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THERMAL-HYDRAULIC DEVELOMENT OF A SMALL, SIMPLIFIED, PROLIFERATION-RESISTANT REACTOR*

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ABSTRACT

This paper addresses thermal-hydraulics related criteria and preliminary concepts for a small (300 MWt), proliferation-resistant, liquid-metal-cooled reactor system. A main objective is to assess what extent of simplification is achievable in the concepts with the primary purpose of regaining economic competitiveness. The approach investigated features lead-bismuth eutectic (LBE) and a low power density core for ultra-long core lifetime (goal 15 years) with cartridge core replacement at end of life. This potentially introduces extensive simplifications resulting in capital cost and operating cost savings including: i) compact, modular, pool-type configuration for factory fabrication, ii) 100+% natural circulation heat transport with the possibility of eliminating the main coolant pumps, iii) steam generator modules immersed directly in the primary coolant pool for elimination of the intermediate heat transport system, and iv) elimination of on-site fuel handling and storage provisions including rotating plug.

Stage 1 natural circulation model and results are presented. Results suggest that 100+% natural circulation heat transport is readily achievable using LBE coolant and the long-life cartridge core approach; moreover, it is achievable in a compact pool configuration considerably smaller than PRISM A (for overland transportability) and with peak cladding temperature within the existing database range for ferritic steel with oxide layer surface passivation. Stage 2 analysis will follow iteration with core designers. Other thermal hydraulic investigations are underway addressing passive, auxiliary heat removal by air cooling of the reactor vessel and the effects of steam generator tube rupture.

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1.0 INTRODUCTION

Argonne National Laboratory (ANL) has been the foremost institute in the US for development of technologies for advanced nuclear power systems. In the area of liquid-metal-cooled fast reactors, ANL developed and operated the Experimental Breeder Reactor-I (1951-1964) and the Experimental Breeder Reactor-II (1964-1966) at its testing site in Idaho. It had key roles in the technology developments and safety approaches for the Fast Flux Test Facility (FFTF), Clinch River Breeder Reactor (CRBR) design, and the SAFR and PRISM concepts. It was the developer of the Integral Fast Reactor (IFR) concept which features high burnup U/Pu/Zr metal fuel form and on-site fuel processing using Argonne's pyrometallurgical processing technology.[1] The mission for these reactors was to support development in the U.S. of the liquid metal fast breeder reactor (LMFBR) whose purpose was not only to produce electrical power but also to breed fissionable materials and so to create a near inexhaustible source of fuel for operation of "burner" reactors like water moderated, water cooled reactors (LWRs). To achieve high breeding ratios, i.e., short fuel doubling times, multi-zone, tight-lattice, high power density core designs were required. The coolant of choice worldwide for these LMFBR systems was sodium owing to its superior heat transfer characteristics and low density which meant tolerable coolant pumping requirements. The investment in LMFBR technology has been immense, amounting to $10's of billions in the US alone since EBR-I.

Today the situation has changed compared to the decades of the 50's-80's. The need to deploy breeder reactors to provide fuel for ever increasing numbers of burner reactors is ended, at least for the time being. Growth in electricity demand rates has abated, nuclear energy investment and production costs have been high (nowhere near the low electricity cost originally projected as "too cheap to meter"), and at the same time deregulation of the electric utility industry in many geographic areas places economic competitiveness at the forefront; plus the situation has become clogged with fuel cycle and proliferation concerns, and public support has waned with the occurrence of two major accidents. Today most countries find that it is hard to justify the mission of breeder reactor, and this is reflected in the scaleback or even elimination of such programs in various countries.

This introduces the opportunity to reexamine the role of nuclear power as an element of a diversified national energy strategy for the 21st century. It is clear that there is merit to do this because the original promise of nuclear energy still remains – cheap, abundant, safe electricity generation without burning of irreplaceable fossil fuels. Moreover, there is even a turnaround sensed in public and political opinion about nuclear power insofar as it contributes to lowering of hydrocarbon emissions. Internationally, nuclear power offers an attractive near term solution to improving national economies where indigenous energy resources are lacking. To realize the potential benefit of this requires international acceptance of the siting of nuclear power plants (NPPs), and this introduces the challenge for future NPP designs to address proliferation concerns.

The activity described in this paper is one element of ongoing efforts at ANL to address reactor technologies that can fulfill the requirements for nuclear power to be viable as an important energy resource for the 21st century, both for domestic (U.S.) and international utilization. Reactor concepts are being developed and evaluated in relation to proposed specific applications. Here we address one such concept which is intended to be a low-cost contender for commercial electricity production. The approach is to achieve capital and operating cost savings through extreme measures of simplifying the system: i.e., by providing a robust system with minimal maintenance needs, providing a small, modular pool-type configuration that lends itself to the economy of factory fabrication and overload transportation, and providing an extremely long-life core design which eliminates fuel shuffling and partial reloads and requires refueling outages only at very long intervals (~15 years). The reactor system is required to be exportable to developing countries, and so the approach includes measures for proliferation resistance. The system described is sized for the export market at 300 MWt; however, plant power can be varied for greater needs by coupling multiple modules as was the approach for PRISM.[2] In the particular concept to be described, we limit ourselves to a design approach that offers the possibility to achieve deployment in an early time frame. That is, a major goal was to select "proven" technologies such that all research and development needed to begin detailed prototype design could be accomplished in about 5 years.

To date, this activity has been an internal, interdisciplinary activity involving the Reactor Analysis, Reactor Engineering, and Chemical Technology Divisions at ANL. The results presented here pertain to the development of the overall concept, and, in particular, the analysis effort to achieve 100% natural circulation heat transport within the constraints of small, modular, transportable configuration and existing materials technology. Core design work related to the 1st stage thermal-hydraulics approach described here is presented in a companion paper in this conference.[3]

2.0 REQUIREMENTS/APPROACH

The requirements for the compact, proliferation-resistant, liquid metal cooled reactor system are summarized together with the current approach to meet the requirements.

2.1 Proliferation Resistance

The reactor system is to be designed to be exportable to developing countries, and hence proliferation-resistance is required. The overall NPP approach consists of numerous, diverse features to preclude proliferation; those features relevant for this reactor concept are mainly: i) "lifetime" reactor fueling, and ii) no on-site fuel handling. The vision is that the reactor vendor will retain ownership of the reactor fuel and will provide all services related to periodic core replacement. In this approach, the core is an integral (1-piece) assembly contained in a cartridge; the cartridge is inserted into the coolant pool for system operation and replaced like a battery at the end of its lifetime (~15 years). There is no access to the fuel within the cartridge. There is no provision for removing or shuffling individual rods or rod assemblies. There are no features in the vessel head structure for refueling or fuel shuffling (such as rotating plug). There is no in-vessel nor ex-vessel fuel storage. At end-of-life the cartridge is removed by its owner into a combination shielding/cooling/shipping cask and taken by the owner to his facility for disposition; similarly, a fresh cartridge is replaced into the coolant pool for renewed operation.

2.2 Ultra-long Core Lifetime

Consistent with the proliferation-resistant objective is the goal to minimize the impact of the cartridge replacement process and maximize plant capacity factor by introducing an ultra-long life core design (with a 15 year goal). It is required to achieve this within the existing technology as regards fuel burnup and material exposure ranges, nominally 100,000 MWd/T peak and 200 dpa (cladding), respectively. Structural materials are limited to ~20 dpa exposure. This is to be achieved by design of a derated core in which the volumetric power generation is reduced by a factor of about eight compared to LMFBR cores.[3] This decrease in core specific power enables a high volume fraction of coolant in the lattice which introduces the possibility of significant natural convection for core cooling.

2.3 Factory Fabrication

Significant cost savings can be introduced by maximizing the extent of factory fabrication of the reactor system and components and minimizing the extent of on-site fabrication and construction. Accordingly, the system is to be of modular construction to the greatest extent possible and configured in a pool rather than loop arrangement. The two major vessels (the coolant and guard, or containment, vessels) need to be small enough to be transported by various means to the site, including overland. The dimensions and weights of vessels that can be transported were examined for PRISM A,[2] and those dimensions are used here as an upper limit of vessel size [length: 18.9 m (62 ft) and diameter: 6.1 m (20 ft)].

2.4 Existing Materials Technology

The requirement to complete R&D needed to begin prototype design in a 5-year period means that materials must be selected based on proven experience in the proposed reactor environment, including the fuel, coolant, core structural materials, and other structural materials. The same is true regarding the compatibility of materials. As regards the coolant selection, Pb is presently excluded since it requires a high system operating temperature which exceeds the current materials database. Sodium and lead-bismuth eutectic (LBE) have been investigated in this study, although results presented here pertain solely to the LBE option. The LBE is considered a viable option owing to extensive development in Russia.[4] Similarly, the use of ferritic stainless steel in the core and hot leg regions with oxide-layer corrosion protection is regarded as a proven technology in Russia; according to Russian practice, austenitic stainless steel can be adopted in the cold leg regions, including the vessel.[5] In adopting the Russian approach, criteria are introduced to maintain peak cladding temperature below 600C (as far below as reasonably achievable), and to avoid coolant stagnation in design of the pool system.

2.5 Existing Power Conversion Technology

In this concept we limit consideration to electricity generation by conventional steam power. The steam pressure is specified as nominally 7 MPa, and design approaches that can produce steam superheat to improve efficiency while retaining simplicity are sought. The present approach has been to incorporate modular steam generators in the coolant pool based on the design and operating parameters of the B&W Oconee once-through, tube and shell type; other design approaches are under consideration. The goal is to exclude rupture of a steam-line or manifold in the vessel, albeit steam generator tube rupture (SGTR) is retained as a design-basis event. Vessel overpressure relief capacity is required to handle a maximum SGTR event; a pressure relief (quench) tank is required to minimize radiological consequences. SG isolation values are required to minimize consequences.

2.6 Coolant Purity

It is required to provide systems for the purpose of maintaining coolant purity and maintaining corrosion protection. As regards purity, it is required to prevent the precipitation and/or accumulation of impurities that could degrade the long term performance of the heat transport system or endanger normal coolant flow through the core. Coolant purification should take place continuously during reactor operation and not require periodic shutdown for either coolant or system cleanup. The approach is to use cold traps in "coolant conditioning" trains for removal of corrosion products while maintaining oxygen level in a range adequate to accomplish the corrosion protection mission.

2.7 Passive Auxiliary Heat Removal

It is required to provide passive means of removing decay heat, and effecting reactor cooldown, in the event that the SG heat sink is lost. The primary system temperature is not to exceed 750C for short time to accomplish this, and, to avoid deleterious precipitation, the primary system temperature is not to go below 280C except under operator-controlled conditions. The approach to accomplish this is based on the Reactor Vessel Auxiliary Cooling System (RVACS) developed for PRISM A which uses the atmosphere air for cooling as the ultimate heat sink.

2.8 Seismic Isolation

Because the geographical areas for siting this type of NPP may be vulnerable to seismic activity, and because coolant-sloshing loads may be unacceptable in a system cooled with LBE, it is required that the NPP nuclear island lend itself to seismic isolation. This was investigated for PRISM A; the PRISM A approach has been adopted here, involving the use of seismic isolator pads to support the nuclear island plus high-deformation bellows for connection of steam lines and process piping between the isolated nuclear island and the nonisolated balance of plant.

2.9 Containment

It is considered unrealistic to design a nuclear reactor system without containment. The containment must serve as a nominally leaktight radiological barrier for certain types of operations or events where Po contamination may be involved: 1) changeout of steam generator modules, 2) SGTR event with blowdown and Po retention in a quench tank in containment, 3) periodic replacement of cold trap media, and 4) replacement of the fuel cartridge (infrequent). The containment is included in the seismic isolation zone. Coolant lost from the reactor system is to be collected in containment, and the design shall be such to prevent loss of coolant level to the extent of interfering with the normal heat transport function. The containment shall be habitable during normal operation.

2.10 In-service Inspection

The system is to be configured such that all boundaries of the reactor system where leakage could result in loss of coolant shall be accessible for in-service inspection.

3.0 SUMMARY OF TOP LEVEL GOALS TO ACHIEVE SIMPLIFICATION, ECONOMY, AND PASSIVE SAFETY

The requirements and approach for the small power reactor concept are focused at this stage to achieve radical simplification of the system in order to reduce costs as low as possible. The extent to which these simplifications can be justified through R&D in the near term will largely determine whether such concepts can attract the support needed to go forward. The main potential simplifications result from main design decisions:

3.1 Ultra Long-life, Cartridge Core

This core design approach supports requirements for nonproliferation, high plant capacity, low operating costs, and eliminates need for costly rotating plug and fuel handling provisions on-site. The approach to achieve the ultra long life (15 year goal) within existing burnup and exposure technology is to introduce a low power density core (as contrasted to the high power density LMFBR core designs). With the large lattice spacing and large coolant fraction, coolant pressure drop in the core can be lessened, and coolants other than sodium thereby become viable candidates. We wish to take advantage of this by investigating the feasibility of natural circulation heat transport in a low pressure-drop core using a heavy liquid metal coolant, LBE. Natural circulation capability benefits passive safety as an auxiliary means of decay heat removal. But the real economic payoff in terms of reduced capital investment and reduced operating costs is in the complete elimination of the main coolant pumps (MCPs). Hence a primary goal of the concept is to assess the possibility to achieve 100+% transport of rated power by natural circulation and to evaluate the feasibility of eliminating the MCPs. A related goal is to develop an approach for coolant conditioning that enable long term, stable natural circulation flow while providing for oxide-layer corrosion protection.

3.2 LBE Inertness

One reason LBE is considered as an attractive coolant option is its inertness with air and with the steam/water working fluid. Coolant leaks to the atmosphere will not result in violent combustion as with sodium but rather will result in benign freezing. Hence guard piping requirements may be eliminated or relaxed. The main advantage, however, is the possibility to place the steam generators directly in the coolant pool. This simplifies the natural circulation heat transport path. The real economic payoff of this approach in terms of reduced capital investment and reduced operating costs is in the elimination of the intermediate heat transport systems (IHTS) including the IHTS piping, pump, heat exchangers, and ancillary systems. Hence a second primary goal of the concept is to take advantage of the LBE inertness to enable the SGs to be placed in the primary pool and to eliminate the IHTS. A related goal is to provide for benign system response to a steam generator tube rupture event.

3.3 Other LBE Properties

The inertness of LBE is not its only attractive feature. It is an excellent heat transport material in its own right and fully capable of achieving single-phase natural convection in a low pressure (nominally atmospheric pressure) system. It has a very high boiling point of 1667C compared to sodium (892C). Low pressure operation avoids high energy coolant boundary failure and associated loss of coolant. The main advantage of the very high boiling point, however, is the possibility to eliminate the class of accidents involving coolant boiling in the core which have historically been part of the accident spectrum analyzed for the sodium cooled reactor. The goal is to take advantage of the LBE coolant selection to show that such accidents are below the level of regulatory concern owing to the extraordinary margin to coolant boiling.

4.0 OVERVIEW OF REACTOR CONCEPT

The current state of the reactor concept is illustrated in Figs. 1-3. This concept forms the basis for current core design activities,[3] tradeoff studies, and identification of issues. The cross section of the primary pool, Fig. 1, illustrates the extreme extent of system simplification. The coolant pool is contained in the heat transport module whose vessel has overall dimensions 5.5 m outside diameter and 14 m height. (These dimensions are significantly smaller than the PRISM A requirements for factor fabrication and overland transportation.) Into the coolant pool is inserted the reactor module which is the "flow-through fuel cartridge." This entire cartridge is replaced at the goal 15 year lifetime. The cartridge contains the fixed core at the bottom. There is no top head access nor any other access to the fixed fuel elements in the core. Coolant, however, enters at the bottom of the cartridge through an array of inlet holes and a distributor plate, passes through the core, up the riser and exits back into the pool through holes in the top region of the cartridge. The outside diameter of the cartridge is 2.8 m.

Inserted into the annulus between the cartridge and the pool vessel are four steam generators. Coolant from the upper pool passes into the steam generators and down the SG once-through tubes. It exits the tubes at the SG bottom and flows down the annulus ~6 m before reentering the cartridge. This is an extremely simplified heat transport path for natural circulation.

The reference core conditions used for thermal-hydraulic analysis include 300 MWt, 2.5 m core diameter, 2.0 m active fuel height (UN reference fuel), 0.5 m plenum height, 12.7 mm fuel rod diameter, and 1.47 pitch-to-diameter ratio in a triangular lattice. The steam generators are based on the Babcock and Wilcox Oconee design of once-through tube and shell units designed to produce superheated steam (Fig. 4). The LBE flows downward through the tubes. Feedwater is delivered internally to the bottom of the unit where it enters the shell region. Tube OD is 16.7 mm, wall thickness is 2 mm, and the pitch-to-diameter of the triangular array is 1.35. Steam pressure was specified to be 7.0 MPa at the SGs. The shell is double walled for application of leak-before-break. A SG module can be isolated by shutoff valves in the feedwater and steam lines in the event of leakage or for the purpose of changing a module. Structural supports and expansion seals are used to channel the coolant through the SGs with minimal bypass leakage. There is a liner between the SGs and the coolant vessel connecting between the SG top structural supports and extending above the coolant level into the cover gas region. This liner prevents coolant bypass of the SGs during normal operation while providing a pathway for steam leaked into the coolant at the bottom of the SGs to rise into the cover gas region. This liner also elevates the cold leg to the entire height of the coolant vessel so that, consistent with practice in Russia, the vessel material can be austenitic stainless steel.

There are no main coolant pumps in this reactor concept. However there is a requirement for conditioning the coolant to preserve purity while maintaining oxygen in the proper range for corrosion protection. The coolant conditioning trains will each have capacity of ~10% of the nominal natural circulation flowrate (~11 x 10^3 kg/s total), and this will require pumps in the train(s). These trains can be aligned to take suction from either the hot leg or the cold leg and to deliver to an outlet manifold in the cold leg. The design of this outlet manifold achieves the requirement to avoid stagnation in a pool configuration. These pumps can be used by the operator to accelerate the time to reach full natural circulation coolant flowrate, or, during normal operation, to fulfill the coolant conditioning functions.

The coolant vessel is contained in a guard vessel which, as in PRISM, is also the containment boundary. The guard vessel fulfills the requirement to retain any leaked coolant and prevent coolant loss to the extent that the normal heat transport path would be interrupted. The gap between the coolant vessel and the containment vessel contains the normal containment atmosphere and is sized for in-service inspection equipment. Analysis is underway exploring the possibility to use "venetian conductors" in this gap which would introduce benefits of: 1) increasing the effective mass of the system thereby reducing transient effects without increasing the LBE mass; 2) providing conduction heat transport between the coolant and guard vessels which benefits the auxiliary heat removal function; and 3) enabling in-service inspection.

The outside of the guard (containment) vessel is used for auxiliary heat removal in case the steam generators are unavailable. The reactor vessel auxiliary cooling system (RVACS) is based on natural circulation heat removal using atmospheric air as the ultimate heat sink (Fig. 3) as developed for PRISM. There is very little heat loss from the system unless the bulk coolant heats up during a transient such that hot coolant flows over the top of the vessel liner in the cover gas region (by thermal expansion) and flows down along the entire height of the vessel.

The containment boundary is the guard vessel in the reactor silo and the interior surface of the head access region above the silo. The head access region contains the steam collector and feedwater distribution piping, the blowdown quench tank for the overpressure relief system (for accommodation of SGTR and retention of Po), and the trains of coolant conditioning and covergas cleanup systems. This volume also accommodates head adaptors and casks for replacement of the steam generator modules.

The nuclear island consisting of the modular reactor system, steam and feedwater piping, RVACS system, and containment volume are all supported by seismic resistor pads for seismic isolation (Fig. 3).

5.0 NATURAL CIRCULATION HEAT TRANSPORT MODEL

A natural circulation analytical model has been developed for analysis of reactor system concepts. This model has been used for prediction of the performance characteristics of the system (under ideal conditions), for evaluation of features and components of the heat transport circuit, and for tradeoff studies to identify optimum parameter values. For the purposes of these analyses, the core model has been simple uniform power. An iteration with core design activities will enable more realistic core conditions to be used for future analyses.[3] The main parameters of interest in these analyses have been the core outlet temperature, the peak cladding temperature, and the steam superheat. While no criteria or limitation has been placed on outlet temperature, there is obvious advantage if the hot leg temperature is $\lesssim$450C enabling use of austenitic stainless steel. A limitation of 600C is placed on the peak cladding temperature to remain within the range of proven technology using ferritic cladding material;[5] however, owing to unknown and potentially large uncertainties, there has been motivation to determine how low a peak cladding temperature can be reasonably achieved. The criterion for steam superheat is

>50C. The criterion for fuel specific power is 10-14 KW/kgU such that the peak burnup does not exceed 100,000 MWd/T after 15 full power years.

Heavy liquid metal coolant natural circulation conditions are being calculated with a one-dimensional formulation. The Bernoulli form of the steady state fluid momentum equation,

$$\bar{\nabla}\rho\frac{u^2}{2}+\frac{4}{D_h}\frac{1}{2}\rho|\bar{u}|\bar{u}\,f+\sum_i\frac{1}{2}\rho|\bar{u}|\bar{u}\,K_i\,\delta(\bar{x}-\bar{x}_i)=-\bar{\nabla}P+\rho\bar{g}, \tag{1}$$

is integrated over a closed loop. The following assumptions are made:

1) Changes in density between T_{in} and T_{out} are small enough that the approximation,

$$\rho=\bar{\rho}-\bar{\rho}\beta(T-\bar{T}),$$

is valid. In fact, this is a good approximation for lead-bismuth eutectic;

2) The power is uniform radially and axially across the core;

3) The coolant temperature increases linearly with height inside the core and decreases linearly with height inside the SG. This is an excellent assumption for a uniform axial power profile in the core. Inside the SG, the coolant temperature decreases at different rates over the superheated steam, boiling, and subcooled water zones. It turns out that most of the coolant temperature decrease occurs in the boiling zone and is approximately linear over the boiling zone height. The center of the boiling zone is thus the relevant SG thermal center elevation;

4) The local hydraulic diameter is unvarying within each of the core and SG. Thus, the fuel rod diameter and pitch are the same across the core radial extent;

5) Friction factors are assumed to be constant and not vary with velocity over the range of interest. This is a good assumption for a degree of roughness midway between a smooth surface and a completely rough surface;

6) Other pressure losses can be represented by loss coefficients. This is a good assumption for spacer grids in the core as well as contraction and expansion losses;

7) A specified fraction of the total flow bypass the SG.

The effective driving pressure rise due to buoyancy is

$$\oint\rho\bar{g}\cdot d\bar{x}=\oint(\rho-\bar{\rho})\bar{g}\cdot d\bar{x}=\bar{\rho}\beta g\,(T_{out}-T_{in})L_{diff}. \tag{2}$$

With the various assumptions, the integrated momentum equation can be solved for the coolant velocity through the core in terms of the effective buoyant pressure rise. However, the temperature rise across the core from energy conservation is

$$\Delta T_{rise} = T_{out} - T_{in} = \frac{Q_{tot}}{\rho u_c A_c c} = \frac{4Q_{tot}}{\rho u_c c D_{h,c} S_c}. \tag{3}$$

Using this equation to eliminate the core temperature rise provides an equation for the velocity through the core in terms of total power,

$$u_c = \left(\frac{2Q_{tot} g \beta L_{diff}}{\bar{\rho} c D_{h,c} S_c} \right)^{1/3} \frac{1}{\left[\frac{L_c}{D_{h,c}} \left(f_c + \frac{\sum_i K_{i,c} D_{h,c}}{4L_c} \right) + \left(\frac{A_c Y}{A_{HX}} \right)^2 \left(f_{HX} + \frac{\sum_i K_{i,HX} D_{h,HX}}{4L_{HX}} \right) \right]^{1/3}}. \tag{4}$$

The velocity through the SG is given by

$$u_{SG} = u_c \frac{A_c Y}{A_{SG}}. \tag{5}$$

The temperature difference between the cladding outer surface and the bulk coolant is

$$\Delta T_{clad-cool} = \frac{q}{h_c} \tag{6}$$

where q is the local heat flux and h_c is the local heat transfer coefficient. The temperature difference varies with location in the core due to power profile effects as well as local variation of h_c around the circumference of the cladding. It follows that

$$\Delta T_{clad-cool} = \frac{\bar{q} P_{ax} P_{rad}}{\bar{h}_c \in_{min}}. \tag{7}$$

Detailed power factors are not yet available from the Stage 1 analyses. However, $P_{ax}P_{rad} \cong 1.75$ may be assumed for the present. Taking $P_{ax} \cong P_{rad}$ thus provides $P_{rad} \cong 1.32$. It is assumed that the peak cladding temperature occurs near the top of the core where a local axial power factor of unity should be bounding. For a triangular rod array having a pitch-to-diameter ratio of 1.375 or larger, the ratio of local-to-mean heat transfer coefficient has a lower bound of $\in_{min} = 0.925$.[6] Thus, we may approximate

$$\Delta T_{clad-cool} \cong 1.43 \frac{\bar{q}}{\bar{h}_c}. \tag{8}$$

Of course, the coolant temperature rise at core locations corresponding to the peak in the radial power distribution will somewhat exceed the mean value obtained from Equation 3. However, this radial profile effect will tend to be mitigated by coolant intermixing due to crossflow and spacer grids.

There is also a temperature difference across the cladding thickness given by

$$\Delta T_{clad} = \frac{q\delta_{clad}}{k_{clad}} \cong \frac{1.32\,\bar{q}\delta_{clad}}{k_{clad}}. \tag{9}$$

The peak cladding temperature is thus approximated as

$$T_{clad,peak} = T_{out} + \Delta T_{clad-cool} + \Delta T_{clad}. \tag{10}$$

Borishanski and Firsova[7] presented a Nusselt number correlation for liquid metal flow through a triangular rod array,

$$\begin{aligned} Nu = {} & 24.15 \log_{10}\left[-8.12 + 12.76\left(\frac{p}{d}\right) - 3.65\left(\frac{p}{d}\right)^2\right] \\ & + 0.0174\left\{1 - \exp\left[-6\left(\frac{p}{d} - 1\right)\right]\right\}(Re\,Pr - 200)^{0.9}, \end{aligned} \tag{11}$$

valid for $0.007 \le Pr \le 0.03$, $1.1 \le p/d \le 1.5$, $30 \le RePr \le 2000$. For liquid metal flow through circular tubes, the correlation of Lubarsky and Kaufman correlates most uniform heat flux data for fully developed turbulent flow,[8]

$$Nu = 0.625(Re\,Pr)^{0.4}. \tag{12}$$

The Stage 1 analyses reported below employ the correlation,

$$Nu_L = 5.0 + 0.025(Re\,Pr)^{0.8}, \tag{13}$$

inside both the core and SG. For concept reference conditions in the core, Equation 13 predicts Nu = 10.7 versus 16.6 from Equation 11. In the SG, Equation 13 yields Nu = 8.8 versus 7.7 from Equation 12. However, when Equation 13 is used in the core, the power peaking factors as well as the local heat transfer coefficient factor are set equal to unity. This tends to offset the smaller magnitude of Equation 13 relative to Equation 11 such that the peak cladding temperatures reported below are quite comparable to those that would have been otherwise obtained with Equations 8, 9, and 11.

Analysis of heat exchanger conditions is directed at determination of the particular combination of feedwater flow and steam superheating that removes the total core power from the LBE coolant passing through the SG. Because a fraction of the coolant flow, 1-Y, bypasses the SGs, it is necessary for the metal coolant temperature to be reduced to a value,

$$T_{m,SG} = \frac{T_{in} - (1 - Y)T_{out}}{Y}, \tag{14}$$

below the nominal core inlet temperature.

The local coolant-to-water/steam heat flux removed from the coolant is given by

$$r_{in} q = \frac{T_m - T_w}{R} \tag{15}$$

where

$$R = \frac{1}{r_{in} h_m} + \frac{\ln \frac{r_{out}}{r_{in}}}{k_s} + \frac{1}{r_{out} h_{crud}} + \frac{1}{r_{out} h_w}. \tag{16}$$

The total height of the heat exchanger is divided into four distinct water/steam heat transfer regions corresponding to subcooled water, boiling with a wetted wall, boiling with a dried out wall, and superheated steam.

Over the subcooled water zone, the metal coolant temperature decreases with the downward coordinate according to the equation,

$$\rho_m c_m u_m r_{in}^2 \frac{dT_m}{dx} = -\frac{2(T_m - T_w)}{R_{sub}}, \tag{17}$$

Approximating the water temperature as unvarying at the mean of the water inlet subcooled and saturation temperatures,

$$T_{m,SG} - T_w = (T_{m,sub} - T_w)\exp\left(-\frac{2x}{\rho_m c_m u_m r_{in}^2 R_{sub}}\right), \tag{18}$$

where $T_{m,sub}$ is the metal temperature as it enters the top of the subcooled water region. Because the liquid metal removes the water subcooling over the height of the subcooled zone,

$$T_{m,sub} = T_{m,SG} + \frac{\rho_{w,l} u_{w,l} A_w c_{w,l} \Delta T_{sub}}{\rho_m u_m A_m c_m}, \tag{19}$$

Using Equation 19 to eliminate $T_{m,sub}$ and solving Equation 18 for x provides the height of the subcooled zone,

$$X_{sub} = \frac{\rho_m u_m c_m r_{in}^2 R_{sub}}{2} \ln\left(\frac{T_{m,HX} - T_w + \frac{\rho_{w,l} u_{w,l} A_w c_{w,l} \Delta T_{sub}}{\rho_m u_m A_m c_m}}{T_{m,SG} - T_w}\right). \tag{20}$$

As noted above, T_w is approximated as $T_{w,sat}$ - $\Delta T_{sub}/2$.

Over the two boiling zones, T_w is unvarying at $T_{w,sat}$. Thus, similar equations hold. Energy conservation over the wetted wall boiling region provides

$$T_{m,sat,wet} = T_{m,sub} + \frac{\rho_{w,l} u_{w,l} A_w h_{w,vap} \gamma_{w,wet}}{\rho_m u_m A_m c_m} \tag{21}$$

such that

$$X_{m,sat,wet} = \frac{\rho_m c_m u_m r_{in}^2 R_{sat,wet}}{2} \ln\left(\frac{T_{m,sub} + \dfrac{\rho_{w,l} u_{w,l} A_w h_{w,vap} \gamma_{w,wet}}{\rho_m u_m A_m c_m} - T_{w,sat}}{T_{m,sub} - T_{w,sat}} \right). \tag{22}$$

Here $\gamma_{w,wet}$ is flow quality at the top of the wetted wall boiling region. Similarly,

$$X_{m,sat,dry} = \frac{\rho_m c_m u_m r_{in}^2 R_{sat,dry}}{2} \ln\left(\frac{T_{m,sat,wet} + \dfrac{\rho_{w,l} u_{w,l} A_w h_{w,vap} \alpha_{w,dry}}{\rho_m u_m A_m c_m} - T_{w,sat}}{T_{m,sat,wet} - T_{w,sat}} \right), \tag{23}$$

where $\gamma_{w,dry} = 1 - \gamma_{w,wet}$.

In the superheated steam region, the steam temperature varies significantly while the liquid metal temperature is approximated as a constant at $T_m = \frac{1}{2}\left(T_{m,sat,dry} + T_{out}\right)$. Thus

$$T_{w,sup} = T_m - \left(T_m - T_{w,sat}\right) \exp\left(-\frac{2\pi X_{sup}}{\rho_{w,v} c_{w,v} u_{w,v} A_w R_{sup}} \right), \tag{24}$$

where the height of the superheated region, X_{sup}, is the remaining available height of the HX,

$$X_{sup} = X_{SG} - X_{sub} - X_{sat,wet} - X_{sat,dry}. \tag{25}$$

Over the height of the superheated zone, energy conservation requires that

$$T_{out} = T_{m,sat,dry} + \frac{\rho_{w,v} c_{w,v} u_{w,v} A_w \left(T_{w,sup} - T_{w,sat}\right)}{\rho_m c_m u_m A_m}. \tag{26}$$

Eliminating the superheated steam exit temperature, $T_{w,sup}$, between Equations 24 and 26 provides an equation that determines the water/steam mass flux, $\rho_{w,v} u_{w,v}$, which also equals $\rho_{w,l} u_{w,l}$.

The height of the wetted wall and dried out wall boiling regions depend upon $\gamma_{w,wet}$. This quantity is nominally set equal to 0.99 implying a dried out boiling region of negligible height.

The basis for this value is an assessment based upon application of the Biasi correlation[9] in the high quality regime that predicts critical heat flux/burnout at qualities of 0.99 or greater; for instance, 0.993 for the reference conditions.

In Equation 16, Equation 12 is appropriate for the liquid metal-side heat transfer coefficient, although Equation 13 is used in the calculations reported below. A thermal conductance (i.e., inverse thermal resistance) of h_{crud} = 71 $KW/(m^2 \cdot K)$ is assumed to represent the effects of crud deposition on the water/steam side of the tube walls. This value is recommended for a normal buildup of crud in PWR steam generators.[10]

In the subcooled and superheated regions, the water side heat transfer coefficient is assumed given by the Dittus-Boelter correlation,

$$Nu = 0.023\ Re^{0.8} Pr^{0.4}, \tag{27}$$

evaluated with liquid water and steam properties, respectively. The Chen correlation[11] is assumed in the wetted wall boiling zone. Consistent with the overall formulation, it is evaluated at a single point within the zone at which the quality is equal to half that at the top of the zone. In the dried out wall boiling region, the Groeneveld 5.7 correlation as modified by Bjornard and Griffith[12] is assumed.

The set of heat exchanger equations is solved together iteratively until convergence of the water/steam mass flux is achieved.

6.0 RESULTS OF STAGE 1 ANALYSES

Stage 1 analyses include tradeoff studies as well as parameter variations relative to a set of reference conditions. The core inlet temperature was selected to be 292C, feedwater subcooling 20C, and SG length 3.0 m. Table 1 shows the effects of varying the active core height, fission gas plenum height above the active core, core diameter, and the elevation difference between the SG and core thermal centers. Of particular interest for coolant natural circulation are the coolant temperature rise across the core, the peak cladding temperature, and the steam exit superheat. Reference conditions involve a 2.0 meter active core height, 0.5 meter tall fission gas plenum, 2.5 meter core diameter, 6.25 meter thermal centers elevation difference, and a 3.0 meter SG height. For these conditions, a core coolant temperature rise and peak cladding temperature of 185 and 522 degrees Centigrade are calculated, respectively; a steam superheat of 68 degrees Centigrade is calculated.

Table 1. Results of Natural Circulation Analysis; Core Power = 300 MWt, Pb-Bi Eutectic Coolant, Core Inlet Temperature = 292 C, Feedwater Subcooling = 20 C, SG length = 3.0 m.

Active Core Height, m	2.0	2.0	2.0	2.0	3.0	2.0	2.0	2.0	2.0	2.0
Fission Gas Plenum Height, m	0.5	0.5	0.5	0.5	0.5	0.5	0.5	0.5	2.0	2.0
Core Diameter, m	2.5	2.5	2.5	2.5	2.5	2.0	2.2	3.0	2.5	2.5

Thermal Centers Elevation Difference, m	3.25	4.25	6.25	10.0	6.25	6.25	6.25	6.25	6.25	10.0
Core ΔT, C	230	210	185	158	193	219	201	185	197	169
Peak Cladding Temp., C	570	549	522	493	516	575	548	512	535	504
Steam Exit Superheat, C	92	81	68	53	73	97	83	48	74	59

Figure 5 shows the effects of variations in fuel rod diameter, triangular pitch-to-diameter ratio, and fuel specific power. On this basis, the Stage 1 reference concept incorporates 1.27 centimeter diameter rods with a pitch-to-diameter ratio of 1.48 corresponding to a core coolant-to-fuel rod volume ratio of 1.40. The fuel specific power achieves the desired core operational lifetime of fifteen years.

The effects of varying the SG tube height below and above the 3 meter reference height are shown in Fig. 6.

For the reference conditions, Fig. 7 illustrates the steam generator tube wall temperature on the water/steam side as well as the heat flux as functions of height inside the SG. Corresponding bulk temperatures of the downflowing lead-bismuth eutectic and upflowing water/steam are presented in Fig. 8. Clearly, the most effective heat transfer and the major portion of the reduction in the liquid metal temperature occurs over the wetted boiling region, although this region represents less than one-third of the SG height. The heat transfer rate in this zone is effectively controlled by the liquid metal side heat transfer coefficient whereas the water/steam side coefficients are limiting in the subcooled and superheated zones. The effects of varying the SG tube height below and above the 3 meter reference height are shown in Fig. 6.

For a given liquid metal flowrate plus the temperatures entering and exiting the SG, there is a unique feedwater/steam flowrate that removes the required total amount of energy from the liquid metal while the cumulative effect of heat transfer over the SG tube height is to cool the liquid metal from the entrance temperature to the temperature at the exit. The steam superheat attained is dependent upon this feedwater flowrate as well as the liquid metal flowrate and entrance temperature.

7.0 OTHER THERMAL-HYDRAULIC INVESTIGATIONS

In addition to calculating the system conditions that are able to transport 100% of the rated core power by natural convection, other thermal-hydraulic-related issues are being addressed to support evaluation of the candidate concepts. In any design approach which eliminates an intermediate heat transport system by placing the SGs directly in the coolant pool, the consequences of steam generator tube rupture (SGTR) will need to be evaluated. We postulate a maximum SGTR event as a sudden, complete guillotine break of a coolant tube near the bottom of the steam generator. Either feedwater or steam is postulated to flow via the downward direction only to expand into the cold leg at the bottom of the SG. Models are under development to describe first the transient pressurization effect and secondly the behavior of the steam void volume. Of particular interest is the estimate of small versus large bubble formation, the spectrum of bubble sizes, and the volume of steam contained in sufficiently small size bubbles to be convected downward the 6 m to conceivably enter the core. This analysis will provide a basis for evaluating power perturbations that may result from such an event.

The reactor vessel auxiliary cooling system (RVACS) was developed for PRISM, and the analytical methodology for vessel cooling is available. Work is underway to couple this to transient conditions in the reactor pool and to thermal coupling between the coolant vessel and the guard vessel. Of particular interest is the timescale as well as the magnitude of the system heatup for a loss-of-heat sink type sequence. At a given level of system heatup, controlled by a design decision, the coolant spills over the vessel liner, and the entire vessel height becomes available for heat removal. Preliminary scoping analyses indicate the need to enhance the air-side heat transfer coefficient. Water cooling has been investigated, but preliminary scoping analyses have raised concern about too effective heat removal; that is, the system must not become overcooled to the extent that significant precipitation and sludge formation takes place in the coolant pool.

A key consideration for the reactor concept is the inherent relationship between the system thermal-hydraulics operation, protection of materials from corrosion by surface passivation, materials corrosion to the extent it occurs even with surface passivation, and coolant conditioning which removes corrosion products while maintaining the range of oxygen content required for surface passivation. Coolant conditioning also includes the capability to add molecular hydrogen using, for example, the palladium tube developed for the ANL STEP program, when hydrogen addition is needed to combat excessive oxygen from steam generator leakage, for example. These interrelated processes will determine how reliable the natural convection heat transport can be during the 15 year cartridge lifetime. Hence it is important to model the combined processes, including the natural infusion of hydrogen into the system via the presence of the steam generator. An integral code needs to be developed to specify the requirements and control the operation of the coolant conditioning system to achieve reliable operation with minimal sludge formation or deposits over the 15 year goal cartridge lifetime.

8.0 CONCLUSIONS

8.1 100%+ natural circulation heat transport is achievable with LBE coolant in the low/pressure drop, low power density core introduced for ultra-long core lifetime.

8.2 100%+ natural circulation is achievable in a vessel configuration smaller than PRISM A, and hence the goal of modular, factory fabrication and overland transportable vessels appears achievable.

8.3 The calculated peak cladding temperature is in the range of existing materials technology (ferritic steel with oxide layer passivation).

8.4 Appreciable steam superheat is achievable in the reference steam generator.

8.5 The concept has developed to the point that iteration with core designers is warranted (Stage 2 analysis).

8.6 Additional thermal-hydraulic analyses are required to support the concept development, primarily i) effects of steam generator tube rupture, ii) auxiliary heat removal by air cooling of the guard vessel, and iii) coolant conditioning requirements for reliable, long term natural convection performance.

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NOMENCLATURE

A	=	flow area, m^2
c	=	specific heat, $J/(Kg\cdot K)$
D_h	=	hydraulic diameter, m
d	=	fuel rod diameter, m
f	=	friction factor
g	=	gravitational acceleration, m/s^2
h	=	heat transfer coefficient, $W/(m^2\cdot K)$
$\overline{h}$	=	mean heat transfer coefficient, $W/(m^2\cdot K)$
K	=	frictional loss coefficient
k	=	thermal conductivity, $W/(m\cdot K)$
L	=	length for frictional losses, m
L_{diff}	=	difference in elevation between HX and core thermal centers, m
Nu	=	$\frac{hD_h}{k}$ = Nusselt number
P	=	pressure, Pa
P_{ax}	=	peak core axial power factor
P_{rad}	=	peak core radial power factor
Pr	=	Prandtl number
p	=	fuel rod triangular pitch, m
Q_{tot}	=	total reactor power, W
q	=	heat flux, W/m^2
$\overline{q}$	=	mean heat flux in core, W/m^2
R	=	thermal resistance divided by radius, $m\cdot K/W$

Re	=	Reynolds number
r	=	radius, m
r_{in}	=	inner radius of SG tube wall, m
r_{out}	=	outer radius of SG tube wall, m
S_c	=	core wetted perimeter, m
T	=	temperature, K
$\bar{T}$	=	$\frac{1}{2}(T_{in} + T_{out})$, K
$T_{m,sat,dry}$	=	coolant temperature at top of dried out wall boiling region, K
$T_{m,sat,wet}$	=	coolant temperature at top of wetted wall boiling region, K
$T_{m,sub}$	=	coolant temperature at top of subcooled region, K
$T_{w,sup}$	=	steam temperature at top of superheated region, K
u	=	velocity, m/s
X	=	height, m
x	=	position coordinate, m
Y	=	fraction of coolant flow passing through HX

Greek Letters

β	=	$-\frac{1}{\rho}\frac{dp}{dT}$ = compressibility, K^{-1}
$\gamma_{w,wet}$	=	flow quality at top of wetted wall boiling zone
	=	fraction of water heat of vaporization removed in wetted wall boiling zone
$\gamma_{w,dry}$	=	$1 - \gamma_{w,wet}$
ΔT	=	temperature difference, K
ΔT_{sub}	=	water subcooling, K
δ_{clad}	=	cladding thickness, m

ϵ_{min} = minimum ratio of local-to-mean heat transfer coefficients around fuel rod circumference

ρ = density, Kg/m^3

$\bar{\rho}$ = density evaluated at mean coolant temperature, Kg/m^3

Subscripts

c = denotes core

clad = denotes cladding

cool = denotes coolant

dry = denotes dried out wall saturated boiling region

HX = denotes heat exchanger

in = denotes inlet temperature

in = denotes inner radius of HX tube wall

l = denotes liquid water

m = denotes liquid metal coolant

out = denotes outlet temperature; denotes outer radius of SG tube wall

sat = denotes saturated boiling region

SG = denotes steam generator

sub = denotes subcooled water region

sup = denotes superheated steam region

v = denotes water vapor/steam

w = denotes water/steam substance

wet = denotes wetted wall saturated boiling region

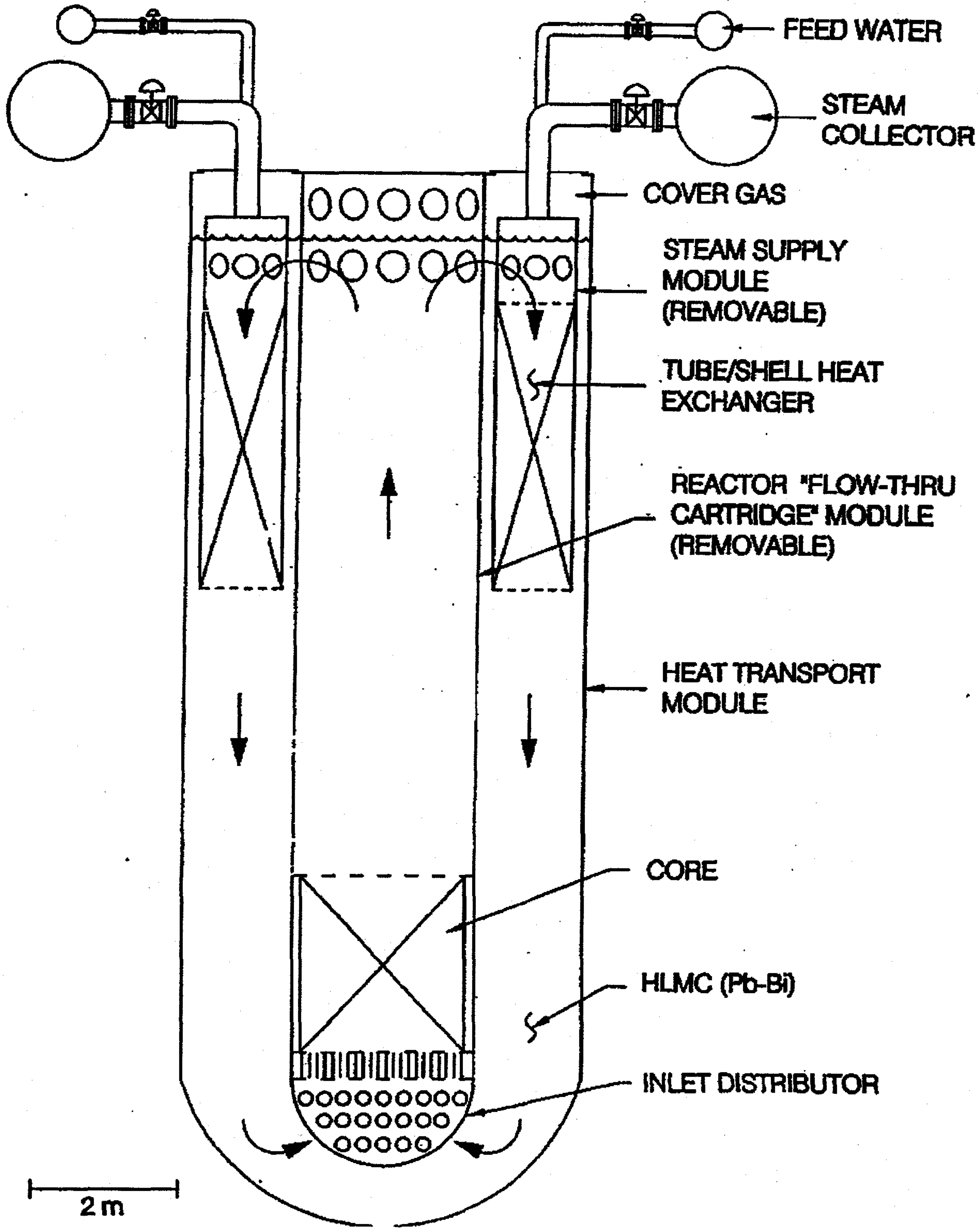

Figure 1. Concept for Simplified, Modular, Small Reactor Featuring Flow-thru Fuel Cartridge.

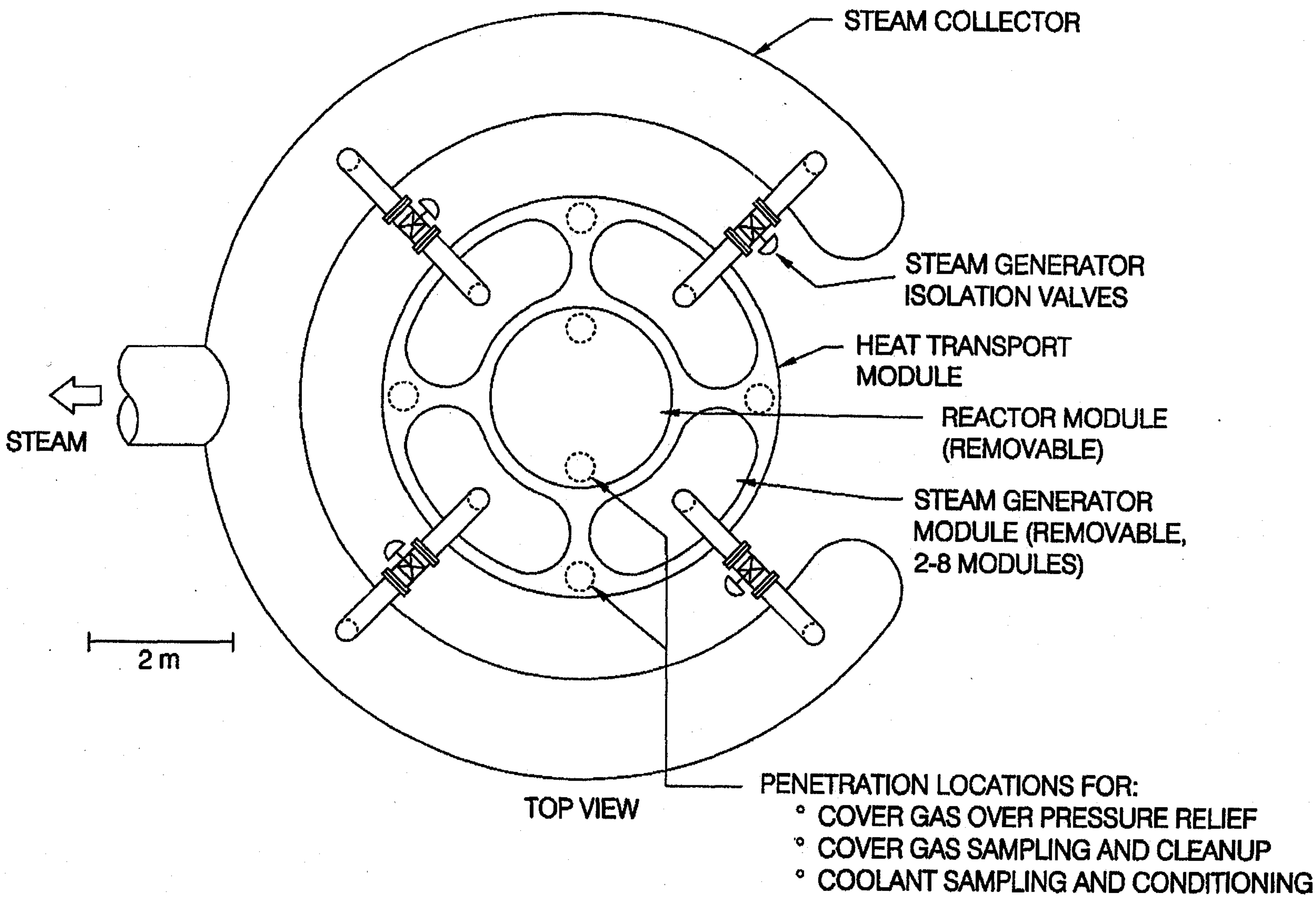

Figure 2. Top View of Simplified, Modular, Small Reactor.

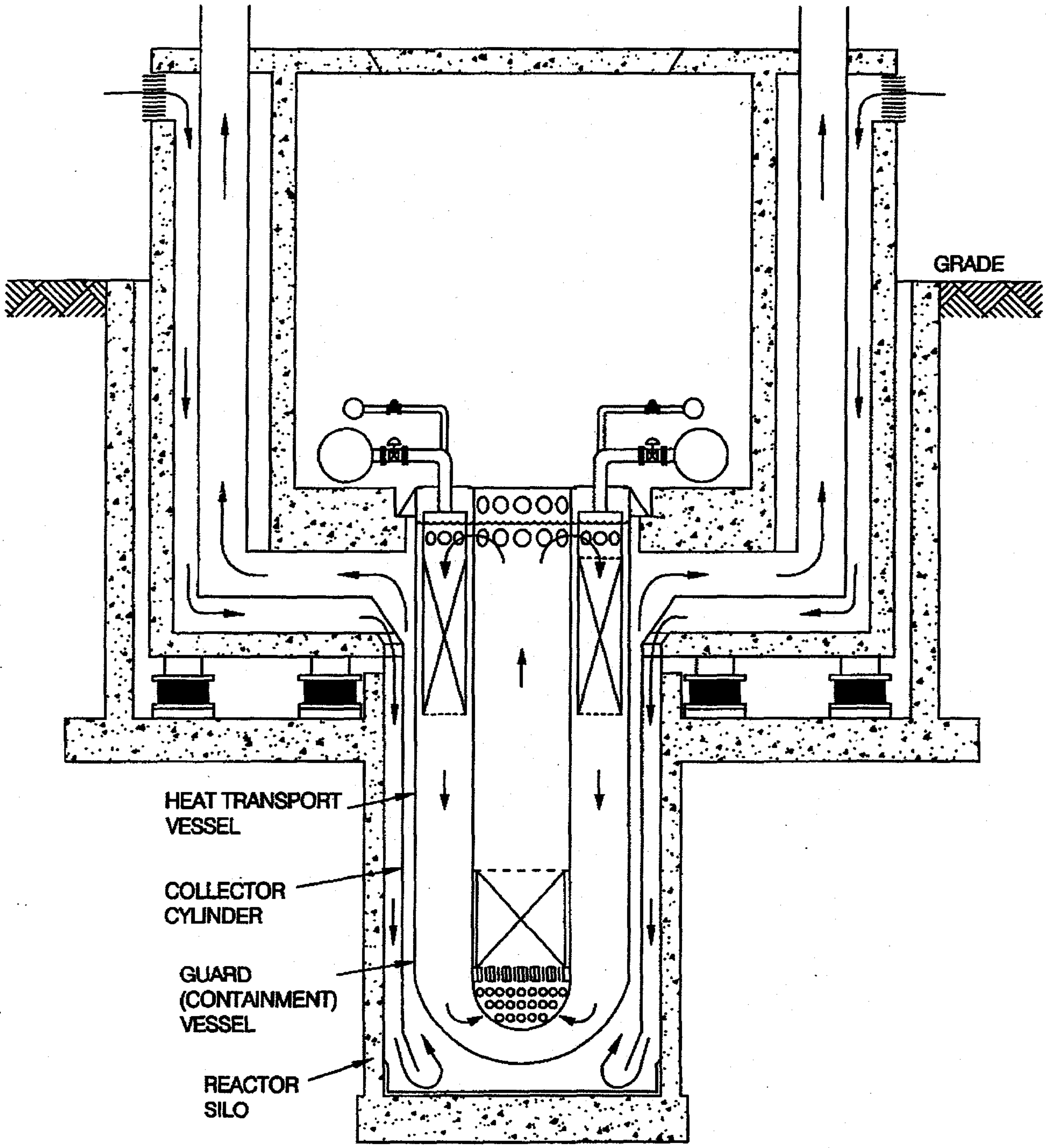

Figure 3. Seismic Isolation Approach for Nuclear Island; also shows RVACS.

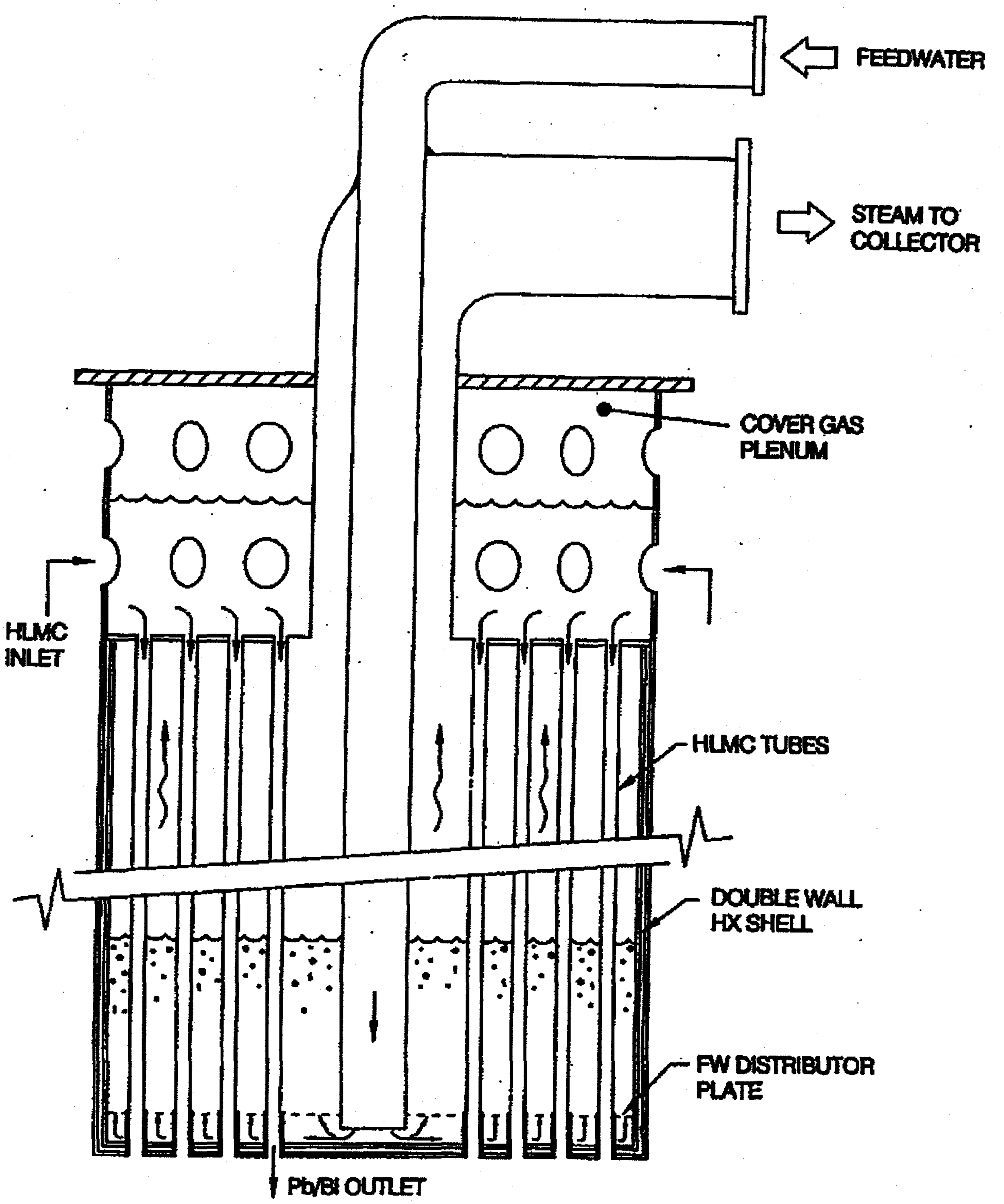

Figure 4. Illustration of Once-through Tube/Shell Steam Generator Module.

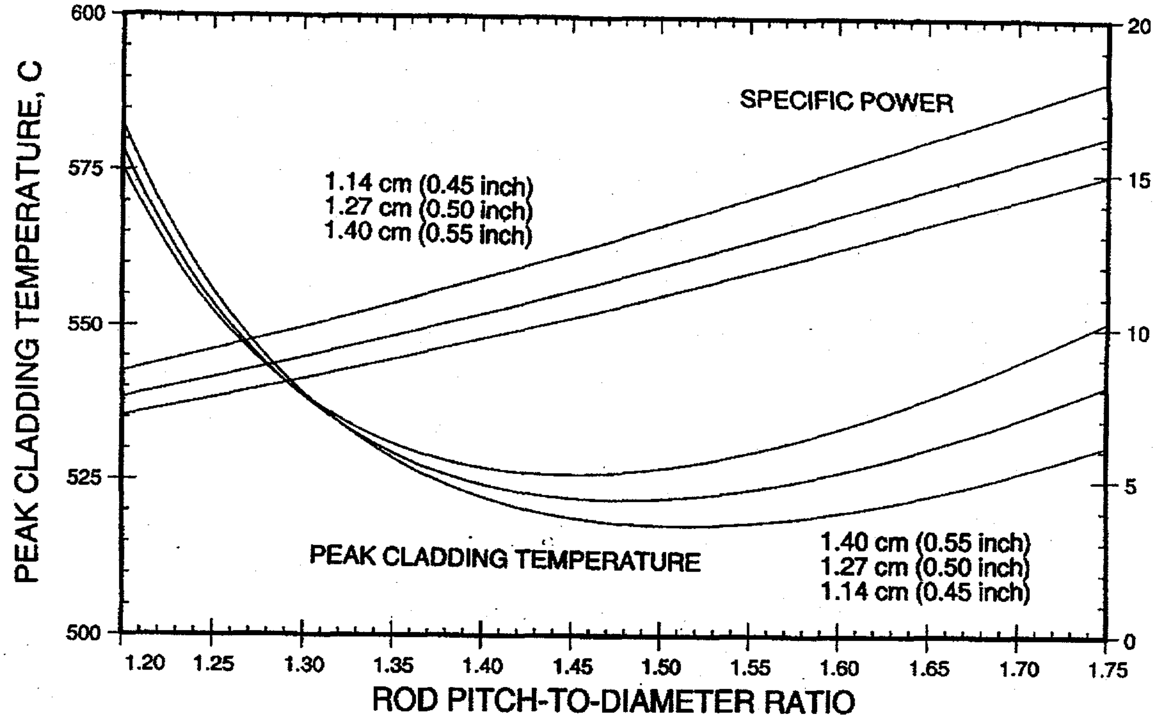

Figure 5. Effects of Fuel Rod Pitch –to-Diameter Ratio on Peak Cladding Temperature for Different Fuel Rod Diameters.

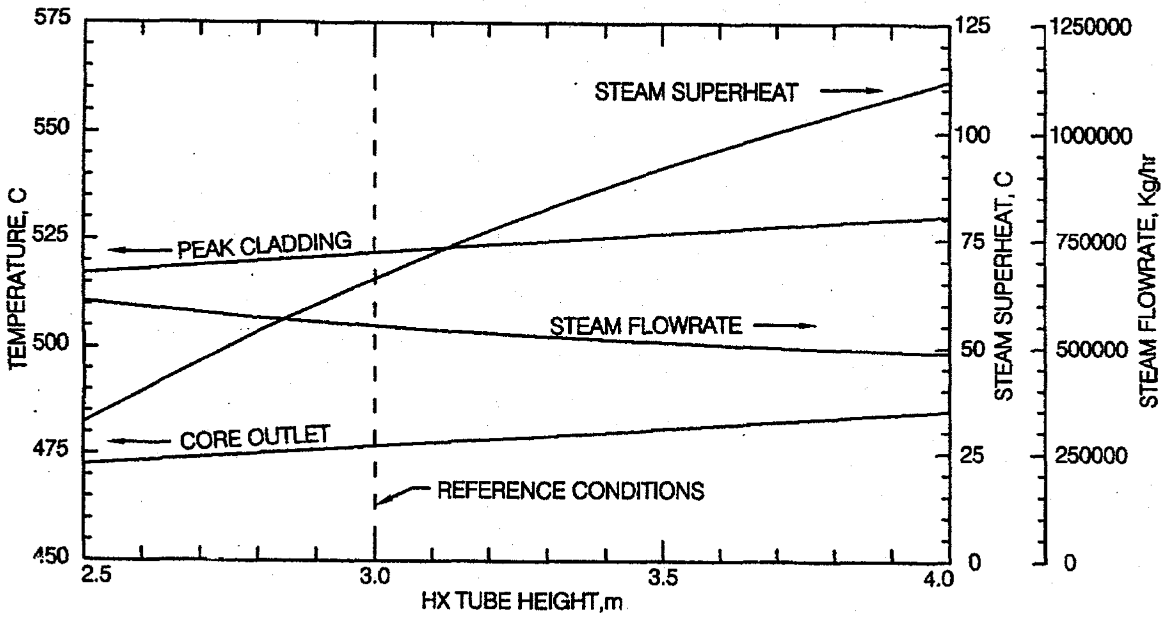

Figure 6. Effects of Steam Generator Tube Height on Core Outlet Temperature, Peak Cladding Temperature, HX Feedwater/Steam Flowrate, and Steam Superheat.

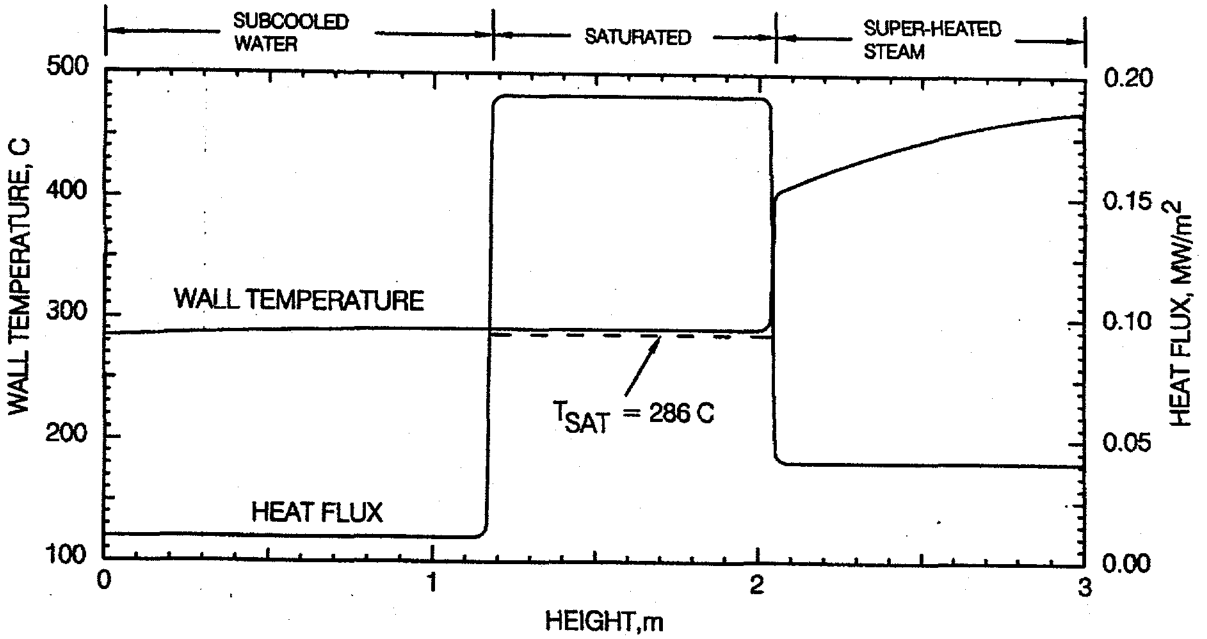

Figure 7. Heat Transfer Conditions on Water/Steam Side of Heat Exchanger for STAR Reference Conditions.

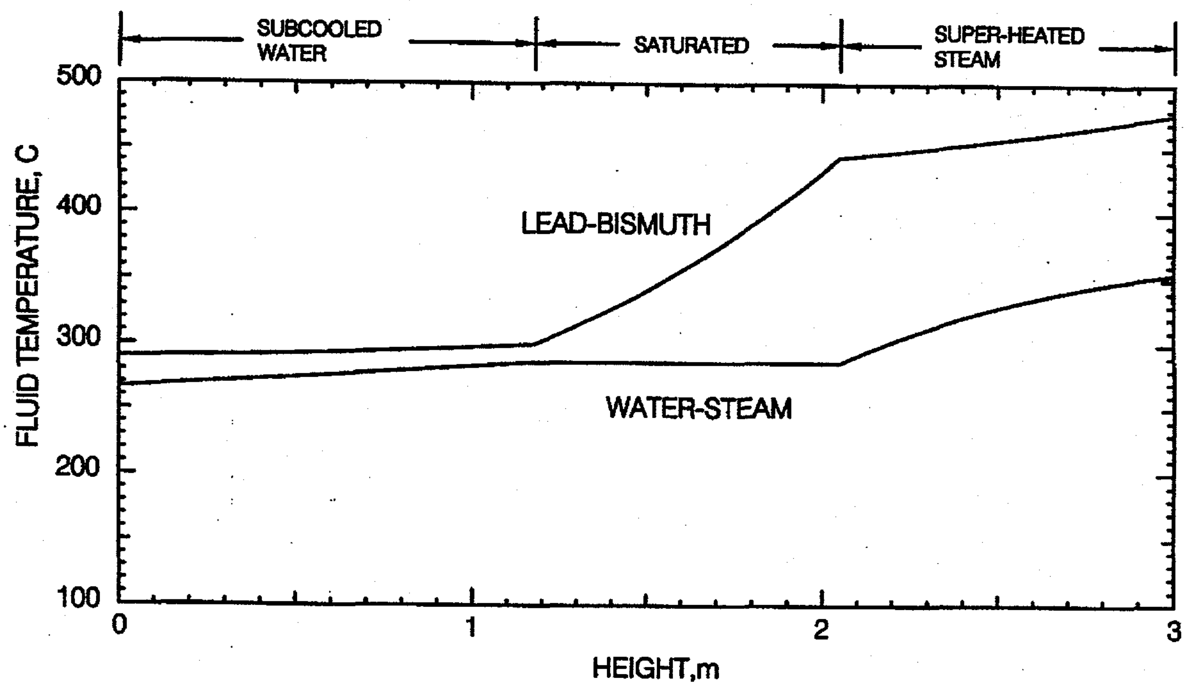

Figure 8. Lead-Bismuth Eutectic and Water/Steam Temperatures Inside Heat Exchanger for Reference Conditions.