Scaling analysis for the OSU AP600 test facility (APEX)

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Abstract

In this paper, the authors summarize the key aspects of a state-of-the-art scaling analysis (Reyes et al., 1995. Westinghouse Electric Corporation, WCAP-14270) performed to establish the facility design and test conditions for the Advanced Plant Experiment (APEX) at Oregon State University (OSU). This scaling analysis represents the first, and most comprehensive, application of the Hierarchical Two-Tiered Scaling (H2TS) Methodology (Zuber, 1991. US Nuclear Regulatory Commission, Washington DC, NUREG/CR-5809) in the design of an integral system test facility. The APEX test facility, designed and constructed on the basis of this scaling analysis, is the most accurate geometric representation of a Westinghouse AP600 nuclear steam supply system. The OSU APEX test facility has served to develop an essential component of the integral system database used to assess the AP600 thermal hydraulic safety analysis computer codes. © 1998 Elsevier Science S.A. All rights reserved.

1. Introduction

Westinghouse has proposed using innovative passive safety systems to enhance the overall safety of their AP600 pressurized water reactor (PWR) power plant. The AP600 passive safety systems would use pre-pressurized or gravity drained tanks, natural circulation, and evaporation to perform the same function as the active, powered, safety systems found in currently operating PWRs. As with any new light water reactor, the AP600 must satisfy specific certification criteria set forth by the US Nuclear Regulatory Commission (NRC). In particular, the thermal hydraulic computer codes used to evaluate the performance of the AP600’s emergency core cooling system must be shown to realistically describe the behavior of the reactor system during a loss-of-coolant-accident (LOCA). This required comparing code calculations to applicable experimental data (Title 10 CFR 50.46, 1987). To help meet this requirement, Westinghouse Electric and the US Department of Energy (DOE) developed the APEX test facility at Oregon State University (OSU).

This paper presents two important sections of the detailed scaling analysis (Reyes et al., 1995) which was performed to ensure that the APEX facility would be capable of obtaining key thermal...
hydraulic data for the validation of the AP600 thermal hydraulic safety analysis computer codes. The final proprietary report, which exceeded 600 pages, was issued in January 1995.

1.1. OSU AP600 research program objectives and chronology

The objectives of the OSU AP600 research program were as follows:

- Perform a detailed scaling analysis to design a test facility capable of simulating important AP600 thermal hydraulic phenomena,
- Construct an AP600 integral system test facility,
- Perform tests that meet the applicable quality assurance requirements of ASME NQA-1, NQA-2 and 10 CFR 50 Appendix B,
- Obtain passive safety system performance data for a wide range of design basis accidents,
- Obtain data to validate the AP600 thermal hydraulic computer codes.

The OSU AP600 research project was initially sponsored by the Westinghouse Electric Corporation and DOE in 1991 as part of the DOE Advanced Light Water Reactor program. During 1991 and 1992, an extensive effort was made by OSU and Westinghouse to perform a comprehensive scaling analysis to develop the design drawings for the APEX facility. This included an instrumentation plan, construction cost estimates and selection criteria for a construction contractor. A draft scaling analysis report for the APEX facility was issued for review and comment in July 1992. The scaling analysis received valuable input and favorable reviews by the NRC and the Advisory Committee on Reactor Safeguards (ACRS). Facility construction began in January 1993 and was completed on March 31, 1994. Cold shake-down tests were performed in April; followed by hot shake-down tests in May of the same year. During this time period, the test, maintenance, and calibration procedures were developed, databases for instrument calibration and component inventory were established, as-built drawings were prepared and the data acquisition system was installed and validated. In June 1994, prior to matrix testing, quality assurance teams from DOE, Westinghouse, the Electric Power Research Institute and the ARC utility steering committee performed on-site readiness reviews to determine if the APEX testing program met the requirements for nuclear safety testing. The successful results of the readiness review cleared the way for formal matrix testing to begin in June 1994. A total of 28 tests were performed by a joint OSU/Westinghouse test team for a wide range of AP600 accident scenarios. These tests were audited by the NRC. Testing for Westinghouse/DOE was successfully completed in December 1994.

In January 1995, the NRC contracted with OSU to perform confirmatory testing and beyond design basis accident tests. Thus far a total of 38 tests have been successfully performed by OSU for the NRC. This includes large and small break loss-of-coolant-accidents (LOCAs) with multiple safety system failures, station blackout tests, and parametric studies of core uncovering, flow oscillations under low pressure saturation conditions and transport of non-condensibles in the primary system. Many of these tests are counterparts to those performed in the SPES-2 facility in Italy (Rigamonti, 1994) and the ROSA-AP600 facility in Japan (Boucher et al., 1994).

1.2. Description of the APEX test facility

Among AP600 integral system test facilities, the OSU APEX test facility is the best geometric representation of a Westinghouse AP600. The primary loop, passive safety systems, actuation logic, portions of the non-safety grade chemical and volume control system (CVS) and residual heat removal system and the routing of the interconnecting piping have been faithfully represented in the APEX. In addition, it is the only test facility that has been specifically designed and constructed to examine the long term sump recirculation cooling of the AP600.

The primary system includes a reactor pressure vessel (RPV) that models the upper and lower reactor internals, the core barrel, the downcomer and the core. An electrically heated rod bundle provides 600 kW of thermal power to the core simulator. The primary loop also includes a pres-
surizer, two steam generators, two hot legs, four cold legs, four reactor coolant pumps, two direct vessel injection (DVI) lines and two core make-up tank (CMT) pressure balance lines (PBL) as in the AP600 design.

APEX includes a complete passive safety system; a four stage automatic depressurization system (ADS), two accumulators, two CMTs, a passive residual heat removal (PRHR) heat exchanger, an In-containment refueling water storage tank (IRWST), and a primary and a secondary containment compartment sump. Fig. 1 presents a drawing of the primary system with some of the key passive safety systems.

In addition to modeling the physical aspects of the AP600, APEX includes over 750 instruments to measure system temperatures, pressures, liquid levels, flow rates and mass. It also includes a complete set of automatic safety system actuation logic for the operation of all of the passive safety systems. A detailed description of the APEX facility is provided by Hochreiter and Reyes (1995).

1.3. AP600 SBLOCA passive safety system operation

The general operation of the AP600 passive safety features has already been described in the literature (Vijuk and Bruschi, 1988). However, prior to discussing the details of the APEX scaling analysis, it is necessary to provide the reader with a brief introduction to the AP600 design. Fig. 2 presents a schematic which describes the important passive safety system features. The expected sequence of events during a SBLOCA is illustrated in Fig. 3. The first phase is the subcooled blowdown and natural circulation phase. During this phase, the primary system begins to depressurize and drain causing a reactor scram and the issuance of an ‘S’ signal.

The S-signal causes the CMTs and the PRHR system isolation valves to open. Buoyancy driven flow through the PRHR heat exchanger transfers the core decay heat to the IRWST. Similarly, a
natural circulation flow path is established through the CMT pressure balance lines causing cold borated water from the CMTs to enter the reactor vessel downcomer. Eventually the CMTs begin to empty causing the ADS valves on the pressurizer to open in a staged manner to increase the primary system depressurization rate. This begins the ADS blowdown phase of the SBLOCA transient. When either of the CMTs empty to 20% of their initial liquid volume, the fourth stage ADS (ADS4) valves, located on the hot legs, open to completely depressurize the primary system. During the depressurization process, the accumulators will also inject their supply of coolant into the downcomer. The break and ADS reduce the system pressure to permit the elevation head from the IRWST to drive coolant into the reactor vessel downcomer. This begins the IRWST injection phase of the SBLOCA. All of the break and ADS4 liquid flow is collected in the reactor cavity sump which surrounds the vessel. Eventually the reactor sump fills to a level that exceeds the height of the primary coolant loops; in the region of the steam generator lower channel head. The long-term cooling phase begins when the reactor sump and the IRWST liquid levels equilibrate and a natural circulation flow path is established from the sump, through the sump screen piping connected to the DVI line, into the reactor vessel downcomer. All of the break and ADS4 steam flow is vented to the containment where it is condensed. The condensate is collected in the IRWST and the sump and drained by gravity back into the reactor vessel downcomer. Under these conditions, the core cooling process will continue indefinitely.
2. APEX scaling objectives and methodology

The objective of the scaling study was to obtain the physical dimensions of a test facility that would simulate the flow and heat transfer behavior of importance to AP600 passive safety system operation and long-term cooling. Based on an understanding of the AP600 passive safety system operation, the APEX scaling analysis was divided into four modes of operation, each corresponding to a different phase of an AP600 Small Break LOCA (SBLOCA).

- Closed loop natural circulation scaling
- Open system depressurization scaling
- Venting, draining and injection scaling
- Long-term recirculation scaling

Providing the scaling analysis details for each of the four modes of operation would render the present paper too lengthy. Therefore, the following sections shall summarize portions of the closed loop two-phase natural circulation scaling analysis and the open system depressurization scaling analysis.

Meeting these scaling objectives presented a formidable challenge. To satisfy these objectives in an organized and clearly traceable manner, an APEX scaling methodology (ASM) was devel-
The model for this scaling methodology was largely drawn from the USNRC’s severe accident scaling methodology presented by Zuber (1991). A flow diagram for the ASM is presented in Fig. 4. The following sections summarize the execution of each step of the ASM.

3. Experiment objectives

The first task performed as part of the ASM was to specify the experiment objectives. The experiment objectives defined the scope of testing needed to respond to specific certification and
design needs. These objectives determined which phases of the transient should be simulated in the test facility, and hence the test facility capabilities. For the APEX test facility, the general objective was to perform SBLOCAs that lead to ADS actuation, followed by a period of IRWST injection and subsequent long term sump recirculation cooling. Containment cooling simulation was not considered in the test objectives.

To meet the experiment objectives, the APEX facility was designed and constructed to simulate key phenomena of cold leg breaks, hot leg breaks, inadvertent ADS operation, cold leg/CMT balance line breaks, DVI line breaks, station blackouts and a variety of control logic failures.

4. Plausible phenomena identification and ranking

The second task performed as part of the ASM was the development of plausible phenomena identification and ranking tables (PPIRTs) (Shaw et al., 1985). The nature of scaling forbids exact similitude between the AP600 and the test facility operating conditions. As a result, the design and operation of the test facility was based on simulating the processes most important to passive safety system performance and long-term cooling. To determine which of the AP600 processes were most important to passive safety system performance, a PPIRT for AP600 SBLOCAs was developed.

The ranking process was performed by evaluating the phenomena expected to occur in the major components of the AP600 for each of the phases of a typical SBLOCA transient as shown in Fig. 3. Existing data on standard PWRs, coupled with engineering judgement and calculations for the AP600, were used to determine which SBLOCA thermal hydraulic phenomena might impact core liquid inventory or fuel peak clad temperature. These phenomena were ranked and included in the AP600 SBLOCA PPIRT as shown in Table 1. If the phenomena could significantly impact reactor vessel liquid inventory or peak cladding temperature, it was ranked high (H). Similarly, phenomena determined to have moderate, low or no impact on the screening parameters, were ranked (M), (L), or (−), respectively. For phenomena not previously evaluated as part of the extensive research on existing PWRs, a rank of plausible (P) was assigned. Those phenomena ranked (H) or (P) were given priority in the scaling analysis.

Subsequent to the start of testing, it became clear that the initial PPIRT was too conservative. The early phases of an AP600 SBLOCA had little or no impact on the reactor vessel liquid inventory. The important phenomena occurred at low pressure (i.e. less than 0.3 MPa/50 psia) subsequent to ADS 4 actuation. This is clearly reflected in the revised PIRTs which were developed at the Idaho National Engineering Laboratory by Fletcher et al., 1996 with the aid of a broad base of thermal hydraulic experts and the experience gained through experiments performed in the three AP600 integral system test facilities. For purposes of facility scaling however, a conservative approach is still recommended whenever new safety system technology is being examined. With regard to assessment of facility adequacy, only the important phases of the PIRT need to be examined.

5. Hierarchical two-tiered scaling analysis (H2TS) method

Having identified the important phenomena through the use of the SBLOCA PPIRT, the next step in the ASM was to perform a scaling analysis for each phase of the SBLOCA. The outcome of the scaling analysis was a set of characteristic time ratios (dimensionless \( P \) groups) and similarity criteria for each mode of operation.

Based on a review of existing scaling approaches, it was determined that the H2TS method developed by Zuber (1991) was best suited to develop the similarity criteria needed to scale the systems and processes of importance to AP600 SBLOCA and long-term cooling behavior. A brief description of the H2TS method shall follow. The interested reader is encouraged to study the complete description of the methodology provided in Appendix D of NUREG/CR-5809 (Zuber, 1991).
Table 1
Pressible phenomena initial ranking table (PPIRT) for AP600 small break LOCAs

<table>
<thead>
<tr>
<th>Component phenomenon</th>
<th>Blowdown</th>
<th>Saturation natural circulation</th>
<th>ADS blowdown</th>
<th>IRWST injection cooling</th>
<th>LCS recirculation cooling</th>
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<td>Break</td>
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<td>Vessel–core</td>
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<td>Decay heat</td>
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<td>Forced convection</td>
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<td>RCP performance</td>
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<td>SG heat transfer</td>
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<td>Secondary conditions</td>
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<td>Hot leg</td>
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<td>Vapor condensation rate</td>
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<td>CMT balance lines</td>
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<td>Accumulators</td>
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<td>Injection flow rate</td>
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The four basic elements of the H2TS method as applied to the APEX facility scaling are described in the following sections.

5.1. System subdivision

For purposes of scaling, the AP600 was subdivided into two major systems; a reactor coolant system and a passive safety system. Figs. 5 and 6 show how each of these systems were further subdivided into interacting subsystems (or modules) which were further subdivided into interacting constituents (materials), which were further subdivided into interacting phases (liquid, vapor, or solid). Each phase was characterized by one or more geometrical configurations and each geometrical configuration was described by one or more field equations (mass, energy, and momentum conservation equations). The field equations were used to characterize several transport processes.

5.2. Scale identification

After subdividing the AP600 systems of interest, the next step was to identify the scaling level at which the similarity criteria should be developed. This was determined by the type of phenomenon being considered. For example, thermal hydraulic phenomena involving integral system interactions, such as primary system depressurization or loop natural circulation, were examined at the ‘system’ level. That is, the control volume for these phenomena was the entire primary loop.

Phenomena such as PRHR decay heat removal, CMT, accumulator, and IRWST passive safety injection, automatic depressurization and LCS recirculation cooling were examined at the ‘subsystem’ level. Thermal hydraulic phenomena important to individual components such as the reactor core, pressurizer, steam generators, hot legs, cold legs, reactor coolant pumps, and interconnecting piping were examined at the ‘component’ level. Specific interactions between the steam–liquid mixture and the stainless steel structure were examined at the ‘constituent’ level. A set of control volume balance equations was written for each hierarchical level. The top-down scaling analysis served the following purposes:

- It established the governing time scale for the hierarchical level (i.e. control volume) of interest,
- It identified all of the transport phenomena pertinent to the governing control volume balance equations,
- It provided the dimensionless groups that characterize each of the transport phenomena and which were used to establish the relative importance of the phenomena. These groups were called characteristic time ratios.

The top-down scaling analysis was performed by writing the governing control volume balance equations for each hierarchical level. Thus, for each constituent ‘k’, the following balance equation was implemented:

\[
\frac{dV_k \psi_k^+}{dr} = \Delta(Q_k \psi_k \|_{\text{in}} - Q_k \psi_k \|_{\text{out}}) + S_k^+ \tag{1}
\]

where

\[
\Delta(Q_k \psi_k) = [Q_k \psi_k \|_{\text{in}} - Q_k \psi_k \|_{\text{out}}]
\]

In Eq. (1), the \(\psi_k\) term represents the conserved property; \(\psi_k = \rho, \rho u\) or \(pe\) (mass, momentum or energy per unit volume), \(V_k\) is the volume of the control space, \(Q_k\) is the volumetric flow rate, \(j_{kn}\) is the flux of property \(\psi_k\) transferred from constituent \(\text{‘}k\text{’}\) to \(\text{‘}n\text{’}\) across the transfer area \(A_{kn}\). Hence, \(\Delta(Q_k \psi_k)\) represents the usual mass, momentum or energy convection terms, \(\sum j_{kn} A_{kn}\) represents transport process terms such as heat transfer and \(S_k\) represents the distributed sources or body forces acting internal to the control volume.

Eq. (1) was expressed in dimensionless form by specifying the following dimensionless groups in terms of the constant initial and boundary conditions:

\[
V_k^+ = \frac{V_k}{V_{k,0}}, \quad \psi_k^+ = \frac{\psi_k}{\psi_{k,0}}, \quad Q_k^+ = \frac{Q_k}{Q_{k,0}},
\]

\[
j_{kn}^+ = \frac{j_{kn}}{j_{kn,0}}, \quad A_{kn}^+ = \frac{A_{kn}}{A_{kn,0}}, \quad S_k^+ = \frac{S_k}{S_{k,0}}
\]

5.3. Top-down scaling analysis

The third element of the H2TS method required performing a ‘top-down’ (system) scaling analysis for each of the hierarchical levels. The top-down scaling analysis served the following purposes:

- It identified relevant transport phenomena pertinent to control volume balance equations at each hierarchical level.

The equations were then reduced to dimensionless form as:

\[
V_k^+ = \frac{V_k}{V_{k,0}}, \quad \psi_k^+ = \frac{\psi_k}{\psi_{k,0}}, \quad Q_k^+ = \frac{Q_k}{Q_{k,0}},
\]

\[
j_{kn}^+ = \frac{j_{kn}}{j_{kn,0}}, \quad A_{kn}^+ = \frac{A_{kn}}{A_{kn,0}}, \quad S_k^+ = \frac{S_k}{S_{k,0}}
\]
Fig. 5. AP600 reactor coolant system subdivision and hierarchy process.
Fig. 6. AP600 passive safety system subdivision and hierarchy process.
Substituting these groups into Eq. (1) yields:
\[ V_{k,0}\psi_{k,0} \frac{d\psi_{k}^{+}}{dt} = Q_{k,0}\psi_{k,0} \Delta[Q_{k}^{+}\psi_{k}^{+}] \]
\[ \pm \sum_{i} (j_{kn,0}A_{kn,0})j_{kn}A_{kn} + S_{k,0}S_{k}^{+} \]

(4)

Dividing both sides of this equation by \( Q_{k,0}\psi_{k,0} \) yields:
\[ \tau_{k} \frac{d\psi_{k}^{+}}{dt} = \Delta[Q_{k}^{+}\psi_{k}^{+}] \pm \sum_{i} \Pi_{kn}j_{kn}A_{kn} + \Pi_{k}S_{k}^{+} \]

(5)

where the residence time of constituent \( 'k' \) is
\[ \tau_{k} = V_{k,0} \]

(6)

and the characteristic time ratio for a transfer process between constituents \( 'k' \) and \( 'n' \) is given by:
\[ \Pi_{kn} = \frac{j_{kn,0}A_{kn,0}}{Q_{k,0}\psi_{k,0}} \]

(7)

The characteristic time ratio for the distributed source term within the control volume is given by:
\[ \Pi_{k} = \frac{S_{k,0}}{Q_{k,0}\psi_{k,0}} \]

(8)

To determine which transfer processes governed the overall evolution of a SBLOCA, numerical estimates of the characteristic time ratios for the prototype and the model were obtained for each phase of the transient at each hierarchical level of interest. This ranking process permitted the designer to identify the critical attributes of the APEX experiment.

Physically, each characteristic time ratio, \( \Pi_{k} \), is composed of a specific frequency, \( \omega_{k} \), which is an attribute of the specific process, and the residence time constant, \( \tau_{k} \), for the control volume. That is:
\[ \Pi_{k} = \omega_{k}\tau_{k} \]

(9)

The specific frequency defines the mass, momentum or energy transfer rate for a particular process. The residence time defines the total time available for the transfer process to occur within the control volume. A numerical value of:
\[ \Pi_{k} \ll 1 \] means that only a small amount of the conserved property would be transferred in the limited time available for the specific process to evolve. As a result, the specific process would not be important to the phase of the transient being considered. A numerical value of:
\[ \Pi_{k} \gg 1 \]

(11)

means that the specific process evolves at a high enough rate to permit significant amounts of the conserved property to be transferred during the time period, \( \tau_{k} \). Such processes would be important to the phase of the transient being examined.

In summary, at each hierarchical level, the products of the top-down scaling analysis are a control volume time constant given by Eq. (6) which characterizes the overall time scale for the transient of interest and a set of dimensionless groups given by Eqs. (7) and (8) which characterize the rates at which the governing transport processes occur within the control volume. Specific examples of top-down scaling are presented in Sections 7.2 and 8.2 of this paper.

5.4. Bottom-up scaling analysis

The fourth element of the H2TS method required performing a ‘bottom-up’ (process) scaling analysis. The purpose of the bottom-up scaling analysis was to provide the closure relations for the characteristic time ratios given by Eqs. (7) and (8). The closure relations consisted of models or correlations for specific processes such as critical flow, flow pattern transitions, critical heat flux and other local phenomena. These closure relations were used to develop the final form of the scaling criteria for purposes of scaling the individual processes of importance to system behavior. Specific examples of the bottom-up scaling analysis are presented in Sections 7.3 and 8.3 of this paper.

5.5. Scaling criteria development

The scaling criteria for APEX were developed by setting the characteristic time ratios for the
dominant processes in the AP600 to those for APEX at each hierarchical level. Therefore, the dominant processes were replicated in the model by carefully selecting the physical geometry, fluid properties and operating conditions so that the following scaling criterion was on the order of one for each of the dominant processes:

$$\frac{\Pi_k^{\text{APEX}}}{\Pi_k^{\text{AP600}}} \approx 1$$  \hspace{1cm} (12)

Using the closure relations obtained from the bottom-up scaling analysis and Eqs. (7), (8) and (12) yielded the scaling criteria needed to optimize the model’s design and operation to best simulate the important thermal hydraulic processes.

5.6. Evaluation of scale distortion

The effect of a distortion in APEX for a specific process was quantified as follows:

$$DF = \frac{[\Pi_k]_{\text{AP600}} - [\Pi_k]_{\text{APEX}}}{[\Pi_k]_{\text{AP600}}}$$  \hspace{1cm} (13)

The distortion factor, DF, physically represents the fractional difference in the amount of conserved property transferred through the evolution of a specific process in the prototype to the amount of conserved property transferred through the same process in the model during their respective residence times. A distortion factor of zero indicates that the model ideally simulates the specific process. A distortion factor of +0.05 indicates that the specific process in the model transfers 5% less of the conserved property (on a scaled basis) than the same process in the prototype. The distortion factor can also be written as:

$$DF = 1 - [\omega_k]_R [\tau_k]_R$$  \hspace{1cm} (14)

or

$$DF = 1 - [\Pi_k]_R$$  \hspace{1cm} (15)

The relative degree to which a specific transfer process would impact a particular phase of a transient was determined by comparing the magnitudes of the maximum characteristic time ratios for each of the transfer processes that arise during the transient phase of interest.

For the AP600 modes of passive safety system operation, all of the transfer processes that impact the system and subsystem transient behavior exhibit one of two properties that enabled the analyst to determine the maximum characteristic time ratio for a transfer process using the initial conditions of each phase of the transient. They were:

1. The maximum transfer rate for the process (i.e. specific frequency) occurred at the start of each phase of operation and decayed with time towards an equilibrium condition. The majority of transfer processes fell into this category. For example, at the onset of the ADS blowdown phase, the system pressure, the break and vent valve mass and energy flow rates, the secondary side and passive heat exchanger heat transfer rates, the core power, the metal stored energy release rate and the two-phase natural circulation flow rates were at their maximum values for that phase. Similarly, subsequent to depressurization, at the onset of long term cooling, the decay power and hence the recirculation flow rate was at its maximum value for this phase of the transient. For these transfer processes, the initial conditions for each phase of the transient were used to evaluate the maximum characteristic time ratios.

2. The maximum transfer rate for the process, although reaching a peak value later in the transient, was limited by the initial conditions of the system. All of the passive safety injection processes fell into this category. For the pre-pressurized accumulators, the maximum injection flow rate was limited by the initial accumulator charging pressure. For a gravity driven system, the maximum injection flow rate was limited by the initial liquid level in the safety injection tank. For these transfer processes, the limiting injection flow rates, as determined from initial system conditions, were used to evaluate the maximum characteristic time ratios.

Further insights will be gained as the method is applied in Sections 7 and 8 of this paper.
6. Rationale for scaling choices

The H2TS method described in Section 5 enabled the development of a set of scaling criteria that was used to determine the dimensions and operating conditions of the APEX test facility. Because these scaling criteria were expressed in terms of ratios of model to prototype fluid properties, material properties, and geometrical properties, some choices in each of these areas were required to achieve closure in the design process. This section presents the rationale for the scaling choices implemented in the design of the APEX test facility.

6.1. Selection of the working fluid

The working fluid selected for use in the APEX facility was water. The reasons for this selection were as follows:
1. Because water is the working fluid in the AP600, fluid property similarity is achieved whenever the system pressure and temperatures are matched in the model. This greatly simplified the scaling process for phenomena that occurs at low pressure such as IRWST injection and long term cooling.
2. By using water, the property routines used in the benchmark codes did not need to be modified. Thus, the benchmark codes were used directly to calculate phenomena observed in APEX.
3. Water was the most economical choice.

6.2. Selection of the component materials

Stainless steel was selected as the construction material for the APEX reactor coolant system and passive safety systems. The primary reasons for this selection were as follows:
1. The internal surfaces of the AP600 reactor coolant system and passive safety systems are stainless steel. Therefore, material property similarity is preserved and the scaling process simplified.
2. Although stainless steel is more expensive than carbon steel, it has greater corrosion resistance. This reduces the potential of blocking DP taps and extends the life of the facility; making it a more economical choice over the long term.

Other materials were implemented in regions where excessive stored energy might create significant distortions to system behavior. For example, the reflectors in the APEX were filled with a low mass ceramic to reduce the stored energy content in the core.

6.3. Selection of the operating pressure

The maximum operating pressure for APEX is 400 psia. The primary reasons for this choice were as follows:
1. The primary function of APEX is to investigate the low pressure passive safety injection and long term recirculation cooling process which occurs near atmospheric pressure.
2. Vessel wall thickness requirements increase with system pressure. Similarly, the saturation temperature increases with system pressure. A reduced scale facility operating at prototypical pressure would have a disproportionately large metal mass and, therefore, excessive stored energy. This would affect the depressurization rate and the transition to long term cooling.
3. Operation at reduced pressure and temperature permit a wider range of instrument options.

6.4. Selection of the length, diameter and time scale

The length ratio for APEX is 1/4. This length scaling ratio was applied to all the piping lengths and component elevations except in specific components where other more important criteria took precedence.

For natural circulation processes (i.e. thermosyphons), the time scale corresponding with the 1/4 length scale requirement was 1/2. Therefore, to maintain proper event sequence timing, all of the transport processes were scaled to satisfy the 1/2 time scale requirement.

The diameter scaling ratio for all vertical components and piping was 0.1443. The diameter scaling ratio for all horizontal or inclined piping
containing two-phase fluid was 0.1612. The reasons for these selections were as follows:
1. Using these length and diameter scaling ratios, the fluid volume requirements were not excessive (e.g. IRWST volume was approximately 3000 gallons).
2. Core power requirements associated with fluid volume were also reasonable (2% decay power was approximately 400 kW).
3. The time scaling ratio of 1/2 made long term cooling test durations reasonable.
4. The ratio of component lengths to diameter indicated that multidimensional flow effects would scale well whenever fluid property similitude existed ($L_R/d_R = 1.73$).
5. The 1/4 length scaling ratio assured that there was sufficient elevation difference between the hot and cold legs and permitted accurate measurements of differential pressure.
6. The diameter ratio for vertical piping and components assured that the skin friction pressure drop in the model would not exceed the combined skin friction and form pressure drop in the AP600 (on a scaled basis) for the case of fluid property similitude. Excessive line resistance would distort system loop flow behavior.
7. The diameter ratio for horizontal and inclined lines expected to contain two-phase fluid was based on scaling countercurrent flow processes and two-phase flow regime transitions.
8. The length and diameter scale requirements were easily met with commercially available pipe and drawn tubing.
9. Construction and material costs were reasonable for the geometric scale that was chosen.

7. Closed loop two-phase natural circulation scaling

This section describes the analysis that was performed to obtain the scaling criteria for closed loop two-phase natural circulation in the AP600. The SBLOCA shown in Fig. 3 depicts two periods of loop natural circulation. The first natural circulation period occurs at high pressure, early in the transient, when the CMTs, PRHR, and the steam generators provide buoyancy driven core heat removal. Natural circulation also plays an important role later in the transient, when long-term sump recirculation cooling is established. As a result, the form of the governing equations for both periods of the transient is identical. Fluid property similitude greatly simplified the scaling process for the long-term sump recirculation cooling period.

The parameters that were scaled to address the phenomena of interest to two-phase natural circulation were time scale, length scale, primary system volume, primary side form losses, reactor vessel geometry, core flow area, core height, core power, heater rod diameter, hot and cold leg diameters and lengths, pressurizer surge line diameter and length, and all the component relative elevations.

The geometry of interest for the top-down scaling analysis was the entire AP600 primary coolant loop. Therefore, the scaling analysis presented in this section, provides the basis for sizing most of the primary loop components. The specific phenomena of interest identified by the SBLOCA PPIRT and addressed in this section are:
- Two-phase natural circulation mass flow rates;
- The transition from stable two-phase natural circulation flow to intermittent or oscillatory flow;
- Hot leg countercurrent flow limitations (CCFL);
- Core heat transfer and critical heat flux under two-phase natural circulation conditions.

The similarity criteria required to scale these phenomena are presented in this section. Fig. 7 presents the loop natural circulation scaling analysis flow diagram. First, a top-down/system scaling analysis was performed. This included an analysis at the constituent level for transient conditions in the core, and an analysis at the system level (loop) for steady-state conditions. The objective of the top-down scaling analysis for this hierarchical level was to:
1. scale the steady-state two-phase fluid mass flow rates, and
2. scale the two-phase natural convection core heat transfer.
Following the top-down scaling analysis, a bottom-up scaling analysis was performed to develop similarity criteria to scale:
1. Flow regime transitions,
2. Hot leg counter-current flow limitations (CCFL),
3. Core heat transfer regimes, and

The similarity criteria developed through these analyses were compiled and integrated with the single-phase liquid natural circulation scaling criteria to produce a set of scaling ratios for the APEX test facility.

7.1. Top-down constituent level scaling for the core

The transfer processes between two constituents are examined in this analysis; a two-phase mixture and the metal fuel/heater rods. Heat transfer to metal walls and to the ambient will be examined in the next section. The control volume balance equations for a core having a constant cross-sectional flow area, $a_c$, is given as follows:

Mixture mass:

$$\frac{d}{dt} (\rho_{TP} V_{TP}) = \Delta (\rho_{TP} Q_{TP}) - \Delta (a_c \Delta \rho v_g)$$  \hspace{1cm} (16)
Mixture momentum:

\[
d\frac{\partial}{\partial t} (\rho_{TP} u_{TP} V_{TP}) = \Delta (\rho_{TP} u_{TP} Q_{TP}) + \Delta \rho g z V_{TP} + \Delta \left( \frac{2 \rho_{gs} \rho_{ls} a_{c} v_{g}^2}{(1 - \alpha) \rho_{TP}} \right) - \frac{\rho_{TP} u_{TP} Q_{TP}}{2} \left( \frac{fl}{dh} + K \right) + \Pi_{p} \rho_{TP} u_{TP} Q_{TP} \left( \frac{fl}{dh} + K \right)
\]  

(17)

Mixture enthalpic energy equation:

\[
d\frac{\partial}{\partial t} (\rho_{TP} h_{TP} V_{TP}) = \Delta (\rho_{TP} h_{TP} Q_{TP}) + H_{st} A_{s}(T_{s} - T_{TP}) \big|_{\text{Boundary}} + \Delta \left( \frac{2 \rho_{gs} \rho_{ls} a_{h} h_{g} v_{g}^2}{\rho_{TP}} \right)
\]  

(18)

Solid energy equation:

\[
d\frac{\partial}{\partial t} (\rho_{s} C_{vT} T_{s} V_{s}) = H_{st} A_{s}(T_{s} - T_{TP}) \big|_{\text{Boundary}} + q_{s}
\]  

(19)

The two-phase mixture equations presented above require less information than would be required for an analysis performed at the phase level. The mixture equations differ from the standard drift flux model in that it only requires information at the constituent level. Thus, a separate mass conservation equation for the liquid or vapor phase is not included.

These equations were non-dimensionalized by dividing the individual terms by their respective initial or boundary conditions. The non-dimensionalized balance equations in terms of characteristic time ratios (i.e. \( \Pi_{s} \) groups) are as follows:

Mixture mass:

\[
\tau_{TP.o} \frac{d}{dt} \left( \rho_{TP} u_{TP} V_{TP} \right) = \Delta (\rho_{TP} u_{TP} Q_{TP}) - \Pi_{Nd}
\]  

(20)

Mixture momentum:

\[
\tau_{TP.o} \frac{d}{dt} \left( \rho_{TP} u_{TP} V_{TP} \right) = \Delta (\rho_{TP} u_{TP} Q_{TP}) + \Pi_{Fr} \Delta \rho + g z + V_{TP} + \Pi_{p} \Pi_{Nd} \Delta \left( \frac{2 \rho_{gs} \rho_{ls} a_{c} v_{g}^2}{(1 - \alpha) \rho_{TP}} \right)
\]  

(21)

Mixture enthalpic energy equation:

\[
\tau_{TP,o} \frac{d}{dt} (\rho_{TP} h_{TP} V_{TP}) = \Delta (\rho_{TP} h_{TP} Q_{TP}) + \Pi_{HT}[H_{st} A_{s} (T_{s} - T_{TP})]_{\text{Boundary}} + \Pi_{p} \Pi_{Nd} \Pi_{s} \Delta \left( \frac{2 \rho_{gs} \rho_{ls} a_{h} h_{g} v_{g}^2}{\rho_{TP}} \right)
\]  

(22)

Solid energy equation:

\[
\tau_{s,o} \frac{d}{dt} \left( \rho_{s} C_{vT} T_{s} V_{s} \right) = H_{st} A_{s} (T_{s} - T_{TP}) + \Pi_{g} q_{s}
\]  

(23)

The fluid mixture residence time and the characteristic time ratios are as follows:

Mixture residence time:

\[
\tau_{TP,o} = \frac{V_{TP,o}}{Q_{TP,o}}
\]  

(24)

Solid structure specific frequency:

\[
\frac{1}{\tau_{s,o}} = \frac{H_{st,o} A_{s,o}}{\rho_{s,o} C_{vT,o} V_{s,o}}
\]  

(25)

Characteristic time ratios:

\[
\Pi_{Fr} = \frac{H_{TP,o} \rho_{TP}}{z_{s} g h_{o} \Delta \rho_{o}} \quad \text{Fr Froude number}
\]  

(26)

\[
\Pi_{p} = \frac{\rho_{gs,o} \rho_{ls,o}}{z_{s} (1 - z_{s}) \Delta \rho_{o}} \quad \text{Density ratio}
\]  

(27)

\[
\Pi_{Nd} = \frac{V_{g,o} z_{s,o}}{H_{TP,o} \rho_{TP,o}} \quad \text{Drift flux number}
\]  

(28)

\[
\Pi_{p} = \left( \frac{fl}{dh} + K \right) \quad \text{Friction number}
\]  

(29)

\[
\Pi_{HT} = \frac{H_{st,o} A_{s} (T_{s} - T_{TP})}{H_{TP,o} \rho_{TP,o} a_{c}}
\]  

(30)

Modified Stanton number
\[ \Pi_s = \frac{h_{l,0}(1-x_0)_{l,0} \Delta \rho_o}{H_{TP,o} \rho_{TP,o}} \]  

Enthalpy ratio  

\[ \Pi_{\varphi} = \frac{q_{l,0}}{u_{TP,o} \rho_s C_{vs}(T_s - T_{TP,o}) V_s} \]  

Fluid heat source ratio  

\[ \Pi_{\psi} = \frac{q_{l,0}}{H_{sf,o} \rho_s A_s (T_s - T_{TP,o})} \]  

Heat source ratio  

\[ \Theta_s = \frac{(T_s - T_{TP,o})_{s,o}}{T_{s,o}} \]  

Temperature ratio

The characteristic time ratios represent the basic similarity groups that govern the various transport phenomena. These \( \Pi \) groups are identical to those developed by Ishii and Kataoka (1982), with the exceptions that the heat source ratio is presented in terms of surface heat convection rather than axial energy change and the phase change number does not arise because the analysis is performed at the constituent level.

Note that the fluid mixture balance equations implement the same time scale, \( t_{TP,o} \). This permitted direct comparisons of the fluid mixture \( \Pi \) groups. The time scale for the solid energy equation, \( t_{s,o} \), differs from the fluid mixture time scale. The ratio of these time scales is written as follows:

\[ \epsilon_{\varphi} = \frac{\tau_{s,o}}{\tau_{s,0}} = \frac{V_{s,0} H_{sf,o} A_s}{Q_{l,0} \rho_s C_{vs} V_s} \]  

This equation is rewritten as follows:

\[ \epsilon_{\varphi} = \frac{\delta H_{sf,o}}{u_{l,0} \rho_s C_{vs} \delta} \]  

where \( \delta \) is the conduction depth given by the cross-sectional area of the solid divided by the wetter perimeter (\( A_s / \xi \)). When \( \epsilon_{\varphi} \ll 1 \), the fluid mixture variables change more rapidly than those of the solid. When \( \epsilon_{\varphi} \gg 1 \), the solid variables change more rapidly than those of the fluid mixture. Thus, the time scale ratio presented in Eqs. (35) and (36) indicate the degree of coupling between the fluid mixture transport phenomena and the solid transport phenomena.

7.2. Top-down system scaling for the primary loop

This section presents an analysis that describes how the two-phase natural circulation flow rate (two-phase fluid velocity and mass flow rate) in a thermal hydraulic model would be expected to scale with facility size. This analysis results in an analytical expression for the two-phase fluid velocity. Fig. 8 depicts the loop geometry considered for this analysis. The loop is divided into two regions; a two-phase region with a fluid density \( \rho_{TP} \) and a single-phase region with a fluid density \( \rho_l \). The simplifying assumptions are as follows:

Integral analysis assumptions:
1. Steady-state flow,
2. One dimensional flow along the loop axis,
3. Uniform fluid properties at every cross-section,
4. Homogeneous flow,
5. Chemical equilibrium—no chemical reactions,
6. Thermal equilibrium—both phases at the same temperature,
7. The sum of convective accelerations due to vaporization and condensation are negligible,
8. Viscous effects included in determination of form losses only.

The assumptions listed above were applied to the mass, momentum and energy equations for the core to obtain a simplified set of conservation equations. The simplified set of equations were then integrated over their respective single-phase and two-phase regions to obtain the loop balance equations. These equations were used to obtain an analytical expression for the fluid velocity at the core entrance. The coefficients of the fluid velocity equation were scaled to obtain a two-phase fluid velocity scaling ratio, a two-phase power density scaling ratio and a two-phase time scaling ratio.

7.2.1. Loop mass conservation equation

For steady-state, one-dimensional flow, the mass conservation equation at every flow cross section along the loop is written as:

\[ \dot{m} = \rho_l u_l a_l = \text{constant} \]  

where \( \dot{m} \) is a constant mass flow rate for the
Fig. 8. Regions of single-phase and two-phase natural circulation within a PWR.

system, \( \rho_i \) and \( u_i \) are the fluid density and fluid velocity within the \( i \)th component respectively, and \( a_i \) is the flow cross-sectional area of the \( i \)th component. Steady-state conditions require that:

\[
\rho_i u_i a_i = \rho_c u_c a_c
\]

(38)

where \( \rho_c \), \( u_c \) and \( a_c \) are the fluid density, fluid velocity and flow cross-sectional area at the core entrance respectively.

7.2.2. Loop momentum conservation equation

Applying the assumptions stated previously, the steady-state momentum balance equation is integrated over the single and two-phase regions to
obtain a general force balance relating frictional and buoyancy forces. That is:

$$\sum_{i=1}^{N} \frac{\rho_i u_i^2}{2} \left( \frac{f_i}{d_i} + K \right) = (\bar{\rho} - \bar{\rho}_{\text{TP}}) L_{\text{th}} g$$  \hspace{1cm} (39)$$

Using Eq. (38), Eq. (39) is re-written in terms of single and two-phase regions as follows:

$$\frac{\rho \bar{u}^2}{2} \left[ \frac{1}{\rho_{\text{TP}}} \sum_{i=1}^{N} \left( f_i + K \right) \left( \frac{a_i}{a_c} \right)^2 \right] + \frac{1}{\rho_{\text{TP}}} \sum_{i=1}^{N} \left( f_i + K \right) \left( \frac{a_i}{a_c} \right)^2 = (\bar{\rho} - \bar{\rho}_{\text{TP}}) L_{\text{th}} g$$  \hspace{1cm} (40)$$

For this analysis, \( \bar{\rho}_{\text{TP}} \) is the average two-phase fluid density given by the following definition for a two-phase fluid under homogeneous equilibrium conditions.

$$\bar{\rho}_{\text{TP}} = \frac{\rho_{\text{ls}}}{1 + x_c \frac{\Delta \rho}{\rho_{\text{gs}}}}$$  \hspace{1cm} (41)$$

7.2.3. Loop energy conservation equation

Under steady-state conditions, the energy balance for the core, which governs the rate of energy transport into the system is given by:

$$\rho_c u_c h_{\text{TP}} - h_1 = \dot{q}_s$$  \hspace{1cm} (42)$$

where \( \dot{q}_s \) is the core heat generation rate less heat losses from the core. The equilibrium vapor quality used in Eq. (41) is defined as follows:

$$x_c = \frac{h_{\text{TP}} - h_{\text{ls}}}{h_{\text{lg}}}$$  \hspace{1cm} (43)$$

Substituting the energy Eq. (42) into Eq. (43) yields:

$$x_c = \frac{\dot{q}_s - \rho_c u_c a_c h_{\text{sub}}}{\rho_c u_c a_c h_{\text{lg}}}$$  \hspace{1cm} (44)$$

where the subcooling enthalpy, \( h_{\text{sub}} = h_{\text{ls}} - h_c \) and the latent heat of vaporization, \( h_{\text{lg}} = h_{\text{lg}} - h_{\text{ls}} \).

7.2.4. Fluid velocity equation

Substituting Eq. (44) into Eq. (41) yields:

$$\bar{\rho}_{\text{TP}} = \frac{\rho_{\text{ls}}}{1 + \left( \frac{\dot{q}_s - \rho_c u_c a_c h_{\text{sub}}}{\rho_c u_c a_c h_{\text{lg}}} \right) \left( \frac{\Delta \rho}{\rho_{\text{gs}}} \right)}$$  \hspace{1cm} (45)$$

Substituting Eq. (45) into Eq. (40), applying the boussinesq approximation such that \( \rho_c \approx \rho_{\text{ls}} \), and performing a significant amount of algebra, yields the following cubic equation for fluid velocity:

$$u_c^3 + \phi_u u_c^2 + \phi_b u_c - \phi_c = 0$$  \hspace{1cm} (46)$$

where:

$$\phi_u = \frac{\dot{q}_s \Delta \rho}{a_c \rho_{\text{gs}}}$$

$$\phi_b = \frac{\Delta \rho}{a_c^2 \rho_{\text{gs}}}$$

$$\phi_c = \frac{1}{a_c \rho_{\text{ls}}}$$

$$F_{\text{Sp}} = \sum_{i=1}^{N} \left( \frac{f_i}{d_i} + K \right) \left( \frac{a_i}{a_c} \right)^2$$  \hspace{1cm} (47a)$$

$$F_{\text{TP}} = \sum_{i=1}^{N} \left( \frac{f_i}{d_i} + K \right) \left( \frac{a_i}{a_c} \right)^2$$  \hspace{1cm} (47b)$$

$$F_{\text{TP}} = F_{\text{Sp}} + F_{\text{TP}}$$  \hspace{1cm} (47c)$$

7.2.5. Primary loop similarity criteria

To simulate the same fluid velocity behavior in the model as in the full scale prototype, the coefficients of Eq. (46) must be scaled properly. It is interesting to note that Schroeder (1991) examined the mathematical basis for scaling equations similar to Eq. (46) in his text titled Fractals, Chaos, Power Laws.

A set of two-phase, natural circulation similarity criteria were obtained by scaling the coefficients \( \phi_u \), \( \phi_b \), \( \phi_c \) of Eq. (46) such that the following transformation was possible:

$$\beta^3 (u_{\text{m}}^3 + \phi_u u_{\text{m}}^2 + \phi_b u_{\text{m}} - \phi_c) = u_p^3 + \phi_u u_p^2 + \phi_b u_p - \phi_c$$  \hspace{1cm} (51)$$

where, \( \beta^3 \) is a constant factor and the subscripts \( m \) and \( p \) represent values for the model and the
prototype respectively. Reyes (1994) demonstrates that Eq. (46) is a constraint catastrophe function whose coefficients can be scaled as follows:

\[
\beta u_m = u_p \tag{52a}
\]

\[
\beta \phi_{am} = \phi_{ap} \tag{52b}
\]

\[
\beta^2 \phi_{bm} = \phi_{bp} \tag{52c}
\]

\[
\beta^3 \phi_{cm} = \phi_{cp} \tag{52d}
\]

Rearranging these equations in terms of scaling ratios yields:

\[
u_R = 1/\beta \tag{53a}
\]

\[
\phi_{aR} = 1/\beta \tag{53b}
\]

\[
\phi_{bR} = 1/\beta^2 \tag{53c}
\]

\[
\phi_{cR} = 1/\beta^3 \tag{53d}
\]

where the subscript R represents a ratio of the model parameters to the prototype parameters.

7.2.6. Two-phase similarity criteria at saturated conditions

For the special case of saturated conditions in APEX, the value of \(h_s\) is zero. Furthermore, to achieve kinematic similarity, similitude between flow areas has been maintained. That is:

\[
\left(\frac{a_c}{a_c'}\right)_R = 1 \tag{54}
\]

In addition, efforts were made such that the single-phase and two-phase friction numbers (\(F_{SP}\) and \(F_{TP}\)) would be on the same order in the model and the prototype. That is:

\[
(F_{SP})_R \approx 1 \tag{55}
\]

\[
(F_{TP})_R \approx 1 \tag{56}
\]

\[
(F_{I})_R \approx 1 \tag{57}
\]

Setting \(h_s\) to zero and applying the requirements presented in Eqs. (54)–(57) to the coefficients of Eqs. (47a), (47b) and (47c) and taking the ratio of the model values to the prototype values yields:

\[
\phi_{aR} = \left(\frac{q_s}{a_c}\right)_R \left(\frac{\Delta \rho}{\rho_b \rho_g h_{ig}}\right)_R \tag{58a}
\]

\[
\phi_{bR} = \left(\frac{q_s}{a_c}\right)_R \left(\frac{\Delta \rho}{\rho_b \rho_g h_{ig}}\right)_R \tag{58b}
\]

\[
\phi_{cR} = \left(\frac{q_s}{a_c}\right)_R \left(\frac{\Delta \rho}{\rho_b \rho_g h_{ig}}\right)_R \tag{58c}
\]

The scaling criteria for saturated conditions were obtained by substituting Eqs. (58a), (58b) and (58c) into Eqs. (53b), (53c) and (53d). Substituting Eq. (58a) into Eq. (53b) yields:

\[
\frac{1}{\beta} = \left(\frac{q_s}{a_c}\right)_R \left(\frac{\Delta \rho}{\rho_b \rho_g h_{ig}}\right)_R \tag{59}
\]

Setting Eq. (59) equal to Eq. (53a) yields:

\[
\frac{q_s}{\rho_b a_c h_{ig}} = 1 \tag{60}
\]

The requirement of Eq. (60) has implications on the vapor quality. For saturation conditions, the vapor quality, as defined in Eq. (44), is simplified as follows:

\[
x_e = \frac{q_s}{\rho_b a_c h_{ig}} \tag{61}
\]

Substituting Eq. (61) into Eq. (60) yields:

\[
x_R = \left(\frac{\Delta \rho}{\rho_b \rho_g h_{ig}}\right)_R \tag{62}
\]

Another scaling criterion is obtained by substituting Eqs. (59) and (58c) into Eq. (53d). This yields:

\[
q_R = a_R \left(L_{th}\right)_{1/2} \left(\frac{\rho_b \rho_g h_{ig}}{\Delta \rho}\right)_R \tag{63}
\]

Combining Eqs. (60) and (63) yields the fluid velocity scaling ratio:

\[
u_R = \left(L_{th}\right)_R^{1/2} \tag{64}
\]

The time scaling ratio is obtained by dividing the system length scaling ratio by the velocity scaling ratio given by Eq. (65). Thus:

\[
\tau_R = \frac{l_R}{\left(L_{th}\right)_R^{1/2}} \tag{65}
\]

The mass flow rate scaling ratio is obtained by:

\[
m_R = \left(\rho_b h_{th}\right)_R^{1/2} a_R \tag{66}
\]

For the case of a fixed length scaling ratio and geometric similarity:

\[
(L_{th})_R = l_R \tag{67}
\]

The scaling ratios then become identical to those developed by Ishii and Kataoka (1983). Eqs.
(63)–(66) are expressed in terms of $L_{th}$, the distance between the thermal centers of the heat source (core) and the heat sink (steam generator). This does provide some added flexibility in the test facility design since the thermal center length can be preserved without having to satisfy Eq. (67).

### 7.2.7. Integral system scaling ratios for single and two-phase regions

As depicted in Fig. 8, both a single-phase region and a two-phase region can exist during two-phase natural circulation. As a result, it is important that the transition from single-phase flow to two-phase flow be scaled properly. This section addresses that concern and presents a unified set of scaling criteria for the single and two-phase regions.

The velocity ratio for steady-state, single-phase natural circulation flow (Reyes et al., 1995) is:

$$u_{R,sp} = \left( \frac{\beta_T \rho_{lo} C_{plg}}{R} \right)^{1/3} \left( \frac{q_{sp}}{\Delta c} \right)^{1/3}$$

(68)

The fluid residence time scaling ratio for steady-state single-phase natural circulation flow is:

$$\tau_{R,sp} = \left[ \frac{\rho_{lo} C_{plg} \Delta \rho_{sp}}{\beta_T^{1/2} \Delta c} \right]^{1/3}$$

(69)

To assure that the single to two-phase flow transitions are scaled properly in APEX, the two-phase power scaling ratio was set equal to the single-phase power scaling ratio. By substituting the two-phase power scaling ratio into Eq. (68), while assuming geometric similarity, the following single-phase velocity scaling ratio was obtained:

$$u_{R,sp} = U_{R,sp}^{1/2} \left( \frac{\beta_T \rho_{gs} h_{lg}}{\Delta \rho C_p} \right)^{1/3}$$

(70)

Similarly, the single-phase residence time scaling ratio is:

$$\tau_{R,sp} = U_{R,sp}^{1/2} \left( \frac{\beta_T \rho_{gs} h_{lg}}{\Delta \rho C_p} \right)^{-1/3}$$

(71)

Therefore, with respect to integral system flow behavior and single to two-phase flow transitions; requiring the same power scale resulted in unifying the single and two-phase scaling ratios. Therefore, the system scaling ratios for steady-state natural circulation with single-phase and two-phase flow regions, material property similitude and a fixed length scale are given by:

Power scaling ratio:

$$q_{R,sp} = q_{R,sp} = a_R^{1/2} \left( \frac{\rho_{gs} h_{lg}}{\Delta \rho} \right)_{R}$$

(72)

Fluid velocity scaling ratio:

$$u_{R,sp} = U_{R,sp} \left( \frac{\beta \rho_{gs} h_{lg}}{\Delta \rho C_p} \right)^{1/3}$$

(73)

Fluid residence time scaling ratio:

$$\tau_{R,sp} = \tau_{R,sp} \left( \frac{\beta \rho_{gs} h_{lg}}{\Delta \rho C_p} \right)^{-1/3}$$

(74)

The scaling ratios presented above are identical to those developed by Kocamustafaogullari and Ishii (1986) for the case of material property similitude. The scaling ratios presented above are functions of pressure. This suggests that high pressure natural circulation can be simulated in a low pressure test facility. However, when the same working fluid is used in the model and the prototype, time scaling and velocity scaling distortions arise in the single phase region because of fluid property differences. This distortion is examined in Section 9.2 of this paper.

### 7.3. Bottom-up scaling of two-phase natural circulation processes

The previous sections presented top-down scaling analyses at two levels: the constituent level, which provides specific similarity criteria for the core and the integral system level, which provides similarity criteria for the loop as a whole.

As shown in Fig. 7, bottom-up scaling analyses are needed to scale important processes in the core, the hot leg and other components. This section presents scaling analyses for:

1. Two-phase friction and loss coefficients,
2. Flow regime transitions, and
3. Core two-phase flow processes.

#### 7.3.1. Scaling two-phase friction and loss coefficients

As identified in the top-down scaling analysis, it
is important that the friction number for the two-phase region of the model be the same as that of the full scale AP600. This requires a closer examination of two-phase friction factors and loss coefficients.

Dividing the two-phase friction number as defined by Eq. (49) into a friction factor component and a loss coefficient component, a two-phase friction multiplier and loss coefficient multiplier is obtained as follows (Ishii and Kataoka, 1983).

\[ F_{TP} = \sum_{i} N_{f} + \sum_{i} N_{k} \]  

where

\[ N_{f} = \frac{f}{d_{h}} \left( 1 + \frac{\Delta \rho \chi_{L}}{\rho_{L}} \right) \left( \frac{a_{L}}{a_{i}} \right)^{2} \]  

and

\[ N_{k} = K \left( 1 + \frac{\Delta \rho \chi_{L}}{\rho_{L}} \right) \left( \frac{a_{L}}{a_{i}} \right)^{2} \]  

where \( f \) is the darcy friction factor for liquid and \( K \) is the loss coefficient. For the range of fluid velocities encountered in natural flows, the dominant losses will be due to form losses rather than friction. Therefore, the loss coefficients were modeled in APEX to match those expected for the full scale AP600. Because proper flow scaling requires scaling the total pressure drop number in the two-phase region rather than in each individual component, it was possible to satisfy this criterion through the careful use of flow orifices and fluid property similitude at low pressure.

7.3.2. Scaling two phase flow pattern transitions

Transitions in two phase flow patterns significantly impact the integral behavior of the system. Therefore an assessment was made for each component in the system to determine which two phase flow pattern transitions may be delayed or entirely missed. In their paper, Schwartzbeck and Kocamustafaogullari (1989) catalogued the applicable flow pattern transition criteria. These criteria were used to determine if two phase flow pattern transitions would be properly scaled in the horizontal and vertical sections of APEX. The following equations summarize the flow transition scaling criteria that was implemented based on the work of Schwartzbeck and Kocamustafaogullari (1989).

7.3.3. Horizontal pipes:

- Stratified-smooth to stratified wavy:
  \[ u_{R}^{3/4} = \alpha_{R} \left( \frac{1 - x}{1 - x_{L}} \right)^{1/2} \left( \frac{\Delta \rho^{3} \chi_{L}}{\rho_{L} \rho_{e}} \right)_{R} \]  

- Stratified to intermittent or annular-dispersed liquid (Taitel and Dukler, 1976):
  \[ u_{R} = D_{R}^{1/2} \left( \frac{\Delta \rho}{\rho_{L}} \right)^{1/2} \]  

- Intermittent or dispersed bubbly to annular-dispersed:
  \[ \alpha_{R} = \left( \frac{\rho_{L}}{\Delta \rho} \right)_{R} \]  

- Intermittent to dispersed bubbly:
  \[ \alpha_{R}^{0.45} = \frac{f_{R}(\alpha_{R}) a_{R}^{0.3}}{\left( \rho_{L} / \Delta \rho \right)_{R}^{0.5} (1 - x_{L}^{0.9})} \]  

7.3.4. Vertical pipes:

- Bubbly to slug flow:
  \[ \alpha_{R} = \left( \frac{\rho_{L}}{\Delta \rho} \right)_{R} \]  

- Slug to churn flow:
  \[ l_{R} = a_{R}^{1/2} f_{t}(x, \rho) \]  

- Slug/churn to annular flow:
  \[ u_{R} \left( \frac{\rho_{L}^{2}}{\sigma \Delta \rho} \right)_{R}^{1/4} = 1 \]  

These flow pattern transition scaling criteria simplify with fluid property similarity. It was impossible to satisfy all of these criteria in the scaled APEX model. Therefore, emphasis was given to the important flow pattern transitions identified for various components.

7.3.5. Hot and cold leg scaling

Under two-phase natural circulation conditions, the flow behavior in the hot leg can undergo transitions from stratified flow to either
intermittent-slug flow or annular-dispersed flow. This was an important feature of the FLECHT-SEASET experiments in which the hot leg diameter was increased to a maximum in an attempt to model these transitions as closely as possible (Hochreiter, 1985).

The type of flow pattern in the hot leg greatly influences the loop flow rate. A transition from an intermittent-slug flow to a stratified flow in the hot leg will cause a significant change in the mode of heat transfer removal in the steam generators. Therefore it was important to assure that this flow pattern transition would occur at the same relative liquid level \( h/d \) in the APEX hot leg as in the AP600. This type of flow transition can occur in any of the horizontal piping that may be in a drain-down’ situation. Therefore, special attention was given to the reactor coolant loop hot and cold legs.

Based on Taitel and Dukler’s work (1976), appropriate scaling of the hot legs required satisfying the modified Froude number ratio. (This dimensionless group has also been used by Wallis to scale flooding behavior in vertical tubes.) The modified Froude number is given by:

\[
Fr = \frac{j_g}{\left( g D_{HL} \Delta \rho / \rho \right)^{1/2}}
\]  
(85)

Exact scaling requires that flow regime transitions occur at the same critical Froude number and dimensionless liquid level in the model and in the AP600 hot legs. For identical fluid properties, with \( z_R = 1 \), the ratio of the model to the AP600 critical Froude number is written for the hot leg as follows and set equal to one,

\[
Fr_R = \frac{u_{HL,R}}{(D_{HL,R})^{1/2}} = 1
\]  
(86)

where it has been recognised that the hot leg vapor velocity ratio equals the hot leg two-phase velocity ratio, \( u_{HL,R} \). This criterion requires that the local velocity ratio equal the square root of the hot leg diameter ratio. That is,

\[
u_{HL,R} = (D_{HL,R})^{1/2}
\]  
(87)

Because the mass flow rate ratio is fixed by global loop parameters, and must be constant throughout the entire loop, increasing a component diameter must necessarily decrease the local velocity ratio. Therefore, the correct approach is to solve Eq. (87) simultaneously with the mass conservation equation for the hot leg. The mass conservation equation for the hot leg is as follows:

\[
\dot{m}_{HL,R} = u_{HL,R} a_{HL,R}
\]  
(88)

Substituting Eq. (88) into Eq. (87) and expressing the hot leg area ratio in terms of the hot leg diameter ratio squared yields:

\[
\dot{m}_{HL,R} = (D_{HL,R})^{5/2}
\]  
(89)
Rearranging Eq. (89) to solve for the hot leg diameter ratio in terms of the mass flow rate ratio, yields:

$$D_{HL,R} = (\dot{m}_{HL,R})^{2/5}$$  \hspace{1cm} (90)

The AP600 design implements cold legs that are elevated above the hot legs. During drain-down situations, two-phase flow pattern transitions become important. The analysis performed for the cold legs was the same as that performed for the hot legs. Thus the scaling ratios were identical. Fig. 9 shows the flow regime transition boundaries for the AP600 and the APEX model hot legs. The APEX model hot leg overlays the AP600 curve exactly. It should be noted that by satisfying the modified Froude ratio given in Eq. (86), the counter-current flooding limitation (CCFL) is also approximated (Ohnuki et al., 1987).

7.3.6. Core heat transfer
The core simulator in the APEX test facility provides the energy that drives single and two-phase natural circulation in the loop. The number of rods, the rod spacing and rod diameter were determined by considering the core heat transfer processes of interest. This was done by examining three of the core heat transfer similarity criteria developed at the constituent level. They are the Modified Stanton Number, \( \Pi_{HT} \), the Biot Number, \( \Pi_{Bi} \) and the temperature ratio, \( \Theta_s \).

Boiling is an extremely effective heat transfer mechanism. As a result, the temperature difference between the heater surface and the fluid saturation temperature is usually quite small. Therefore, requiring exact similitude of the boundary layer temperature difference by forcing \((\Pi_{HT})_R = (\Pi_{Bi})_R = (\Theta_s)_R = 1 \) was not necessary.

However, a heat transfer phenomenon which was scaled is the critical heat flux. The following scaling criterion was implemented:

$$\Pi_{CHF,R} = \left( \frac{q''_{CRIT}}{q''_{c,ROD}} \right)_R = 1$$  \hspace{1cm} (91)

The following critical heat flux correlation, developed by Katto (1978) for low flow two-phase natural circulation, was used for scaling purposes.

$$q''_{CRIT} = \frac{G_c h_{lg} d_{h,c}}{4l_c} \left[ \left( \frac{\sigma_{hl}}{G_{c,k}^2} \right)^{0.043} + \frac{h_{sub}}{h_{lg}} \right]$$  \hspace{1cm} (92)

where \( G_c \) is the mass flux through the core, given by:

$$G_c = \frac{\dot{m}_c}{a_c}$$  \hspace{1cm} (93)

\( \sigma \) is the surface tension and \( d_{h,c} \) is the subchannel hydraulic diameter. For a square array of heated rods, the hydraulic diameter is given by:

$$d_{h,c} = \frac{4[s^2 - \pi d^2/4]}{\pi d}$$  \hspace{1cm} (94)

where \( s \) is the pitch (distance between the centerlines of adjacent rods) and \( d \) is the heater rod diameter.

The heater rod heat flux is given by:

$$q''_{c,ROD} = \frac{F_{q_c} d_l}{N \pi d l_c}$$  \hspace{1cm} (95)

where \( F_{q_c} \) is the hot channel peaking factor, \( \dot{q} \) is the total core power, and \( N \) is the total number of rods. Substituting Eqs. (95) and (92) into the similarity criteria given by Eq. (91) yields:

$$\frac{N \mu d k (d_{h,c})_R G_R}{(F_{q_c})_R q_R} \left[ \left( \frac{\sigma_{hl}}{G_{c,k}^2} \right)^{0.043} + \frac{h_{sub}}{h_{lg}} \right] = 1$$  \hspace{1cm} (96)

Eq. (96) offers some flexibility in selecting the number of rods, the rod diameter, and the rod spacing (hydraulic diameter). In addition to meeting the CHF criteria, core symmetry, geometric similarity and economic factors (i.e. rod number and standard rod sizes) were also considered in the selection.

7.3.7. Two-phase drift velocity
An equation for the drift velocity, \( v_{gi} \), was required to evaluate the drift flux number. The following equation, developed by Ishii (1977) was used:

$$v_{gi} = 0.2\left[ 1 - \left( \frac{\rho_{gs}}{\rho_{hl}} \right)^{1/2} \right] \left[ 1 + x(\Delta \rho / \rho_{hl}) \mu_c \right]$$

$$+ 1.4 \left( \frac{\sigma g \Delta \rho}{\rho_{hl}^2} \right)^{1/4}$$  \hspace{1cm} (97)

Substituting this equation into the drift flux number yields:
Scaling the drift velocity requires:

\[ \Pi_{\text{Nd}} = \left\{ \begin{array}{lr} 0.2 \left(1 - \left(\frac{\rho_{w}}{\rho_{c}}\right)^{0.5}\right)[1 + x(\Delta \rho / \rho_{c})] \\ + 1.4 \left(\frac{\sigma g \Delta \rho}{\rho_{w}}\right)^{1/4} \frac{x \Delta \rho}{\rho_{w}} \end{array} \right. \]  

(98)

7.3.8. Core two-phase friction and loss coefficients

Because geometric similarity was preserved between APEX and the AP600, and because form losses dominated the loop pressure drop, the pressure drop distribution in the APEX primary loop was properly preserved on a one-fourth scale basis. The scaled pressure drop across the core is reasonably preserved under two-phase conditions. The orifice number criteria was satisfied for the core. That is:

\[ \Pi_{o.c} = 1 \]  

(100)

for the core.

7.4. Evaluating system scaling ratios for APEX

By specifying a length scaling ratio and the diameter scaling ratio for the primary loop, the system scaling ratios (power, velocity, residence time and mass flow rate scaling ratios) were evaluated for both the single and two-phase regions of the loop. Multiplying the geometric scaling ratios by the dimensions of the full-scale nuclear steam supply system provided the dimensions for similar components in the small scale model. Table 2 summarizes the scaling ratios for the APEX test facility.

<table>
<thead>
<tr>
<th>Table 2</th>
<th>OSU APEX primary loop and core scaling ratios</th>
</tr>
</thead>
<tbody>
<tr>
<td>Core</td>
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</tr>
<tr>
<td>Length scaling ratio</td>
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</tr>
<tr>
<td>Rod diameter ratio</td>
<td>2.78:1</td>
</tr>
<tr>
<td>Hydraulic diameter ratio</td>
<td>4.28:1</td>
</tr>
<tr>
<td>Flow area ratio</td>
<td>1:48.04</td>
</tr>
<tr>
<td>Power ratio</td>
<td>1:96</td>
</tr>
<tr>
<td>Velocity ratio</td>
<td>1:2</td>
</tr>
<tr>
<td>Residence time scaling ratio</td>
<td>1:2</td>
</tr>
<tr>
<td>Mass flux ratio</td>
<td>1:4</td>
</tr>
<tr>
<td>Mass flow rate ratio</td>
<td>1:96.08</td>
</tr>
<tr>
<td>Hot and cold legs</td>
<td></td>
</tr>
<tr>
<td>Length scaling ratio</td>
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</tr>
<tr>
<td>Pipe diameter ratio</td>
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<tr>
<td>Pipe flow area ratio</td>
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</tr>
<tr>
<td>Pipe volume scaling ratio</td>
<td>1:192.16</td>
</tr>
<tr>
<td>Velocity ratio</td>
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<tr>
<td>Residence time scaling ratio</td>
<td>1:2</td>
</tr>
<tr>
<td>Mass flow rate ratio</td>
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</tr>
<tr>
<td>Pressurizer surge line</td>
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<tr>
<td>Length scaling ratio</td>
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<tr>
<td>Surge line diameter ratio</td>
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<td>Other primary loop components*</td>
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<td>Flow area ratio</td>
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</tr>
<tr>
<td>Volume scaling ratio</td>
<td>1:192.16</td>
</tr>
</tbody>
</table>

* Not including safety systems and reactor vessel downcomer.

7.5. Summary of natural circulation scaling analysis

The top-down system scaling analysis implemented a homogeneous equilibrium model to obtain the integral system two-phase scaling ratios for a natural circulation system. The primary focus of the analysis was to examine how the global loop flow rate is affected by scale. The analysis presented herein resulted in a cubic equation for the mixture velocity, Eq. (46). This means that the mass flow rate in the system may achieve the ‘most stable’ of three flow rate solutions or possibly oscillate between solutions. By using a
method of scaling single-state variable catastrophe functions (Reyes, 1994), a set of mathematical requirements were imposed on the coefficients of the cubic velocity equation. These mathematical requirements were satisfied to assure that the APEX model would be capable of achieving a similar family of scaled flow rate solutions. Implementing these mathematical requirements permitted one to obtain the fundamental scaling constant $\beta$. This scaling constant was then used to obtain the important two phase scaling ratios. An important result of this analysis was the observation that the family of scaled flow rate solutions could only be obtained when the two phase scaling criteria were satisfied as a set. Scaled loop flow rate behavior could not be obtained when each criterion was treated independently.

In summary, the following observations are made for the APEX test facility regarding natural circulation scaling:

1. The set of two-phase scaling criteria have been satisfied as a set in APEX so it would be capable of producing a family of scaled loop flow rates characteristic of the full scale AP600.
2. An examination of the two phase scaling ratios obtained in this analysis reveal that they are identical to those of Ishii and Kataoka (1983) when the thermal center length scaling ratio equals the component length scaling ratio.
3. For long term cooling at low pressure conditions, fluid property similarity is maintained operationally. The greatest challenge to simulating AP600 behavior was establishing the single and two-phase region pressure drop similarity. Careful selection of the pipe diameters and the use of orifices has given good results as is shown in Sections 10 and 11 of this paper.

8. System depressurization scaling analysis

One of the significant difficulties countered in operating a reduced pressure facility is that of establishing the proper initial conditions for testing. For APEX, much of the phenomena of interest, such as long-term recirculation cooling, occurs at pressures well within the capability of the facility. However, to reach the onset of long-term cooling, the system must first evolve through a variety of states (mass and energy inventories), whose trajectories are controlled by the dominant transport processes such as break flow rate and core power. The initial state of the full scale system, just prior to a transient, cannot be duplicated in a reduced pressure system such as APEX. Therefore, a method was needed to define the initial and boundary conditions for APEX, just prior to the transient, such that the prototypic trajectory of system states can be duplicated on a ‘scaled’ basis, leading to the correctly scaled initial conditions for long-term cooling. This section describes the set of similarity criteria used to scale ‘open system’ depressurization transients.

The specific objectives of this section are as follows:
1. Define the initial and boundary conditions for the range of tests being considered,
2. Define the break sizes,
3. Provide a method of comparing full-scale pressure to model pressure by defining a reference pressure,
4. Present a method to achieve the properly scaled initial conditions for long-term cooling,
5. Maintain the timing of events on a scaled basis, and
6. Identify the transition pressure at which fluid property similitude begins.

To meet these specific objectives, the governing set of equations for the depressurization of a two-phase fluid system were developed. This resulted in obtaining a depressurization rate equation. Next, a top-down system level scaling analysis was performed for a system containing multiple vent and injection paths. This included the development of scaling criteria for break flow rate dominated and volumetric expansion dominated systems. Last, a bottom-up analysis was performed to describe the scaling of the source terms (such as core decay heat) and the local transport processes such as the critical flow through the breaks. Fig. 10 presents a list of system and local processes addressed by the scaling analysis.
8.1. Governing equations for the two-phase fluid system depressurization

The mass conservation equation for a control volume undergoing a depressurization event is given by:

\[
\frac{dM}{dr} = \sum \dot{m}_{in} - \sum \dot{m}_{out} \tag{101}
\]
where \( M \) is the fluid mass within the control volume and \( \dot{m} \) represents the mass flow rate entering or leaving the control volume.

The energy conservation equation for the fluid is expressed as follows:

\[
d\left( \frac{U}{M} \right) = \left( \frac{\dot{e}}{\bar{c}_p} \right) \frac{dP}{dV} + \dot{Q}\frac{dV}{dt} \tag{102}
\]

where \( U \) is the internal energy of the fluid within the control volume, \( h \) is the enthalpy of the fluid entering or leaving the control volume, \( \dot{Q} \) is the rate of net energy into the system; \( P \) is the system pressure and \( V \) is the system volume.

The specific internal energy and the specific volume are defined respectively as follows:

\[
e = \frac{U}{M} \tag{103}
\]

\[
v = \frac{V}{M} \tag{104}
\]

The total changes in specific internal energy is written in terms of partial differentials with respect to pressure and specific volume as follows:

\[
d\epsilon = \left( \frac{\dot{e}}{\bar{c}_p} \right) \frac{dP}{dV} \tag{105}
\]

Substituting Eq. (103) into Eq. (102) yields:

\[
\frac{dM\epsilon}{dt} = \Sigma(\dot{m}h)_{in} - \Sigma(\dot{m}h)_{out} + \dot{Q}_{Net} - P \frac{dV}{dt} \tag{106}
\]

Expanding the first term on the LHS of Eq. (106), substituting Eq. (101) and rearranging yields:

\[
M \frac{d\epsilon}{dt} = \Sigma[\dot{m}_{in}(h_{in} - e)] - \Sigma[\dot{m}_{out}(h_{out} - e)] + \dot{Q}_{Net} - P \frac{dV}{dt} \tag{107}
\]

Substituting Eq. (105) into Eq. (107), and rearranging yields:

\[
M \left( \frac{\dot{e}}{\bar{c}_p} \right) \frac{dP}{dV} = \Sigma\dot{m}_{in}(h_{in} - e) - \Sigma\dot{m}_{out}(h_{out} - e) + \dot{Q}_{Net} - P \frac{dV}{dt} \tag{108}
\]

Using Eq. (104), and mass conservation equation, the last term on the RHS of Eq. (108) is written as:

\[
M \left( \frac{\dot{e}}{\bar{c}_p} \right) \frac{dV}{dt} = \left( \frac{\dot{e}}{\bar{c}_p} \right) \frac{dV}{dt} - v \left( \frac{\dot{e}}{\bar{c}_p} \right) \tag{109}
\]

Substituting back into Eq. (108) yields:

\[
M \left( \frac{\dot{e}}{\bar{c}_p} \right) \frac{dP}{dV} = \Sigma\dot{m}_{in} \left[ h_{in} - e + v \left( \frac{\dot{e}}{\bar{c}_p} \right) \right] - \Sigma\dot{m}_{out} \left[ h_{out} - e + v \left( \frac{\dot{e}}{\bar{c}_p} \right) \right] + \dot{Q}_{Net} \tag{110}
\]

which is the ‘depressurization rate equation’. For a control volume with rigid boundaries, which is typically the case, Eq. (110) becomes:

\[
M \left( \frac{\dot{e}}{\bar{c}_p} \right) \frac{dP}{dV} = \Sigma\dot{m}_{in} \left[ h_{in} - e + v \left( \frac{\dot{e}}{\bar{c}_p} \right) \right] - \Sigma\dot{m}_{out} \left[ h_{out} - e + v \left( \frac{\dot{e}}{\bar{c}_p} \right) \right] + \dot{Q}_{Net} \tag{111}
\]

Eq. (111) provides the basis for scaling the depressurization behavior.

8.2. Top-down system level depressurization scaling analysis

The mass conservation equation, Eq. (101), is expressed in dimensionless form by dividing each term by its respective initial condition and further dividing by the mass flow rate of the fluid leaving the break. This results in the following dimensionless mass balance equation:

\[
\tau_{sys} \frac{dM^+}{dt} = \Pi_m \dot{m}^+_{in} - \dot{m}^+_{out} \tag{112}
\]

where the superscript \( + \) terms indicate normalization with respect to initial conditions. The residence time constant, \( \tau_{sys} \), for the depressurization transient is given by:
The characteristic time ratio is given by:

\[ \tau_{\text{sys}} = \frac{M_o}{\Sigma \dot{m}_{\text{out}, o}} \tag{113} \]

and the characteristic time ratio is given by:

\[ \Pi_m = \frac{\Sigma \dot{m}_{\text{in}, o}}{\Sigma \dot{m}_{\text{out}, o}} \tag{114} \]

Here, \( \Pi_m \) is the system mass flow rate ratio. For a constant injection flow rate, \( \Pi_m \) represents the total liquid mass injected into the control volume during the residence time (\( \tau_{\text{sys}} \)).

Eq. (111) can be expressed in dimensionless form by dividing each term by its respective initial condition and further dividing both sides of the equation by the fluid energy flow rate initially leaving the break. This results in the following dimensionless energy balance equation:

\[
\tau_{\text{sys}} \epsilon_o M^+ \left( \frac{\partial e}{\partial P} \right)_v + \frac{dP^+}{dt} = \Pi_h \Sigma \dot{m}_{\text{in}, o} \left[ h_{\text{in}} - e + v \frac{\partial e}{\partial v} \right]_p^+ \\
- \Sigma \dot{m}_{\text{out}, o} \left[ h_{\text{out}} - e + v \frac{\partial e}{\partial v} \right]_p^+ + \Pi_f \dot{q}_{\text{Net}}^+ \tag{115}
\]

The characteristic time ratios are given by the following:

\[
\Pi_h = \frac{\Sigma \dot{m}_{\text{in}, o} \left[ h_{\text{in}} - e + v \frac{\partial e}{\partial v} \right]_p^+}{\Sigma \dot{m}_{\text{out}, o} \left[ h_{\text{out}} - e + v \frac{\partial e}{\partial v} \right]_p^+} \tag{116}
\]

\[
\Pi_f = \frac{\dot{q}_{\text{Net}, o}}{\Sigma \dot{m}_{\text{out}, o} \left[ h_{\text{out}} - e + v \frac{\partial e}{\partial v} \right]_p^+} \tag{117}
\]

\( \Pi_h \) is the energy flow rate ratio. It represents the ratio of the total energy change due to fluid injection to that caused by the break flow. \( \Pi_f \) is the power ratio. It represents the ratio of the net heat input to the system to the rate of fluid energy transport through the break.

The key fluid property for the depressurization process is the dilation, \( \epsilon_o \). For saturated conditions at the break and vent paths, it is given by:

\[
\epsilon_o = f \left( \frac{P_{\text{in}}}{h_{\text{fg}}} \right) \tag{118}
\]

Eq. (118) reveals that the dilation couples the system intensive energy change to the intensive energy at the break. For high pressure systems venting to the ambient, the fluid properties at the break are determined at critical flow conditions. Therefore, \( h_{\text{out}} \) equals \( h_{\text{out}}^* \), where the superscript denotes critical flow conditions.

For saturated mixtures, it can be shown that:

\[
\epsilon_o = f \left( \frac{P_{\text{in}}}{h_{\text{fg}}} \right) \tag{119}
\]

To determine which of the transport processes are most important to a depressurization transient and consequently which parameters should be scaled in the test facility, an order of magnitude analysis was performed. Fig. 11 presents the numerical values for the characteristic time ratios presented above as evaluated for AP600 conditions. The characteristic time ratios were evaluated at 5% and 3% decay power using both the Henry–Fauske (Henry and Fauske, 1971) and homogeneous equilibrium critical flow models to estimate the break flow rates at saturated liquid conditions. Fig. 11 indicates that for break diameters greater than \( 5 \text{ cm} \) \((\sim 2 \text{ in.})\), the depressurization process is dominated by the mass flow rate leaving the system through the break. That is, the values for \( \Pi_h \) and \( \Pi_f \) are much smaller than unity. However, as the break diameters decrease, these two \( \Pi \) groups become increasingly important and the depressurization behavior is dominated by the volumetric expansion within the control volume.

Thus, for scaling the depressurization behavior associated with breaks smaller than 1.25 cm \( (1\text{ in.}) \), the characteristic time ratios associated with the energy equation were most important. However, for scaling the depressurization behavior associated with breaks larger than 5 cm \( (2\text{ in.}) \), scaling the break mass flow rate was most important.
8.2.1. Scaling depressurization processes dominated by break or vent path flow rate (AP600 breaks ≥ 5 cm)

For breaks greater than or equal to 5 cm (2 in.) in diameter in the AP600, Π mâ, Π h, and Π f will be less than one. Therefore, the break flow rate will dominate the depressurization rate. That is, the two-phase fluid residence time constant for the system will be dictated by the break flow rate. The two-phase fluid residence time constant, Eq. (113), is written in terms of a time scaling ratio as follows:

\[ t_{\text{sys,R}} = \frac{\rho_{\text{TPO}} V_{\text{TPO}}}{\sum \dot{m}_{\text{out,o}} m_{\text{R}}} \]  

(120)

or in terms of mass flux as follows:

\[ t_{\text{sys,R}} = \frac{\rho_{\text{TPO}} V_{\text{TPO}}}{\sum C_D G_{e,o} a_{e,o}} \]  

(121)

where \( C_D \) is the discharge coefficient of the break or ADS valve. All of the processes in APEX were scaled such that:

\[ t_{\text{sys,R}} = 0.5 \]  

(122)

Furthermore, the system volumes were scaled such that:

\[ V_R = 1/192 \]  

(123)

Substituting Eqs. (122) and (123) into Eq. (121) yields:

\[ \left[ \frac{\sum C_D G_{e,o} a_{e}}{\rho_{\text{TPO}}} \right]_{\text{R}} = \frac{1}{96} \]  

(124)

This requirement was evaluated for each of the AP600 breaks, 5 cm (2 in.) or greater, and the ADS valves in the bottom-up scaling analysis. Sections 10 and 11 show APEX comparisons to computer calculations and full pressure experiments.

8.2.2. Scaling the injection flow rates (\( \Pi_{\text{in,R}} \))

Eq. (114) is written as a \( \Pi \) group ratio as follows:

\[ \Pi_{\text{in,R}} = \frac{\sum G_{in} a_{in}}{\sum C_D G_{e,o} a_{e}} \]  

(125)

where \( G_{in} \) is the injection mass flux and \( a_{in} \) is the injection flow area. Because of geometric similarity, the discharge coefficients for the APEX and AP600 injectors are identical.

Setting Eq. (125) equal to one yields:

\[ [\sum C_D G_{e,o} a_{e}]_{\text{R}} = [\sum G_{in} a_{in}]_{\text{R}} \]  

(126)
Substituting Eq. (126) into Eq. (124) yields the scaling ratio for the injection flow rates:

\[
\frac{\sum G_{in} a_{in}}{\rho_{TPo} R} = \frac{1}{96}
\]  

(127)

A similar analysis was performed to obtain cross-sectional flow areas for the CMT, Accumulators, and IRWST injector nozzles. APEX data comparisons are presented in Sections 10 and 11.

8.2.3. Scaling depressurization processes dominated by system energy/volumetric expansion (breaks/vents ≤ 2.5 cm)

For breaks less than or equal to 2.5 cm in diameter in the AP600, \( \Pi_F \) is much greater than one prior to ADS operation. Both \( \Pi_m \) and \( \Pi_h \) remain much less than \( \Pi_F \) during this same period. Therefore, the system vapor generation rate, which dictates the fluid volumetric expansion, will dominate the depressurization rate. Under these conditions, it was necessary to scale the break flow using the following scaling ratio:

\[
\left[ \frac{\Pi_F}{\epsilon_o R} \right]_R = 1
\]

(128)

Substituting Eqs. (117), (118) and (128) yields the break mass flow rate scaling ratio required to simulate transients dominated by the volumetric expansion of the fluid. That is,

\[
\left[ \frac{q_{net}}{P_o \left( \frac{T}{T_o} \right)^{\frac{\epsilon}{P}} \sum C_D G_{e,o} \epsilon_{e} R} \right] = \frac{1}{96}
\]  

(129)

Eq. (129) was used to scale AP600 breaks 2.54 cm (1 in.) in diameter or less.

8.3. Bottom-up depressurization scaling analysis

The objective of this bottom-up analysis was to obtain the closure relations needed to evaluate the scaling ratios developed through the top-down analysis as shown in Fig. 10. These closure relations are dependent on fluid properties. Therefore, the first step in the bottom-up analysis was the development of the method to relate fluid properties in the AP600 to those at reduced pressure in APEX. The following sections examined two of the processes of interest; pressure scaling and core decay power.

8.3.1. Pressure scaling

The success of scaling depressurization behavior in reduced pressure test facilities ultimately lies in the fact that several properties of fluids in phase equilibria exhibit self-similarity over a wide range of conditions. As explained by Briggs and Peat (1989) in their discussion on Mandelbrot fractals, self-similarity is a repetition of detail at descending scales. The following important property group exhibits self similarity over a wide range of saturation temperatures:

\[
\frac{P_{o}}{h_{ig}}
\]

(130)

This group represents the ratio of the pressure work associated with phase change to the total energy associated with phase change. In this equation, \( P \) is the saturation pressure, \( v_{ig} \) is the change in specific volume and \( h_{ig} \) is the latent heat of vaporization. Note that the dilation for saturated mixtures depends on this group. If one graphs this quantity against the corresponding saturation temperature a linear trend is observed beginning at the triple point temperature, \( T_t \), and extending nearly to the critical point temperature, \( T_c \). The saturation temperature can be expressed as a dimensionless quantity as follows:

\[
\theta = \frac{T - T_t}{T_c - T_t}
\]

(131)

Fig. 12 presents a graph of \( \psi \) versus \( \theta \) for water at saturated conditions (Irey et al., 1967; Keenan et al., 1969). This figure illustrates that the data is quite linear for the range of dimensionless temperatures given by:

\[
0 \leq \theta \leq 0.8
\]

(132)

As the critical point is approached, for values of \( \theta \) greater than 0.8, the trend becomes non-linear. The intercept is the value of \( \psi \) evaluated at the triple point. That is:

\[
\psi(0) = \psi_t
\]

(133)

The slope is a constant given by
Numerous fluids display the same trend as observed in Fig. 12 and can be plotted on a single graph in terms of \((c_i - \psi_t)\) versus \(\theta\).

Based on the results, the equation relating \(\psi\) and \(\theta\) is given by:

\[
\psi = c_i \theta + \psi_t \tag{135}
\]

For water, \(c_i = 0.0926\) and \(\psi_t = 0.05038\).

### 8.3.1.1. Equation of state for saturated pressure and temperature

The Clausius–Clapeyron equation is the classical differential equation that defines the slope \(dP/dT\) for a phase equilibrium curve. It is derived by assuming that the Gibb's free energies for the two phases being considered are equal, (Lay, 1990). Using the standard definition for the Gibb's free energy and relating the change in entropy to the latent heat of vaporization and the saturation temperature yields the well-known Clausius–Clapeyron equation:

\[
\frac{d\psi}{d\theta} = c_i \tag{134}
\]

The Clausius–Clapeyron equation is used with Eq. (135) to obtain an equation of state for the saturation pressure and temperature. Rewriting Eq. (135) as follows:

\[
\frac{P}{v_{ig}} = \frac{h_{fg}}{T} = bT + a \tag{137}
\]

where

\[
a = \psi_t - bT_i \tag{138}
\]

and rearranging Eq. (137) yields:

\[
h_{fg} = \frac{P}{v_{ig}} = \frac{P}{(bT + a)} \tag{139}
\]

where:

\[
b = \frac{c_i}{T_c - T_t} \tag{140}
\]

Substituting Eq. (139) into the Clausius–Clapeyron equation yields:

\[
\frac{dP}{dT} = \frac{P}{T(bT + a)} \tag{141}
\]

Separating the variables and integrating both sides yields an equation of state for saturation pressure in terms of saturation temperature. That is:
\[
\int_{P_i}^{P_f} \frac{dP}{P} = \int_{T_i}^{T_f} \frac{dT}{T(bT + a)}
\]

the solution of which is:

\[
P = P_i \left[ \left( \frac{a + bT_i}{a + bT_f} \right) \left( \frac{T_f}{T_i} \right) \right]^{1/a}
\]

Where \( P_i \) and \( T_i \) are the saturation pressure and temperature at the triple point, respectively. Eq. (143) is the state equation that relates saturation pressure to saturation temperature.

Comparisons of Eq. (143) to tabulated values of saturation pressure and temperature of various fluids indicates excellent agreement; having \( R^2 \) values greater than 0.994.

8.3.1.2. Scaled processes using similar fluids in phase equilibrium. Let us now turn our attention to the problem of relating the saturation pressure and temperature in APEX to the same properties in a full pressure AP600. The scaling process is simplified for the case of APEX and the AP600 because they implement the same working fluid. Assuming that the depressurization process evolves along the saturation curve, then Eq. (143) is directly applicable.

Eq. (143) has been normalized using the saturation pressure and temperature corresponding to the triple point of the fluid. However, the saturation pressure and temperature corresponding to any point between \( \theta \) equal to zero and 0.8 would serve equally well. For depressurization processes, it is convenient to select the initial saturation pressure and temperature as the ‘reference’ parameters. Thus, Eq. (143) becomes:

\[
P = P_o \left[ \left( \frac{a + bT_o}{a + bT_R} \right) \left( \frac{T_R}{T_o} \right) \right]^{1/a}
\]

where the subscript ‘\( o \)’ denotes a reference property evaluated at the initial saturated equilibrium conditions and the constants ‘\( a \)’ and ‘\( b \)’ are as defined previously. Eq. (144) is written as a scaling ratio as follows:

\[
\left( \frac{P}{P_R} \right) = \left[ \left( \frac{a + bT_o}{a + bT_R} \right) \left( \frac{T_R}{T_o} \right) \right]^{1/a}
\]

where the subscript ‘\( R \)’ denotes the ratio of model to plant properties. Because the same fluid is used in both cases, the coefficients \( a \) and \( b \) are identical in APEX and the AP600.

Substituting Eq. (137) into Eq. (145) and rearranging yields:

Fig. 13. Effect of pressure scaling on the term on the right-hand-side of Eq. (146).
Fig. 13 reveals that the right-hand side of Eq. (146) is essentially unity for all values of pressure ratios \( P/P_o \) in the model and the prototype. Thus:

\[
\left( \frac{P}{P_o} \right)_R = 1
\]

Substituting this result into Eq. (146) yields:

\[
\left( \frac{P}{P_o} \right)_R = \left[ \frac{(h_{fg} T/\nu_{fg})}{(h_{fg} T/\nu_{fg})_R} \right]^{1/(a + 1)}
\]

(146)

Essentially the same result can be obtained using the graphical approach presented by Kocamustafaogullari and Ishii (1986).

Substituting Eq. (148) into Eq. (145) yields an equation that can be used to relate the saturation temperatures in the model to those of the plant. After some algebra, one obtains:

\[
T_P = \frac{a}{T_m(a + bT_o)_R - b}
\]

(150)

The results herein are applicable to the range of dimensionless temperatures, \( \theta \), between zero and 0.8.

Eqs. (149) and (150) relate the saturation pressures and temperatures of a reduced pressure scale model to those of a full pressure prototype. This allows direct data comparisons when the process being examined evolves along the phase equilibrium curve. Fig. 14 shows the pressure scaling relationship between the AP600 and the APEX facility.
8.3.2. Core decay power scaling

The purpose of this analysis was to obtain time dependent core power profiles for APEX that best simulate the AP600 depressurization behavior while establishing prototypical initial conditions for long-term cooling. The following specific requirements were satisfied:

1. The total core energy (integrated power) deposited into the system was scaled to assure prototypical initial conditions for long-term cooling.

2. When fluid property similitude exists, the core power was scaled according to the ideal power scaling ratio, 1:96. Fluid property similitude will exist subsequent to reaching ADS 4 activation.

8.3.2.1. Core decay power profiles. Fig. 15 presents the ideal power for APEX using the American National Standard for Decay Heat Power in Light Water Reactors, 1979 (ANSI/ANS-5.1.24, 1979) and assuming fluid property similitude from the onset of the transient. That is:

\[
g_{\text{core,ideal,R}} = 0.0104 \times (1:96) \quad (151)
\]

The actual APEX core power is also shown on Fig. 15. Approximately 1 min after transient initiation, the APEX core power crosses the ideal power curve. APEX power is held constant to 140 s to obtain the correct integrated energy. After 140 s the power is permitted to drop to the ideal power curve for the remainder of the transient. Note that the time scale for the APEX decay power curve is half that of the AP600 to be consistent with the other transport processes in the facility.

Because ADS actuation occurs early in SBLOCAs greater than 5 cm (i.e. less than 600 s for a 5-cm break), adjusting the decay power to achieve pressure scaled two-phase loop natural circulation is not necessary since this portion of the transient is very short. The APEX power curve satisfies the requirement that subsequent to reaching ADS 4 activation, the power must be scaled according to the ideal power scaling ratio given by Eq. (151).

![Fig. 15. APEX decay power profile.](image-url)
8.3.2.2. Integrated power profiles. Establishing initial conditions for ADS 4 operation and long-term cooling requires that the total energy input into the system, to the point of reaching the transition pressure, be properly scaled. The integrated core power ratio is written as follows:

\[
(E_{\text{core}})_R = \frac{\int_{t_m}^{\infty} q_{\text{core},m} \, dt_m}{\int_{t_p}^{\infty} q_{\text{core},p} \, dt_p}
\]  

(152)

where \( t_m \) is the time it takes in the model to reach the transition pressure and \( t_p \) is the time it takes in the AP600 to reach the transition pressure. Since the ideal core power scaling ratio is 1:96 and the ideal time scale is 1:2, the integrated core power ratio should be:

\[
(E_{\text{core}})_R = 1:192
\]  

(153)

Fig. 16 presents the integrated core power for the profile given by Fig. 15. Note that the integrated power for the model is relatively close to that of the ideal integrated power throughout the transient. After reaching \( \sim 200 \) s, the APEX and ideal curves overlap.

8.4. Summary of depressurization scaling analysis

Top-down and bottom-up scaling analyses were performed to define the initial conditions for APEX depressurization tests. A method of relating AP600 pressure and fluid properties at saturation conditions to APEX conditions and of scaling AP600 decay power was developed.

The depressurization behavior was divided into two main categories; break/vent flow dominated behavior and fluid volumetric expansion dominated behavior. Break and ADS valve sizes for APEX, corresponding to AP600 scenarios, were selected by implementing the depressurization rate equation. The adequacy of these scaling methods are examined in Section 9 through 11 of this paper.
9. Assessment of scaling distortions and critical attributes

This section presents the dominant transport processes for the AP600 SBLOCA, assesses the degree of distortion in APEX relative to the dominant processes and specifies the 'critical physical attributes' of the OSU APEX Test Facility that were used in the quality assurance program.

9.1. Dominant processes

The method used to determine which processes governed a particular mode of AP600 SBLOCA operation was described in Section 5.1 of this paper. Eqs. (9)–(11) provide the physical meaning of the characteristic time ratio. To identify the dominant processes for the SBLOCA, and how they scale in APEX, numerical estimates of the characteristic time ratios for the AP600 and APEX were obtained for each hierarchical level of interest. The scaling analysis results were summarized by plotting the AP600 characteristic time ratios, as evaluated using initial conditions, against the specific frequencies for the four phases of an AP600 SBLOCA considered in the detailed scaling analysis. That is, natural circulation, depressurization, venting, draining, and injection, and sump recirculation.

Fig. 17 presents the characteristic time ratios for all of the AP600 processes identified by the SBLOCA PPIRT. The characteristic time ratios, \( \Pi_k \), are plotted against the specific frequency, \( \omega_k \), of each process. The slope of the data for each phase is the fluid residence time in the control volume, \( t_k \), as given by Eq. (9). Fig. 17 indicates that all of the natural circulation and long-term recirculation processes have essentially the same fluid residence time, \( t_k \). Fig. 17 also reveals that a majority of the processes examined have characteristic time ratios significantly less than one. Therefore, these processes did not merit addi-
tional consideration from the standpoint of scaling. Furthermore, for modes of operation involving multiple processes, it was observed that among the set of processes having $\Pi_k$ greater than one, a few processes have values of $\Pi_k$ several orders of magnitude greater than the values for the remaining processes comprising the set. This permitted an additional reduction in the number of processes which needed to be considered for scaling purposes. That is, in some cases only one or two of the $\Pi_k$ values dominated the governing equation and hence the mode of operation.

The characteristic time ratios were used to develop the similarity criteria for APEX by requiring

$$[\Pi_k]_{\text{APEX}} = [\Pi_k]_{\text{AP600}} \quad (154)$$

for the dominant processes and by requiring all of the residence time constants to satisfy:

$$[t_k]_{\text{LR}} = 0.5 \quad (155)$$

The characteristic time ratios for the dominant AP600 transport processes are summarized in Table 3.

9.2. Scaling distortions

As with any reduced scaled system, it was impossible to satisfy all of the similarity criteria simultaneously. Therefore, some scaling distortions are present in the APEX facility. Eq. (13) of Section 5.3 presents the method used to quantify distortions in APEX.

In addition to scaling the characteristic time ratios for the dominant processes, extensive effort was made to preserve the one-half scale time constant requirement given by Eq. (155). Table 4 summarizes the fluid residence time scaling results and Table 5 presents the distortion factors for all of the dominant processes which were identified. These tables indicate that the dominant processes were reasonably scaled in APEX.

9.2.1. Evaluation of two-phase natural circulation core transport processes (pressure scaled)

Numerical values for the core fluid residence time, the specific frequencies, the characteristic time ratios and the distortion factors for the core transport processes that occur under, pressure scaled, two-phase natural circulation conditions were calculated for the AP600 and APEX. The fluid velocity at the core inlet was calculated using Eq. (46). The calculations assumed saturated conditions in the primary loop with an APEX reference pressure of 320 psia and a corresponding AP600 reference pressure of 1080 psia.

For these operating conditions and geometry, the residence time of the fluid in the APEX core was found to be $\sim 4.4$ s. For the AP600 core, the fluid residence time was found to be $\sim 8.7$ s. Thus a one-half time scale was maintained.

The specific frequencies indicated that the dominant processes were characterized by the friction number, the enthalpy number and the drift velocity number. The least important processes were characterized by the phase change number, the density number and the Froude number.

The friction number was preserved because it is an adjustable parameter. The drift velocity number was divided into a local and distributed drift velocity. The distributed velocity was found to be reasonably modeled in the facility having a distortion factor of 4%. The local drift velocity was not preserved. However, this did not significantly affect the void-quality distribution. The Froude Number was also preserved.

The largest distortion occurred in the enthalpy number. This was expected because it is a ratio of fluid property groups. This distortion was necessary to preserve the vapor quality at the core exit.

9.2.2. Evaluation of two-phase natural circulation core transport processes (fluid property similitude)

Numerical values for the core fluid residence time, the specific frequencies, the characteristic time ratios and the distortion factors for the core transport processes that occur under two-phase natural circulation conditions at low AP600 pressure when fluid property similarity exists were also calculated for the AP600 and APEX. The fluid velocity at the core inlet was calculated using Eq. (46). The calculations assumed saturated conditions in the primary loop with an APEX and AP600 pressure of 320 psia. For these operating conditions and geometry, the residence time of the
Table 3
Summary of characteristic time ratios and residence times for the dominant processes

<table>
<thead>
<tr>
<th>Characteristic time ratio, $\Pi_i$</th>
<th>Residence Time, $\tau_{ri}$</th>
<th>Operational mode</th>
<th>Critical physical attribute</th>
</tr>
</thead>
<tbody>
<tr>
<td>$\Pi_{Ri} = \frac{\beta_i \delta_i \rho_i \dot{q}_i}{\rho_i C_p \rho_i \dot{A}_i}$</td>
<td>$\frac{V_{i,a}}{Q_{i,a}}$</td>
<td>1φ natural circulation</td>
<td>$q_{c}, l_{c}, a_{c}, F_{sp}^{<em>}, L_{th}^{</em>}$</td>
</tr>
<tr>
<td>$\Pi_F = \left( \frac{\beta_i}{\beta_{th}} + K \right)$</td>
<td>$\frac{V_{i,a}}{Q_{i,a}}$</td>
<td>1φ and 2φ natural circulation and LCS recirculation</td>
<td>$\left( \frac{\beta_i}{\beta_{th}} + K \right)$</td>
</tr>
<tr>
<td>$\Pi_h = \left( \frac{h_{i,a}(1-\beta_i) \Delta \rho_i a_{i}}{q_{c}} \right)$</td>
<td>$\frac{V_{i,a}}{Q_{i,a}}$</td>
<td>2φ natural circulation and LCS recirculation</td>
<td>$q_{c}, l_{c}, a_{c}, F_{sp}^{<em>}, F_{TP}^{</em>}, L_{th}^{*}$</td>
</tr>
<tr>
<td>$\Pi_g = \left( \frac{d_{sys}}{\rho_i C_p \rho_i G \dot{A}_i} \right)$</td>
<td>$\frac{V_{i,a}}{Q_{i,a}}$</td>
<td>2φ system depressurization</td>
<td>$q_{sys}$, $V_T$, $C_D$, $a_e$</td>
</tr>
<tr>
<td>$\Pi_{CMT} = \left( \frac{H_{WL}}{a (T_w - T_{SAT})} \right)$</td>
<td>$\frac{P_{CMT}}{CMT_{draining}}$</td>
<td>CMT draining (hot wall)</td>
<td>$D_{CMT}^{<em>}, l_{CMT}, V_{CMT}^{</em>}$</td>
</tr>
<tr>
<td>$\Pi_{CMT, l} = \left( \frac{H_{WL}}{a (T_w - T_{SAT})} \right)$</td>
<td>$\frac{P_{CMT}}{CMT_{draining}}$</td>
<td>CMT draining (cold wall)</td>
<td>$D_{CMT}^{<em>}, l_{CMT}, V_{CMT}^{</em>}$</td>
</tr>
<tr>
<td>$\Pi_{HC} = \left( \frac{H_{WL}}{\rho_i C_p (R_{c} - R_{c}) a_{i}} \right)$</td>
<td>$\frac{P_{HC}}{CMT_{draining}}$</td>
<td>CMT draining (cold wall)</td>
<td>$D_{CMT}^{<em>}, l_{CMT}, V_{CMT}^{</em>}$</td>
</tr>
<tr>
<td>$\Pi_{IRWST} = \left( \frac{H_{IRWST}^{*}}{a_{i}} \right)$</td>
<td>$\frac{P_{IRWST}}{IRWST_{injection}}$</td>
<td>IRWST injection</td>
<td>$P_{IRWST}^{<em>}, l_{IRWST}^{</em>}$</td>
</tr>
<tr>
<td>$\Pi_{IRWST, l} = \left( \frac{H_{IRWST}^{*}}{a_{i}} \right)$</td>
<td>$\frac{P_{IRWST}}{IRWST_{draining}}$</td>
<td>IRWST draining</td>
<td>$P_{IRWST}^{<em>}, l_{IRWST}^{</em>}$</td>
</tr>
</tbody>
</table>
Table 3 (Continued)

<table>
<thead>
<tr>
<th>Characteristic time ratio, $\Pi$,</th>
<th>Residence Time, $\tau_{cv}$</th>
<th>Operational mode</th>
<th>Critical physical attribute</th>
</tr>
</thead>
<tbody>
<tr>
<td>$\Pi_{i,DC} = \left( \frac{q_{DC}}{(\rho h)_{DVI}} \right)_o$</td>
<td>$\rho V_{DC} \over \rho V_{DVI,o}$</td>
<td>Downcomer fluid heatup</td>
<td>$\left( \frac{t}{d+K} \right)_{DVI}$</td>
</tr>
<tr>
<td>$\Pi_{m,\text{sump}} = \left( \frac{m_{SL}}{\Sigma m_{in}} \right)_o$</td>
<td>$\rho V_{\text{sump}} \over \Sigma m_{in}$</td>
<td>LSC filling and recirculation</td>
<td>$\left( \frac{t}{d+K} \right)_{\text{SL}}$</td>
</tr>
</tbody>
</table>

* Required to scale fluid velocity.

The calculated values for the specific frequencies indicate that the dominant processes were characterized by the friction number, the enthalpy number and the drift flux number. The least important processes were characterized by the phase change number, the density number and the Froude number. The friction number was preserved because it is an adjustable parameter. The enthalpy number was preserved because of fluid property similarity.

9.2.3. Evaluation of break flow rate dominated depressurization processes

Two depressurization cases were examined for purposes of scaling: a 2-in. cold leg break and a double-ended DVI line break. For breaks larger than 5 cm (2 in.) in diameter in the AP600, $\Pi_F$ became much less than one; which indicated that the depressurization rate was dominated by the amount of mass and energy leaving with the fluid through the break. The 2-in. break was the smallest break/vent flow dominated transient and the double-ended DVI line break was the largest break flow dominated case studied in the APEX facility. In addition, all cases of depressurization caused by ADS operation were dominated by the mass and energy leaving the system. Numerical values for the system fluid residence time, the specific process frequencies, the characteristic time ratios and the distortion factors were calculated for the AP600 and APEX for each of these cases. Because all of the characteristic time ratios were less than one, an examination of their distortion factors was not needed.

Table 4
Summary of residence time constant scaling ratios (desired value ($\tau_{cvR} = 0.5$))

<table>
<thead>
<tr>
<th>Residence time</th>
<th>Value</th>
<th>Operational mode</th>
</tr>
</thead>
<tbody>
<tr>
<td>$\tau_{l,o}R$</td>
<td>0.5</td>
<td>$1\phi$ natural circulation</td>
</tr>
<tr>
<td>$\tau_{f,o}R$</td>
<td>0.5</td>
<td>$1\phi$ and $2\phi$ natural circulation and LCS recirculation</td>
</tr>
<tr>
<td>$\tau_{TP,o}R$</td>
<td>0.5</td>
<td>Depressurization</td>
</tr>
<tr>
<td>$\tau_{CMT}R$</td>
<td>0.5</td>
<td>CMT draining</td>
</tr>
<tr>
<td>$\tau_{ACC}R$</td>
<td>0.5</td>
<td>Accumulator injection</td>
</tr>
<tr>
<td>$\tau_{IRWST}R$</td>
<td>0.5</td>
<td>IRWST draining</td>
</tr>
<tr>
<td>$\tau_{DC}R$</td>
<td>1.0</td>
<td>Downcomer fluid transfer</td>
</tr>
<tr>
<td>$\tau_{\text{sump}}R$</td>
<td>0.5</td>
<td>Sump recirculation</td>
</tr>
</tbody>
</table>

Fluid in the APEX core was estimated to be 3.75 s. For the AP600 core, the fluid residence time was estimated to be 7.5 s; thus a one-half time scale was maintained. For the condition of fluid property similitude, the vapor quality at the core exit was preserved.

The calculated values for the specific frequencies indicate that the dominant processes were characterized by the friction number, the enthalpy number and the drift flux number. The least important processes were characterized by the phase change number, the density number and the Froude number. The friction number was preserved because it is an adjustable parameter. The enthalpy number was preserved because of fluid property similarity.
Table 5
APEX distortion factors for the AP600 dominant processes identified using the H2TS methodology

<table>
<thead>
<tr>
<th>Characteristic time ratio</th>
<th>Distortion factor (DF %)</th>
<th>Operational mode</th>
</tr>
</thead>
<tbody>
<tr>
<td>$\Pi_{Ri}$</td>
<td>0</td>
<td>1φ natural circulation</td>
</tr>
<tr>
<td>$\Pi_F$</td>
<td>0</td>
<td>1φ/2φ natural circulation</td>
</tr>
<tr>
<td>$\Pi_g$</td>
<td>0</td>
<td>2φ natural circulation with fluid property similitude/LCS recirculation</td>
</tr>
<tr>
<td>$\Pi_h$</td>
<td>(not scaled)</td>
<td>2φ natural circulation pressured scaled—core vapor quality preserved instead of $\Pi_b$</td>
</tr>
<tr>
<td>$\Pi_{F/CMT}$</td>
<td>17.4</td>
<td>CMT draining with hot walls</td>
</tr>
<tr>
<td>$\Pi_{C/IRWST}$</td>
<td>2.8</td>
<td>CMT draining with cold walls</td>
</tr>
<tr>
<td>$\Pi_{IRWST}$</td>
<td>6.3</td>
<td>IRWST draining (property similitude)</td>
</tr>
<tr>
<td>$\Pi_{IRWST}$</td>
<td>0</td>
<td>IRWST draining (property similitude)</td>
</tr>
<tr>
<td>$\Pi_{m,IRWST}$</td>
<td>-9.5</td>
<td>IRWST heatup (pressure scaled)</td>
</tr>
<tr>
<td>$\Pi_{d,IRWST}$</td>
<td>19.1</td>
<td>IRWST heatup (pressure scaled)</td>
</tr>
<tr>
<td>$\Pi_{DC}$</td>
<td>-16.7</td>
<td>Downcomer heat transfer during accumulator injection</td>
</tr>
<tr>
<td>$\Pi_{m, sump}$</td>
<td>~0</td>
<td>See Davis et al. (1996)</td>
</tr>
</tbody>
</table>

As anticipated, the fluid residence time for the DEDVI break case was significantly shorter than that of the 2-in. break case. For both cases, the APEX residence times were found to be approximately one-half that of the AP600 as required by the scaling criterion. Thus by preserving the physical parameters that comprise the residence time ratios (i.e. break flow area and system volume), the depressurization rates observed in APEX were reasonably scaled for breaks larger than 5 cm in diameter. This is demonstrated in Sections 10 and 11.

9.2.4. Evaluation of volumetric expansion dominated depressurization process

For 2.54 cm (1 in.) diameter breaks or smaller, the volumetric expansion due to vapor generation was found to dominate the depressurization rate. This was reflected in the value of $\Pi_F$, which exceeded a value of one for the AP600, and dominated the depressurization rate.

For a pressure scaled facility, the energy flow rate and the mass flow rate through the break cannot be matched simultaneously on a scaled basis. Two approaches could have been implemented to satisfy $\Pi_{F,R}$; either the core power could have been adjusted or the break size could have been adjusted.

Raising the power such that $q_{s,R}$ is $\sim 1:41$, would permit one to satisfy $\Pi_{F,R}$. However, raising the power would also distort the CMT recirculation and draining process which is governed by system energy. That is, interruption of CMT recirculation flow and the subsequent initiation of CMT draining depends on the rate at which steam accumulates in the CMT balance lines. This would also distort the timing for ADS actuation.

Reducing the break size while preserving $q_{s,R}$ at 1:96 would preserve $\Pi_F$. For this case the system volumetric expansion would be properly scaled and the rate at which steam accumulates in the CMT balance lines should also be reasonably modeled. Thus, the timing for the onset of CMT draining and ADS actuation should be preserved. Draining of the CMT will eventually actuate ADS which once again places the system in a break/vent flow dominated mode. This later approach was implemented in APEX with good success as shown in the following sections.

9.3. Critical attributes

The performance of safety tests in support of nuclear reactor licensing required adhering to strict quality assurance procedures. In particular, the requirements of ASME NQA-1 and NQA-2 to assure the quality of test data were satisfied as part of the OSU AP600 research program. All
aspects of the test that affect the quality of the test results were required to have demonstrable controls. This required that all of the critical physical attributes of a test facility be identified and controlled. The following definition was implemented. A critical physical attribute is a geometric property of a prototype that must be preserved in the test facility to adequately model the phenomena of importance.

An interesting feature of the H2TS methodology, not formally recognized previously, is that identification of the test facility critical physical attributes is a natural product of its implementation. Therefore, this section presents the list of test facility critical physical attributes obtained using the H2TS methodology and incorporated as an integral feature of the quality assurance program at OSU. This list was generated by studying the characteristic time ratios presented in Table 3. As shown in this table, the definitions of these characteristic time ratios include certain physical characteristics that must be preserved in APEX to best simulate the dominant processes for the AP600. Based on this review, the critical physical attributes of the OSU APEX Test Facility were compiled in Table 6. Identifying the critical attributes was a valuable asset to the Quality Assurance Program established for APEX testing.

10. Scaling assessment using NOTRUMP

To assess the scaling of the APEX facility, both the proposed scaled facility and the AP600 plant were modeled using the Westinghouse NOTRUMP (Meyer, 1985) small break LOCA systems code. The AP600 calculations were performed as part of the Westinghouse Standard Safety Analysis Report (SSAR) on the AP600. The objective of this study was to investigate if the APEX facility, which was scaled from first principles, would have a similar response to a small break transient in the AP600 based on code calculation. The comparisons helped verify the single- and two-phase natural circulation scaling logic discussed in Section 7. It was also the first assessment performed to determine if the APEX scale factors on length, velocity, and time would compare favorably with a code calculated AP600 transient. These initial comparisons were also valuable in testing the pressure scaling logic developed in Section 8 of this paper. The NOTRUMP comparisons supported the idea that many important aspects of an AP600 high pressure transient

<table>
<thead>
<tr>
<th>Critical attributes for the OSU APEX test facility</th>
</tr>
</thead>
<tbody>
<tr>
<td>1. Break flow areas</td>
</tr>
<tr>
<td>2. Primary side and PXS volume</td>
</tr>
<tr>
<td>3. Downcomer (from DVI centerline) and lower plenum metal mass</td>
</tr>
<tr>
<td>4. Primary side loop total friction and form loss coefficients</td>
</tr>
<tr>
<td>5. All primary side and PXS relative elevations</td>
</tr>
<tr>
<td>6. Pressurizer metal mass</td>
</tr>
<tr>
<td>7. Pressurizer inside diameter</td>
</tr>
<tr>
<td>8. Pressurizer volume</td>
</tr>
<tr>
<td>9. Rod diameter</td>
</tr>
<tr>
<td>10. Subchannel flow area</td>
</tr>
<tr>
<td>11. Total core power</td>
</tr>
<tr>
<td>12. Pressurizer surge line total friction and form loss coefficients</td>
</tr>
<tr>
<td>13. Pressurizer surge line diameter and bend radii</td>
</tr>
<tr>
<td>14. Steam generator tube surface area</td>
</tr>
<tr>
<td>15. Steam generator total friction and form loss coefficients</td>
</tr>
<tr>
<td>16. Hot leg inside diameter</td>
</tr>
<tr>
<td>17. ADS 1-4 line total friction and form loss coefficients</td>
</tr>
<tr>
<td>18. ADS valve throat areas</td>
</tr>
<tr>
<td>19. ADS sparger flow areas</td>
</tr>
<tr>
<td>20. CMT injection line total friction and form loss coefficients</td>
</tr>
<tr>
<td>21. CMT injection line inside diameter</td>
</tr>
<tr>
<td>22. CMT volume</td>
</tr>
<tr>
<td>23. CMT inside diameter</td>
</tr>
<tr>
<td>24. CMT metal mass</td>
</tr>
<tr>
<td>25. CMT balance line total friction and form loss coefficients</td>
</tr>
<tr>
<td>26. Accumulator volume</td>
</tr>
<tr>
<td>27. Accumulator line total friction and form loss coefficients</td>
</tr>
<tr>
<td>28. Accumulator injection line inside diameter</td>
</tr>
<tr>
<td>29. IRWST cross-sectional area</td>
</tr>
<tr>
<td>30. IRWST volume</td>
</tr>
<tr>
<td>31. Sump tank cross-sectional areas</td>
</tr>
<tr>
<td>32. Sump tank volumes</td>
</tr>
<tr>
<td>33. DVI line total friction and form loss coefficients</td>
</tr>
<tr>
<td>34. DVI diffuser flow area</td>
</tr>
<tr>
<td>35. Sump recirculation loop total friction and form loss coefficients</td>
</tr>
<tr>
<td>36. PRHR tube surface area</td>
</tr>
<tr>
<td>37. PRHR total friction and form loss coefficients</td>
</tr>
</tbody>
</table>
Table 7
Initial conditions for APEX test facility to model a 2-in. cold leg break

<table>
<thead>
<tr>
<th>Reactor cooling system</th>
<th></th>
</tr>
</thead>
<tbody>
<tr>
<td>Core power</td>
<td>0.700 MWt</td>
</tr>
<tr>
<td>Core flow</td>
<td>116.7 lb s⁻¹</td>
</tr>
<tr>
<td>Pressurizer pressure</td>
<td>400 psia</td>
</tr>
<tr>
<td>Core inlet temperature</td>
<td>410.4°F</td>
</tr>
<tr>
<td>Core outlet temperature</td>
<td>415.6°F</td>
</tr>
</tbody>
</table>

| Secondary                              |                  |
| Steam generator temperature            | 407.6°F          |
| Break size simulated                   | 2-in. cold leg break |

The break which was selected was a 5 cm (2 in.) break of the cold leg on the core makeup tank side of the plant. The initial conditions for the APEX facility for this break are shown in Table 7 and were used as the input to the APEX NOTRUMP calculations. The reference pressure conditions for both the plant calculation and the APEX test calculation were chosen in accordance with the scaling requirements presented in Section 8. Fig. 18 shows the NOTRUMP predicted pri-

![Fig. 18. AP600 plant pressurizer and steam generator pressures for a 2-in. cold leg break.](image-url)
mary and secondary pressures for the AP600 plant and indicates that the primary pressure is constant and slightly larger than the secondary pressures at 200 s. A similar comparison is shown in Fig. 19 for the APEX facility which has the equivalent 5 cm (2 in.) cold leg break modeled. Fig. 19 indicates that the primary pressure becomes constant at 55 s. The NOTRUMP calculated results in Fig. 19 do not take into account the one-half time scaling factor for APEX. The NOTRUMP calculation of the APEX transient used the scaling study dimensions, volumes, areas, lengths, scaled injection lines pressure losses, pressure scale conditions; such as the accumulator setpoints, steam generator secondary side safety valve setpoints, core power, break areas, ADS
Fig. 20. Normalized pressure comparisons between AP600 plant and APEX facility.

valve areas, and the CMT design. All the information needed to model the APEX facility was derived from the detailed scaling study (Reyes et al., 1995).

Table 7 shows the scaling relationships between the plant and the APEX facility, which accounts for the time scale difference (two-to-one for APEX) and the normalization of the flows, pressure, two-phase mixture levels and total system mass. The initial time shift is different for the AP600 and the APEX because the AP600 depressurizes from 2250 psia, whereas the APEX facility depressurizes from 400 psia. This initial subcooled depressurization is not scaled in the test facility, but its effects must be accounted for when establishing the starting point for scaling similitude.

To make more meaningful comparisons, the calculated system parameters such as levels were normalized using their respective maximum levels for either the AP600 plant or the APEX facility. The pressures were normalized using the initial pressure at which similitude was sought, as shown in Table 8. The mass flows for the AP600 were normalized using the mass flow scaling factor of 96 as given in Section 7, and the total mass for both the AP600 and the test were normalized using the initial mass at time zero.

Fig. 20 compares the normalized pressure transient for the plant and the APEX Test Facility and indicates that reasonably good agreement is achieved particularly in the timing of the depressurization. The normalized CMT levels are shown
Table 8
Scale factors to relate the AP600 plant to OSU NOTRUMP calculations

<table>
<thead>
<tr>
<th></th>
<th>AP600</th>
<th>OSU</th>
</tr>
</thead>
<tbody>
<tr>
<td>Time</td>
<td>t-ta</td>
<td>2*(t-to)</td>
</tr>
<tr>
<td>Pressure</td>
<td>P/1080</td>
<td>P/320</td>
</tr>
<tr>
<td>Flow</td>
<td>W/96</td>
<td>W</td>
</tr>
<tr>
<td>Mixture elevation</td>
<td>Fraction of component</td>
<td>Fraction of RCS total</td>
</tr>
<tr>
<td>Mass</td>
<td></td>
<td></td>
</tr>
</tbody>
</table>

in Figs. 21 and 22 and the normalized accumulators levels are shown in Figs. 23 and 24. These figures are in good agreement and indicate that the scaling correctly preserves the time of the events for the APEX facility as compared to the AP600. The accumulator levels in particular represent the effects of the pressure scaling since the pressure scaling is used to establish the initial pressure of the accumulator. The good agreement on the level decrease also indicates that the gas volume scaling is appropriate as well as the line resistance.

The CMT levels do not agree as well as the accumulator levels. The timing is appropriate, which indicates that the CMT recirculation phase is being scaled correctly and that the drain down phase begins at the proper time. The difference in the slopes of the levels reveals some differences in the APEX line resistances relative to the AP600 plant. Also observed is that the AP600 plant accumulator is stopping the CMT flow before the APEX accumulator diminishes the APEX CMT flow.

Fig. 21. Normalized CMT1 level for AP600 plant and APEX facility.
The mass inventory similitude has been preserved as seen in Fig. 25 which shows the normalized mass inventory for both the test facility and the AP600 plant. By preserving the integrated mass within the two systems, the global quantities such as integrated mass flow in and integrated mass flow out are also preserved. Since the time curves are similar, and since the injection flows from the CMT and Accumulator are similar (as seen from the level plots), the integrated mass flow out the break and ADS is also similar. The good agreement in mass inventory is strongly dependent on the fact that the AP600 ADS 4 operation in conjunction with IRWST injection limits the minimum mass inventory in the reactor vessel.

The comparisons of the NOTRUMP calculations for the AP600 plant and the APEX facility represented the first assessment of the scaling basis using code comparisons with AP600. The agreement between the two calculations was very good; supporting the scaling results obtained through the analysis. Subsequent to this initial assessment effort, numerous tests and code calculations have been performed to assess the adequacy and range of applicability of the APEX test facility. For example, numerous code comparisons to data from the ROSA-AP600, SPES-2 (Rigamonti, 1994) and APEX facilities were performed by the Idaho National Engineering Laboratory (Fletcher et al., 1996; Davis et al., 1996). Such comparisons were very valuable in understanding facility scaling distortions and AP600 passive safety system behavior.

The next section presents an early comparison of actual APEX data for a 5 cm (2 in.) break
compared to test data from the SPES test facility in Italy.

11. Comparison of SPES-2 and APEX data

Subsequent to the construction of the APEX test facility, counterpart tests were performed in the SPES and APEX test facilities (Hochreiter et al., 1995). These tests provide points of comparison for the facilities which enable one to understand scaling and operating differences. This section examines a 2-in. small break loss of coolant accident. Table 9 presents the general scaling ratios for the test facilities relative to the full-scale AP600.

Because of the difference in scale of the two facilities, scaling factors were applied to the APEX and SPES-2 results to compare time, pressure, and flow rates. The APEX time scale was multiplied by a factor of two and its pressure scale normalized using the reference pressure (maximum pressure on secondary side). Similarly, the SPES-2 pressure scale was normalized using the reference pressure for the test. The flow rate normalization factor in SPES-2 was the maximum flow rate observed for the process being examined. For purposes of comparison, the flow rate
normalization factor in APEX was the maximum flow rate observed for the identical process in SPES-2 multiplied by the ratio 395/96. Thus, the flow rates were compared on a similar basis.

A 2-in. cold leg break was simulated in both the APEX and SPES-2 facilities. The break location for these tests was at the bottom of a single cold leg. Each system was at its steady-state initial condition at the time of break initiation. The subsequent depressurization behavior was recorded for each facility and key data plots are presented herein for purposes of comparison. The vertical axis of each graph was normalized as described previously. The APEX time scale was multiplied by the scaling factor of two to compare with the SPES-2 time scale.

Fig. 24. Normalized accumulator 2 level for AP600 plant and APEX facility.

Fig. 26 compares the APEX and SPES-2 pressure histories for the reactor vessel following the initiation of the simulated 2-in. cold leg break. As can be observed, the trends are very similar. Figs. 27–30 present the data comparisons for the key passive safety systems. In general, the data comparisons indicate that the agreement between SPES-2 and APEX is very good for the early portion of a 2-in. break transient on a scaled basis.

12. Conclusions and lessons learned

The process of scaling, constructing and operating a major test facility for the purpose of obtain-
The following are just a few of the lessons learned:

1. The H2TS Methodology proved to be useful in obtaining the similarity groups that should be preserved between APEX and the AP600. The methodology enabled establishing priorities for preserving the similarity groups, assuring that the important processes were identified and addressed, providing specifications for the test facility design, including the critical attributes, and quantifying the facility biases due to scaling distortions.

2. Preserving geometric similarity was found to be essential for integral system testing which involves examining a wide range of thermal hydraulic phenomena. For example, the SPES-2

Table 9  
Comparison of APEX and SPES-2 scaling ratios

<table>
<thead>
<tr>
<th>Scaling ratios</th>
<th>APEX</th>
<th>SPES-2</th>
</tr>
</thead>
<tbody>
<tr>
<td>Lengths</td>
<td>1:4</td>
<td>1:1</td>
</tr>
<tr>
<td>Relative elevations</td>
<td>1:4</td>
<td>1:1</td>
</tr>
<tr>
<td>Flow areas</td>
<td>1:48</td>
<td>1:395</td>
</tr>
<tr>
<td>Volumes</td>
<td>1:192</td>
<td>1:395</td>
</tr>
<tr>
<td>Decay power</td>
<td>1:96</td>
<td>1:395</td>
</tr>
<tr>
<td>Fluid velocity</td>
<td>1:2</td>
<td>1:1</td>
</tr>
<tr>
<td>Fluid transient time</td>
<td>1:2</td>
<td>1:1</td>
</tr>
<tr>
<td>Mass flow rate</td>
<td>1:96</td>
<td>1:395</td>
</tr>
</tbody>
</table>

Note: ratios are relative to the full-scale AP600.
Fig. 26. Comparison of OSU APEX and SPES-2 2-in. break pressure histories.

Fig. 27. Comparison of OSU APEX and SPES-2 CMT-2 liquid level histories.
facility demonstrated that thermal stratification in the cold legs can be totally precluded if the cold leg diameters are too small. Similarly, the ROSA-AP600 facility revealed that thermal stratification can be greatly exaggerated if the PRHR return line is connected directly to the cold leg instead of the steam generator channel head as in the AP600 design. Geometric similarity is an important ingredient in the assessment of a facility’s ability to simulate phenomena on a scaled basis. In this respect, the approach taken for APEX scaling was appropriate. That is, among AP600 test facilities, it has the closest aspect ratio (i.e. $L/D$) and is the best geometric representation of the AP600 design. The APEX test data is most applicable to AP600 transients subsequent to ADS 4 blowdown, particularly low pressure long-term recirculation cooling.

3. The original PPIRT was found to be very conservative in its ranking of important phenomena because of the limited previous experience with the AP600 passive safety systems. After design basis accident testing began, it became apparent that phenomena occurring before ADS 4 actuation had little or no impact on the outcome of a SBLOCA. Operation of ADS 4 overwhelmed the process causing all the SBLOCA transients to look very similar. This result is reflected in the revised PIRTs developed by INEL (Burtt et al., 1996). With respect to the AP600 test facilities, judgements as to facility adequacy based on scaling comparisons in the high pressure regimes (i.e. before ADS 4) are of little safety significance to AP600.

4. The AP600 research program benefitted from having data from three test facilities to benchmark the AP600 computer codes. The comparisons provided significant insights into the system behavior expected for AP600 and helped quantify the effects of scale distortion. For example, although APEX was primarily designed to investigate low pressure long term cooling, comparisons with SPES-2 and ROSA-AP600 data show that the key safety metric,
Fig. 29. Comparison of OSU APEX and SPES-2 ACC-1 liquid level histories.

Fig. 30. Comparison of OSU APEX and SPES-2 ACC-1 injection flow rate.
minimum liquid level in the reactor vessel, was very similar in the facilities throughout the entire blowdown process.

5. Defining the critical attributes of the APEX test facility during the scaling process greatly aided the quality assurance process for the remainder of the research program.

6. A comparison of the calculated behavior of APEX and AP600 for a 5-cm (2-in.) cold leg break indicates excellent agreement. Furthermore, comparisons of full pressure SPES data with the reduced pressure APEX data also shows excellent agreement. Davis et al. (1996) presents excellent comparisons of RELAP5 calculations for AP600 and APEX under low pressure long term cooling conditions.

In conclusion, the scaling analysis for the APEX facility is traceable and auditable. It was used to design the APEX test facility which has provided a significant quantity of data in support of the benchmarking of the AP600 thermal hydraulic safety analysis codes.

Acknowledgements

The authors gratefully acknowledge the Westinghouse Electric Corporation and the Department of Energy for their sponsorship of this scaling analysis and the OSU AP600 research program. We also acknowledge the dedicated efforts of the many researchers involved in developing advanced passive safety systems to benefit future generations.

Appendix A. Nomenclature

- $a$: cross-sectional area
- $a_c$: cross-sectional flow area of core $= na_{sc}$
- $a_i$: cross-sectional flow area of $i$th component
- $a_{sc}$: cross-sectional flow area of the heated subchannel
- $A$: surface area
- $A_s$: total rod heat transfer surface area in core $= nA_{sc}$
- $A_{sc}$: surface area of a single fuel rod $= \pi d l$
- $A_v$: total rod cross-sectional area in core $= nA_{sc}$
- $ACC$: accumulator
- $ADS$: automatic depressurization system
- $C_{dp}$: discharge coefficient
- $CMT$: core makeup tank
- $C_p$: specific heat at constant pressure
- $C_v$: specific heat at constant volume
- $CVS$: chemical and volume control system
- $DF$: distortion factor
- $D$: diameter
- $d$: fuel rod diameter
- $d_h$: hydraulic diameter of the heated subchannel ($d_h = 4a_{sc}/\pi$)
- $E$: energy balance
- $e$: energy per unit mass
- $F$: friction number
- $f$: Darcy friction factor
- $G$: mass flux
- $g$: gravitational acceleration
- $H$: averaged convective heat transfer coefficient
- $h$: enthalpy
- $h_{lg}$: latent heat of vaporization
- IRWST: in-containment refueling water storage tank
- $j$: flux
- $K_i$: loss coefficient of the $i$th component
- $k_s$: solid thermal conductivity
- $l$: axial length
- LCS: lower containment sump
- $L_{th}$: thermal center length
- $M$: mass balance
- $MM$: momentum balance
- $m$: mass flow rate
- $N$: number of rods or subchannels
- $P$: pressure
- $q'$: linear power
- $q''$: heat generation density
- $q_s$: core heat generation rate
- $Q$: volumetric flow rate
- $R_{th}$: thermal resistance
- $s$: pitch
- $T$: temperature
- $\Delta T$: fluid temperature difference across the length of the core
\( t \) time
\( u \) velocity
\( V \) volume
\( v \) specific volume
\( v_{gj} \) drift velocity; \( v_{gj} = (1 - x)(u_g - u_l) \)
\( w \) work
\( x_c \) equilibrium vapor quality
\( z \) axial elevation

Greek symbols
\( \alpha \) vapor volume fraction
\( \alpha_s \) solid thermal diffusivity
\( \beta \) equation of motion scaling constant
\( \beta_T \) thermal expansion coefficient
\( \delta \) conduction depth (\( \delta = A_{sc}/\zeta \))
\( \sigma \) surface tension
\( \tau \) residence time constant
\( \theta \) dimensionless temperature ratio
\( \mu \) absolute viscosity
\( \nu \) kinematic viscosity
\( \xi \) wetted perimeter
\( \Pi \) characteristic time ratio
\( \rho \) density
\( \Delta \rho \) density difference \( \Delta \rho = (\rho_{lb} - \rho_{gs}) \)
\( \rho_{gs} \) saturated vapor density
\( \rho_{lb} \) saturated liquid density
\( \gamma \) specific heat ratio
\( \psi \) conserved property

Subscripts
\( C \) cold
\( f \) fluid
\( g \) vapor
\( gs \) saturated vapor
\( H \) hot fluid region
\( HL \) hot leg
\( i \) \( i^{th} \) component
\( l \) liquid
\( ls \) saturated liquid
\( o \) initial condition
\( R \) model to prototype ratio
\( s \) solid
\( sp \) single phase
\( sub \) subcooled
\( tp \) two-phase
\( \langle \phi \rangle \) volume averaged parameter \( = \frac{1}{V} \int \phi \, dv \)

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Meyer, P.E., 1985. NOTRUMP, a nodal transient small break and general network code. WCAO-10079-P-A.


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