Input} = \frac{\dot{m}}{M} \Delta H \theta \quad (B.15)$$

$$\text{Heatup} = \int_{T_0}^T \frac{\dot{m}}{M} \theta C_p dT \quad (B.16)$$

$$\text{Heat Loss} = \int_0^\theta \frac{UA}{3600} (T - T_T) d\theta \quad (B.17)$$

However,

$$A = C_1 \text{ Vol} = C_1 C_2 \dot{m} \theta \frac{P_{\text{std}} T}{P T_{\text{std}}}$$

Therefore Equation (B.17) becomes

$$\text{Heat Loss} = \int_0^\theta \frac{U C_1 C_2 \dot{m}}{3600 M} \theta \frac{P_{\text{std}} T}{P T_{\text{std}}} (T - T_T) d\theta \quad (\text{B.18})$$

Combining Equations (B.15), (B.16) and (B.18)

$$\frac{\dot{m}}{M} \Delta H \theta = \frac{\dot{m}}{M} \theta \int_{T_0}^T C_p dT + \frac{U C_1 C_2 \dot{m}}{3600 M} \frac{P_{\text{std}}}{T_{\text{std}}} \int_0^\theta \theta \frac{T}{P} (T - T_T) d\theta \quad (\text{B.19})$$

where

- A = area of the tubes at time θ , ft²
- C₁ = area of tubes exposed/volume of bubble, ft²/ft³
- C₂ = hydrogen generation at standard conditions, ft³/lb-mole
- C_p = specific heat of components of bubble, Btu/lb-mole-°R
- ΔH = heat of reaction, Btu/lb-mole
- $\dot{m}$ = mass flow rate of water, lb/sec
- M = molecular weight of water, lb/lb-mole
- P = reaction pressure, psia
- T = reaction temperature, R
- T₀ = initial temperature of reactants, R
- T_T = temperature of tubes, R
- U = over-all heat transfer coefficient, Btu/hr ft²°F
- Vol = bubble volume, ft³
- θ = time, seconds

Since T and P cannot be defined explicitly as a function of θ, the last integral cannot be solved.

In order to obtain a solution of the temperature as a function of time and pressure, the assumption of no mixing from one time interval

to the next was made and the equation for the instantaneous heat balance at any time was solved. The instantaneous heat balance at time θ is

$$\frac{\dot{m}}{M} \Delta H = \frac{\dot{m}}{M} \int_{T_0}^T C_p dT + \frac{U C_1 C_2 \dot{m}}{3600 M} \theta \frac{P_{std} T}{P T_{std}} (T - T_T) \quad (B.20)$$

or

$$\Delta H = \int_{T_0}^T C_p dT + \frac{U C_1 C_2}{3600} \theta \frac{P_{std} T}{P T_{std}} (T - T_T) \quad (B.21)$$

however, if

$$C_p = (22.22 + 7.5\gamma) + 3.435 \times 10^{-3} T$$

(T in R and C_p in Btu/lb-mole $^{\circ}R$)

then

$$\begin{aligned} \Delta H = & 22.22 (T - T_0) + 7.5\gamma (T - T_0) + 1.717 \times 10^{-3} (T^2 - T_0^2) \\ & + \frac{U C_1 C_2}{3600} \theta \frac{P_{std} T}{P T_{std}} (T - T_T) \end{aligned} \quad (B.22)$$

Since

$$\gamma = \frac{C_o}{P} (28.9 T - 3.725 \times 10^{-3} T^2)$$

where

$$\begin{aligned} \Delta H &= 72,000 \text{ Btu/lb-mole} \\ U &= 143 \text{ Btu/hr ft}^2 \text{ } ^{\circ}F \\ C_1 &= 5.0 \text{ ft}^2/\text{ft}^3 \\ C_2 &= 379 \text{ ft}^3/\text{lb-mole} \\ T_T &= 1123 \text{ R} \\ T_0 &= 1210 \text{ R} \end{aligned}$$

$$C_o = \frac{9,600 - 2.96 (T-1210) - 0.275 \theta \frac{T}{P} (T-1123) - 2.29 \times 10^{-4} (T^2 - 1210^2)}{(28.9 T - 3.725 \times 10^{-3} T^2) (T-1210)} P \quad (B.23)$$

Since C_o can be determined as a function of T , P , and θ , a series of curves can be plotted of C_o versus T at constant pressure for various values of reaction time. Figures 6.3 through 6.7 are the curves for reaction times of 10^{-4} , 10^{-2} , 10^{-1} , 1, and 3 seconds, respectively. These curves are used in conjunction with the left hand side of Figure 6.2 which gives the heat transfer coefficient versus C_o for various bubble front velocities. Thus a curve of the outside heat transfer coefficient for the tubes versus the reaction temperature can be obtained for any time during a reaction if the reaction pressure and bubble front velocity are known.

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FIG. B.1 OPTIMUM PARTICLE SIZE AND h/h_0 VERSUS VELOCITY FOR SODIUM

APPENDIX C. AIR-WATER MODEL TESTS

C-1. PURPOSE OF THE TESTS

The Air-Water Model Tests were run to determine whether the finite-difference mathematical model, BUG 1, could adequately represent the two-phase flow phenomenon resulting from the rapid formation of a gas in a liquid. High-pressure air or helium was injected into the bottom of a cylinder of water to produce flow conditions similar to those present during a reaction. These tests, however, were not meant to mock up a sodium-water reaction in the steam generator.

C-2. DESCRIPTION OF EQUIPMENT AND INSTRUMENTATION

A diagram of the experimental apparatus is shown in Figure C. 1. In general, the equipment consisted of a gas reservoir with a capacity of 40 ft³ coupled through a 1-3/4-inch ID pipe and a solenoid valve to the test section. The initial apparatus, or preliminary model, was a clear acrylic cylinder, 8.4-in. ID x 30 in. high, open at the top. The advanced model was composed of the same type of cylinder with four disk and donut baffles added. Both test sections are shown in Figure C. 1.

The gas bubble pressure was measured by a pressure transducer located near the bottom at the wall of the test section. A pressure transducer was also installed in the gas line just upstream of the solenoid valve. The signal from these transducers was recorded by a light beam galvanometer with a frequency response of 5,000 cps. Typical pressure traces for these transducers are shown in Figure C. 2. High-speed motion pictures (approximately 1500 frames/second) were taken of each test to record the development and movement of the water surface and bubble interface. An 1800 rpm timing motor with an indicator was also included in the range of the camera to provide an accurate calibration of the camera speed. Strain gages attached to the support rods for the baffles in the advanced model were included during these tests to measure the hydraulic forces on the baffles.

The test procedure was as follows: the gas inlet inside of the test section was covered with aluminum foil, and the cylinder was filled approximately half full of water (14 inches above inlet pipe for preliminary model and 17 inches above bottom of cylinder for advanced model). The supply tank was filled with air or helium over a pressure range of 40 to 100 psig. The timing motor, oscillograph, and motion picture camera were started; the solenoid valve was tripped introducing the high-pressure gas into the test section.

C-3. TEST PROGRAM AND RESULTS

The test conditions for both the preliminary and advanced models are tabulated below:

<u>Test</u>	<u>Model</u>	<u>Gas</u>	<u>Tank Pressure, psig</u>
9026	Preliminary	Helium	40
9030	"	"	60
9031	"	"	80
9032	"	"	100
9022	"	Air	40
9018	"	"	60
9019	"	"	60
9017	"	"	80
9025	"	"	100
9156	"	Helium	40
9162	"	"	60
9164	"	"	80
9167	"	"	100

The liquid surface remained fairly level for the preliminary (un-baffled) model and the velocity of the top of the bubble was about twice that of the liquid surface. The movement of the liquid level and bubble surface, scaled from the movie film, is plotted in Figures C.3 through C.10. The times shown for the movement of the liquid surface and the bubble are measured from the time of the first appearance of the bubble. Reproducibility was good as may be seen from Figure C.8. Apparent bubble volumes were calculated from the photographs and were found to be about twice the actual volume of gas entering the model as determined from the displacement of the liquid surface. This indicates that liquid is entrained in the bubble. Results for one run are shown in Figure C.11.

In the advanced model tests, the top surface of the water did not remain level. The water formed a parabolic or domed profile due to the effect of the baffles. The liquid interfaces could not be distinguished on the movie film beyond the time that the bubble reached the first baffle because there was much mixing thereafter. Therefore, analysis of the advanced models was not pursued. The shape of the bubble and the liquid surface prior to reaching the first baffle is shown in Figures C.12 through C.15.

C-4. ANALYSIS OF RESULTS

The pressure traces from the transducer in the preliminary model were used as the input to the finite-difference mathematical model, described in Appendix A. The mathematical model then predicted the movement and shape of the bubble. The results for several of these cases are plotted

in Figures C. 3, C. 6, and C. 10. The agreement between the experimental data and the predicted values is rather poor. The observed movements are considerably greater than those predicted for the given pressure trace. This indicates that the mathematical model is conservative, in that for a given gas generation rate it would predict higher pressures than would be found experimentally.

The reason for the discrepancy has not been fully established, although several possibilities have been considered:

(a) It is not believed to be caused by the grossness of the mesh used in the finite-difference approximation, since changing the mesh size by a factor of 2 in a test problem did not change the results appreciably (Figure C. 16).

(b) Neglecting friction in the mathematical model should have led to discrepancies in the opposite direction to those actually found.

(c) Time zero for the experimental curves was taken at the time the bubble first appeared on the films; for the predicted curves, it was the time the pressure traces began to deviate from the steady-state values. The pressure traces may have started to change before the bubble appeared; however, shifting the predicted curves to the left in Figures C. 3, C. 6, or C. 10 while bringing the predicted and experimental curves closer together, still does not change the fact that the predicted curves are considerably flatter than the experimental ones.

(d) Only the static pressure of the gas was considered. The kinetic energy of the gas, directed upward, may have been imparted to the liquid without being picked up by the static pressure tap at the wall. As a check as to whether the total available energy of the gas would be sufficient to give the observed movements, the calculations were rerun assuming the bubble pressure to be the same as the static pressure in the supply tank. The predicted curves were shifted above the experimental ones, as shown by comparing Figure C. 16 with Figure C. 6, indicating that the kinetic energy of the gas might be sufficient to account for the discrepancy.

(e) The mathematical model assumes that the gas and liquid phases do not mix. The motion picture films, however, showed considerable mixing. In some cases, the bubble volume consisted of about 50% liquid. To investigate this effect, a computer problem was rerun using the assumption that a boundary node emptied at twice the rate calculated by a mass balance. The results are plotted in Figure C. 3. The calculated bubble height became quite similar to the experimentally determined value. A shift of 0.004 to 0.005 second in the value of time zero would cause the curves to nearly overlap. However, the calculated liquid level was almost unchanged from that previously calculated. Therefore, introducing the added bubble pene-

tration into the calculations does not appear to account for all of the differences observed between the calculated and experimental interface movements.

FIG. C.1 AIR-WATER MODEL TEST APPARATUS

FIG. C.2 TYPICAL PRESSURE TRACE FROM AIR-WATER MODEL TESTS

FIG. C.3 AIR-WATER MODEL TEST - HELIUM AT 40 PSIG

FIG. C.4 AIR-WATER MODEL TEST - HELIUM AT 60 PSIG

FIG. C.5 AIR-WATER MODEL TEST - HELIUM AT 80 PSIG

FIG. C.6 AIR-WATER MODEL TEST - HELIUM AT 100 PSIG

FIG. C.7 AIR-WATER MODEL TEST - AIR AT 40 PSIG

FIG. C.8 AIR-WATER MODEL TEST - AIR AT 60 PSIG

FIG. C.9 AIR-WATER MODEL TEST - AIR AT 80 PSIG

FIG. C.10 AIR-WATER MODEL TEST - AIR AT 100 PSIG

Preliminary Model

Run 9011, 100 psig Helium

$$H_0 = 14"$$

$$V_w = (r^2 \pi)(H - H_0)$$

Curve	Time, Sec	H, Liquid Level, In.	Tot. Vol Increase, In. ³	$\frac{V_g}{V_w}$	Bubble Vol. In. ³
a	0	14	0		0
b	0.0056	14.15	7.55	2.67	20.12
c	0.0112	14.4	20.1	1.87	37.55
d	0.0168	15.06	53.3	1.98	105.94
e	0.0224	15.41	121	1.87	227.49
f	0.0280	19.3	266	2.04	540.88
g	0.0336	21.55	380	1.94	737.49

FIG. C.11 GAS BUBBLE PROFILE - PRELIMINARY MODEL - HELIUM AT 100 PSIG

Run 9156

FIG. C.12 GAS BUBBLE AND WATER SURFACE PROFILES - ADVANCED MODEL - HELIUM AT 40 PSIG

FIG. C.13 GAS BUBBLE AND WATER SURFACE PROFILES - ADVANCED MODEL - HELIUM AT 60 PSIG

FIG. C.14 GAS BUBBLE AND WATER SURFACE PROFILES - ADVANCED MODEL - HELIUM AT 80 PSIG

FIG. C.15 GAS BUBBLE AND WATER SURFACE PROFILES - ADVANCED MODEL - HELIUM AT 100 PSIG

FIG. C.16 EFFECT OF MESH SIZE ON LIQUID LEVEL AND BUBBLE HEIGHT

APPENDIX D. SODIUM-WATER TUBE THINNING TESTS

D-1. PURPOSE OF THE TESTS

In sodium-heated steam generators, a leak in one water tube can cause the successive failure of adjacent tubes by the corrosion-erosion effect of the sodium-water reaction resulting from the injection of water from the primary leak into the surrounding sodium. The purpose of the tests described in this appendix was to ascertain the extent, mode, and pattern of tube damage developed when a controlled amount of water was injected at a fixed rate into sodium and directed at arrays of tubes simulating those in the Full-Size Steam Generator. These tests were carried out under sodium and water conditions simulating flooded operation of the Full-Size Steam Generator.

Since very little time was available for performing these tests, the effects of many variables on wastage had to be ignored. The effects of sodium flow, sodium and water temperatures and pressures, leak geometry and orientation, and baffles were not explored. Also, only a limited number of tests could be performed in the allotted time. It must be emphasized, therefore, that these tests were only scoping in nature and that they were not designed to answer all the unknowns associated with wastage.

In addition to the primary purpose of finding out what happened to the tubes, the test was instrumented to obtain information on the sodium-water reaction temperatures developed in the proximity of the test tubes, the heat transfer coefficient on the outside of the tubes in the reaction zone, and the temperatures developed in the tubes.

D-2. DESCRIPTION OF TEST RIG

A schematic of the test rig is presented in Figure D.1. The primary component of the rig is the reaction vessel which houses the injection device and test tube array. This calrod-heated vessel was fabricated from 10-inch, Schedule 40 carbon steel pipe and stands approximately 6 feet high. A bolted flange is provided at the top of the vessel for disassembly and the vessel support is pivoted at its base so that the whole assembly can be lowered to a horizontal position to facilitate cleaning and disassembly. A 1-inch drain line is provided at the bottom of the tank and several stand pipes were installed at the top of the vessel for instrumentation penetrations into the tank.

A 6-inch Schedule 40 mild steel pipe leads from the top of the vessel to a reaction products receiver tank. This line contains a rupture disk set to burst at 50 psig (at room temperature), and a 2-inch by-pass line around the disk for relief of the reaction products for the low leak rate experiments. The reaction products receiver tank, located approximately 17 feet from the reaction vessel, is filled with transformer oil and is used to collect any sodium and solid reaction products which are carried through the relief line.

The system used to inject water into the sodium consists of a high pressure (6,000 psig) nitrogen supply, a 1600 in.³ surge tank connected through a solenoid valve to a calrod-heated 200 in.³ water reservoir, and an injection nozzle-rupture disk assembly. The design of the stainless steel injection device is shown in Figure D.2. The nozzle consists primarily of an orifice that meters the water and a rupture disk designed to burst at 2100 to 2300 psig at 350 F. The tube between the injection nozzle and water reservoir enters the reaction vessel above the bolted flange and is jacketed to within several inches of the nozzle assembly for insulating purposes. The Kistler quartz pressure transducer included in this line is used to determine the time at which the injection nozzle rupture disk bursts, initiating the reaction. A vent line is provided on the injection water tank to facilitate dumping unreacted water in the case of an incomplete injection.

A system was also provided for pressurizing two of the dummy tubes upon which the water is injected to simulate the internal pressure present in the steam generator tubes. A second pressure transducer, located between the tube pressurizer tank and the tube, was used to determine when a tube failure occurred.

D-3. DESCRIPTION OF TEST SPECIMENS

The test specimens, or dummy tube array, were placed opposite the injection nozzle. Two basic tube arrangements were used: one arrangement simulating the boiler downcomer tubes (Figure D.3) and one arrangement simulating the boiler tube bundle (Figure D.4). The figures indicate the spacing and relation to the injection nozzle of the 2-1/4 Cr - 1 Mo tubes. Also included in the figures is the location of the reaction zone thermocouple. During several of the tests, a thermocouple was attached to the inside wall of the tube next to the reaction zone thermocouple. Also, several tubes were pressurized to simulate the internal tube pressure during operation of the steam generator. Figure D.5 shows the pressurized and instrumented tube design and the location of these tubes during the nine tests. Figure D.6a is a photograph of the tube array and injection nozzle just before it was installed in the reaction vessel. The manifold for the two pressurized tubes, as well as the reaction zone and the tube thermocouples can be seen in the picture.

D-4. TEST PROCEDURE

The injection nozzle and test tube array, including the pressurized and instrumented tubes, were assembled and attached to the jacketed water injection tube. This assembly, which is an integral part of the top of the reaction vessel, was then lowered into the vessel and the flange secured. The rupture disk, relief piping, and water lines were then attached and a pressure test of the injection equipment was made. The reaction vessel was heated to 350 F while it was evacuated, backfilled, and purged to obtain a pure nitrogen atmosphere in the vessel.

The water injection system was evacuated and the water reservoir was filled with 4 pounds of demineralized water under vacuum and a second pressure check of this system was conducted. The reaction vessel was then filled with 75 pounds of sodium. The injection water tank was pressurized to 1450 psig with nitrogen and heated to 350 F; two tubes were pressurized to 2650 psig. The surge tank was then pressurized to 2950 psig and the system was ready for triggering. The high-speed, light beam oscilloscope, which recorded the reaction zone and tube temperatures and the water injection and tube pressures, was energized. The reaction was initiated by opening the remotely operated solenoid valve between the surge tank and water reservoir thus bursting the rupture disk in the injection nozzle. In the event this rupture disk did not burst, the water pressure was relieved through the remotely operated dump valve on the water reservoir.

Following the reaction, the sodium and reaction products were drained. The vessel was then allowed to cool and the tube array was removed and the equipment was cleaned. The removal of a tube array following Test 6 is shown in Figure D.6b. The tubes were removed and examined, and weight loss and diameter measurements were made. These values were then compared with the pretest measurements made on each tube.

D-5. TEST PROGRAM

The test program for the nine tests is presented in Table D.1. The 5/8-inch tubes refer to the boiler downcomer region and the 1-inch tubes to the boiler bundle. The three orifice sizes shown were used to give a large range of water injection rates. The temperature and pressure conditions chosen for the tests simulated flooded operation of the steam generator. These were chosen because the analysis showed that the greatest water leak rate occurred at this time. Since time permitted only one series of 9 tests, no other temperature and pressure conditions were considered.

D-6. TEST RESULTS

The results of the nine tests are summarized in Table D. 2. A brief description of each test and the results of the post-test examination are presented below:

Test 1 (June 4, 1965) 5/8-inch tubes, 0.016-inch orifice

The initial triggering of the injection system produced no reaction. Further pressurization of the water, however, did burst the injection rupture disk, initiating the reaction. After approximately six minutes, the temperature in the reaction zone stopped fluctuating indicating that the reaction had ceased. When the water system was vented, approximately two-thirds of the original charge of water was discharged indicating that the experiment had not gone to completion. When the apparatus was disassembled, the entire tube array and injection nozzle were covered with solid reaction products as shown in Figure D. 7a. Also, a large amount of solid reaction products remained in the reaction vessel as shown in Figure D. 7b. After decontamination, it was found that the injection rupture disk had cracked rather than "orange peeled," and that the crack had plugged with reaction products thus terminating the reaction. The crack in the rupture disk is shown in Figure D. 8.

The tube on which the water jet impinged and the two adjacent tubes were wasted locally. Also, a large area of the impinged tube and the two adjacent tubes was shiny, indicating it was washed by the reaction. Figure D. 9 shows a longitudinal and radial section through the impinged tube at the location of maximum wastage. The thinned area faces the injection nozzle. All other tubes had no significant wastage. Metallurgical examination of the tubes in the thinned region showed no transformation indicating that the tube temperature remained below 1400 F. Also, an examination of the wasted area of the impinged tube showed a uniform loss of material rather than an intergranular type of attack. This is illustrated in the photomicrograph in Figure D. 10.

Test 2 (June 14, 1965) 5/8-inch tubes, 3/32-inch orifice

This test was initiated prematurely because leakage occurred past the triggering valve. The rupture disk in the reaction relief system failed almost instantaneously and most of the sodium was blown into the relief system (Figure D. 8). The major wastage from this test was also on the impinged tube; however, it was in the form of an oval pit about 1/4 of an inch long in the axial direction and 1/8 of an inch wide across the face of the tube. Again, the pit was opposite the injection nozzle. The recording oscillograph was not energized during most of the run due to the premature initiation of the reaction. The thermocouple inside the tube adjacent to the impinged tube broke loose and, therefore, did not register the tube temperature. The maximum reaction zone temperature recorded was 1057 F but this must have occurred during the end or immediately after completion of the water injection. The maximum temperature must

have exceeded 1400 to 1425 F, the austenization temperature of the test tube alloy, because metallographic examination of the metal in the crater area showed that it had undergone considerable microstructural change. Such a change could only result from the alloy having been heated above its austenization temperature and then rapidly cooled. A photomicrograph of the cratered area showing partial transformation is shown in Figure D.11. A hardness traverse of the wasted region, using a Tukon Superficial Hardness Tester, showed that the extent of the transformation varied considerably with the distance from the impinged point. The hardness values for this traverse and their locations are shown in Figure D.12. The increase in hardness, in the absence of cold working, could have been brought about only by heating above approximately 1375 to 1400 F for a short time to produce partial austenization followed by cooling rapidly to below approximately 800 F.

The test tube array was covered with reaction products as shown in Figure D.13a. The injection nozzle rupture disk (Figure D.8), burst completely and did not inhibit the water injection.

Test 3 (June 22, 1965) 5/8-inch tubes, 0.016-inch orifice

For this test, a small pocket of gas was trapped behind the rupture disk in the injection nozzle to insure complete failure of the disk. Triggering difficulties similar to Test 1 were again experienced. Also, the orifice plugged with reaction products after the reaction had run for about 16 seconds (Figure D.13b). Audible reports were heard coming from the reaction vessel during the test, and the temperature trace of the reaction zone was extremely erratic. Examination of the injection rupture disk (Figure D.8) after the test revealed that the disk had broken into three leaves which were bent back under the remaining parts of the disk, presumably due to pressure pulses during the test. The target tube from Test 3 showed a maximum wall thinning of just under 0.003 in. in an area opposite the orifice opening and about 3/8 in. below the vertical centerline. The maximum reaction zone temperature recorded in Test 3 was 1286 F.

Test 4 (June 25, 1965) 5/8-inch tubes, 3/32-inch orifice

Repressurization and triggering similar to that done in Tests 1 and 3 were necessary to rupture the injection nozzle disk and initiate the reaction. This test ran the full four seconds with the water rate of one pound per second. The relief system rupture disk burst, and sodium was blown into the relief piping and tank. Very little sodium was left in the reaction vessel after the reaction. Some reaction products remained on the tube array as shown in Figure D.13c. The

reaction zone temperature averaged about 1800 F during the four seconds, with one peak as high as 2014 F as illustrated in Figure D. 14.

No significant wastage was measured on the tube (Refer to Table D.2). Metallurgical examination of the impinged tube (Figure D. 15) showed no appreciable transformation. A hardness survey of the material using a Tukon hardness machine, however, indicated that some transformation may have occurred on the side of the tube facing the injection nozzle. Microhardness readings taken 18 mils below the surface of the material resulted in a hardness equivalent to Rockwell B 73-75 on the side away from the nozzle and a reading equivalent to Rockwell B 93-95 on the side facing the nozzle. This indicates that the tube area was probably above 1400 F some time during the reaction.

Test 5 (July 15, 1965) 1-inch tubes, 0.016-inch orifice

No triggering problems were encountered; however, the nozzle plugged with reaction products after about 15 seconds (Figure D. 13d). Both previous tests using the small (0.016 inch) orifice also plugged soon after initiation of the reaction. One-inch-diameter target tubes simulating the boiler bundle of the steam generator were used for this test. The characteristic discoloration and wash pattern were found; however, very little wastage resulted. The orifice rupture disk (Figure D. 8) had a diagonal split about 3/8 of an inch long and some flattening of the disk was evident, possibly resulting from pressure pulses from the reaction. The reaction zone and tube temperatures did not rise appreciably.

Test 6 (July 21, 1965) 5/8-inch tubes, 0.025-inch orifice

No difficulties were encountered in triggering this test; however, the reaction products plugged the nozzle after 2 seconds of a planned 40-second test. Upon disassembly and cleaning of the test apparatus, it was found that the injection nozzle rupture disk (Figure D. 8) had opened in a horizontal crack similar to the injection rupture disk in Test 1. Examination of the 5/8-inch tubes showed a wash pattern centered about 3/4 of an inch below the centerline of the injection nozzle; however, no apparent wastage was noted. Also, the reaction zone temperature peaked at a very low value, 530 F. Reaction product buildup on the tube array and injection nozzle is illustrated in Figure D. 16a.

Test 7 (July 24, 1965) 1-inch tubes, 0.025-inch orifice

The orifice plugged about 2 seconds after the initiation of this test. A small build up of reaction products on the nozzle and tube array was found when the reaction vessel was disassembled (Figure D.16b). The rupture disk (Figure D.8), burst leaving a 1/8-inch-diameter hole, the edges of which were collapsed back toward the orifice in several places. This could have been caused by reaction pressure pulses at the nozzle. No significant wastage was found on the tubes since the duration of the test was so short. The reaction zone temperature rose slightly to 413 F and no change in the tube temperature was recorded.

Test 8 (July 28, 1965) 1-inch tubes, 0.025-inch orifice

This test was the only test utilizing the 0.025-in. diameter orifice which ran to completion. All four pounds of water were injected in a span of 49 seconds. The injection nozzle and tube array were covered with an extensive buildup of reaction products (Figure D.16c). Approximately one-third of the injection nozzle rupture disk burst open; however, the segment that burst was on the lower portion of the disk and not directly in front of the orifice. The jet of water, therefore, must have been deflected by the intact portion of the disk (Figure D.8). Three to five mils of wastage were evident on a tube adjacent to the injection nozzle and on one tube in the row farthest from the nozzle. The reaction zone temperature, Figure D.17, varied from above 200 F to 1200 F and was extremely erratic during the first 14 seconds of the reaction. The tube thermocouple registered a maximum temperature of 710 F and maximum rise of 44 F/sec.

Test 9 (August 2, 1965) 1-inch tubes, 3/32-inch orifice

The relief system rupture disk burst immediately upon initiation of the reaction and much of the sodium was blown into the relief system, as had been experienced in the two previous one pound per second water injection rate tests. Also, the injection nozzle rupture disk burst in such a way as to provide a large hole in front of the 3/32-inch orifice (Figure D.8). As in previous tests, the injection nozzle and tube array (Figure D.16d) were covered with reaction products; however, the test ran to completion. The impinged tube was wasted quite uniformly to a depth of 3.4 mils. The reaction zone temperature varied between 200 and 1550 F indicating that water cooling took place in the reaction zone during about 1-1/2 seconds of the reaction (Figure D.18). The tube temperature did not change appreciably.

D-7. DISCUSSION OF RESULTS

D-7.1. WASTAGE

The Sodium-Water Tube Thinning Tests confirmed the high rates of wastage experienced in previous experiments. Wastage rates from 0.1 to 1.0 mils/sec were noted as compared to 0.03 to 5 mils/sec in earlier experiments conducted by The Detroit Edison Company. The tests also showed that wastage could be propagated from one tube bank to another (approximately 2-1/4-inch-centerline spacing) in the boiler downcomer region of the steam generator.

Plugging of the reaction nozzle orifice by reaction products occurred during all three tests which incorporated a 0.016-inch-diameter orifice, and two out of the three tests which incorporated a 0.025-inch-diameter orifice. In two of the tests, Tests 6 and 7, the nozzle plugged during the first 2 or 3 seconds and no measurable wastage was recorded. Future tests that will be conducted at APDA for the AEC will incorporate an injection nozzle of different design and will show what effect the nozzle geometry has on plugging. Incomplete rupture of the injection nozzle rupture disk during three of the tests, Tests 1, 5, and 6, may have induced nozzle plugging. The incomplete ruptures were apparently caused by the drop in pressure during the bursting process caused by water flow through the small diameter orifices. Also, the build up of reaction products around the nozzle probably caused deflection and a gradual reduction of the spray of water into the sodium. This could account for the inconsistent wastage results experienced during the test.

The effect of the two different tube arrays on wastage is inconclusive. Both arrays experienced wastage and both were used in tests where no wastage developed. The type of wastage in the two arrays was different; however, the two tests using the 5/8-inch tubes with wide spacing (2-1/4-in. centerline) resulted in local damage on the impinged tubes and those adjacent to it. The tests on the 1-in. tubes (1-3/8-in. centerline) resulted in fairly uniform damage to one or two tubes located randomly in any of the three rows. This could be due to deflection of the water jet by the impinged tube since the orifice is only 3/8 inch from the tube for the 1-inch tube tests. An optimum spacing may exist for each leak rate which results in the greatest amount of thinning on the impinged tube.

To check the reproducibility of the results, test conditions for three of the tests were duplicated. These tests were:

<u>Test No.</u>	<u>Orifice Dia, Inches</u>	<u>Tube OD, Inches</u>
1 & 3	0.016	5/8
2 & 4	3/32	5/8
7 & 8	0.025	1

The results for each pair of tests were not reproducible (refer to Table D.2). The nonreproducibility could have been caused by the injection device since there was a large variation in the way the injection rupture disks burst.

The distribution of the wastage varied from test to test.* The most tubes effected were the impinged tube and two adjacent tubes in Test 1. The wastage on the adjacent tubes was more uniform and less severe than that on the impinged tube. In Tests 2 and 8, two tubes were effected. In Test 2, wastage on the impinged tube was confined to a pit approximately 5/16 inch in diameter and 28 mils deep directly opposite the injection nozzle. One adjacent tube was thinned more uniformly to a depth of 3 mils. In Test 8, a tube adjacent to the impinged tube and a tube adjacent to the nozzle were thinned fairly uniformly to a depth of 3 to 5 mils. Tests 5 and 9 resulted in uniform damage to one tube only. The above data indicate that the range and distribution of the wastage is difficult to correlate using this data alone. Further extensive testing is necessary to establish a correlation for these variables.

Although most of the sodium was ejected into the relief system during all three of the high water rate tests (1 lb/sec), wastage did occur in two of the tests. The extensive wastage in Test 2, however, could have occurred before the main injection because of the premature reaction caused by valve leakage. The reaction zone temperature apparently did not effect the wastage rate appreciably. No wastage occurred in the test where the reaction zone temperature reached 2000 F. The tests which resulted in the greatest wastage per pound of water experienced maximum reaction zone temperatures of approximately 1000 F. The wastage, when related to the amount of water injected, provides a convenient method of comparing the data from different tests. The wastage per pound of water for the nine sodium-water tube thinning tests ranged from approximately 1 mil/lb for Tests 2, 8, and 9 to approximately 16 mil/lb for Test 1. The same range of values was also experienced in the tests run by DECo. A tabulation of this parameter can be found in Table 6.3 for both the APDA and Detroit Edison tests.

*Losses less than 3 mils were not considered significant since this could occur from removal of scale by sodium.

D-7.2. TEMPERATURE EFFECTS

The maximum reaction zone temperature exceeded the saturation temperature of sodium at atmospheric pressure, 1630 F, during one test only. The reaction zone temperature reached 2000 F during Test 4, a high leak rate test in which most of the sodium was ejected from the reaction vessel. It is possible that the pressure in the vessel could have reached 75 psia (the saturation pressure of sodium at 2000 F) during this test since inertial and frictional forces caused by the movement of the sodium were probably significant. The temperature trace in Figure D-14 shows that the reaction zone temperature was above 1630 F during most of the 4-second injection time. The temperature trace during most of the tests was quite erratic often registering rapid transients between the high reaction temperature and the water temperature (Figure D. 17). The two high water rate tests in which good temperature data were obtained resulted in the highest reaction zone temperatures (the maximum temperature for Test 2 was not recorded due to premature reaction initiation). The high temperature measured during these tests were probably related to the high pressures caused by ejection of the sodium. In the lower leak rate tests, the reaction zone temperature was probably limited by the presence of excess sodium at a 4-psig pressure.

Post test examination of the tubes from Tests 2 and 4 showed local heating above the austenization temperature, 1400 F, of 2-1/4 Cr-1 Mo. The overheating was confined to a small part of the impinged tube in each test. The temperatures attained in the tube wall and the time the wall was heated up could not be determined from the data available; therefore, a quantitative analysis of the heat transfer coefficient on the outside of the tube was not attempted.

The maximum temperature recorded by the thermocouple inside the tube next to the reaction zone thermocouple was 710 F during Test 8 (Figure D-17). The reaction zone temperature during the test was extremely erratic with maximums at about 1200 F and minimums near 200 F. This test also resulted in the maximum rate of temperature rise in the tube (44 F/sec). Since the reaction zone temperature was below the tube temperature during parts of the test, no attempt to determine the outside heat transfer coefficient from this data was made. A rise of 26.5 F/sec in the tube temperature was recorded during Test 3 where the reaction zone temperature varied between 480 and 1290 F. A transient heat transfer calculation using this information resulted in a coefficient of less than 200 Btu/hr ft² °F. Although the reaction zone thermocouple was installed near the outside of the thermocouple containing tube, the temperature data from both thermocouples did not correspond during many of the tests.

TABLE D-1

SODIUM-WATER TUBE THINNING TEST - PROPOSED PROGRAM

	Test Number								
	1	2	3	4	5	6	7	8	9
Orifice Diameter, in.	0.016	3/32	0.016	3/32	0.016	0.025	0.025	0.025	3/32
Rate of H ₂ O Injection, lb/sec	0.04	1.0	0.04	1.0	0.04	0.1	0.1	0.1	1.0
Amount of H ₂ O Injected, lb	4	4	4	4	4	4	4	4	4
Duration of Injection, sec	100	4	100	4	100	40	40	40	4
Water Pressure, psig	2950	2950	2950	2950	2950	2950	2950	2950	2950
Water and Sodium Temperature, F	350	350	350	350	350	350	350	350	350
Amount of Sodium Available for Reaction, lb	75	75	75	75	75	75	75	75	75
Sodium Pressure, psig	4	4	4	4	4	4	4	4	4
Tube Size, in.	5/8	5/8	5/8	5/8	1	5/8	1	1	1

TABLE D. 2

SODIUM-WATER TUBE THINNING TEST DATA

RUN NO. DATE	1 6-4-65	2 6-14-65	3 6-22-65	4 6-25-65	5 7-15-65	6 7-21-65	7 7-24-65	8 7-28-65	9 8-2-65
Orifice Diameter, in.	0.016	3/32	0.016	3/32	0.016	0.025	0.025	0.025	3/32
Planned Rate of H ₂ O Injection, lb/sec	0.04	1.0	0.04	1.0	0.04	0.1	0.1	0.1	1.0
Amount of H ₂ O Injected, lb	1 - 2 (Est.)	4.0	--	4.0	1.0	--	--	4.0	4.0
Duration of Injection, sec	~ 180	--	~ 16	3.7	~ 15	~ 1.5	~ 2.4	~ 49	3.7
Initial Temp of Na & H ₂ O, F	350	350	350	350	350	350	350	350	350
Initial Press. of H ₂ O, psig	3000	--	2950	2950	2950	2950	2950	2950	2950
Tube Size, in.	5/8	5/8	5/8	5/8	1.0	5/8	1.0	1.0	1.0
Maximum Wastage, mils									
Tube No. 11	0.6	0.5	1.3	0	0.3	0.1	2.3	4.5	1.6
12	0.9	0.2	1.7	0.2	2.7	0.2	1.9	2.0	2.6
22	4.9	3.3	1.9	0.4	1.8	0	--	1.3	0.7
23	23.7	28.4	2.6	0.8	1.7	0.4	0	0	3.4
24	6.1	1.0	1.5	0.3	0.3	0.4	0.4	2.2	1.8
32	0.9	0.8	1.6	0.1	0.3	0.1	1.1	3.5	0.5
33	2.6	0.2	2.1	0.1	3.5	0.1	0.2	0.9	0.5
34	0.5	0.3	1.5	0.1	1.1	0.9	1.7	0.3	1.6
Maximum Weight Loss, grams									
Tube No. 11	0.0656	0.0602	0.0599	0.1008	1.2	0.0224	0.3	0.2	0.1
12	0.0978	0.0686	0.0543	0.1323	0.5	0.0334	0.2	-0.1	0
22	0.3226	--	--	0.0756	--	--	--	--	--
23	1.2721	0.3776	0.0334	0.0920	--	0.0597	1.2	0	0.2
24	0.4098	0.4229	0.1742	0.1271	--	0.0823	0.6	0.2	0
32	0.0707	0.0900	0.0465	0.0807	0.4	0.0209	0.4	1.1	0.1
33	0.0669	0.0905	0.0381	0.0703	0.1	0.0156	0.1	0.6	-0.1
34	0.0395	0.0608	0.0333	--	0.4	0.0213	0.2	-0.2	0.5
Maximum Temperature in Reaction Zone, F	1000	1057	1286	2014	640	530	413	1261	1551
Inside Adjacent Tube, No. 22	--	426	456	--	450	384	No Ch.	710	510

FIG. D.1 SODIUM-WATER TUBE THINNING TESTS - SCHEMATIC

FIG. D.2 INJECTION NOZZLE ASSEMBLY FOR SODIUM WATER TUBE THINNING TESTS

FIG. D.3 TUBE ARRAY SIMULATING THE BOILER DOWNCOMER REGION

FIG. D.4 TUBE ARRAY SIMULATING THE BOILER TUBE BUNDLE

BOILER DOWNCOMER TUBE TESTS

BOILER BUNDLE TUBE TESTS

FIG. D.5 TUBE DESIGNS AND ARRANGEMENTS

Fig. D.6a Tube Array and Injection Nozzle Assembly

Fig. D.6b Removing Tube Array From Reaction Vessel

FIG. D.6 SODIUM-WATER TUBE THINNING TEST ASSEMBLIES

Fig. D.7a Tube Array and Injection Nozzle From Test 1

Fig. D.7b View Looking Into Reaction Vessel Showing Reaction Products From Test

FIG. D.7 SODIUM-WATER TUBE THINNING TEST 1

FIG. D.8 INJECTION NOZZLE RUPTURE DISKS FROM TESTS 1-9

Maximum Thinning, mils	23-1/2
Weight Loss, grams	1.272
Tube OD x Wall, in.	0.625 x 0.076
Tube Material	2-1/4 Cr - 1 Mo
Rate of Water Injection, lb/sec	< 0.04
Amount of Water Injected, lb	1-2 (estimated)
Temperature of Sodium and Water, F	350
Pressure of Water, psig	2625

FIG. D.9 SECTION OF IMPINGED TUBE FROM TEST 1 SHOWING WASTAGE

FIG. D.10 PHOTOMICROGRAPH OF WASTED AREA OF IMPINGED TUBE FROM TEST 1

FIG. D.11 PHOTOMICROGRAPH OF METAL IN WASTED AREA OF IMPINGED TUBE FROM TEST 2

Knoop Hardnesses of Impinged Tube Near the Impact Point

Angle*	Depth below original OD surface, mils							
	5	10	15	25	35	45	55	65
-45°	146	159	147	138	139	139	141	--
-30°	154	149	149	146	145	145	140	133
-20°	--	156	155	151	149	146	150	133
-10°	--	--	--	237	235	204	183	170
0°	--	--	--	269	254	250	246	--
+10°	--	--	224	214	182	181	159	152
+20°	146	141	144	147	163	164	153	149
+30°	143	144	142	139	142	136	149	177
+45°	137	136	140	141	144	146	140	143

* Angle of 0° was the impact point

Cross Section of Tube Showing Hardness Traverse

Magnification: 5x

FIG. D.12 HARDNESS TRAVERSE OF IMPINGED TUBE FROM TEST 2

Fig. D. 13a Test 2

Fig. D. 13b Test 3

Fig. D. 13c Test 4

Fig. D. 13d Test 5

FIG. D.13 TUBE ARRAY AND INJECTION NOZZLE ASSEMBLIES FROM TESTS 2-5

FIG. D.14 REACTION ZONE TEMPERATURE - TEST 4

FIG. D.15 PHOTOMICROGRAPHS OF TUBE FROM TEST 4

Fig. D. 16a Test 6

Fig. D. 16b Test 7

Fig. D. 16c Test 8

Fig. D. 16d Test 9

FIG. D.16 TUBE ARRAY AND INJECTION NOZZLE ASSEMBLIES FROM TESTS 6-9

FIG. D.17 REACTION ZONE TEMPERATURE - TEST 8

FIG. D.18 REACTION ZONE TEMPERATURE - TEST 9

APPENDIX E. ADDITIONAL DATA ON HYDROGEN
BUBBLE PRESSURES IN THE FULL-
SIZE STEAM GENERATOR

Figures E.1 through E.4 present additional hydrogen bubble pressure data for reactions in the B&W Full-Size Steam Generator.

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FIG. E.1 HYDROGEN BUBBLE PRESSURE FOR ADDITIONAL CASES AT LOCATION (A)

FIG. E.2 HYDROGEN BUBBLE PRESSURE FOR ADDITIONAL CASES AT LOCATION ⑧

FIG. E.3 HYDROGEN BUBBLE PRESSURE FOR ADDITIONAL CASES AT LOCATION (C)

FIG. E.4 HYDROGEN BUBBLE PRESSURE FOR ADDITIONAL CASES AT LOCATION ①

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