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*Gas Production and Behavior
in the Coolant of the SP-100
Space Nuclear Power System*

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Space Nuclear Power System*

John Morton McGhee

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**GAS PRODUCTION AND BEHAVIOR
IN THE COOLANT OF
THE SP-100 SPACE NUCLEAR POWER SYSTEM**

John Morton McGhee

ABSTRACT OF DISSERTATION

The radiologic generation and subsequent behavior of helium gas in the lithium coolant of SP-100 class space nuclear power reactors was investigated analytically in a two part study. Part One of the study consisted of a calculation of coolant radiologic helium gas production rates in a SP-100 class reactor using the discrete ordinates code TWODANT. Cross sections were developed from ENDF/B-V data via the MATXS6s master cross section library. Cross sections were self shielded assuming one homogeneous core region, and doppler broadened to 1300 K using the cross section preparation code TRANSX. Calculations were performed using an S_4/P_1 approximation and 80 neutron energy groups. Part Two of the study consisted of a theoretical investigation into the behavior of helium gas in the primary loop of lithium cooled space reactors. The SP-100 space power system was used as a representative of such a system. Topics investigated included: 1) heterogeneous and homogeneous nucleation; 2) bubble growth/collapse by diffusion, mechanical temperature/pressure effects, and coalescence; and, 3) the effects on bubble distribution of microgravity, magnetic fields, and inertially induced buoyancy.

Part One results indicate that the primary helium production reactions in natural lithium are $\text{Li}^6(\text{n},\text{t})$ (87%), followed by $\text{Li}^7(\text{n},\text{nt})$ (9%), and $\text{Li}^6(\text{n},\text{nd})$ (4%). For reactors utilizing natural lithium coolant, approximately 50% of total helium production occurs in the core proper, 30% in the lower plenum, 10% in the core inlet/outlet piping, and the remaining 10% is produced in the rod and upper plenums. A reactor operating at 2.5 Mwt for 7 years using natural lithium coolant is predicted to produce approximately 1.3 moles of helium gas. At expected SP-100 end of life operating conditions of 1350 K and 152 kPa, the helium produced occupies a volume of 96 liters. For a reactor using coolant with a Li^6 enrichment of 0.1 atom %, gas production is reduced to 0.15 moles with a corresponding volume of 11 liters. Analysis of gas production cross sections indicates a minimum uncertainty in these results of 5 to 10%. A parametric analysis was conducted which enabled the development of an empirical correlation to predict helium production as a function of coolant Li^6 atom % and void fraction. Additional correlation parameters not verified in the study are suggested to permit extension of the results to geometrically similar systems. Analysis of diffusion coefficients and solubility data for isotopes of hydrogen indicated that these isotopes will not form a gas phase in the coolant.

Results of Part Two indicate that helium gas dissolved in the primary coolant will most likely diffuse out of solution directly into the system accumulator/gas separators before reaching a concentration sufficient to drive a nucleation process elsewhere in the loop. Differences in individual loop flow rates of only a few percent were demonstrated to have a significant impact on the relative gas diffusion

rates in the loop accumulators. Small bubbles ($< 15 \mu\text{m}$ in radius) which may escape the gas separators can be expected to collapse by mass diffusion. Bubble growth by coalescence is not expected to be significant if bubble number density is kept low (low void fraction). No definite conclusions were reached regarding bubble distributions in the primary heat transport loop. Topics for further research are suggested.

Bubble Behavior

Gas Production

Helium

Lithium

SP-100

Space Nuclear Power

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1. INTRODUCTION

1.1 Background

The exploration and development of space requires reliable and abundant power sources both for propulsion and for the operation of on board systems. Historically, the power needs of spacecraft have been met with a variety of sources including solar, radioisotopic generators, chemical batteries, and fuel cells. However, future missions such as:

- 1) Advanced direct broadcast satellites;
- 2) Air traffic control satellites;
- 3) Large scale industrial processing on space stations;
- 4) Electric propulsion orbital transfer systems, and;
- 5) Manned excursions to Mars and other planets;

will require higher power levels for longer periods of time than the missions of the past (Truscello and Davis, 1984). The missions listed above will typically require electric power levels in the tens to hundreds of kilowatts range for periods of a year or more.

The present high cost of launch to low earth orbit and beyond places a premium on mass minimization in the design and construction of space power systems. Launch costs can be calculated on a dollar per kilogram of payload basis and as such are directly proportional to payload total mass. The specific power (power per unit mass, kW/kg) is therefore an important parameter in the comparison of space power systems. Figure 1 compares the regimes of applicability for various power sources for space systems, based primarily on specific power. Although the dividing lines between the power sources depicted in Figure 1 may shift somewhat depending on various considerations such as

safety, reliability, material costs, proximity of the mission to the sun, etc., it is clear that for missions with long term, high power requirements, nuclear reactors enjoy a significant advantage over other power sources. By achieving a high specific power compared to conventional sources, nuclear reactors can provide a power supply with a lower total mass for high power, long term missions such as those listed above.

Space nuclear power systems typically employ a compact, high enrichment reactor design in order to achieve the high specific power required to minimize power system total mass. This design minimizes the mass and volume of the reactor core itself, which is a major, but not the only, component of the total power system mass. A large fraction of the total mass of a space reactor power system is often made up of the heat rejection or radiator sub-system. The physics of the heat rejection process in the near vacuum environment of space make it desirable that the overall system operating temperature should be as high as possible in order to minimize the radiator area and mass. A system working fluid (coolant) with a low vapor pressure is required to minimize the mass of the system pressure vessel and associated piping at high operating temperatures. The liquid metals (eg. Na, K, Li) afford low vapor pressures at high temperatures and for this reason and others they are commonly used as system working fluids in space reactor designs. The often seen space reactor characteristics of small size, high enrichment, and liquid metal coolant are exemplified in the United States prototypical space reactor power system design, the SP-100 Space Nuclear Power System (General Electric, 1985).

Figure 1 - Power Sources for Space Missions
(Angelo and Buden, 1985)

1.2 SP-100 Space Nuclear Power System

NASA, DARPA, and DOE began the SP-100 program in February, 1983, in order to develop the potential of nuclear reactors to meet the increased power requirements of future missions. The goal of the SP-100 program is the development of space nuclear reactor power systems in the 100 to 1000 kWe range. The initial design review phase of the program, completed in October 1985, ended with the selection of a thermoelectric reference design which is currently undergoing ground engineering and development. This engineering development phase, scheduled for completion in the early 1990's, will include the construction and ground testing of a full scale space nuclear reactor system design. Following completion of the ground engineering phase, the complete power system will be tested in an actual space environment in the flight development phase of the program. It is hoped that the SP-100 program will provide the foundation for a generation of modular space nuclear power systems which can be easily adapted to a wide variety of future missions.

The reference design selected in Phase 1 is a compact, uranium nitride fueled, lithium cooled, fast reactor system. Core average enrichment is over 95%, with an effective diameter of approximately 0.35 m. Reactor power level is 2.4 MWt with a net electrical output of 100 kWe from a thermoelectric conversion system. Reactor coolant will be circulated by 12 thermoelectrically powered electromagnetic (TE-EM) pumps. Coolant temperature at the reactor exit will average approximately 1300 K during normal full power operation. Primary system pressure is expected to vary between 62 kPa at beginning of life (BOL) and 152 kPa at end of life (EOL). The pressure vessel, piping, and fuel cladding are constructed from a Nb-.99%Zr-.01%C (PWC-11)

refractory alloy. Total mass of the system is expected to be approximately 4,000 kg, with a full power design lifetime of seven years. A general outline of the major system features is presented in Figures 2 and 3. Although the major features of the system design appear to be set, many details are currently under revision and will continue to be modified as research and development progress.

Figure 2 - SP-100 Overall System Layout
(General Electric, 1988a)

Figure 3 - SP-100 Reactor and Primary Coolant Loop
(General Electric, 1988a)

1.3 Radiologic Gas Production in Lithium Coolant

The selection of a reactor coolant is a complex process, involving the detailed consideration of many factors and tradeoffs between these factors. Lithium has several attractive features as a coolant for a space nuclear power system. Lithium has both the lowest vapor pressure, and the lowest density of any liquid metal, characteristics which enhance mass minimization efforts. In addition, Li is a highly efficient heat transport medium. Lithium's low density, high heat capacity and high thermal conductivity result in low pumping power requirements, which lead to reductions in pump mass. Also, Li is an electrical conductor, which enables the use of seal-less electromagnetic pumps, reducing coolant leakage concerns. These properties taken together make lithium a strong candidate as a coolant for space nuclear power systems.

However, there are disadvantages to the use of lithium as well. Lithium is a solid at room temperature (melting point ≈ 453 K), which makes it difficult to work with. Lithium is corrosive and reactive with oxygen and water, further complicating handling of the metal. Also, lithium is converted in part into hydrogen and helium gas by radiologic action in the core of a nuclear reactor. These disadvantages must be overcome in order to utilize lithium as a reactor coolant. The gas production problem is of particular interest in this study.

Natural lithium is composed of approximately 92.6 a/o Li^7 , and 7.4 a/o Li^6 . Radiologic gas production in natural lithium is known to result primarily from a $\text{Li}^6(n,t)\alpha$ reaction. There is a concern that the presence of the resulting helium and hydrogen gas phase in the

primary coolant of an operating reactor could adversely impact system operations. Several mechanisms are envisioned for this. Gas bubbles in the coolant could blanket fuel pins in the reactor core, reducing the pin heat transfer coefficient and leading to local "hot spots" and subsequent pin damage. Alternatively, gas bubbles could become lodged in heat exchangers directly involved in power production thereby reducing heat exchanger efficiency and leading to a reduction in system output power. Or, in a system with an electromagnetic pump, gas bubbles could reduce the electrical conductivity of the working fluid, leading to a reduction in pump output.

As a result of the above concerns, the current design of the SP-100 primary coolant loop utilizes lithium depleted in Li^6 and includes gas separator/accumulators to remove gases generated in the primary coolant during normal system operations. However, these gas separators are designed to meet certain finite reliability and separation efficiency criteria. As a result, a quantity of gas may escape the separators either in solution or in the form of small bubbles. The possibility also exists that gases which appear in the Primary Heat Transport System (PHTS) external to the separators could adversely impact system operations in the time before they are removed from the coolant by normal separator function. Consequently, the behavior of gas bubbles in the PHTS remains a concern in the SP-100 system. Previous researchers have addressed the topics of coolant radiologic gas generation and gas bubble behavior as a result of these concerns, both for the SP-100 and for other liquid metal reactors.

1.4 Literature Search

Seo and El-Genk (1988) have studied the effects of primary coolant entrained gas on the operations of the SP-100 space nuclear power system. Their analysis of gas production was based on a one energy group, one spatial region model. Average flux magnitude and energy were obtained from an educated guess based on ground based fast breeder reactor spectrums. Hydrogen isotopes were assumed to diffuse away so that only helium production was considered. Helium production was assumed to result exclusively from $\text{Li}^6(n,t)$ and $\text{Li}^7(n,\gamma)$ reactions. Gas production results are presented in Figure 4 for various coolant Li^6 enrichments. Due to the broad assumptions made, the results certainly cannot be considered definitive, but they do indicate that a significant quantity of gas can be generated in the lithium coolant of a SP-100 class reactor. An estimate of the effects of the generated gas on the operation of the SP-100 system were made in addition to the gas production calculation. Two bubble distributions were assumed, an annular flow case with the gas at the center of the coolant channel, and a homogeneous case with the gas uniformly distributed in the coolant. The SP-100 system model SNPSAM (Seo, 1986) was modified to estimate the effects of the two distributions on system performance. Some of the results are presented in Figure 5 for various coolant Li^6 enrichments. Some increase in coolant and cladding temperatures is predicted, but most significantly, large increases in system pressure are estimated for high Li^6 content coolant. Again, these results cannot be considered definitive, but do indicate a significant effect on system operating parameters from radiologically generated gases.

Minkowycz, et al.(1976) numerically modeled the growth and collapse of small inert gas bubbles in forced convective liquid metal loops. Their model considered inertial, mass diffusion, and pressure and temperature effects on bubble radius. Their results indicated that bubbles can be expected to expand slightly as they traverse a typical liquid metal fast breeder reactor core, and that diffusion growth rates are highly dependent on initial bubble size. Singer, et al. (1977) showed that many of the terms retained in the original Minkowycz model were negligible under the steady state or quasi-steady state conditions typical of normal reactor operations. Specifically, Singer found that diffusion and inertial effects could be ignored compared to temperature and pressure effects for normal reactor operations.

Little other work appears to have been done that specifically addresses gas generation and behavior in the coolant of liquid metal reactors. In view of the uncertainty associated with the existing gas production calculations and the lack of gas behavior information specific to a high temperature, liquid metal, microgravity environment, a more detailed study of gas generation and behavior tailored to space nuclear power systems seems to be in order. Specifically, in order to assure that any gas which escapes the PHTS gas separators will not pose a hazard to normal system operations, a study of the nucleation, growth, and dynamics of gas bubbles in the primary coolant loop of SP-100 class reactors is of interest. Also, a detailed estimate of the total amount of gas that would be generated over the lifetime of such a system is required to properly size the system gas accumulators and control system end of life pressure. The information provided by such a study would be a valuable contribution to the design and development

of the the SP-100 system and to lithium cooled space reactors in general.

Figure 4 - SP-100 Coolant Gas Production

(Seo and El-Genk, 1988)

Figure 5 - Effects of Coolant Entrained Gas on SP-100 Operating Parameters

(Seo and El-Genk, 1988)

1.5 Objectives

The objectives of this research are as follows:

- 1) Develop an understanding of coolant radiologic gas production in small, fast, lithium cooled space reactors;
- 2) Develop an understanding of the nucleation, growth, and dynamics of gas bubbles in forced convective liquid metal loops in a microgravity environment, and;
- 3) Apply the above results to predict gas generation and gas behavior in the primary coolant of the SP-100 system during normal system operations.

1.6 Method of Analysis

The method of analysis selected consisted of a systematic program of literature searches and analytic/numeric computations utilizing existing data. No experimental work was performed. The general tasks identified to achieve the program objectives are summarized in the following list:

- 1) Identify major gas production reactions;
- 2) Determine which gases will be retained in the coolant as a gas phase;
- 3) Model a fast lithium reactor with a neutron transport code to determine the relative importance of the various gas production reactions, and the effect on gas production of changes in design and operating parameters such as coolant void fraction and Li^6 enrichment;
- 4) Develop a correlation to predict coolant radiologic gas production in systems similar to the SP-100;
- 5) Identify gas nucleation and removal mechanisms in the coolant;

- 6) Determine the effect on bubble growth and collapse of pressure and temperature changes, mass diffusion, and coalescence, and;
- 7) Summarize the results to produce predictions of gas generation and behavior in a lithium cooled space reactor.

1.7 Order of Presentation

The body of the study is presented in two sections, Chapters 2 and 3. Chapter 2 deals with the calculation of coolant radiologic gas production rates, while the behavior of the radiologically generated gas in the PHTS is analyzed in Chapter 3. Specific objectives, methods of analysis, appropriate literature searches and discussions, and applications of the results to the SP-100 system are included in both Chapters. Following Chapter 3 is an overall discussion and summary of the results, and finally recommendations for future work and concluding remarks are presented in Chapters 5 and 6.

2. RADIOLOGIC GAS PRODUCTION

2.1 Retention of Radiologically Produced Gases in Lithium

Natural lithium is composed of two isotopes, approximately 7.4 atom % Li^6 and 92.6 atom % Li^7 . When bombarded with neutrons, both of these isotopes undergo nuclear reactions to produce gases in the form of helium 4, hydrogen, deuterium, and tritium. In the SP-100 system, these radiologically generated gases will find themselves in a liquid lithium environment at approximately 1300 K and 62-152 kPa, surrounded by a vessel composed primarily of niobium. Preliminary studies (Seo and El-Genk, 1988) confirmed by the results obtained herein, indicate that maximum gas production over a typical seven year SP-100 lifetime should be less than 10 moles. Typical primary loop Li inventories are on the order of 2000 moles or more, resulting in a maximum gas concentration of less than 0.5 atom %.

The quantity of hydrogen and helium isotopes that remain in the coolant in a gas phase will be the difference between radiologic production and any loss mechanisms that may be significant. The gas phase may be lost from the coolant by any of several means: the gases may dissolve; diffuse away; undergo chemical reactions with the coolant or vessel walls; leak from the system; or be transformed into other non-gaseous elements by the same radiation field that originally created them. For the purposes of this study a worst case scenario was assumed regarding gas leakage; i.e., the primary coolant system was considered to be sealed so that no leakage of gas was possible. The remaining loss mechanisms are discussed below.

2.1.1 Helium Loss Mechanisms

Helium, a noble gas, does not react chemically in any appreciable amount with either niobium or lithium. The solubility of helium in lithium has been experimentally determined to be very low (Slotnick, et al., 1965). Likewise, several sources report a near zero permeation of any metal by helium (Stern, 1961), (Keesom, 1942).

Because of the small molar ratio (10/2000) of gas to coolant, removal cross sections for He must be at least twice Li production cross sections for radiologic losses to be even 1% of radiologic production. Review of relevant cross section data reveals that this is not the case, resulting in the conclusion that He⁴ radiologic removal will be negligible compared to production.

In conclusion, it appears that any helium generated in the primary coolant loop will remain there in a gaseous state unless removed by some active means.

2.1.2 Hydrogen Isotopes Loss Mechanisms

Utilizing the same arguments that were made for radiologic removal of helium, a similar result is obtained for isotopes of hydrogen, indicating that a negligible amount of the hydrogen isotopes will be removed from the coolant by radiologic action. However hydrogen, unlike helium, may be removed from the coolant by chemical mechanisms.

Hydrogen and its isotopes react readily with lithium forming LiH. The strength of this reaction is a function of temperature as shown in Figure 6 (Chase, et al., 1985). This figure is a plot of the Gibb's free energy (GFE) of formation for LiH. The Gibb's free energy can be thought of as measure of the "driving force" behind a reaction. When the GFE is negative, LiH is readily formed. When the GFE is 0, no

reaction takes place, and finally for positive GFE's, the reverse reaction is favored, breaking LiH down into Li and H. As can be seen in the Figure 6, at the expected operating temperature and pressure ranges for the SP-100 (1300 K, 62-152 kPa), hydrogen will not be strongly bound to Li. Deuterium and tritium, isotopes of hydrogen, are expected to have chemical and thermodynamic properties similar to hydrogen (Blackledge, 1968), and are also not expected to strongly react with Li at 1300 K.

Although the solubility of hydrogen and its isotopes in lithium decreases with increasing temperature, small concentrations are readily dissolvable even at the 1300 K temperatures of the SP-100 system. Figure 7 (Smith, et al., 1975) presents tritium solubility data for lithium at various temperatures. It appears that lithium can readily absorb up to 1.0 atom % tritium with hydrogen partial pressures of only 3 kPa or less. Thus it appears that all the hydrogen isotopes that could be generated over the reactor lifetime can be easily dissolved in the coolant. This fact would seem to eliminate any concern over hydrogen isotope bubbles forming in the primary coolant loop. However, tritium is radioactive and decays into He^3 with a 12.3 year half life. Although this He^3 has a large (n,p) cross section (Magurno, 1982) and a substantial quantity of it may be converted back into H^3 , some portion of the He^3 may be retained in the coolant. How much is actually retained is a function of the H^3 inventory in the coolant, which in turn depends on the loss rate of H^3 from the coolant through diffusion.

It is well known that hydrogen and its isotopes readily diffuse through most metals. The niobium which forms the 99% of the reactor vessel and piping is no exception. Plots of the diffusion coefficient

for hydrogen, deuterium and tritium in niobium are presented in Figure 8 (Alefeld and Volkl, 1978). Although this data does not extend beyond 1000 K, a steady increase in the coefficient is apparent with increasing temperature.

This high diffusion coefficient implies that much of the hydrogen isotopes produced may readily leak from the system. In general, loss of hydrogen through vessel and piping walls is a complex process consisting of several intermediate steps:

- 1) Removal of H from solution in Li,
- 2) Adsorption of H at the Nb inner wall surface,
- 3) Diffusion of H through Nb vessel wall,
- 4) Release of H from Nb outer wall surface, and,
- 5) Transfer through any vessel outer coating such as multifoil insulation, aeroshell, etc.

Factors such as surface contaminants, interstitial impurities and alloys, and chemical reaction between the medium and the diffusing species may have an impact on the overall rate of transfer in a difficult to quantify way. However, under the simplifying assumption that diffusion is the limiting, or rate controlling step in this process, an estimate can be made of the amount of tritium that will remain in the coolant. Details of this calculation are presented in Appendix E. To summarize the results, a stable H^3 concentration should be established in the primary loop about two days after the start of full power operation. He^3 production from the decay of H^3 retained in the coolant will amount to less than 1/10,000 of the He^4 produced in direct $(n,x)\alpha$ reactions, even under the assumption that none of the He^3 will be converted back into H^3 by (n,p) reactions.

In summary, it appears that hydrogen and its isotopes will readily dissolve in the lithium coolant of a SP-100 class reactor. The isotopes will not form a strong chemical bond with the coolant, and they have a high probability of diffusing through the Nb walls of the pressure vessel and piping. H^3 retained in the coolant will not generate a significant amount of He^3 compared to direct production of He^4 from $(n,x)\alpha$ reactions. In view of these conclusions, the effort to quantify coolant gas production was devoted exclusively to the calculation of He^4 production rates. Production of isotopes of hydrogen was not considered further.

Figure 6 - Gibbs Free Energy of Formation for LiH
(Chase, et al., 1985)

Figure 7 - Solubility of Tritium in Lithium
(Slotnick, et al., 1965)

Figure 8 - Diffusion of Hydrogen Isotopes in Nb
(Alefeld and Volkl, 1978)

2.1.3 Helium Production Cross Sections

As a preliminary step in the calculation of He^4 production rates, the neutron cross sections of lithium were reviewed to determine the possible He^4 production reactions. BNL 325 (Garber and Kinsey, 1976) (the "Barn Book") and the ENDF-B/V Guidebook (Magurno, et al., 1982) list cross sections for both Li^6 and Li^7 as a function of neutron energies. A review of these documents reveals 3 helium production reactions for each isotope. For Li^6 these are:

	Q (Mev)
1. $(n, 2n\alpha) \quad {}_3\text{Li}^6 + {}_0\text{n}^1 \rightarrow {}_2\text{o}\text{n}^1 + {}_2\text{He}^4 + {}_1\text{H}^1$	-3.7
2. $(n, nd) \quad {}_3\text{Li}^6 + {}_0\text{n}^1 \rightarrow {}_0\text{n}^1 + {}_2\text{He}^4 + {}_1\text{H}^2$	-1.5
3. $(n, t) \quad {}_3\text{Li}^6 + {}_0\text{n}^1 \rightarrow {}_1\text{H}^3 + {}_2\text{He}^4$	4.8

and for Li^7 :

1. $(n, 2n\alpha) \quad {}_3\text{Li}^7 + {}_0\text{n}^1 \rightarrow {}_2\text{o}\text{n}^1 + {}_2\text{He}^4 + {}_1\text{H}^2$	-8.7
2. $(n, nt) \quad {}_3\text{Li}^7 + {}_0\text{n}^1 \rightarrow {}_0\text{n}^1 + {}_2\text{He}^4 + {}_1\text{H}^3$	-2.5
3. $(n, \gamma) \quad {}_3\text{Li}^7 + {}_0\text{n}^1 \rightarrow {}_3\text{Li}^8 + \gamma + {}_2\text{He}^4 + \beta^-$	2.0

Trend information on these cross sections is presented in Figures 9, 10, 11, 12, 13 and 14. For purposes of estimating the relative importance of these reactions to He production, the Li^6 data must be multiplied by a factor of .074/.926 relative to the Li^7 data for natural lithium, or by a factor of $z_6/(1.0-z_6)$ for lithium with a Li^6 decimal atom % of z_6 . Also, the Li^7 (n, γ) cross section should be

multiplied by a factor of 2 since this cross section produces two helium atoms per reaction.

Comparison of the above cross section plots indicates that the dominate cross section for natural lithium is the $\text{Li}^6(n,t)$ reaction. Enrichment in Li^7 can be expected to decrease the relative importance of this reaction, and increase the importance of the $\text{Li}^7(n,nt)$ and (n,γ) reactions. The $(n,2n\alpha)$ reactions appear to be relatively insignificant compared to the other reactions. Although the spectrum of the SP-100 is expected to be fairly hard, and few if any thermal neutrons are expected to be present, the $1/v$ behavior of the $\text{Li}^6(n,t)$ and $\text{Li}^7(n,\gamma)$ cross sections could significantly increase the importance of any lower energy neutrons that do exist to the helium production calculation.

Figure 9 - $\text{Li}^6 (n, 2n\alpha)$ Cross Section
(Magurno et al., 1982)

Figure 10 - Li^6 (n,nd) Cross Section
(Magurno et al., 1982)

Figure 11 - Li^6 (n,t) Cross Section
(Magurno et al., 1982)

Figure 12 - $\text{Li}^7(n,2n\alpha)$ Cross Section
(Magurno et al., 1982)

Figure 13 - Li^7 (n,nt) Cross Section
(Magurno et al., 1982)

Figure 14 - ${}^7\text{Li}$ (n, γ) Cross Section
(Magurno et al., 1982)

2.2 Effects of Design and Operating Parameters

Coolant helium production can be calculated as the product of two factors: the sum of the various helium production cross sections, and; the neutron flux. For any particular reactor design, both of these factors are functions of space, time, and energy. Based on preliminary studies, several system design and operating parameters were identified as having a potential impact on helium production. These parameters were divided into two groups as shown below.

Group 1 - Factors not to be addressed in this study.

Over the range of interest covered in this study, and to the level of accuracy desired, these factors were judged to have relationships to gas production of a sufficiently simple nature so that investigation herein was not warranted.

(1) Reactor power - Changes due to thermal effects (Doppler broadening, density changes, etc.) were neglected, so that the flux and gas production cross sections were assumed to be independent of reactor power. As a result, the gas production rate was assumed to be directly proportional to the reactor power.

(2) System full power operating time - The effects of fuel burnup, Li^6 burnup, fission product buildup, and control element movement over the system life were neglected, so that the flux, helium production cross sections and gas production rate were assumed to be independent of time.

(3) System geometry and composition - The neutron flux and coolant present at various points in a reactor are functions of the reactor design. Reactors with different geometries and material compositions can naturally be expected to have different helium production rates.

Since limited resources prevent analysis of all possible reactor design configurations, one base system geometry representative of a SP-100 class reactor was selected for analysis. Changes in the base system geometry and/or material composition (other than changes to the coolant location and composition, and initial fuel enrichment discussed below), were assumed to have a minor impact on the gas production rate.

(4) Core average uranium enrichment - The effect of changes in the initial design specified fuel enrichment on the neutron flux spectrum was neglected, so that the magnitude of the neutron flux (and hence, gas production) were assumed to be inversely proportional to the core average enrichment.

Group 2 - Factors to be investigated to determine their effects.

Within the confines of the base case geometry, the following factors were selected for detailed analysis to determine their impact on helium production.

(1) Coolant void fraction - As radiologically generated helium gas builds up in the coolant, the coolant void fraction may increase, resulting in a decrease in the coolant number density in the core, affecting both the flux energy spectrum and gas production cross sections.

(2) Coolant Li^6 enrichment - Changes in the design specified coolant Li^6 content will affect both the flux energy spectrum and the gas production cross sections.

(3) Coolant spatial location within the system - Since both the neutron flux and the amount of coolant present vary from one point in the reactor to another, different spatial zones in the system are expected to have differing importance to helium production.

Having identified the design and operating parameters to be investigated, a gas production calculation methodology was developed, as discussed in the following sections.

2.3 Objective

The objective of the gas production calculation was to determine the amount of helium 4 radiologically produced in the coolant of a SP-100 class reactor as a function of: 1) coolant Li^6 content, and 2) coolant void content. In addition, an estimate of the relative importance of various spatial locations within the system to helium production was desired.

2.4 Method of Analysis

The method of analysis selected was numerical simulation. This method is relatively inexpensive and fast, and can give reliable results if properly executed. The discrete ordinates neutral particle transport code TWODANT (Alcouffe, et al., 1984) was selected to perform the simulation. TWODANT is a widely used, well documented transport code, with flexible editing features advantageous to the type of calculations to be performed.

In order to meet the objectives of the study, a calculation plan was developed as described below. A base case was defined for analysis by TWODANT with a coolant consisting of natural lithium and 0% void fraction. Geometry for the base case was as described in section 2.5.1 below, with the position of the radial reflector adjusted to produce a multiplication factor of approximately 1.0. For purposes of analysis the system was divided into the following coolant edit zones: 1) piping;

2)lower plenum; 3)core; 4)fuel rod fission gas plenum, and; 5)upper plenum.

Results of the base case calculations were analyzed as follows:

1) Flux Distribution

- a. midplane axial and radial profiles
- b. spectral analysis at three axial locations

2) Fission Power Distribution

- a. midplane axial and radial profiles
- b. central core fission spectrum

3) Helium Production

- a. production summaries by reaction for the five edit zones
- b. production spectrum by reaction at three axial locations.

Following the base case analysis, a sensitivity study was performed to confirm that the problem had been adequately resolved. Two cases were analyzed, one increasing the discrete ordinates S_n order from S_4 to S_8 , and then one doubling the radial mesh spacing.

Next, a parametric analysis was performed to determine the effects of void fraction and Li^6 content on helium production. Six cases were considered for the parametric analysis, as shown in Table 1.

Results of the parametric study were then analyzed to produce a correlation to predict helium production over the ranges investigated.

Table 1 - Parametric Analysis Case Description

<u>Case #</u>	<u>Void Fraction</u>	<u>Li⁶ Atom %</u>
1	0%	7.4
2*	0%	7.4
3*	0%	7.4
4	0%	1.0
5	0%	0.1
6	0%	0.01
7	10%	7.4
8	30%	7.4

*Cases 2 and 3 were Sensitivity Analyses of Case #1

2.5 Model Description

2.5.1 Model Geometry

Descriptions of the SP-100 system were collected from several sources (Jackson, 1987), (Kruger, 1987), (General Electric, 1985) and the resulting data were used to create a general model of a SP-100 class lithium cooled fast reactor system for input to the TWODANT transport code. A overview of the model geometry is presented in Figure 15. Material atom densities and volume fractions for each zone are presented in Tables 2 and 3. Because of the continuing design evolution of the SP-100 system, a detailed duplication of the latest design iteration was considered to be impractical and unnecessary. Instead, a general model was developed which contains all of the significant features thought to be typical of an SP-100 class reactor. All major coolant containing zones are modeled including the core, upper and lower plenums, and the reactor inlet/outlet piping. The coolant was modeled as a separate material to simplify changes in coolant composition required in subsequent analysis.

The system was divided into 44 neutronically homogeneous solver zones for problem execution. These solver zones were combined to form five edit zones (reactor inlet/outlet piping, lower plenum, core, fuel rod fission gas plenum, and upper plenum) in order to summarize the calculation results. The radial reflector was opened to approximate a critical configuration to assure a realistic flux shape. Additional details on TWODANT calculations are provided in Appendix B.

Figure 15 - TWODANT Solver/Edit Zone Key

Table 2 - Base Case Material Atom Densities

Material	Component Atom Densities (atoms/barn-cm)					
	Component # 1		Component # 2		Component # 3	
Fuel97	U ²³⁸	.0009564	U ²³⁵	.0309236	N	.0318800
Fuel73	U ²³⁸	.0086080	U ²³⁵	.0232720	N	.0318800
Liner	Re	.0659500				
PWC11	Nb ⁹³	.0525700	Zr	.0005414	C	.0004112
B4C	B ¹⁰	.0684000	B ¹¹	.0076000	C	.0190000
BeO	Be ⁹	.0676600	O	.0676600		
Be	Be ⁹	.1196000				
Baffle	Ta ¹⁸¹	.0540800				
Clnt	Li ⁶	.0027500	Li ⁷	.0343100		

Material Key:

- 1) Fuel97 - uranium nitride fuel enriched to 97 a/o U²³⁵
- 2) Fuel73 - uranium nitride fuel enriched to 73 a/o U²³⁵
- 3) Liner - natural rhenium fuel rod liner
- 4) PWC11 - Nb-0.99%Zr-0.1%C clad, vessel, and structural material
- 5) B4C - 90% enriched boron carbide safety rod poison
- 6) BeO - beryllium oxide radial reflector and safety rod follower
- 7) Be - first layer of payload shielding
- 8) Baffle - tantalum core bypass plenum wall
- 9) Clnt - natural lithium coolant

Table 3 - Base Case Material Volume Fractions by Solver Zone

<u>Zone</u>	<u>Material 1</u>	<u>Material 2</u>	<u>Material 3</u>	<u>Material 4</u>	<u>Material 5</u>
1	be 1.00				
2	pwcll .190				
3	pwcll 1.00				
4	pwcll .838				
5	pwcll 1.00				
6	pwcll .028	clnt .245			
7	pwcll .020	clnt .183			
8	pwcll .016	clnt .148			
9	pwcll .012	clnt .128			
10	clnt 1.0				
11	clnt 1.0				
12	pwcll .350	clnt .650			
13	pwcll .038	clnt .800			
14	pwcll .038	clnt .800	beo .134		
15	pwcll .190	beo .670			
16	pwcll .800	clnt .200			
17	pwcll .678	clnt .160	beo .134		
18	pwcll .800	clnt .200			
19	pwcll .129	clnt .176	fuel97 .594	liner .066	
20	pwcll .129	clnt .176	fuel73 .594	liner .066	
21	pwcll .129	clnt .176	fuel97 .594	liner .066	
22	pwcll .141	clnt .141	fuel97 .475	liner .053	beo .134
23	pwcll .141	clnt .141	fuel73 .475	liner .053	beo .134
24	pwcll .141	clnt .141	fuel97 .475	liner .053	beo .134
25	pwcll .129	clnt .176	fuel97 .594	liner .066	
26	pwcll .250	clnt .650	baffle .100		
27	beo .683				
28	beo .729				
29	beo .811				
30	beo .878				
31	pwcll .190	b4c .670			
32	pwcll .129	clnt .176	liner .107		
33	pwcll .141	clnt .141	liner .086	beo .134	
34	pwcll .141	clnt .141	liner .086	b4c .134	
35	pwcll .129	clnt .176	liner .107		
36	pwcll .200	clnt .800			
37	pwcll .678	clnt .160	b4c .134		
38	pwcll .200	clnt .800			
39	clnt 1.0				
40	pwcll .038	clnt .800	b4c .134		
41	pwcll .250	clnt .750			
42	pwcll .500	clnt .500			
43	pwcll .250	clnt .750			
44	pwcll .500	clnt .500			

2.5.2 Cross Section Development

Neutron cross sections were developed from the MATXS6s master library. The MATXS6s library, developed from ENDF/B-V data using the NJOY (MacFarlane, 1978) nuclear data processing system, is an 80x24 group, coupled neutron-gamma library intended primarily for fast reactor analysis. The master cross sections from the MATXS6s library were further processed for use by TWODANT with the TRANSX (MacFarlane, 1984) cross section preparation code. Using TRANSX, gamma groups were deleted, helium gas production edits were constructed, and cross sections were self shielded and doppler broadened as shown in Table 4. For self shielding purposes, the reactor was assumed to consist of one homogeneous zone consisting of 73% enriched U core. Complete details of the TRANSX calculations are included in Appendix C. A diagonal transport correction was used throughout, except for rhenium group 77 which used a consistent P correction to accelerate convergence in that group.

Table 4 - Cross Section Self Shielding and Doppler Broadening

<u>Isotope</u>	<u>Self Shielding Zone</u>	<u>Doppler Broadening Temperature - K</u>
He ⁴	4	300
Li ⁶	4	300
Li ⁷	4	300
Be ⁹	4	300
B ¹⁰	4	300
B ¹¹	4	300
C	1	300
C	3	300
N ¹⁴	1	300
N ¹⁵	4	300
O ¹⁶	2	300
O ¹⁷	4	300
Zr	4	300
Nb ⁹³	1	1200
Ta ¹⁸¹	4	1200
Re ¹⁸⁵	1	1400
Re ¹⁸⁷	1	1400
U ²³⁵	1	1400
U ²³⁸	1	1400

Self Shielding Zone Key:

Self Shielding

<u>Zone</u>	<u>Background Cross Section</u>
1	Reactor Core, 36 cm diameter cylinder
2	BeO Reflector, 14 cm diameter cylinder
3	B ₄ C Safety Rod, 6 cm diameter cylinder
4	Infinite Dilute

Notes: 1) Isotopes w/o superscript are natural composition
 2) Doppler Broadening and Self Shielding on some isotopes was limited due to lack of master library data.

2.6 Results and Discussion

2.6.1 Base Case Results

Base case results were calculated using an S_4/P_1 approximation with a mesh spacing of 1 cm both in the axial and radial directions. Mesh spacing was decreased to 0.5 cm in the upper 7 cm of the rod plenum to eliminate negative fluxes encountered in this area. The resulting mesh grid had 4,488 points.

The problem was executed on a Cray X-MP/48 supercomputer, worker 2 at Los Alamos National Laboratory, converging to a k_{eff} of 0.9962 in 9 minutes, with eight outer iterations. A total of 2.99 megawords were required to retain the problem specifications and fluxes completely in main core memory. Flux was normalized to 2.5 MWt, assuming 200 MeV/fission. Flux and fission power distribution plots were produced using a small locally generated program.

2.6.1.1 Neutron Flux Distribution

A one group, normalized plot of the base case radial flux distribution at the core midplane is presented in Figure 16. As would be expected, the flux peaks near the center of the core just outside the central safety rod follower, and drops smoothly with increasing r , reaching a value at the core edge of approximately 30% of the peak value. The flux levels off momentarily near the radial reflector, and then decreases steadily outside the reflector.

A one group, normalized plot of the base case axial flux profile is presented in Figure 17. As in the radial plot, the axial flux peaks in the core center, then drops to a value at the core edge equal to approximately 40% of the peak. Of particular interest in this plot is

the higher flux in the lower plenum than in the upper and rod plenums, apparently as a result of reflected neutrons from the Be shielding at the base of the reactor.

Figure 18 presents a flux spectrum plot near the core midplane at three different axial locations: lower plenum, core, and upper plenum. Flux spectra in the upper plenum and core zones are very similar. However, flux in the lower plenum can be seen to have a considerably larger thermal component than in the other two zones. This may be a result of the absence of thermal poisons in the lower plenum, such as the rhenium rod liner present in the core and rod plenums, and the B_4C safety rods in the upper plenum. A drop in the flux is apparent in both the upper and lower plenums at the Li resonance at 0.25 Mev. This drop is not noted in the core, possibly as a result of two factors: 1) there is more lithium in the plenums (80% by volume vs 18% in the core); and 2) there is a ready source of fission neutrons at this energy in the core which, of course, is not present in the plenums.

A comparison of the magnitude of the average one group flux in each edit zone is presented in Table 5. Again, the difference in the upper and lower plenum flux is apparent. The impact of this flux distribution on system helium production is discussed in section 2.6.1.3 below.

Figure 16 - SP-100 One Group Radial Flux Distribution

SP-100 Radial Flux Distribution

Figure 17 - SP-100 One Group Axial Flux Distribution

Figure 18 - SP-100 Flux Spectrum by Edit Zone

Table 5 - Edit Zone Average One Group Flux
(Base Case, 2.5MWt, 200 MeV/Fission, Natural Lithium)

<u>Edit Zone</u>	<u>Zone Average Flux</u>	
	<u>n/cm²-sec</u>	<u>Normalized</u>
Piping	0.091×10^{14}	0.072
Lower Plenum	0.365×10^{14}	0.289
Core	1.261×10^{14}	1.000
Rod Plenum	0.226×10^{14}	0.179
Upper Plenum	0.035×10^{14}	0.028

Source: TD 170646 032588

2.6.1.2 Fission Power Distribution

A one group, normalized plot of the base case radial fission power profile is presented in Figure 19. Fissions from both U^{235} and U^{238} are included. As indicated in Figure 19, fission power peaks near the core center, just outside the central safety rod. A sudden drop in power is noted in the radial zone containing the BeO safety rod followers. Uranium content was reduced approximately 20% in this zone, due to the presence of safety rod thimbles. Outside the annulus containing the safety rod thimbles, the fission power increases as the uranium enrichment is increased from 73 a/o to 97 a/o. Minimum power occurs at the outer edge of the safety rod annulus, and is approximately 45% of the peak power.

A one group normalized plot of the base case axial fission profile is presented in Figure 20. Again, the fission power peaks near the core center. An increase in power is experienced at both the top and bottom of the core as the enrichment increases from 73% to 97% on the core perimeter. A slight asymmetry is noted: power in the lower half of the core appears to be slightly (0-10%) higher than in the upper core, presumably as a result of the higher neutron flux in the lower region of the reactor (See Figure 17).

Figure 21 presents a normalized plot of the core fission spectrum. The vast majority of fissions occur above 1 kev, confirming the fast nature of the SP-100 core.

Figure 19 - SP-100 Radial Fission Profile

Figure 20 - SP-100 Axial Fission Profile

Figure 21 - SP-100 Fission Spectrum

SP-100 Core Fission Spectrum

2.6.1.3 Helium Production

Base case helium production rates by reaction for the total system are presented in Table 6. Of the six reactions investigated, three were found to significantly contribute to helium production. These were the $\text{Li}^6(\text{n,t})$, $\text{Li}^6(\text{n,nd})$, and $\text{Li}^7(\text{n,nt})$ reactions. For natural lithium, these three reactions accounted for 87%, 4%, and 9% respectively, of the total helium produced. Together, they accounted for more than 99.9% of the helium produced. Further analysis was therefore restricted to these reactions.

Figures 22, 23, and 24 present a spectral analysis of the major helium production reactions identified above.

The $\text{Li}^6(\text{n,nd})$ and $\text{Li}^7(\text{n,nt})$ reactions are high energy inelastic scattering reactions. Helium production from these reactions is confined to an energy range between 2 and 20 MeV. Both reactions peak at about 5 MeV, and have very similar spectrums in the core, lower plenum and upper plenum.

The $\text{Li}^6(\text{n,t})$ reaction cross section increases in a $1/v$ fashion with decreasing energy and in addition, has a notable resonance at 0.25 MeV. Inspection of the spectrum for this reaction shows a peak in He production at the resonance at 0.25 MeV for all three zones: core, upper plenum, and lower plenum. However, there is a noticeably larger contribution from lower energy groups in the lower plenum. Nevertheless, the majority of helium production occurs in the higher energy groups for the lower plenum also (75% above 1 keV, see Figure 25). It appears that neutrons in the thermal and epithermal energy ranges do not make a large contribution to the overall system helium production, even for a system with natural lithium coolant. Even less

of a contribution could be expected from systems using coolant depleted in Li^6 .

Figures 26, 27, and 28 present helium production rates by zone for Li^6 , Li^7 , and for the total of the two isotopes. As mentioned previously, the $\text{Li}^6(n,nd)$ and $\text{Li}^7(n,nt)$ reactions are high energy reactions with thresholds near 2 MeV. As a result, about 70% of the helium production from these two reactions occurs in the core where high energy neutrons are most abundant. Production from $\text{Li}^6(n,t)$ also peaks in the core (46%), but has a significant contribution (41%) from the lower plenum and piping. This effect can be understood in light of the fact that the $\text{Li}^6(n,t)$ reaction has a $1/v$ cross section, and can readily absorb neutrons that are returned to the lower plenum by the radial reflector and Be shield. Lack of significant production in the rod and upper plenums, even though they contain considerable amounts of lithium, indicates that the flux in these zones is not moderated to any large extent. Also, the $\text{Li}^6(n,t)$ reaction in the rod and upper plenums must compete with the B_4C safety rods and rhenium rod liner for low energy neutrons.

Comparison of Figures 26 and 27 suggests that the use of a coolant with low Li^6 enrichment should increase the relative contribution of the core to He production and decrease the contribution of the lower plenum and piping, while leaving the rod and upper plenums unaffected. This point is also apparent in Figure 29 which depicts the ratio of the zone average production rate per kg of coolant to the core average production rate per kg of coolant. This figure shows the relative "importance" of each zone to helium production. A kg of coolant added

to a high importance zone, such as the core, will result in more helium production than the same amount of coolant added to a less important zone, such as the upper plenum.

Table 6 - Base Case Total Helium Production by Reaction
(2.5 Mwt, Natural Lithium Coolant, 0% Void)

<u>Reaction</u>	<u>Atoms He⁴/sec</u>	<u>% of Total</u>
Li ⁶ (n,2n α)	1.373 $\times 10^{12}$	0.04
Li ⁶ (n,nd)	1.414 $\times 10^{14}$	3.91
Li ⁶ (n,t)	3.154 $\times 10^{15}$	87.15
Li ⁶ Subtotal	3.297 $\times 10^{15}$	91.10
Li ⁷ (n,2n α)	1.445 $\times 10^{11}$	0.004
Li ⁷ (n,nt)	3.199 $\times 10^{14}$	8.84
Li ⁷ (n, γ)	2.148 $\times 10^{12}$	0.06
Li ⁷ Subtotal	3.222 $\times 10^{14}$	8.90
Grand Total	3.619 $\times 10^{15}$	100.00

Source: TD 202716 032388

Figure 22 - Base Case $\text{Li}^6(n,t)$ Reaction Spectrum by Edit Zone

SP-100 $\text{Li}^6(n,t)$ Reaction Spectrum

Figure 23 - Base Case Li^6 (n,nd) Reaction Spectrum by Edit Zone

SP-100 Li^6 (n,nd) Reaction Spectrum

Figure 24 - Base Case $\text{Li}^7(n,nt)$ Reaction Spectrum by Edit Zone

SP-100 $\text{Li}^7(n,nt)$ Reaction Spectrum

Figure 25 - Base Case $\text{Li}^6(n,t)$ Lower Plenum Integral Reaction Spectrum

Lower Plenum $\text{Li}^6(n,t)$ Integral Reaction Spectrum

Figure 26 - Base Case Li^6 Major Helium Production Reactions by Edit Zone

Figure 27 - Base Case Li^7 Principle Helium Production Reaction by Edit Zone

Figure 28 - Base Case Natural Li Helium Production Reactions by Edit Zone

Natural Lithium Major Helium Production Reactions by Edit Zone

Figure 29 - Base Case SP-100 He Production per Kg of Coolant by Edit Zone

2.6.1.4 Sensitivity Analysis

In order to verify the adequacy of the model mesh spacing and the S_n order, two variations on the base case were calculated. In the first, the S_n order was increased from 4 to 8. In the second, the radial mesh spacing was decreased from 1 cm to 0.5 cm. Results are presented in Table 7.

The S_8 case resulted in a increase in run time of about 3 minutes over the base S_4 case. The resulting change in core total He production was less than 2%, and k_{eff} was decreased by .003.

Doubling the number of radial mesh points resulted in an increase in run time of 7 minutes over the base case. He production was affected by less than 0.1%, and k_{eff} changed by only .0002.

Based on the data from these two cases, the base case S_n order and mesh spacing were assumed to be adequate to produce reliable results.

Table 7 - Sensitivity Study Results

(Base Case Geometry, 2.5 Mwt, 200 MeV/fission, Natural Lithium)

Case
Description:

Case #	1	2	3		
Case ID	<u>Base Case</u>	<u>S_e Case</u>	<u>Fine Mesh Case</u>		
R Mesh Points	44	44	88		
Y Mesh Points	102	102	102		
Total Points	4488	4488	8976		
S _n Order	4	8	4		
<u>Results</u>	<u>Ref Datum</u>	<u>Datum</u>	<u>Δ%</u>	<u>Datum</u>	<u>Δ%</u>
k _{eff}	0.9962	0.9931	-0.31	0.9964	+0.02
Run Time (min)	10.6	13.3	+25.5	17.6	+66.0
⁴ He Production <u>(atoms/sec)</u>					
Li ⁶ (n,2nα)	1.373×10 ¹²	1.368×10 ¹²	-0.36	1.372×10 ¹²	-0.07
Li ⁶ (n,nd)	1.414×10 ¹⁴	1.410×10 ¹⁴	-0.28	1.413×10 ¹⁴	-0.07
Li ⁶ (n,t)	3.154×10 ¹⁵	3.097×10 ¹⁵	-1.81	3.153×10 ¹⁵	-0.03
Li ⁶ Subtotal	3.297×10 ¹⁵	3.239×10 ¹⁵	-1.76	3.296×10 ¹⁵	-0.03
Li ⁷ (n,2nα)	1.445×10 ¹¹	1.438×10 ¹¹	-0.48	1.443×10 ¹¹	-0.14
Li ⁷ (n,nt)	3.199×10 ¹⁴	3.187×10 ¹⁴	-0.38	3.195×10 ¹⁴	-0.13
Li ⁷ (n,γ)	2.148×10 ¹²	2.096×10 ¹²	-2.42	2.147×10 ¹²	-0.05
Li ⁷ Subtotal	3.222×10 ¹⁴	3.209×10 ¹⁴	-0.40	3.218×10 ¹⁴	-0.12
Grand Total	3.619×10 ¹⁵	3.560×10 ¹⁵	-1.63	3.618×10 ¹⁵	-0.03

2.6.2 Parametric Analysis

Following the completion of the base case analysis, a parametric study was conducted assessing the effect of coolant Li^6 atom % and coolant void content on total helium production. The results of that study are discussed below.

2.6.2.1 Effects of Coolant Li^6 Content on He Production

Three cases were analyzed, in addition to the base case. Results are presented in Table 8. Some negative fluxes and convergence problems were experienced in the last five groups for the 0.1% and 0.01% Li^6 enrichment cases. This was not considered to affect the results, as analysis of the fully converged base case demonstrated that these groups do not contribute significantly to the calculation.

A nearly linear relation was observed between He production and the relative atom densities of the two coolant constituent isotopes, indicating that, as far as overall production of He is concerned, the flux was not significantly affected by the coolant Li^6 enrichment. The relative contributions from Li^6 and Li^7 to He production were 90% and 10%, respectively, for the base case. At a coolant Li^6 content of 1.0 atom %, the relative contributions shift to 62% from Li^6 and 38% from Li^7 . At lower Li^6 contents, He production becomes dominated by Li^7 . Figure 30 presents He production by edit zone for a coolant Li^6 content of 0.1 atom %. Comparison of Figure 30 with Figure 28 (He Production by Edit Zone for Natural Lithium Coolant) plainly shows the increased

importance of both the $\text{Li}^7(\text{n},\text{n}\text{t})$ reaction and the reactor core region to helium production as Li^6 content is reduced.

The small observed non-linearity in the He production rates as a function of coolant Li^6 content is attributed to changes in the energy and spatial distribution of the neutron flux resulting from changes in the coolant Li^6 inventory.

Table 8 - Effects of Coolant Li^6 Content On Helium Production

(Base Case Geometry, 2.5 Mwt, 200 MeV/fission, 0% Void)

Case #	1	4	5	6
Atom % Li^6	7.4	1.0	0.1	0.01
k_{eff}	0.9962	1.0035	1.0048	1.0048
 He^4 Production <u>(atoms/sec)</u>				
$\text{Li}^6 (n, 2n\alpha)$	1.373×10^{12}	1.839×10^{11}	1.838×10^{10}	1.837×10^9
$\text{Li}^6 (n, nd)$	1.414×10^{14}	1.896×10^{13}	1.894×10^{12}	1.894×10^{11}
$\text{Li}^6 (n, t)$	3.154×10^{15}	5.352×10^{14}	6.743×10^{13}	7.205×10^{12}
Li^6 Subtotal	3.297×10^{15}	5.543×10^{14}	6.934×10^{13}	7.396×10^{12}
$\text{Li}^7 (n, 2n\alpha)$	1.445×10^{11}	1.535×10^{11}	1.547×10^{11}	1.549×10^{12}
$\text{Li}^7 (n, nt)$	3.199×10^{14}	3.399×10^{14}	3.427×10^{14}	3.431×10^{14}
$\text{Li}^7 (n, \gamma)$	2.148×10^{12}	3.119×10^{12}	4.216×10^{12}	4.575×10^{12}
Li^7 Subtotal	3.222×10^{14}	3.432×10^{14}	3.471×10^{14}	3.478×10^{14}
Grand Total	3.619×10^{15}	8.975×10^{14}	4.164×10^{14}	3.552×10^{14}

Figure 30 - He Production by Edit Zone for 0.1 Atom % Li^6 Coolant

0.1 Atom % Li^6 Major Helium Production Reactions by Edit Zone

2.6.2.2 Effects of Coolant Void Content on He Production

Two cases (10% and 30% void fraction) were analyzed in addition to the base case. The void was assumed to be distributed uniformly throughout the coolant. Results, as presented in Table 9, indicate a nearly linear relation between void content and He production. The 10% void fraction resulted in a 8.9% reduction in helium production and the 30% void fraction resulted in a 27.6% reduction in He production. Again, the non-linearity is considered to be a result of changes in the flux spectrum and spatial distribution resulting from the increased void fraction.

Table 9 - Effect of Coolant Void Fraction on Helium Production
(Base Case Geometry, 2.5MWt, 200 MeV/fission, Natural Li)

Case #	1	7	8
Coolant Void Fraction - %	0.0	10.0	30.0
k_{eff}	0.9962	0.9949	0.9923
He^4 Production <u>(atoms/sec)</u>			
Li^6 (n,2 α)	1.373×10^{12}	1.250×10^{12}	9.945×10^{11}
Li^6 (n,nd)	1.414×10^{14}	1.286×10^{14}	1.021×10^{14}
Li^6 (n,t)	3.154×10^{15}	2.867×10^{15}	2.284×10^{15}
Li^6 Subtotal	3.297×10^{15}	2.997×10^{15}	2.387×10^{15}
Li^7 (n,2 α)	1.445×10^{11}	1.312×10^{11}	1.040×10^{11}
Li^7 (n,nt)	3.199×10^{14}	2.912×10^{14}	2.318×10^{14}
Li^7 (n, γ)	2.148×10^{12}	1.953×10^{12}	1.561×10^{12}
Li^7 Subtotal	3.222×10^{14}	2.933×10^{14}	2.335×10^{14}
Grand Total	3.619×10^{15}	3.290×10^{15}	2.621×10^{15}

2.6.2.3 Development of Helium Production Correlation

The reliable execution of a neutronics code like TWODANT is a time consuming, detailed process that requires considerable expertise and resources. For purposes of design and development, it is desirable to have a simpler but yet accurate means of estimating the helium production of SP-100 class systems. To this end, an empirical correlation was developed to represent the observed helium production rates as a function of system material and operating parameters.

Three separate sub-correlations were developed for production from Li^6 , production from Li^7 , and the effects of coolant void fraction. These sub-correlations were then combined with other relationships assumed to be linear (Group 1 Parameters as listed in Section 2.2 above) to produce a single master correlation. Three types of curve fits were attempted: exponential ($y=ae^{bx}$); logarithmic ($y=a+b(\ln(x))$), and; power ($y=ax^b$). Best results were obtained with the power law for all three sub-correlations.

Helium production as a function of Li^6 content was fit with a power law as follows:

$$y=a(z_6)^b \quad (1)$$

where:

y - total He production due to Li^6 (atoms/sec at 2.5 Mwt)

z_6 - Li^6 atom % (decimal fraction)

a - empirical constant - 3.777×10^{16}

b - empirical constant - 0.9216

The empirical constants a and b in equation (1) were selected to provide a "best fit" to the observed data in a least squares sense.

The coefficient of determination (r^2) for this fit was 0.99957, where 1.00000 indicates a perfect fit. Observed Li^6 production rates are predicted to within $\pm 6.8\%$.

Helium production as a function of coolant Li^7 enrichment was fit in a fashion similar to the Li^6 data as follows:

$$y = a(z_7)^b \quad (2)$$

where:

y = total helium production from Li^7 (atoms/sec at 2.5 Mwt)

z_7 = Li^7 atom % (decimal fraction)

a = empirical constant = 3.473×10^{14}

b = empirical constant = 0.9800

The coefficient of determination for the Li^7 fit was 0.9973, and the correlation predicts the observed production rates to within 0.2%.

The effects of coolant void fraction on helium production were also fit with a power law :

$$y = a(1.0 - \beta)^b \quad (3)$$

where:

y = fraction of He produced relative to 0% void

β = coolant void fraction (decimal fraction)

a = empirical constant = 1.000

b = empirical constant = 0.9046

The coefficient of determination for the void fraction fit was 0.9999, with an error of less than 0.1%.

Linear weighting factors were developed as shown in Tables 10 and 11 to weight the mass of coolant in each edit zone for the observed

production of He^4 in that zone. These factors can be thought of as the ratio of each edit zone's helium production rate per kg of lithium in the zone to the system average helium production rate per kg of lithium. Two weighting strategies were developed, one based on natural lithium coolant and one based on 0.1 atom % Li^6 coolant. Both strategies produce identical results for the base case geometry, but may produce slightly different productions for coolant mass distributions other than the base case. The most appropriate strategy should be selected based on the expected coolant enrichment.

Finally, the above correlations and weighting factors were combined to produce a master correlation for He Production:

$$N_{\text{He}} = t f_{\text{con}} W [(1-\beta)^{0.9046}] (\gamma_6 w_6 + \gamma_7 w_7) / z_{235} \quad (4)$$

where:

N_{He} - total helium produced in system (moles)

β - coolant void fraction (decimal fraction)

z_6 - coolant atom % Li^6 (decimal fraction)

z_{235} - core average uranium enrichment (U^{235} atom % as a decimal fraction)

W - reactor power (W)

m_p - mass of coolant in piping (kg)

m_{lp} - mass of coolant in lower plenum (kg)

m_c - mass of coolant in core (kg)

m_{rp} - mass of coolant in rod plenum (kg)

m_{up} - mass of coolant in upper plenum (kg)

t - operating time (s)

γ_6 - system He production rate due to Li^6

$$= 3.777 \times 10^{16} \times z_6^{0.9216} \quad (\text{atoms/sec}) \quad (5)$$

w_6 - Li^6 zone production weighted coolant mass (kg)

For coolant enrichment similar to natural lithium:

$$= .6603 \times m_p + 1.1731 \times m_{lp} + 2.5392 \times m_c + .4704 \times m_{rp} + .0792 \times m_{up} \quad (6)$$

For coolant enrichment similar to 0.1 Atom % Li^6 :

$$= .7733 \times m_p + 1.7789 \times m_{lp} + 1.7089 \times m_c + .3112 \times m_{rp} + .0535 \times m_{up} \quad (7)$$

γ_7 - system He production rate due to Li^7

$$= 3.473 \times 10^{14} \times (1 - z_6)^{0.9800} \quad (\text{atoms/sec}) \quad (8)$$

w_7 - Li^7 zone production weighted coolant mass (kg)

For coolant enrichment similar to natural lithium:

$$= .1621 \times m_p + .5974 \times m_{lp} + 3.7442 \times m_c + .4611 \times m_{rp} + .0857 \times m_{up} \quad (9)$$

For coolant enrichment similar 0.1 Atom % Li^6 :

$$= .1680 \times m_p + .6156 \times m_{lp} + 3.7177 \times m_c + .4561 \times m_{rp} + .0849 \times m_{up} \quad (10)$$

N_A - Avagadro's number = $.602252 \times 10^{24}$ (atoms/mole)

W_{nom} - base case power level = 2.5×10^6 (W)

M_{nom} - base case total coolant mass = 16.117 (kg)

E_{nom} - base case core average uranium enrichment

$$= 0.855 \text{ (U}^{235} \text{ atom \% as a decimal fraction)}$$

$$f_{\text{con}} = (E_{\text{nom}}) / (W_{\text{nom}} \times M_{\text{nom}} \times N_A) = 3.523 \times 10^{-32} \quad (\text{mole/W-s-kg-atom}) \quad (11)$$

The f_{con} factor is a normalization/conversion factor required since all code production calculations were done in atoms/sec at 2.5 MWt for a total system lithium content of 16.117 kg and an average core enrichment of 85.5 atom %.

The accuracy of the master correlation is limited by the accuracy of the Li^6 subcorrelation to $\pm 3.6\%$. Accuracy is improved at lower Li^6 enrichments. Figure 31 presents a comparison of the observed helium production and production predicted by correlation as a function of Li^6 content. Figure 32 presents similar plot as a function of coolant void fraction.

This correlation produces accurate results over the range of parameters investigated in this study. Extrapolation to other systems should be possible, but only if they have geometries similar to the one used here. An attempt is made to account for minor geometry differences with the w_6 and w_7 zone weighting factors, but the range over which this type of correction will give reasonable results is uncertain. In general, extrapolation to a system with a similar flux shape in space and energy could be expected to produce reasonable results.

To facilitate calculations of He^4 productions for various configurations, the correlation (4) above was coded in a subroutine format in the FORTRAN 77 programming language. A copy of the code (Program CORR) and sample input and output files is presented in Appendix D.

Table 10 - Coolant Edit Zone Weighting Factors
(Natural Lithium)

a	b	c	d	e	f	g
Edit Zone	<u>Coolant Mass</u>		Zone Production		Weighting Factor	
	<u>Kg</u>	<u>% total</u>	<u>as % of total</u>		d/c	e/c
			<u>Li⁶</u>	<u>Li⁷</u>	<u>Li⁶</u>	<u>Li⁷</u>
Piping	2.197	13.63	9.00	2.21	.6603	.1621
Lower Plenum	4.608	28.59	33.54	17.08	1.1731	0.5974
Core	3.106	19.27	48.93	72.15	2.5392	3.7442
Rod Plenum	2.258	14.01	6.59	6.46	0.4704	0.4611
Upper Plenum	3.948	24.50	1.94	2.10	0.0792	0.0857
Total	16.117	100.00	100.00	100.00		

Table 11 - Coolant Edit Zone Weighting Factors

(0.1 Atom % Li⁶)

a	b	c	d	e	f	g
Edit Zone	<u>Coolant Mass</u>		<u>Zone Production</u>		<u>Weighting Factor</u>	
	<u>Kg</u>	<u>% total</u>	<u>as % of total</u>		<u>d/c</u>	<u>e/c</u>
			<u>Li⁶</u>	<u>Li⁷</u>	<u>Li⁶</u>	<u>Li⁷</u>
Piping	2.197	13.63	10.54	2.29	0.7733	0.1680
Lower Plenum	4.608	28.59	50.86	17.60	1.7789	0.6156
Core	3.106	19.27	32.93	71.64	1.7089	3.7177
Rod Plenum	2.258	14.01	4.36	6.39	0.3112	0.4561
Upper Plenum	3.948	24.50	1.31	2.08	0.0535	0.0849
Total	16.117	100.00	100.00	100.00		

Figure 31 - Effects of Li^6 Enrichment on He^4 Production

SP-100 Total System Helium Production
Helium Production Correlation vs TWODANT
Effects of Lithium Enrichment

Figure 32 - Effects of Coolant Void Fraction on He⁴ Production

2.7 Development of Collapsed Cross Section Sets

A cross section set with eighty energy groups can be somewhat unwieldy, and could result in excessive computation times on machines slower than the Cray X-MP/48 used in this study. In order to save computation time in future calculations, a cross section set with fewer energy groups was desired. Accordingly, TRANSX was utilized with the central core zone average flux from the 0% void, 1.0% Li^6 case (TD 173153 032488, solver zone 20, coarse mesh zone 122), to collapse the 80 group cross section set used in this study to 20 group and one group cross section sets. Coarse groups for the 20 group set were formed by collapsing each four adjacent groups in the 80 group set. TWODANT calculations were executed with the new cross section sets to verify their accuracy. Results are presented in Table 12.

Results using the the one group set were inconclusive. TWODANT calculations using self shielded one group cross sections failed to converge. When self shielding was removed, a large increase in k_{eff} of over 5% was calculated. The cause of this behavior is undetermined, but at any rate the one group cross sections appear to be unusable without further work.

The 20 group cross sections reduced run time by almost 90%, and resulted in a change in k_{eff} of only +0.26%. Total He^4 production was decreased by 12%. These results indicate that the twenty group set may be a reasonable compromise between calculational efficiency and accuracy for many jobs. Additional accuracy with fewer than eighty groups could be obtained by producing separate cross section sets for each edit zone, using a group collapse flux specific to each edit zone.

Table 12 - Results Using Collapsed Cross Section Sets
(Base Case Geometry, 2.5Mwt, 1.0% Li^6 , 200 MeV/fission)

	<u>80 Group</u>	<u>20 Group</u>		<u>1 Group*</u>	
		<u>Data</u>	<u>$\Delta\%$</u>	<u>Data</u>	<u>$\Delta\%$</u>
k_{eff}	1.0035	1.0061	+0.26	1.0594	+5.57
Run Time (min)	10.6	1.2	-88.7	0.25	-97.6
He^4 Prod (Atoms/sec)					
Li^6 (n, $2n\alpha$)	1.839×10^{11}	1.949×10^{11}	+6.0	1.500×10^{11}	-18.4
Li^6 (n, nd)	1.896×10^{13}	1.759×10^{13}	-7.2	1.356×10^{13}	-28.5
Li^6 (n, t)	5.352×10^{14}	4.234×10^{14}	-20.9	3.138×10^{14}	-41.4
Li^6 Subtotal	5.543×10^{14}	4.412×10^{14}	-20.4	3.275×10^{14}	-40.9
Li^7 (n, $2n\alpha$)	1.535×10^{11}	1.613×10^{11}	+5.1	1.581×10^{11}	+3.0
Li^7 (n, nt)	3.399×10^{14}	3.417×10^{14}	+0.5	2.620×10^{14}	-22.9
Li^7 (n, γ)	3.119×10^{12}	2.561×10^{12}	-17.9	1.695×10^{12}	-45.7
Li^7 Subtotal	3.432×10^{14}	3.444×10^{14}	+0.3	2.639×10^{14}	-23.1
Grand Total	8.975×10^{14}	7.856×10^{14}	-12.5	5.914×10^{14}	-34.1

* No self shielding

2.8 Effects of Cross Section Self Shielding

Rather broad assumptions made to produce the eighty group cross section set used in this study, including the homogenization of the reactor core for purposes of self shielding as discussed in Appendix C. In order to estimate the effects of the calculated self shielding factors used in the study, it was desired to compare the study results against a non-selfshielded case. To this end, TRANSX was used to produce an identical cross section set to the one used in this study, except that all cross sections were calculated at an infinite dilute background cross section. TWODANT was then used to reexecute Case #5 (0.1 a/o Li^6 , 0% void) with the new unselfshielded cross section set. The results are presented in Table 13. As can be seen in Table 13, using infinite dilute cross sections increased the calculated k_{eff} by 0.11% and decreased total He^4 production by 0.24%. Thus it appears that, for the 80 group cross section set at least, the calculated self shielding effects are rather minor.

Table 13 - Effects of Self Shielding on Calculated He⁴ Production

(Base Case Geometry, 2.5MWt, 200 MeV/fission, 0.1% Li⁶, 0% Void)

Case#	5	9	
Description	Self Shielded	Infinite Dilute	
		<u>Data</u>	<u>Δ%</u>
k _{eff}	1.0048	1.0059	+0.11
He ⁴ Prod			
<u>(atoms/Sec)</u>			
Li ⁶ (n,2nα)	1.838×10 ¹⁰	1.834×10 ¹⁰	-0.22
Li ⁶ (n,nd)	1.894×10 ¹²	1.892×10 ¹⁰	-0.11
Li ⁶ (n,t)	6.743×10 ¹³	6.709×10 ¹³	-0.50
Li ⁶ Subtotal	6.934×10 ¹³	6.900×10 ¹³	-0.49
Li ⁷ (n,2nα)	1.547×10 ¹¹	1.545×10 ¹¹	-0.13
Li ⁷ (n,nt)	3.427×10 ¹⁴	3.420×10 ¹⁴	-0.20
Li ⁷ (n,γ)	4.216×10 ¹²	4.184×10 ¹²	-0.76
Li ⁷ Subtotal	3.471×10 ¹⁴	3.464×10 ¹⁴	-0.20
Total Li	4.164×10 ¹⁴	4.154×10 ¹⁴	-0.24

2.9 Calculation Uncertainties

Many approximations are inherent in multigroup, discrete ordinates calculations such as the ones performed in this study. Results for helium production are essentially the product of the calculated flux and the He^4 production cross sections summed over the model energy group structure and spatial mesh. The uncertainty in the flux is affected by factors such as the multigroup approximation, finite Legendre expansion, S_n order, the finite difference approximation, and uncertainties in the cross sections. Uncertainties in the recoverable energy per fission and in the fission cross sections themselves affect the absolute value of the flux through the power normalization process. Changes in material composition over the system life resulting from fuel burnup and fission product buildup (both assumed to be negligible in this study) also contribute to the uncertainty in the flux. Although an attempt was made to minimize the effects of many of these approximations and assumptions, a quantitative analysis of the total uncertainty in the flux is beyond the scope of this study.

However, as an estimate for a lower limit on the uncertainty in helium production rates, the results will certainly be no more accurate than the accuracy of the helium production cross sections. This data was extracted from ENDF/B-V using the NJOY nuclear data processing system and is presented for the two most important He^4 production cross sections in Figures 33 and 34.

It appears that the $\text{Li}^6(n,t)$ cross section has a standard deviation of less than 5 % over the entire spectrum. The majority of helium production occurs in the .01 to 1 Mev range where the standard deviation varies between 1 and 5 %.

The Li^7 (n,nt) cross section has a standard deviation of less than 6 % over the majority of the groups which contribute to He^4 production. He^4 production in the groups with larger standard deviations accounts for less than 15 % of the total He^4 production from this reaction.

From these observations, it can be estimated that the uncertainty (standard deviation) in total He^4 production computed in this study can be no less than 5 to 10%, depending on the coolant Li^6 enrichment.

Figure 33 - $\text{Li}^6(n,t)$ Cross Section Uncertainty

$\text{Li}^6(n,t)$ Relative Standard Deviation

Figure 34 - $\text{Li}^7(n,\text{nt})$ Cross Section Uncertainty

$\text{Li}^7(n,\text{nt})$ Relative Standard Deviation

2.10 Comparison with GE Results

In order to verify the results of the gas production calculations it was desired to compare the predicted rates of He^4 production to observed values. Unfortunately, since the SP-100 was still in a design and development stage at this writing, no experimental data was available. However, other calculational estimates were available for comparison.

General Electric (GE) has calculated He^4 production rates for the most recent version of the SP-100 system, known as the RFS UD3 design (Brandon, 1988). A comparison of these results with projections made using the program CORR (Appendix D) is presented in Table 14. CORR embodies the results of the present study in a He^4 production correlation as described in section 2.6.2.3 above. Details of CORR calculations are presented in Appendix F. GE calculations were made using region averaged one group fluxes and cross sections for two methods: "ORIGIN2", and "GASP".

To summarize the results of the comparisons, CORR predicts approximately 30% less helium production from Li^6 and about double the production from Li^7 , compared to the GE estimates. Total He production as calculated by CORR is approximately 50% more than the GE figures. Lack of detailed information on the calculation methods used by GE prevents an analysis of the reasons behind this difference. However, a 50% difference is considered to be fairly reasonable agreement in view of the wide disparity in the methods of calculation, and the assumptions inherent in extrapolating the results of the present study to a different system using CORR.

Table 14 - GE He⁴ Production Estimates vs CORR Estimates

Lifetime He⁴ Production (grams)
(2.5 MWt, 7.3 Year Full Power Life)

Comparison with "ORIGEN2" results:

	GE	Present Study	
<u>Isotope</u>	<u>ORIGEN2</u>	<u>CORR</u>	<u>% Difference</u>
Li ⁶	0.2020	0.1321	-35%
Li ⁷	0.3796	0.7431	+96%
Total Li	0.5817	0.8752	+50%

Comparison with "GASP" results:

	GE	Present Study	
<u>Isotope</u>	<u>GASP</u>	<u>CORR</u>	<u>% Difference</u>
Li ⁶	0.1924	0.1238	-36%
Li ⁷	0.3655	0.7416	+103%
Total Li	0.5580	0.8654	+55%

2.11 Summary

Three major helium production reactions have been identified in SP-100 class reactors. These are $\text{Li}^6(n,t)$, $\text{Li}^7(n,nt)$ and $\text{Li}^6(n,nd)$. In reactors using natural lithium these reactions account for 87%, 9%, and 4%, respectively, of the total helium produced. For reactors using low enrichment (0.1 Atom %) Li^6 coolant, $\text{Li}^7(n,n't)$ and $\text{Li}^6(n,t)$ reactions account for 83% and 16% respectively of the total helium produced.

Spatially, more helium is produced in the reactor core than in any other region. However, a significant amount of helium is produced outside the core proper, mainly in the lower plenum region, which was found to have a somewhat higher flux than the other non-core regions. For natural lithium coolant, 49% of the helium produced by Li^6 is produced in the core, with 34% being produced in the lower plenum, while 72% of the total helium produced by Li^7 is produced in the core, with 17% produced in the lower plenum. It appears that by reflecting neutrons which would otherwise escape, the Be shielding at the reactor base enhances the production of helium in the lower plenum.

Spectral analysis shows that helium production reactions are confined for the most part to energies above 1 keV. Epithermal neutrons generate a larger portion of the total helium produced in the lower plenum than in other regions, but even in the lower plenum, neutrons below 1 keV produce less than 26% of the helium produced there. Thermal neutrons were not found to be of any importance to helium production. Helium production from Li^6 centers around the 0.25 MeV $\text{Li}^6(n,t)$ resonance, while production from Li^7 is confined to the band of energies between 3 and 10 MeV.

In terms of designing a reactor to minimize helium production, two measures are indicated: 1) minimize coolant Li^6 content, and 2) minimize coolant presence in high He^4 production zones. The most important region in which to minimize coolant presence is the core, followed by the lower plenum. Next, depending on the coolant Li^6 enrichment, coolant content of the piping or rod plenum should be minimized, followed by the upper plenum. As an example, removal of one kg of Li^7 from the upper plenum, the least important region for helium production, has the same effect on helium production as removal of only 20 g of Li^7 from the reactor core, the most important region.

Even under optimum design conditions, a certain amount of gas generation appears to be inevitable. Estimates of total lifetime gas productions for a typical SP-100 system geometry are presented in Table 15 for various coolant Li^6 enrichments.

As can be seen in the table, a helium production of somewhere between 0.13 and 1.32 moles can be expected over a 7 year, 2.5 MW reactor life. At projected SP-100 end of life operating conditions of 152 kPa and 1350 K, the helium produced will occupy a volume between 10 and 100 liters. Gas volumes in this range could be a significant fraction of the total primary heat transport system volume, indicating that coolant radiologic gas generation is an effect which requires careful consideration in the design of a SP-100 class space power system.

Table 15 - SP-100 System Lifetime He⁴ Production

Coolant Li ⁶	He ⁴ Production	He ⁴ Volume @ EOL
<u>Atom %</u>	<u>(moles)</u>	<u>(liters)</u>
0.01	0.130	10
0.10	0.153	11
1.00	0.329	24
7.40	1.327	96

Operating parameters:

Geometry - Study Base Case

End of Life (EOL) Primary System Pressure - 152 kPa

EOL Primary System Temperature - 1350 K

Reactor Power - 2.5 Mwt

Full Power Lifetime - 7 years

Coolant Void Fraction - 0 %

3. GAS BEHAVIOR IN THE PRIMARY HEAT TRANSPORT SYSTEM

Having determined the amount of gas which will be radiologically generated in the coolant of a SP-100 class reactor, attention is now turned to the second part of this study: the behavior of the gas in the reactor primary heat transport system.

3.1 Objective

The objective of the gas behavior section of this study is the determination of the behavior of radiologically generated helium gas in the primary coolant loop of SP-100 class reactors during normal system operations.

3.2 Method of Analysis

The method of analysis consisted of a review of previous relevant work and analytic/numerical computations. No experimental work was performed. A summary of the program of investigation is presented below.

1) A literature search was performed to provide background material for subsequent analysis. Topics investigated included:

- * Gas bubble homogeneous and heterogeneous nucleation in liquid metals;
- * Dynamics of the growth, coalescence, and breakup of gas bubbles in a turbulent flow field;
- * Lithium/helium material properties as functions of temperature
- * SP-100 Primary Heat Transport System (PHTS) design parameters, and;

* Two phase flow patterns in a microgravity environment.

2) The RFS UD3 SP-100 primary heat transport loop was selected as a representative heat transport system. Based on this design, a discrete component, computational model of the loop was constructed. This model was used to predict loop hydraulic parameters throughout the study.

3) Using the computational model developed in step 2) above, a temperature and pressure profile was determined for the entire primary loop. This information was combined with He solubility data to determine a solubility profile for the loop, identifying points of minimum solubility.

4) Data on bubble nucleation was combined with solubility profiles from step 3) above to determine the most likely locations and mechanisms of bubble nucleation in the primary loop.

5) A preliminary investigation was conducted into the following mechanisms affecting bubble, growth, collapse and distribution:

- * Mechanical pressure/temperature effects;
- * Mass diffusion effects;
- * Coalescence effects;
- * Bubble breakup due to turbulence and shear forces;
- * Electro-magnetic field effects,
- * Micro-gravity effects, and;
- * Inertially induced buoyancy effects.

6) The information obtained in steps 1) through 5) above was combined in an attempt to produce a consistent bubble nucleation/growth/behavior model for the entire primary loop. Based on this model, the overall behavior of gas in the primary loop was summarized.

Results of the analysis are presented in the following sections.

3.3 Primary Heat Transport System Model

The SP-100 Reference Flight System, Update 3 (RFS UD3), as presented at the 19 July 88 SP-100 Project Integration Meeting (PIM)(General Electric, 1988a) was selected as a reference design for primary heat transport system analysis. In addition to materials presented at the PIM, certain data was extracted from the draft SP-100 Reference Flight System Design Description (General Electric, 1988b). A short description of the PHTS is presented below.

3.3.1 SP-100 RFS UD3 System Description

The overall layout of the PHTS is presented in Figure 35. Although strictly speaking there is no up or down in space, for reference purposes up is considered herein to be the direction from the radiation shield towards the reactor. As mentioned before, the primary coolant is lithium, and the pressure vessel and piping are constructed of PWC-11. Two normal operating modes are envisioned: full power, and standby. In the full power mode reactor thermal power is 2.4 Mw at a mass flow rate of 11 kg/s. Reactor exit temperature is approximately 1300 K. Maximum loop pressure is ≈ 62 kPa. In the standby mode, reactor thermal power is 0.28 Mw at a mass flow rate of 2.4 kg/s. Reactor exit temperature is 680 K and maximum pressure in the loop is ≈ 11 kPa. Approximately 10% of the reactor thermal power is used to

power primary loop pumps, and the remaining 90% is dissipated in thermoelectric converters which provide the electrical output of the system.

Beginning at the exit from the lower reactor plenum, the coolant flows through six 4.25 cm diameter circular pipes to six identical gas separator/accumulator devices. A schematic of the gas separator/accumulator is presented in Figure 36. The purpose of these devices is to remove any entrained gases in the coolant flow and to accommodate thermal expansion of the coolant. The separator (the terms separator, accumulator, and separator/accumulator are used interchangeably) functions by first inducing a swirling flow in the coolant as it enters the center section of the separator. Gas bubbles larger than a certain minimum size are then removed from the coolant by a wire mesh screen which traps the gas within the separator and allows the coolant to pass through. Centrifugal and surface tension forces prevent the gas from leaving the center section of the separator where it accumulates over the life of the system. An initial charge of helium gas provides beginning of life system pressurization. As coolant operating temperature changes, coolant expansion/contraction is accommodated by the initial He gas charge. The accumulators are sized to prevent interference of this accumulator function with the gas separator function.

Upon leaving the gas separators, the coolant at each separator is split into two 4.0 cm diameter circular pipes and through these pipes flows to 12 identical thermoelectric electromagnetic pumps. A TE-EM pump detail is presented in Figure 37. Power for these pumps is derived from a thermoelectric assembly which uses the temperature difference between the primary and secondary coolant loops as a heat

source. These pumps provide the circulating force in both the primary and secondary coolant loops.

Coolant leaving the TE-EM pumps passes through 12 4.0 cm diameter circular pipes to 12 identical thermoelectric Power Conversion Assemblies (PCA's). A detail of a PCA is presented in Figure 38. At each PCA, the flow to that PCA is first split into six 2.0 cm diameter inlet headers. Each of these headers feeds six 0.1 x 2.7 cm rectangular flow passages. In these rectangular flow passages (432 in the whole system) heat is extracted from the coolant to provide the electric power output of the system. Coolant is subsequently collected in outlet headers configured similarly to the inlet header system.

Coolant leaves the PCA's in 12 4.0 cm diameter circular pipes. These twelve pipes are subsequently combined into six 4.2 cm diameter circular pipes which are connected to the reactor inlet. A schematic of the reactor assembly is presented in Figure 39. Coolant flows up around the inside edge of the reactor vessel in a annular passage with an average width of approximately 1.5 cm. Upon reaching the top of the annular passage the coolant flows into a large upper plenum region (≈ 28 cm diameter) and from there flows downward through the reactor core itself. The core consists of 1,020 circular wire wrapped fuel rods on a triangular pitch. The fuel rods are divided into 30 separate assemblies by a hexagonal honeycomb baffle. Details of the core layout are presented in Figures 40 and 41. Upon leaving the core, the coolant is collected in a large lower plenum (≈ 32 cm in diameter) from which the coolant flows in six 4.25 cm ID pipes to the six gas separators/accumulators and repeats its circuit.

Figure 35 - SP-100 PHTS Overall Layout
(General Electric, 1988a)

Figure 36 - Gas Separator/Accumulator Detail
(General Electric, 1988a)

Figure 37 - TE-EM Pump Detail

(General Electric, 1988a)

Figure 38 - Power Conversion Assembly
(General Electric, 1988a)

Figure 39 - Reactor Assembly Detail

(General Electric, 1988a)

Figure 40 - Core Layout Detail
(General Electric 1988a)

Figure 41 - Fuel Pin Dimensions

Pitch: 8.13 mm
Pin Diameter: 7.75 mm
Wire Wrap Diameter: 0.38 mm
Lead: 137.2 mm

3.3.2 Computational Model Description

In order to provide various loop hydraulic parameters required for subsequent analysis, a computational model of the SP-100 PHTS was developed. The model predicts loop pressure drops, coolant physical properties, Reynolds Numbers, flow velocity, and other parameters on a node to node basis within the loop. A description of the model is presented below, and a commented program listing is provided in Appendix G. Material properties were obtained from experimentally confirmed correlations with the exception of the diffusion coefficient of helium in lithium, which was estimated from the Stokes-Einstein relationship. Material property subroutines are also provided in Appendix G.

The PHTS computational model was constructed by selecting a set of points or nodes in the flow loop and individually modeling the flow from node to node within the loop. Nodes were selected such that each pair defines a loop component assumed to have constant hydraulic parameters. A node and component listing is presented in Table 16.

Based on preliminary design information, transfer of 90% of the thermal energy produced in the reactor core was assumed to occur in the PCA's, and the remaining 10% was assumed to occur in the TE-EM pump. Thermal losses from the reactor vessel and loop piping were assumed to be negligible. The initial accumulator gas charge was taken to be that which produced a maximum loop pressure of approximately 62 kPa during beginning of life full power operation. The PHTS internal volume was assumed to be independent of temperature, with the result that changes in the accumulator gas volume were a function of changes in coolant volume only. This information, together with the system geometry data presented in Table 17, was used to define a standard PHTS

configuration. Given the above data, the user is required to provide only four parameters to model normal operations at any point in the system 7 year full power lifetime: reactor thermal power, reactor exit temperature, system total mass flow rate, and additional moles of gas in the accumulators above the startup amount.

A simple model of each component was constructed using one of four types of flow channel geometries: circular, rectangular, annular, or a triangular pitch fuel rod equivalent flow channel. Pressure losses are calculated as the sum of three terms: friction pressure drop, a pressure bends loss term, and an entrance/exit pressure loss term. Friction losses are calculated based on the standard Moody friction factor chart, using the equivalent hydraulic diameter concept for non-circular flow passages (White, 1986):

$$\Delta p = f y \rho V^2 / (2D_h) \quad (12)$$

where:

Δp = friction pressure loss (Pa)

f = Darcy friction factor (dimensionless)

$$= 64/Re \quad \text{for } Re < 2000 \quad (13)$$

$$= (1/[-1.8 \log(6.9/Re + (\epsilon_s/(3.7D_{eq}))^{1.11})]) \text{ for } Re > 2000 \quad (14)$$

= (Haaland equation)

y = length of flow channel (m)

ρ = fluid density (kg/m^3)

V = fluid velocity (m/s)

D_h = flow channel hydraulic diameter (m^2)

$$= 4A_c/P_w \quad (15)$$

D_{eq} = flow channel equivalent hydraulic diameter

- D_h for circular flow channels
- $2D_h/3$ for non-circular flow channels

Re - Reynolds number (dimensionless)

$$= \rho D_{eq} V / \mu$$

μ - fluid dynamic viscosity (kg/m-s)

A_c - flow channel cross sectional area (m^2)

P_w - flow channel wetted perimeter (m)

ϵ_s - flow channel surface roughness (m)

All flow surfaces were assumed to have a roughness of $1.5 \mu m$ (smooth). Friction losses in the core were based on the CRT model for triangular pitched, wire wrapped fuel elements (Walter and Reynolds, 1981).

In order to avoid unduly complicating the computational model, several simplifying assumptions were made regarding bends and entrance/exit losses. Bends losses are highly dependent on several factors such as radius of bend and the degree of bend (90, 45, 30 degrees, etc) (White, 1986). For simplicity, these factors were neglected and all bends were assumed to have a loss factor (K_L) equal to 15%. Entrance losses are highly dependent on entrance geometry, while exit losses are not (White, 1986). Both entrance and exit losses are strong functions of the rate of expansion or contraction of the flow passage. Again for simplicity's sake, all entrance and exit losses were assumed to result from a sudden expansion or contraction with no rounding of inlets or exits. Both entrance and exit losses are assumed to occur immediately after a node. For a flow from a channel of diameter D_1 to a channel of diameter D_2 , K_L was computed as follows (White, 1986):

$$\begin{aligned}
K_L &= (1 - (D_1/D_2)^2)^2 && \text{for } D_1 < D_2 \\
&= 0.42(1 - D_2/D_1)^2 && \text{for } D_1 > D_2
\end{aligned}
\tag{16}$$

Entrance/exit losses were then calculated from the following formula:

$$\Delta p = K_L \rho V^2 / 2 \tag{17}$$

where:

Δp = entrance/exit pressure loss (Pa)

V = fluid velocity in flow channel with smallest diameter (m/s)

The velocity pressure change is calculated via the following formula:

$$\Delta p = \rho(V_1^2 - V_2^2)/2 \tag{18}$$

where:

Δp = velocity pressure change (Pa)

V_1 = initial fluid velocity (m/s)

V_2 = final fluid velocity (m/s)

Total pressure change is calculated by adding the velocity pressure change to the friction, bends, and entrance/exit losses.

The simple model described above was considered sufficient to model the major flow characteristics of the PHTS for the purposes of this study. However it should be noted that many details of loop construction considered to have a minor impact on overall loop performance (e.g. accumulator wire mesh filter, core inlet fuel rod support plates, auxiliary cooling loop hardware in the lower plenum, etc.) are not addressed by this model. As a result, the model results should be used with care if applied to other studies.

Table 16 - PHTS Computational Model Nodes and Components

<u>Node #</u>	<u>Node Description</u>	<u>Component Modeled</u>
1	Accumulator Exit	Piping #1
2	TE-EM Pump Suction	TE-EM Pump
3	TE-EM Pump Discharge	Piping #2
4	PCA Inlet	PCA Inlet Header
5	PCA Heat Exchanger Inlet	PCA Heat Exchanger
6	PCA Header Inlet	PCA Exit Header
7	PCA Exit	Piping #3
8	Pipe Junction	Large Piping #1
9	Reactor Vessel Inlet	Reactor Annulus
10	Upper Plenum Inlet	Upper Plenum
11	Core Inlet	Reactor Core
12	Lower Plenum Inlet	Lower Plenum
13	Reactor Vessel Exit	Large Piping #2
14	Accumulator Inlet	Accumulator
1	Accumulator Exit	

Table 17 - System Geometry Data for PHTS Computational Model

Node #	Node ID	Data for Flow to Next Node				
		Channel Geometry	Channel Length (m)	% of Total Mass Flow	# of Bends	Channel Dimensions (m)
1	Accumulator Exit	Circular	0.50	1/12	1	ID 0.04
2	TE-EM Pump Suction	Rectangular	0.75	1/12	1	0.0178 x 0.0268
3	TE-EM Pump Discharge	Circular	1.00	1/12	2	ID 0.04
4	PCA Inlet	Circular	0.10	1/72	1	ID 0.02
5	PCA Heat Ex Inlet	Rectangular	0.28	1/432	2	0.0270 x 0.001
6	PCA Header Inlet	Circular	0.10	1/72	1	ID 0.02
7	PCA Exit	Circular	1.50	1/12	3	ID 0.04
8	Pipe Junction	Circular	1.50	1/12	3	ID 0.0425
9	Reactor Ves Inlet	Annular	0.80	1.0	2	D ₁ =0.32, D ₂ =0.35
10	Upper Plenum Inlet	Circular	0.30	1.0	1	ID 0.28
11	Core Inlet	Triangular Pitch Eq Fuel Chan	0.56	1/2700	0	Pitch = 0.00813 Rod Dia= 0.00775 Wire Dia=0.00038 Wrap Lead=0.1372
12	Lower Plenum Inlet	Circular	0.10	1.0	1	ID 0.32
13	Reactor Ves Exit	Circular	2.00	1/6	3	ID 0.0425
14	Accumulator Inlet	Annular	0.60	1/6	0	D ₁ = **, D ₂ =0.22

Notes:

1. Total Coolant Mass in PHTS: 110 kg
2. Total Separator Gas Volume: 35 liters @ 1300 K, 35 kPa, BOL.
3. Accumulator inner diameter is variable, based on coolant volume at operating temperature.

3.3.3 PHTS Computational Model Results

The PHTS computational model was run for four points in the system history which were considered to be of particular interest as limiting cases. These are full power operation at beginning of life (BOL) and end of life (EOL), and standby power operation at BOL and EOL. Standard input values assumed for these operating modes are presented in Table 18. These input values were obtained from current system descriptions (General Electric 1988b). The additional He in the system at EOL was estimated at 0.25 moles based on calculations described in the previous section, assuming a coolant Li^6 enrichment of ≈ 0.1 a/o. Several of the more general results are presented below, while other results are discussed in following sections.

A plot of primary system pressure as a function of loop location is presented in Figures 42 and 43. Total pressure losses in the loop are approximately 34 kPa at full power and 2.6 kPa at standby power. Maximum loop pressure falls from 62 kPa BOL, full power to 11 kPa BOL, standby power, as a result of contraction of the lithium coolant at the lower (680 K vs 1300 K) standby operating temperatures. The presence of 0.25 additional moles of helium in the accumulators at EOL increases system maximum pressures to 152 kPa at full power and 32 kPa at standby power. Figure 44 depicts the relative magnitude of friction, entrance/exit, and bends pressure losses in the system. The majority of the pressure loss in the system occurs in the PCA's and the core as a result of friction losses in these components.

A plot of system temperature as a function of loop location is presented in Figures 45 and 46. As mentioned before, 90% of loop temperature losses were assumed to occur in the PCA's and 10% were

assumed to occur in the TE-EM pumps. The temperature rise across the core was approximately 52 K at full power and 28 K at standby power.

A plot of system Reynolds number (Re) is presented in Figures 47 and 48. This plot is of interest because the pressure loss correlations used in the computational model are valid only over certain Reynolds number ranges. Transition from laminar to turbulent flow regimes generally occurs at $Re \approx 2000$ to 4000, for isothermal, internal flow in circular channels. A similar transition range is observed for the non-circular geometries (approaching a flat plate channel) used in the PHTS hydraulic model. Model friction loss calculations are unreliable in this range. Also, model bends and expansion/contraction loss calculations are valid only in the turbulent flow regime ($Re > 4000$). As can be seen in Figure 47, the PHTS flow regime is clearly turbulent for all components at full power. The lowest Reynolds numbers observed are 7,000 to 10,000, occurring in the core and the PCA's. All model pressure losses should therefore be reliable at full power. At standby power, however, laminar flow is observed in the core and PCA's. Consequently, bends losses and expansion/contraction losses should be considered unreliable in these components, but fortunately these losses make up only a small part of the total system losses, and this uncertainty should not significantly affect the overall calculation. No component is observed to operate in the transition regime at standby power, so friction losses should be reliable in all components.

A plot of the ratio of the entrance length for fully developed flow to the actual component length is presented in Figure 49. Hydraulic entrance length for a channel of diameter D was calculated from the following formula:

$$\begin{aligned}
L_e &= D \, 0.06 \, Re & Re < 2300 \\
&= D \, 4.4 \, (Re)^{0.167} & Re > 2300
\end{aligned}
\tag{19}$$

A ratio close to or greater than one indicates that the flow may not be fully developed in that component. As can be seen in the Figure, the majority of the loop components fall into this category. As a result, the actual pressure losses in most of the loop components can be expected to be slightly higher (on the order of a few percent) than the values calculated herein.

In summary, the PHTS hydraulic model appears to be reasonably accurate for the purposes of this study. Other results obtained from the model are discussed in the following sections.

Table 18 - PHTS System Parameters for Normal Operating Modes

<u>Operating Mode Description</u>	<u>Total Mass Flow Rate (kg/s)</u>	<u>Reactor Exit Temperature (K)</u>	<u>Reactor Power (MW)</u>	<u>Excess Helium (moles)</u>
<u>Full Power</u>				
Beginning of Life	11.0	1300.0	2.40	0.00
End of Life	10.5	1350.0	2.40	0.25
<u>Standby Power</u>				
Beginning of Life	2.4	680.0	0.28	0.00
End of Life	2.4	680.0	0.28	0.25

Figure 42 - PHTS Full Power Pressure Profile

Figure 43 - PHTS Standby Power Pressure Profile

SP-100 PHTS PRESSURE PROFILE STANDBY POWER

Figure 44 - PHTS Pressure Loss Components

SP-100 PHTS PRESSURE LOSS COMPONENTS BEGINNING OF LIFE

Figure 45 - PHTS Full Power Temperature Profile

Figure 46 - PHTS Standby Power Temperature Profile

TEMPERATURE IN SP-100 PRIMARY COOLANT LOOP BEGINNING OF LIFE, STANDBY POWER

Figure 47 - PHTS Reynolds Number Profile, Full Power

REYNOLDS NUMBER IN SP-100 PRIMARY COOLANT LOOP BEGINNING OF LIFE, FULL POWER

Figure 48 - PHTS Reynolds Number Profile, Standby Power

REYNOLDS NUMBER IN SP-100 PRIMARY COOLANT LOOP BEGINNING OF LIFE - STANDBY POWER

Figure 49 - PHTS Component Entrance Length to Actual Length Ratio

HYDRAULIC ENTRANCE LENGTH TO ACTUAL LENGTH RATIO SP-100 PHTS, BEGINNING OF LIFE

3.4 Bubble Formation

Once system operation begins, radiologically generated helium will begin to accumulate in the lithium coolant of the PHTS. The rate of generation is calculated to be slow, and the turbulent nature of the loop flow is expected to keep the coolant well mixed. The distribution of helium in the coolant is therefore expected to be uniform. Helium will initially remain in solution in the coolant, but as generation continues, the coolant will eventually become saturated and the dissolved He can be expected to come out of solution to form a gas phase. The time required to saturate the coolant can be estimated based on data from the PHTS hydraulic model and the production calculations of the previous section. Assuming a lithium inventory of $110 \text{ kg} = 0.255 \text{ m}^3$, a seven year system lifetime He production of 0.25 moles = 0.001 kg, and a maximum BOL He solubility of $1.65 \times 10^{-6} \text{ kg/m}^3$ (1300 K, 62 kPa) the time required to saturate can be estimated as:

$$t = (0.255)(1.65 \times 10^{-6}) / (0.001/7) = 0.0029 \text{ years} \approx 1.1 \text{ days} \quad (20)$$

Shortly after this time, the He will be removed from solution via a nucleation process forming new gas bubbles, or it may diffuse directly out of solution into pre-existing bubbles. An investigation of the helium nucleation and diffusion processes is therefore of interest in order to determine the mechanism and location of the appearance of the gas phase within the loop.

Four gas formation processes are considered in this subsection: homogeneous nucleation, heterogeneous nucleation on a smooth surface, heterogeneous nucleation with the aid of trapped gases, and mass diffusion at the system accumulator bubbles. The goal of this analysis is the determination of the relative He supersaturation required to

drive the various types of gas formation processes in the SP-100 PHTS. This information will be combined with helium solubility data to determine the most probable gas formation process at various points in the loop.

3.4.1 Homogeneous Nucleation

Many treatments of the nucleation process are available in the literature. The discussion presented below follows an analysis of boiling nucleation by Cole (1974) using a classical kinetic approach. By approximation, nucleation in superheated (boiling) and supersaturated liquids can be treated similarly (van Stralen and Cole, 1979).

Homogeneous nucleation refers to the appearance of a gas nucleus in the bulk liquid, away from container surfaces or suspended particles. Gas molecules are assumed to be uniformly mixed with the bulk liquid and to have a distribution of energies such that only a few molecules have more than the average energy. These molecules are assumed to agglomerate via a step-wise reversible collision process to produce a cluster of gas molecules:

where : A_x = cluster of x molecules,

A_1 = single gas molecule,

A_{x+1} = cluster of x+1 molecules.

A thermodynamic analysis of the above relationship produces an equation for the energy of the cluster/liquid system relative to the energy of the liquid solution alone, as a function of the gas cluster radius:

$$\Delta A = 4\pi r^2 \sigma (1 - 2r/3r_{cr}) \quad (22)$$

where:

$$\Delta A = A - A_0 \quad (J),$$

A = energy of gas cluster/liquid solution system (J),

A₀ = energy of the liquid solution with gas cluster radius

equal to 0 (J).

σ = liquid/gas interfacial surface tension (N/m),

r = gas cluster radius (m),

r_{cr} = critical gas cluster radius (m).

The above equation is depicted graphically in Figure 50. As can be seen there, at radius r_{cr} the cluster is in a meta-stable state.

The system attempts to minimize its total energy with the result that the addition of one more molecule over r_{cr} results in rapid growth, while the loss of one molecule results in collapse. This r_{cr} is called the critical gas nucleus radius.

The Laplace equation relating the pressure differential across a curved surface, and Henry's Law relating gas pressure to concentration can be combined to obtain a relationship for the supersaturation necessary to produce a gas nucleus of radius r_{cr} (van Stralen, 1979):

$$P_B - P_L = 2\sigma/r_{cr} \quad (\text{Laplace Equation}) \quad (23)$$

$$P_B - P_L = K(C_0 - C_s) \quad (\text{Henry's Law}) \quad (24)$$

$$K(C_0 - C_s) = 2\sigma/r_{cr} \quad (25)$$

$$(C_0 - C_s)/C_s = 2\sigma/(r_{cr} K C_s) \quad (26)$$

$$(C_0 - C_s)/C_s = 2\sigma/r_{cr} P_L \quad (27)$$

$$\Delta C_0/C_s = 2\sigma/r_{cr}P_L \quad (28)$$

where:

P_L - liquid pressure (Pa)

P_B - pressure inside gas nucleus (Pa)

K - Henry's Law Constant (note that $P_L = KC_s$) (m^2/s^2)

C_0 - actual or analytic gas concentration in
liquid (kg/m^3)

C_s - saturation gas concentration in the liquid (kg/m^3)

ΔC_0 - supersaturation = $C_0 - C_s$ (kg/m^3)

$\Delta C_0/C_s$ - relative supersaturation (dimensionless)

Note that the effects of r on surface tension and vapor pressure have been assumed to be negligible in the above analysis. Moreover, it is assumed that the Laplace equation is applicable at radius r_{cr} , that the gas nucleus is spherical in shape, and that the gas is incompressible.

Given the above relationship, it would be possible to calculate the supersaturation required for nucleation if r_{cr} was known. An expression for r_{cr} can be obtained from the theory of absolute reaction rates (Cole, 1974):

$$r_{cr} = \{ (3kT/4\pi\sigma) [\ln(n_t kT/hJ) - E_d/kT] \}^{0.5} \quad (29)$$

where:

k - Planck's Constant = 1.38×10^{-23} (J/K)

T - temperature (K)

n_t = number density of gas molecules (atoms/m³)

h = Boltzmann's Constant = 6.26×10^{-34} (J/s)

J = nucleation rate (nucleations/s-m³)

E_d = activation energy for diffusion of a molecule of gas
through the liquid (J)

Evaluation of this expression depends on knowledge of both J and E_d . E_d can be reasonably taken to lie somewhere between 0.0 and E_x , the energy of formation a gas cluster of radius r_{cr} ($0 < E_d < 4\pi r_{cr}^2 \sigma / 3$). The rate of nucleation (J) can be assumed to lie somewhere between 10^1 and 10^{12} nuc/s-m³ for what is normally thought of as a gas ebullition process. Over these ranges, a plot of r_{cr} as a function of J and E_d can be constructed (see Figure 51). As can be seen in the Figure, r_{cr} is approximately 1 nm, and is not a strong function of either J or E_d . Accurate knowledge of J and E_d is therefore not required to obtain an approximate estimate for r_{cr} (Cole, 1974).

Taking $r_{cr} \approx 10^{-9}$ m, the relative supersaturation for homogeneous nucleation can be calculated:

$$SS_{homo} = 2\sigma / r_{cr} P_L = (2.0 \times 0.265) / (10^{-9} \times 152000.0) = 3,486 \quad (30)$$

This supersaturation is only an estimate based on estimates of r_{cr} .

However, based on the results presented in Figure 51, this supersaturation can be expected to be accurate to within $\pm 30\%$, which will be shown below to be sufficient for the purposes of this study.

Now the total helium production over the lifetime of the system is ≈ 0.25 moles = 0.001 kg. The solubility of He in lithium at 1300 K and 152 kPa is 4.0×10^{-6} kg/m³. The total amount of coolant in the PHTS is approximately 110.0 kg = 0.255 m³. Assuming that all the He gas generated remains in solution, the relative supersaturation at EOL can be calculated as:

$$SS_{EOL} = 0.001 / (0.255 \times 4.0 \times 10^{-6}) = 980 \quad (31)$$

This is only 1/3 the required supersaturation for homogeneous nucleation. Thus it can be concluded that homogeneous nucleation will not be an important means of nucleation in the SP-100 system. With this conclusion, we turn our attention to the consideration of another bubble formation process: heterogeneous nucleation.

3.4.2 Heterogeneous Nucleation on Smooth, Gas Free Surfaces

Heterogeneous nucleation is defined as nucleation on a surface in the liquid, such as a container wall or foreign particle suspended in the liquid. A thermodynamic analysis of nucleation in the presence of such a smooth, gas free surface can be made using arguments similar to those utilized in the analysis of the homogeneous case. Although the details of such an analysis will not be reproduced here, the following result is obtained (Cole, 1974):

$$SS_{hetero} = (C_0 - C_s) / C_s = [SS_{homo}] [F(\theta, Geo)] \quad (32)$$

where F is a complex function of contact angle (θ), and surface geometry (Geo). A plot of F for spherical surfaces is presented in Figure 52. Similar plots can be obtained for other surface geometries. As can be seen in the Figure, F varies between 0 and 1, depending on the contact angle. Liquid metals are known to have very low or 0

contact angles when in contact with pure metal surfaces (Addison, 1984). Thus it would appear that F should be close to 1 for liquid metals, with the result that the supersaturation required for heterogeneous nucleation on a smooth, gas free metal surface is not very different from that required for homogeneous nucleation.

Figure 50 - Vapor Nucleus Energy

Vapor Nucleus Energy vs Radius

Figure 51 - Vapor Nucleus Critical Radius

Figure 52 - Heterogeneous Nucleation F Factor

HETEROGENEOUS NUCLEATION GEOMETRIC FACTOR FOR SPHERICAL SURFACES

3.4.3 Nucleation in Cavities Containing Trapped Gas

Even the smoothest of surfaces found in a physical heat transfer system contains a large number of microscopic cavities. The geometry of many of these cavities is such that they are difficult for the surrounding fluid to penetrate and wet. In addition, gas is sometimes trapped in them at some point in the system life. An unwetted cavity containing trapped gas can have a dramatic effect on the nucleation process.

In the presence of trapped gas in a surface cavity of radius r_c , the Laplace equation can be rewritten as:

$$P_B - P_L = 2\sigma/r_c - P_G \quad (33)$$

where:

P_G - pressure due to trapped gas

Letting $P_B - P_L = K(C_0 - C_s)$ as before:

$$K(C_0 - C_s) = (2\sigma/r_c)(1 - \xi) \quad (34)$$

where:

$$\xi = P_G r_c / 2\sigma$$

Zeta (ξ) is a dimensionless parameter that can vary between 0 (no trapped gas), and 1. Clearly, as ξ approaches 1 a arbitrarily small supersaturation is required for nucleation. This prediction is born out by a large body of experimental evidence which shows that the preferred location of boiling nucleation is almost always at microscopic surface cavities which can be inferred to contain small amounts of trapped gas. However, the excellent wetting properties of liquid metals mentioned in the previous section tend to quench, or drive out the gas from a large number of surface cavities which would ordinarily serve as nucleation sites in a more everyday liquid, such as

water. As a result, liquid metal systems are typically observed to have relatively high incipient boiling superheats (20-200 K) compared to water, with only the steepest walled (most difficult to wet) surface cavities serving as nucleation sites (Dwyer, 1976).

Assuming as before, that boiling nucleation and nucleation from dissolved gases can be treated in a similar fashion, an analysis of incipient boiling superheats in liquid metals can be applied to determine the inert gas supersaturation required for nucleation on an physical engineering surface.

Incipient boiling superheat in liquid metals is a function of many variables, including surface geometry, surface temperature/pressure history, partial pressure of inert gas in surface cavities, and others. Dwyer (1969) has proposed a correlation for predicting incipient boiling superheats in liquid metals, based on the "equivalent cavity model". In this model (See Figure 53) a force balance is made using the Laplace equation for a unwetted, oxide covered surface inside a small idealized cavity. This balance is used to produce an estimate for the radius of curvature of the liquid surface inside the cavity at preconditioning temperatures and pressures. Another force balance is used to determine the pressure required to form a vapor nucleus at incipient boiling, now assuming that the liquid interface has moved up the walls of the cavity to a well wetted, un-oxidized surface. Dwyer's correlation can be expressed as follows:

$$P_v + \psi T / (r')^3 - P_L = 2\sigma / ar' \quad (35)$$

where:

P_v = liquid vapor pressure at temperature T (Pa)

ψ = a measure of inert gas concentration in the

nucleation cavity, a function of cavity geometry,
 contact angle, and moles of gas in the cavity (m-N/K)

T = liquid temperature at incipient boiling (K)

r' = radius of liquid meniscus in the equivalent cavity at
 surface pre-conditioning temperature and
 pressure (m).

P_L = liquid pressure at operating conditions (Pa)

α = r/r' (dimensionless)

r = vapor bubble radius at incipient boiling (m)

The value of r' is obtained from the following relationship:

$$(r')^3 - \{2.0\sigma'/(P'_L - P'_V)\}(r')^2 - \psi'T'/(P'_L - P'_V) = 0 \quad (36)$$

The primed symbols refer to values taken at the surface pre-conditioning pressure and temperature. The ratio ψ/ψ' can be assumed to be 1.0 for the present purposes.

Now ψ and α are not measurable and must be evaluated from experimental data. Excellent agreement with the experimental data of Chen (1968) (potassium flowing inside a 0.5" diameter smooth tube, Reynolds # $\approx$ 5,000) was obtained for the following values of ψ and α :

$$\psi = 6.1 \times 10^{-16} \text{ m-N/K} \quad (37)$$

$$\alpha = 0.72 \quad (38)$$

Dwyer's correlation is interesting in several respects: it has experimental confirmation for nucleation on physical heat transfer surfaces; it does not require knowledge of the size of the surface cavities; it allows for trapped inert gas; and it accounts for the effects of the nucleation surface pressure/temperature history. Dwyer's correlation may be extended to account for nucleation as a result of dissolved gas supersaturation by adding the term KC_0 to

represent the pressure available inside the cavity from any gas dissolved in the liquid:

$$KC_0 + P_v + \psi T / (r')^3 - P_L = 2\sigma / \alpha r' \quad (39)$$

This simply states that the partial pressure resulting from dissolved gases, plus the liquid vapor pressure, plus the partial pressure from trapped gas in the cavity must equal the liquid pressure plus $2\sigma/r$ for nucleation to occur. If pressure and temperature are taken as normal system operating setpoints, the above equation can be solved for C_0 .

The relative supersaturation at which nucleation can be expected ($\Delta C_0 / C_s$) is then easily computed. Table 19 presents calculated He supersaturations in lithium for various values of ψ , α , P_L' , and T' .

As can be seen in the table, the supersaturations are somewhat sensitive to values assumed for α and ψ . Also, surface preconditioning has a marked effect. Reduction of P_L' and T' to standby power values results in a reduction in supersaturation from approximately 108% to 1.4%. This not a likely scenario however, since operating procedures for the SP-100 system call for temperature changes in the PHTS to be relatively slow, giving the inner surfaces of the PHTS time to condition to the current operating parameters. Experimental observation suggests that the time required to stabilize at precondition is on the order of 20 minutes. Thus, heterogeneous nucleation could most likely be expected at surface cavities of the PHTS piping at relative supersaturations somewhere in the range from 30% to 160%.

Of note are the effects of flow rate on nucleation in liquid metal systems. It is well known that increased flow rates result in a decrease in the incipient boiling superheat. The reasons for this

phenomena are not clear, and they may or may not impact nucleation from dissolved gases. The Dwyer model presented here does not take flow rate into account. Also of concern is the possibility that local flow conditions, such as pressure changes near an entrance or exit, could provide more favorable nucleation conditions than those used above. These concerns, together with the uncertainty in the values of ψ and α , and uncertainties in the effects of surface preconditioning, all require that the predictions of this model be used with caution, as there may be a significant margin of error.

The FORTRAN computer code used to produce the values in Table 19 is listed in Appendix G, along with sample input and output files.

Figure 53 - Equivalent Cavity Model for Nucleation
(Dwyer, 1976)

Table 19 - Supersaturation for Nucleation on Walls of PHTS

Case	Relative Super Saturation (%)	ψ (N-m/K)	α	T (K)	P _L (kPa)	T' (K)	P' _L (kPa)
1	107.8	6.1×10^{-16}	0.72	1300	62.0	1300	62.0
2	1.4	"	"	"	"	680	8.0
3	30.8	6.1×10^{-14}	"	"	"	1300	62.0
4	111.0	0.0	"	"	"	"	"
5	74.0	6.1×10^{-16}	1.00	"	"	"	"
6	160.5	"	0.50	"	"	"	"
7	99.9	"	0.72	1350	152.0	1350	152.0
8	138.9	"	"	680	9.5	680	9.5
9	138.7	"	"	680	30.5	680	30.5

Case Descriptions:

Case #1 - Base Case. System assumed to be preconditioned to normal BOL, full power operating parameters. Recommended experimental values of ψ and α .

Case #2 - Surface preconditioned to BOL, standby power, temperature and pressure.

Cases #3 and #4 - Effects of change in ψ .

Cases #5 and #6 - Effects of change in α .

Case #7 - EOL operating parameters.

Case #8 - Standby Power, BOL

Case #9 - Standby Power, EOL

3.4.4 Diffusion in the Gas Separator/Accumulators

As described in section 3.3.1 earlier, a He gas bubble will be maintained in each of the six loop gas separators to serve a volume accumulator function, allowing room for coolant expansion and contraction as system temperature changes. Since these bubbles will exist from the start of system operation, no initial supersaturation is required to begin nucleation. The He will simply diffuse out of solution and into the bubble when the supersaturation is great enough to drive mass transfer from the Li/He solution to the bubble. The resistance to this diffusion process can be represented as a mass transfer coefficient. An estimate for the mass transfer coefficient can be obtained from an analogy with heat transfer correlations (Bird, et al., 1960).

The mass transfer rate at a surface in a flow channel may be expressed as:

$$W_A = Nu_{AB} \Delta C_0 D_{AB} A_s / D_h \quad (40)$$

where:

W_A = mass transfer rate (kg of He/sec)

Nu_{AB} = dimensionless mass transfer Nusselt number
(also known as the Sherwood Number)

$\Delta C_0 = C_0 - C_s$ (kg/m³)

C_0 = analytic He concentration in bulk Li (kg/m³)

C_s = saturation concentration of He in bulk Li (kg/m³)

D_{AB} = diffusion coefficient of He in Li (m²/sec)

A_s = mass transfer surface area (m²)

D_h - channel hydraulic diameter (m)

The Sherwood number can be estimated from Nusselt number correlations by replacing the heat transfer Prandtl number with the mass transfer Schmidt number in the Nusselt number correlations.

The Schmidt Number is defined as follows:

$$Sc = \mu / (\rho D_{AB}) \quad (41)$$

where:

Sc - Dimensionless Schmidt Number

μ - Li dynamic viscosity (kg/m-s)

ρ - Li density (kg/m³)

For lithium at 1300 K:

$$\mu = 1.66 \times 10^{-4} \quad (\text{kg/m-s}) \quad (42)$$

$$\rho = 431.5 \quad (\text{kg/m}^3) \quad (43)$$

$$D_{AB} = 6.16 \times 10^{-8} \quad (\text{m}^2/\text{s}) \quad (44)$$

with the result that:

$$Sc = 6.245 \quad (45)$$

The construction of the PHTS gas separator is such that the helium bubble is contained in the center of the separator with Li flowing in an annulus around the bubble (See Figure 36). He that diffuses into the bubble will cross the inner wall of the Li annulus. Assuming that the helium bubble is maintained in a cylindrical shape by centrifugal forces, and ignoring the effects on lithium flow of the deflectors, filter, and swirl vanes, an estimate can be made for the mass transfer coefficient at the bubble surface by analogy with existing heat transfer correlations. Correlations for heat transfer from the inner wall of an annular channel are available from many sources. Listed

below is a experimentally confirmed correlation from the Heat Exchanger Design Handbook (Schlunder et al., 1983):

$$Nu = (.86(D_i/D_o)^{-.16}) (.012(Re^{.87}-280)Pr^{.4}) (1+(D_h/L)^{.67}) \quad (46)$$

where:

D_i - annulus inner diameter (m)

D_o - annulus outer diameter (m)

Re - Reynolds Number = $\rho D_h V / \mu$

L - length of annulus (m)

V - liquid velocity in the channel (m/s)

Pr - Prandtl Number

This correlation is recommended over the following range of variables:

$$0 < (D_i/L) < 1 \quad (47)$$

$$1.5 < Pr < 500 \quad (48)$$

$$2300 < Re < 10^6 \quad (49)$$

In the PHTS accumulators at full power (1300 K):

$$D_i = 0.11 \text{ (m)} \quad (50)$$

$$D_o = 0.22 \text{ (m)} \quad (51)$$

$$Re = 4.24 \times 10^4 \quad (52)$$

$$Sc = 6.245 \quad (53)$$

$$L = 0.60 \text{ (m)} \quad (54)$$

Substituting these values into the above correlation and replacing the Prandtl number with the Schmidt number gives the result:

$$Nu_{AB} = 327 \quad (55)$$

The mass transfer surface area A_s is equal to the surface area of the inner cylinder of the annulus (cylinder end surface neglected):

$$A_s = \pi(0.11)(0.6) = 0.207 \quad (\text{m}^2) \quad (56)$$

The hydraulic diameter of an annulus is the outer diameter minus the inner diameter:

$$D_h = D_o - D_i = 0.22 - 0.11 = 0.11 \quad (\text{m}) \quad (57)$$

Now

$$W_A = \text{Nu}_{AB} \Delta C_o D_{AB} A_s / D_h \quad (58)$$

$$= 327(\Delta C_o)(6.16 \times 10^{-8})(0.207)/0.11 \quad (59)$$

$$= 3.80 \times 10^{-5} (\Delta C_o) \quad (\text{kg/s}) \quad (60)$$

If the total radiologic production of He over a seven year operational life is taken to be 0.25 moles = 0.001 kg, the production rate per second per accumulator (six accumulators in system) is:

$$(0.001)/\{(7.0)(3.1567 \times 10^7)(6)\} = 7.54 \times 10^{-13} \quad (\text{kg/s}) \quad (61)$$

Now, in order for the mass transfer rate in the accumulator to equal the production rate:

$$3.80 \times 10^{-5} (\Delta C_o) = 7.54 \times 10^{-13} \quad (62)$$

$$\Delta C_o = 1.99 \times 10^{-8} \quad (\text{kg/m}^3) \quad (63)$$

At beginning of life conditions in the accumulator of 1300 K and 35 kPa, $C_s = 9.33 \times 10^{-7} \text{ kg/m}^3$, therefore:

$$\Delta C_o / C_s = 0.021 \approx 2\% \quad (64)$$

At EOL, full power (1350 K, 128 kPa), $C_s = 3.57 \times 10^{-6} \text{ kg/m}^3$, other parameters remain essentially constant and:

$$\Delta C_o / C_s = 0.0056 \approx 0.6\% \quad (65)$$

Thus, at full power, He can be expected to diffuse out of the coolant into the accumulator bubbles with relative supersaturations in the range from 0.6 to 2%.

At standby power, coolant temperature and flow rate are reduced. These changes act to reduce mass transfer, but their effects are offset by the larger mass transfer surface area presented by the expanded bubble in the accumulator. The above analysis can be repeated with the following parameters appropriate for BOL standby power (680 K):

$$\rho = 494.1 \quad (\text{kg/m}^3) \quad (66)$$

$$\mu = 3.7 \times 10^{-4} \quad (\text{kg/m-s}) \quad (67)$$

$$D_{AB} = 1.45 \times 10^{-8} \quad (\text{m}^2/\text{s}) \quad (68)$$

$$D_i = 0.154 \quad (\text{m}) \quad (69)$$

$$D_o = 0.22 \quad (\text{m}) \quad (70)$$

$$\text{Re} = 3676.6 \quad (71)$$

with the result that:

$$\text{Sc} = 51.64 \quad (72)$$

$$\text{Nu}_{AB} = 64.0 \quad (73)$$

and,

$$W_A = 4.08 \times 10^{-6} (\Delta C_o) \quad (\text{kg/s}) \quad (74)$$

Now the power level at standby is expected to be 0.28 MW vs 2.4 MW at full power. The helium production rate per accumulator at standby can then be expected to be:

$$(0.28/2.4)(7.54 \times 10^{-13}) = 8.80 \times 10^{-14} \quad (\text{kg/s}) \quad (75)$$

Thus the supersaturation required to match production at standby can be found from:

$$4.08 \times 10^{-6} (\Delta C_0) = 8.80 \times 10^{-14} \quad (76)$$

$$\Delta C_0 = 2.16 \times 10^{-8} \text{ (kg/m}^3\text{)} \quad (77)$$

The saturation concentration at the separators at BOL, standby power is $7.43 \times 10^{-8} \text{ kg/m}^3$. The relative supersaturation is thus:

$$\Delta C_0 / C_s = 2.16 \times 10^{-8} / 7.43 \times 10^{-8} = 0.290 \approx 29\% \quad (78)$$

At EOL all relevant parameters remain the same with the exception of the saturation concentration, which increases to $2.38 \times 10^{-7} \text{ kg/m}^3$ due to increased system pressure. The EOL, standby power, relative supersaturation is therefore:

$$\Delta C_0 / C_s = 2.16 \times 10^{-8} / 2.38 \times 10^{-7} = 0.091 \approx 9\% \quad (79)$$

Thus, at standby power, helium can be expected to diffuse out of the coolant into the accumulator bubbles with relative supersaturations of 9 to 29%.

These results should be considered to be somewhat tentative as the geometries of the gas separator and helium gas bubble have been idealized to simplify analysis. In addition, the correlation used for the diffusion coefficient of helium in lithium, an important physical property in the calculation of the mass diffusion rate, has not been confirmed experimentally.

3.4.5 PHTS Solubility Profile

As coolant flows through the PHTS, it is subjected to varying temperatures and pressures. The solubility of He in Li is a strong function of both these parameters. As a result, He solubility varies from point to point in the PHTS. Based on temperature and pressure profiles calculated by the PHTS computational model discussed earlier,

a loop solubility profile can be constructed. The results of such a calculation are presented in Figures 54 and 55.

As shown in Figure 54, the PHTS pressure losses at full power are large enough so that the solubility is dominated by the effects of changes in loop pressure. The effects of changes in temperature on solubility are barely noticeable. Solubility is highest at the TE-EM pump discharge, and falls steadily to reach a minimum in the system gas separators. At EOL solubility is increased to nearly 3.5 times its original value due to increased loop pressure. If the solubility at the accumulator exit is taken to be 1.0, the subsaturation at the TE-EM pump discharge is ≈ 20 to 50%.

At standby power (Figure 55), pressure effects again dominate the solubility profile at beginning of life. Contraction of the coolant as temperature is reduced from 1300 K at full power to 680 K at standby power results in a drop in system pressure. This drop in pressure, coupled with the drop in temperature, reduces the solubility at BOL by a factor of over 100, compared to full power. An interesting effect is observed at EOL standby power. Increased system pressure at EOL has the effect of making pressure changes due to loop flow losses a small fraction of total pressure. With solubility changes due to pressure minimized by this effect, the effects of temperature on solubility dominate. This is the reason for the drop in solubility seen in Figure 55 as the coolant leaves the PCA (coolant temperature drops) and for the rise in solubility as the coolant flows through the core (coolant temperature rises). Parametric analysis indicates that the gas separator will no longer be at the point of minimum solubility in the loop at standby power after accumulation of 0.08 additional moles of He

in the gas separators. This equates to approximately 2 years of full power operation.

Considering the large drop in solubility upon going from full power to standby power, the question arises as to how much He might be released from solution in this process. To estimate an upper bound for the amount of He that could be released, assume that the coolant is saturated at $4.2 \times 10^{-6} \text{ kg/m}^3$ ($\approx$ the maximum solubility expected over the system lifetime). Assuming a total coolant inventory of $110 \text{ kg} = 0.255 \text{ m}^3$, if all the He was to be released, this would amount to:

$$(4.2 \times 10^{-6}) \times (0.255) / 0.004 = 2.7 \times 10^{-4} \text{ moles of H} \quad (80)$$

This is a small amount of He and emphasizes the very low solubility of He in lithium. The initial amount of helium in the separators is approximately 0.1 moles, so the maximum amount of helium that could be released from solution in the transition from full to standby power represents only $\approx 3/1,000$ of the initial helium present in the separators.

Figure 54 - He Solubility in SP-100 PHTS, Full Power

HE SOLUBILITY IN SP-100 PRIMARY COOLANT LOOP

Figure 55 - He Solubility in SP-100 PHTS, Standby Power

HE SOLUBILITY IN SP-100 PRIMARY COOLANT LOOP

3.4.6 Most Probable Gas Removal Scenario

Considering the solubility profiles presented in Figures 54 and 55, it is apparent that the accumulator/separator gas bubbles are placed near the points of lowest He solubility in the PHTS (except for standby power after ≈ 2 years of operation). As a result, forces tending to drive the helium out of solution will be at or near a maximum in these bubbles for the majority of the system life. This fact, in addition to the fact that the supersaturation for gas diffusion in the separators is calculated to be considerably lower than the supersaturation required for nucleation elsewhere in the loop, makes the gas separators/accumulators themselves the most likely location for gas removal in the PHTS.

As previously mentioned, the coolant can be assumed to have a uniform He concentration due to the low production rate, free mixing in the plenums, and highly turbulent flow. Designate this uniform concentration as C_0 . It appears likely that C_0 at full power will be maintained by a mass diffusion process in the separators at a value of 0.6 to 2% above the saturation concentration in the separators. For the majority of the remainder of the loop this concentration will be well below saturation and hence insufficient to drive a nucleation process. Assuming C_0 equal to 102% of the solubility at the accumulator exit at BOL and 100.6% at EOL, the loop solubility data presented in Figure 54 can be recast in terms of a percent supersaturation (see Figure 56). The maximum supersaturation in the loop is found to be 5.2% at BOL and 1.4% at EOL. Considering the supersaturation requirements for nucleation on the surfaces of the PHTS calculated in the previous sections ($\approx 109\%$, See Table 19), it appears unlikely that the 5.2%/1.4% supersaturations will be sufficient to

drive a nucleation process to compete with gas removal through mass diffusion in the separators. Unfortunately, due to uncertainties in the nucleation model, heterogeneous nucleation in cavities containing trapped gases cannot be ruled out completely. However, it appears that this will not be a major contributor to the gas removal process in the PHTS at full power.

At standby power, the supersaturation profiles (Figure 57) present a higher supersaturation, but these data still favor gas removal by diffusion in the separators. Although, the maximum loop supersaturations (29% at BOL and 9% at EOL) are higher than at full power, the supersaturation for nucleation at cavities containing trapped gases is also higher ($\approx 139\%$, See Table 19), with the result that diffusion in the gas separators is still the most likely means of gas removal.

Figure 56 - SP-100 Full Power Loop Supersaturation Profile

SP-100 FULL POWER SUPERSATURATION PROFILE

Figure 57 - SP-100 Standby Power Loop Supersaturation Profile

SP-100 PHTS STANDBY POWER SUPERSATURATION PROFILE

3.4.7 Preferential Gas Removal at One Accumulator

The model SP-100 PHTS discussed above is composed of a series of identical, parallel flow loops. The mass flow rate, loop pressure profile, and loop gas solubility profile have all been assumed to be identical for each loop in the preceeding analysis. However, in the actual manufacture, assembly and operation of a system such as the SP-100, it is impossible to make each flow loop hydraulically identical. The possibility therefore exists that each loop may have a slightly different mass flow rate and pressure profile.

The He solubility in an individual flow loop of the SP-100 system is a strong function of the pressure profile in the loop in question, which in turn is directly related to the loop mass flow rate. It is therefore possible that in the actual flight system small differences in flow parameters in one loop could cause preferential diffusion in the accumulator/separator of that loop by changing the loop pressure profile and thereby changing the helium solubility profile.

Figure 58 presents the calculated effects on He solubility of a pressure change induced by a hypothetical mass flow rate reduction in one loop. The assumption is made that the temperature drop in the affected loop remains constant, that is that the power dissipated in a loop is directly proportional to the mass flow rate. This assumption was confirmed to be reasonable for small differences in flow rate by calculations with a multi-loop modification of SNPSAM (El-Genk, 1988), a numerical model of the total SP-100 Power System. Pressure at the accumulator of the low flow loop increases as the flow rate for that loop is reduced, with the result that the coolant in that accumulator becomes progressively more subsaturated relative to the coolant in the accumulators of the other normal flow loops. The change in the

subsaturations at the low flow accumulator is a fairly linear function of the reduction in mass flow rate.

The solubility data presented above can be used to calculate the gas removal rate at the low flow accumulator relative to the gas removal rate at one of the other "normal" accumulators. Assuming that Nu_{AB} is unaffected by a small reduction in mass flow rate:

$$(3.80 \times 10^{-5})(\Delta C_0^* + 5\Delta C_0) = (6)(7.54 \times 10^{-13}) \quad (81)$$

where ΔC_0^* is the helium supersaturation at the low flow accumulator.

Now the ratio of the two removal rates will be:

$$R = \Delta C_0^* / \Delta C_0 \quad (82)$$

Taking $C_s = 9.33 \times 10^{-7}$ as before (BOL, Full Power), data from Figure 58 can be used to determine C_s^* as a function of the mass flow rate reduction. The above equation can then be solved for C_0 , which can then be used to find R. The results of this process are presented in Figure 59. As shown in the Figure, a 8% reduction in mass flow rate is sufficient to nearly eliminate gas removal at the low flow accumulator.

Actually, the reduction in flow rate could be expected to slightly reduce N_{AB} , and SNPSAM calculations indicate a slight increase in temperature in the low flow loop. Both of these factors will further increase the subsaturation calculated above for the low flow accumulator, resulting in an even lower mass transfer at the low flow accumulator than is presented in Figure 59.

Interestingly, the above results imply that if one loop has a somewhat higher, rather than lower, mass flow rate than any of the other loops, a large portion of the dissolved helium may diffuse out of

solution in the accumulator of that high flow rate loop. This could lead to a gas "overflow" in that accumulator. Figure 60 presents the calculated effect of a higher than normal mass flow rate on gas removal rate. As can be seen in the Figure, an increase of 10% in the mass flow rate of any one loop can be expected to nearly triple the gas removal rate at that loop's accumulator, relative to the removal rate at the other "normal" accumulators.

Figure 58 - Effects of Reduction in Mass Flow Rate on Solubility

EFFECT OF WEAK LOOP ON HE SOLUBILITY BOL, FULL POWER

Figure 59 - Relative Gas Removal Rate in Low Flow Loop Accumulator

Figure 60 - Relative Gas Removal Rate in High Flow Loop Accumulator

3.4.8 Optimization of Gas Separators

The results of the previous sections suggest the possibility of optimizing the loop gas separators to minimize the supersaturation required to drive gas removal by diffusion at the separator bubbles, thereby minimizing the possibility of bubble nucleation outside the separators. A complete optimization of the separators is beyond the scope of this study. The discussion presented below attempts rather to identify some of the more important parameters controlling mass diffusion rates in the separator/accumulators.

Examination of equations (40) and (46) reveals several means of increasing the mass transfer efficiency in the separators. Increasing the mass flow rate in the separators will increase the Reynolds number, which increases Nu_{AB} thereby increasing the mass transfer rate.

Increasing the operating temperature of the separators will increase D_{AB} , which will enhance mass transfer since $W_A \propto D_{AB}^{0.6}$. In addition to these parameters, the accumulator geometry could be varied to improve mass diffusion.

A study of the effects of accumulator geometry on the mass diffusion process can be made using equations (40) and (46). The results of such a study are presented in Tables 20 and 21, for full power operating parameters. In these tables, the accumulator gas volume, system gas production, and mass flow rate were all held constant and accumulator length was varied, resulting in a changing inner diameter in the accumulator. Table 20 uses an accumulator outer diameter of 0.22 m, while Table 21 uses an accumulator outer diameter of 0.12 m.

As shown in Table 20, the supersaturation required to remove produced gases decreases with increasing accumulator length. This is due primarily to the increase in mass transfer surface area as the accumulator length increases. Table 21 displays the effects of the accumulator hydraulic diameter on the supersaturation required to remove produced gases. As shown there, the supersaturation decreases with decreasing accumulator hydraulic diameter. These results suggest that the optimum accumulator design for mass transfer simultaneously maximizes bubble surface area and minimizes the flow channel area between the accumulator bubble and outer wall.

Of course, a design with a small difference between D_i and D_o could interfere with the accumulator function of the device, since any growth in the accumulator bubble could result in the escape of gas from the device. This emphasizes the fact that the accumulators cannot be designed for mass transfer efficiency alone, but must in addition meet other design restrictions, such as mass minimization, reliability, etc. However, it appears from the above results that some mass transfer optimization may be possible within the bounds of other design constraints.

Table 20 - Effects of Accumulator Geometry on Mass Diffusion, I

Length	D_i	D_h	$\Delta C_0/C_s$	A_s	Nu_{AB}
(m)	(m)	(m)	%	$\frac{m^2}{(m)}$	
0.2000	0.1905	0.0295	1.3467	0.1197	239.8
0.3000	0.1556	0.0644	2.0226	0.1466	285.1
0.4000	0.1347	0.0853	2.1553	0.1693	306.6
0.5000	0.1205	0.0995	2.1555	0.1893	319.9
→ 0.6000	0.1100	0.1100	2.1132	0.2073	329.4
0.7000	0.1018	0.1182	2.0568	0.2240	336.5
0.8000	0.0953	0.1247	1.9969	0.2394	342.3
0.9000	0.0898	0.1302	1.9377	0.2539	347.1
1.0000	0.0852	0.1348	1.8809	0.2677	351.2
1.1000	0.0812	0.1388	1.8274	0.2807	354.8
1.2000	0.0778	0.1422	1.7770	0.2932	358.1
1.3000	0.0747	0.1453	1.7299	0.3052	361.0
1.4000	0.0720	0.1480	1.6858	0.3167	363.6
1.5000	0.0696	0.1504	1.6445	0.3278	366.1
1.6000	0.0674	0.1526	1.6057	0.3386	368.3
1.7000	0.0653	0.1547	1.5694	0.3490	370.4
1.8000	0.0635	0.1565	1.5351	0.3591	372.4
1.9000	0.0618	0.1582	1.5028	0.3690	374.3
2.0000	0.0602	0.1598	1.4724	0.3786	376.0

Notes:

- 1) Full Power, 1300K, 35 kPa, $C_s = 9.33 \times 10^{-7} \text{ kg/m}^3$, $D_{AB} = 6.16 \times 10^{-8} \text{ m}^2/\text{s}$
- 2) Constant accumulator gas volume = .005702 m^3 /accumulator
- 3) Constant mass flow rate = 1.833 kg/s-accumulator
- 4) Constant gas removal rate = $7.54 \times 10^{-13} \text{ kg/s}$ -accumulator
- 5) Accumulator outer diameter = 0.22 m
- 6) Bubble shape assumed cylindrical, end surface area neglected
- 7) → Indicates base case geometry

Table 21 - Effect of Accumulator Geometry on Mass Diffusion Process, II

Length	D_i	D_o	$\Delta C_o/C_s$	A_s	Nu_{AB}
(m)	(m)	(m)	%	(m) ²	
0.5100	0.1193	0.0007	0.0157	0.1912	300.6
0.5200	0.1182	0.0018	0.0409	0.1930	305.6
0.5300	0.1170	0.0030	0.0643	0.1949	309.9
0.5400	0.1160	0.0040	0.0861	0.1967	313.7
0.5500	0.1149	0.0051	0.1064	0.1985	317.2
0.5600	0.1139	0.0061	0.1255	0.2003	320.4
0.5700	0.1129	0.0071	0.1433	0.2021	323.5
0.5800	0.1119	0.0081	0.1601	0.2039	326.5
0.5900	0.1109	0.0091	0.1758	0.2056	329.3
0.6000	0.1100	0.0100	0.1906	0.2073	332.0

Notes:

- 1) Full Power, 1300K, 35 kPa, $C_s = 9.33 \times 10^{-7} \text{ kg/m}^3$, $D_{AB} = 6.16 \times 10^{-8} \text{ m}^2/\text{s}$
- 2) Constant accumulator gas volume = .005702 m³/accumulator
- 3) Constant mass flow rate = 1.833 kg/s-accumulator
- 4) Constant gas removal rate = $7.54 \times 10^{-13} \text{ kg/s-accumulator}$
- 5) Accumulator outer diameter = 0.12 m
- 6) Bubble shape assumed cylindrical, end surface area neglected

3.4.9 Summary

Homogeneous nucleation and heterogeneous nucleation on smooth surfaces both appear to be unlikely modes of helium nucleation in the SP-100 PHTS. Helium will most probably diffuse out of solution in the gas separators, near the point of lowest solubility in the loop. The only likely means of gas removal which may compete with this diffusion process is nucleation occurring at PHTS wall surface cavities containing trapped gas. However, the supersaturation required for nucleation at these sites appears to be significantly larger than that required to drive a mass diffusion removal process at the gas separators. Even so, uncertainties in wall nucleation models and local flow conditions preclude a definite elimination of nucleation on PHTS wall surface cavities. It appears that small (8%) reductions in individual loop flow rates can effectively eliminate gas removal in the accumulator of the loop which experiences the low flow rate. Some optimization of the separator/accumulator design to enhance mass transfer at the accumulator bubble may be possible.

3.5 Bubble Growth and Collapse

As discussed in the previous section, it appears most likely that helium will diffuse out of solution directly into the gas accumulator bubbles. However, the possibility of nucleation outside the gas separators cannot be eliminated entirely due to uncertainties in the nucleation models. If for some reason gas bubbles are formed outside the separators, or if helium in the separators manages to escape as a result of a less than 100% separator efficiency or some other unspecified means, the behavior of the gas bubbles released into the PHTS becomes of interest.

The scope of this investigation was limited to normal operations, which include a functional gas separator system. As described in General Electric (1988b), the gas separators are expected to retain all bubbles with radii greater than 15 μm , and the coolant void fraction leaving the separators is expected to be well below 0.1%. It is clear from these parameters that under normal operating conditions only a small amount of gas is expected to leave the separators. The assumption implicit in these design criteria is that as long as gas bubbles remain small and void fractions low, no adverse impact will result. Assuming this to be the case, and assuming the gas separators function as designed, three potentially significant scenarios can be hypothesized:

- 1) Bubbles nucleate outside the gas separators, become entrained in the coolant, and rapidly grow before they reach the separators for removal;
- 2) Small bubbles leaving the gas separator remain entrained in the coolant and rapidly grow before they are returned to the separators for removal; or

3) Small bubbles from either 1) or 2) above become trapped somewhere in the PHTS, grow slowly over an indefinite period of time, and subsequently dislodge to relocate in a sensitive area.

Note that in 1) and 2), if gas bubble growth rates are slow, then separation will occur normally, prior to the bubbles reaching a size much greater than 15 microns in radius. Only in the event of a significant bubble growth on the order of the time required for the coolant to make one circuit of the PHTS (≈ 10 seconds at full power, ≈ 42 seconds at standby power) can bubble growth create difficulties.

The possibility of bubble entrapment outside the gas separators mentioned in scenario 3) above, is discussed briefly in Appendix H, with indefinite conclusions. Although it is appears uncertain whether or not gas will become trapped outside the separators, for the purposes of this study it will be assumed that gas will not be held in the PHTS for a significant amount of time outside of the separators, and scenario 3) above can be eliminated.

Accordingly, the concern of the following sections is that gas leaving the separator or nucleating elsewhere in the PHTS may grow rapidly (on the order of ≈ 60 s) through various effects such as coalescence, diffusion, and/or pressure/temperature changes, with potentially undesirable effects. The following sections discuss several mechanisms which were investigated as having a potential impact on gas bubble growth and/or collapse in the PHTS.

3.5.1 Mechanical Pressure and Temperature Effects

Bubbles leaving the gas separators or wall nucleation/growth sites will experience variations in pressure and temperature as they

circulate in the PHTS. These variations will cause the bubbles to expand and contract as they flow through the loop. A detailed analysis of this process requires an accounting of inertial and diffusion terms which are significant mainly in cases of rapid transients (Minkowycz et al. 1976). Assuming slow transient, steady state, or quasi-steady state conditions, a simplified analysis is appropriate (Singer et al. 1977). This analysis, which ignores inertial and mass diffusion effects and assumes the bubbles to be spherical and in thermal equilibrium with the surrounding coolant, is described below.

Three well known physical relationships can be combined to determine the radius of a gas bubble under steady state conditions. These are:

$$P_v + P_g + P_L = 2\sigma/r \quad \text{the Laplace Equation,} \quad (83)$$

$$P_g = nRT/V_b \quad \text{the Perfect Gas Law,} \quad (84)$$

and,

$$V_b = 4\pi r^3/3 \quad \text{the Volume of a Sphere.} \quad (85)$$

where:

P_v = coolant vapor pressure at temperature T, (Pa)

P_g = inert gas partial pressure inside bubble (Pa)

P_L = coolant pressure (Pa)

σ = coolant surface tension (N/m)

r = gas bubble radius (m)

V_b = gas bubble volume (m^3)

n = amount of inert gas in bubble (moles)

R = Universal Gas Constant = 8.3144 (J/mole-K)

T = coolant temperature (K)

Substituting for V from the spherical volume formula into the perfect gas law yields an expression for P_g :

$$P_g = 3nRT/(4\pi r^3) \quad (86)$$

Substituting this expression for P_g into Laplace's equation and rearranging yields a cubic expression for r:

$$(P_L - P_v)r^3 + 2\sigma r^2 - 3nRT/(4\pi) = 0 \quad (87)$$

This equation can be solved in a straight forward manner to determine r for any given pressure/temperature combination.

Substituting pressures and temperatures from the PHTS computational model into the above equation, the changes in bubble size during a circuit of the PHTS can be calculated. Results of this calculation are presented in Figure 61 for full power and Figure 62 for standby power. In both cases, pressure changes dominate temperature effects. Bubbles leaving the gas separator are compressed as they pass through the pump, and gradually return to their original size as they continue through the loop. As a result of higher surface tension forces at small r, bubbles of small initial radius are affected less than those with a larger initial radius. Maximum compression is approximately 20% of initial radius at full power and 10% at standby power.

In summary, neglecting mass diffusion, it appears that small (radius $<15\mu\text{m}$) bubbles which escape the accumulators will not grow significantly due to changes in temperature and pressure as they traverse the PHTS. They will in fact be slightly compressed upon leaving the accumulators and gradually return to their original size as they circulate in the loop.

Figure 61 - Bubble Radius Changes due to Changes in Temperature and Pressure, Full Power

Figure 62 - Bubble Radius Changes due to Changes in Temperature and Pressure, Standby Power

3.5.2 Mass Diffusion Effects

Bubble growth by mass diffusion in a uniformly supersaturated liquid has been studied by many authors. The discussion presented below follows a treatment by van Stralen and Cole (1979).

Following nucleation, bubble growth is initially an inertially controlled process which can be described by the Raleigh Equation. Initial supersaturation at the bubble wall is equal to the bulk liquid supersaturation. As the bubble grows and gas diffuses from the liquid into the bubble, the concentration at the bubble wall drops and ultimately approaches the liquid saturation concentration. At this point, the concentration gradient at the bubble wall is stable and remains constant during the remainder of the bubble growth process. It can be shown (van Stralen and Cole, 1979) that for a liquid which is initially at a low, uniform, supersaturation, the inertial growth mode of a gas bubble is extremely short and can be neglected. Consequently, at low supersaturations, the rate of growth can be completely described by a diffusion formula:

$$r(t) = (2JaD_{AB}t)^{0.5}, \quad \text{for } Ja < \pi/6 \quad (88)$$

where:

$r(t)$ - bubble radius as a function of time (m)

Ja - dimensionless Jacob Number = $(C_0 - C_s)/\rho_g$

C_0 - gas concentration in bulk liquid (kg/m^3)

C_s - saturation gas concentration in bulk liquid (kg/m^3)

ρ_g - gas density in bubble (kg/m^3)

D_{AB} - diffusion coefficient of gas in liquid (m^2/s)

t - time (s)

The predictions of the above formula have been experimentally verified both in electrolysis processes, and in supersaturations resulting from rapid depressurizations (van Stralen and Cole, 1979).

The above formula predicts that equilibrium (no growth) occurs at $C_0 = C_s$. This ignores the effects of liquid vapor pressure and surface tension, which can be important for small bubbles. Equilibrium actually occurs when the partial pressure from the inert gas dissolved in the bulk liquid is equal to the pressure of the inert gas inside the bubble. This requirement for mass equilibrium can be expressed mathematically as:

$$P_g = KC_0 \quad (89)$$

where:

P_g - inert gas pressure inside the bubble (Pa)

K - Henry's Law Constant (m^2/s^2)

At equilibrium, the bubble must also meet the mechanical equilibrium requirements of Laplace's equation:

$$P_v + P_g = 2\sigma/r + P_L \quad (90)$$

where:

P_v - liquid vapor pressure (Pa)

σ - liquid surface tension (N/m)

r - bubble radius (m)

P_L - liquid pressure (Pa)

Combining the requirements for mass and mechanical equilibrium and noting that $P_L = KC_s$ results in an expression for C_0 for no growth conditions:

$$P_v + KC_0 = 2\sigma/r + KC_s \quad (91)$$

rearranging:

$$C_0 = C_s + 2\sigma/rK - P_v/K, \quad \text{at equilibrium} \quad (92)$$

Substituting this value of C_0 for C_s in the above definition of Ja results in a new expression for Ja which includes surface tension and liquid vapor pressure effects:

$$Ja = (C_0 - (C_s + 2\sigma/rK - P_v/K))/\rho_g \quad (93)$$

Equation (88) can be further modified to account for bubble collapse by including a term for the initial bubble radius, resulting in a general formula to describe diffusion controlled growth/collapse:

$$r(t) = (R_0^2 + 2JaD_{AB}t)^{0.5} \quad (94)$$

where:

Ja is defined by equation (93)

R_0 = initial bubble radius (m)

Note that a positive Ja results in bubble growth, while a negative Ja results in bubble collapse. Equation (94) correctly predicts whether a bubble will grow or not, but only approximates the time rate of change in r , since ρ_g is a function of r . However, (94) provides a useful first order approximation, and will be reasonably accurate except where ρ_g changes appreciably over the interval of interest.

Figures 63 and 64 present the growth/collapse of gas bubbles as predicted by (94) at super/sub saturations of 30%, for several initial bubble sizes. Considering Figure 63, it can be seen that changes in bubble radius are relatively slow compared to the time required for a bubble to traverse the SP-100 PHTS (≈ 10 sec at full power, ≈ 42 sec at standby power, assuming a slip ratio of 1.0), even at a +30%

supersaturation. Bubbles less than $\approx 7 \mu\text{m}$ in radius collapse due to surface tension effects. Figure 64 shows that bubbles less than $\approx 10 \mu\text{m}$ in radius can be expected to collapse fairly rapidly under a 30% subsaturation.

The above observation that small bubbles will rapidly collapse, coupled with the low solubility of He in Li, leads to the possibility that the coolant may become saturated as a result of absorption of He from small bubbles. Table 22 presents void fractions calculated to saturate the coolant at BOL and EOL operating conditions. As can be seen in the table, very small void fractions ($< \approx 10^{-4} \%$) must be maintained to prevent coolant saturation. However, due to the extremely small size of the bubbles in question, the void fractions to saturate the coolant are equivalent to bubble collapse rates of more than 10^5 bubbles per second.

Equation (91) implies that for a given P_L , P_v , K , C_0 and σ , there is a unique bubble radius which has both mass and mechanical equilibrium. Rearranging (91) to solve for r yields:

$$r = 2\sigma / (P_L(SS) + P_v), \quad \text{at equilibrium} \quad (95)$$

where:

$$SS = \text{relative supersaturation} = (C_0 - C_s) / C_s$$

Bubbles created in a liquid which have an initial radius larger than this equilibrium radius will grow, while bubbles with initial radii smaller will ultimately collapse. Figure 65 presents a calculation of equilibrium radii for two conditions representative of SP-100 PHTS full power and standby power operating conditions. As the bulk supersaturation is reduced, the equilibrium radius is increased,

because there is less gas pressure available to support the bubble against surface tension forces.

The loop supersaturation profiles of Figures 56 and 57 can be used with equation (95) to estimate the minimum bubble size stable against collapse by mass diffusion at any point in the SP-100 PHTS. The results of this calculation are presented in Figures 66 and 67. At full power, a large portion of the loop is considerably subsaturated, and no bubble of any size is stable. The minimum stable bubble radius calculated for the remainder of the loop is seen to be $\approx 40 \mu\text{m}$. At standby power, the whole loop is supersaturated, but this is offset by a lower system pressure, with the result that bubbles must have a radius of $150 \mu\text{m}$ or more to be stable for the majority of the loop. Now the largest bubbles which leave the gas separator are expected to be $15 \mu\text{m}$ in radius, so it appears these bubbles will ultimately diffuse away at all operating conditions.

In summary, it appears that any small ($r < 15 \mu\text{m}$) bubbles which may escape the gas separators will ultimately collapse due to mass diffusion. The smaller the bubble, the faster the rate of collapse. Very small ($< 10^{-4} \%$) void fractions are necessary to prevent resaturation of the coolant as these bubbles diffuse away. However, due the small size of the bubbles, these void fractions are equivalent to bubble escape rates on the order of 10^5 or more bubbles per second.

Figure 63 - Bubble Growth due to Mass Diffusion

BUBBLE GROWTH DUE TO MASS DIFFUSION

Figure 64 - Bubble Collapse due to Mass Diffusion

BUBBLE COLLAPSE DUE TO MASS DIFFUSION

Table 22 - Void Fractions to Saturate Coolant

Operating Conditions	Bubble Radius <div>(μm)</div>	Gas Density <div>(kg/m^3)</div>	Void Fraction	
			(%)	Bubbles/s
<u>BOL, Full Power:</u>				
T=1300 K	1.0	0.2064	4.52×10^{-4}	2.75×10^{10}
P=35 kPa	5.0	0.0495	1.88×10^{-3}	9.17×10^8
Sol= $9.33 \times 10^{-7} \text{ kg/m}^3$	10.0	0.0299	3.12×10^{-3}	1.90×10^8
	15.0	0.0234	4.00×10^{-3}	7.24×10^7
<u>EOL, Full Power:</u>				
T=1350 K	1.0	0.2242	1.59×10^{-3}	9.18×10^{10}
P=127.5 kPa	5.0	0.0777	4.60×10^{-3}	2.13×10^9
Sol= $3.57 \times 10^{-6} \text{ kg/m}^3$	10.0	0.0594	6.02×10^{-3}	3.48×10^8
	15.0	0.0532	6.71×10^{-3}	1.15×10^8
<u>BOL, Stby Power:</u>				
T=680 K	1.0	0.5221	1.42×10^{-5}	8.20×10^8
P=9.5 kPa	5.0	0.1098	6.77×10^{-5}	6.29×10^6
Sol= $7.43 \times 10^{-8} \text{ kg/m}^3$	10.0	0.0583	1.27×10^{-4}	1.47×10^6
	15.0	0.0411	1.80×10^{-4}	6.20×10^4
<u>EOL, Stby Power:</u>				
T=680	1.0	0.5369	4.43×10^{-5}	2.56×10^9
P=30.5 kPa	5.0	0.1246	1.91×10^{-4}	1.77×10^7
Sol= 4.05×10^{-6}	10.0	0.0731	3.26×10^{-4}	3.78×10^6
	15.0	0.0559	4.26×10^{-4}	1.47×10^5

Notes:

1. Void fraction is the void fraction required to produce a concentration in the coolant equal to the saturation concentration, assuming all bubbles dissolve in the coolant.
2. Saturation concentration (Sol) is measured at system accumulator.
3. Gas density calculated at mechanical equilibrium by the following formula:

$$\rho_g = 0.004(2\sigma/r + P_L - P_v)/(RT)$$

Figure 65 - He Bubble Radius at Mass Equilibrium

BUBBLE RADIUS AT MASS EQUILIBRIUM

Figure 66 - Minimum Bubble Size Stable Against Collapse by Mass
Diffusion, Full Power

Figure 67 - Minimum Bubble Size Stable Against Collapse by Mass
Diffusion, Standby Power

3.5.3 Coalescence

The topic of bubble coalescence is of interest as a potential means of bubble growth in the SP-100 PHTS. Under the assumption that bubbles may escape the gas separators or be released from wall nucleation/growth sites, they may collide, coalesce, and form larger bubbles downstream from the accumulator.

Gas bubble coalescence under controlled conditions has been studied by Marrucci (1969). Marrucci proposed that for bubbles with a very low approach velocity, the time required for coalescence can be divided into two stages: 1) an extremely rapid thinning of the film separating the two bubbles down to a quasi-equilibrium thickness, and; 2) a further thinning of the quasi-equilibrium film down to rupture. The second stage is found to be a strong function of the quantity of surfactants present in the liquid. For pure liquids and sufficiently dilute solutions, the time required for the second stage is negligible, and the coalescence process for two bubbles of equal radius can be described as an inertially controlled process:

$$t = (R_s/4) \{ (\rho_L r / \sigma)^{0.5} \} \{ \ln(h_i/h_f) \} \quad (96)$$

where:

t - time required for coalescence

R_s - radius of surface of contact between the bubbles (m)

ρ_L - liquid density (kg/m^3)

r - bubble radii (m)

σ - liquid surface tension (N/m)

h_i - initial contact film thickness (m)

- typically 10^{-3} (m)

h_f - final contact film thickness at time of rupture (m)

- typically 10^{-10} (m)

Experimental verification of the above equation has been obtained for dilute aqueous solutions (Marrucci et al., 1969), where coalescence times were found to be on the order of 10^{-4} seconds for bubbles of radius ≈ 1 cm.

Assuming the Li coolant in the SP-100 PHTS to be a "pure" liquid, the maximum time required for coalescence can be estimated from (96). For lithium at 1300 K, the predicted coalescence time varies from 5 μ s for an initial bubble radius of 10 μ m, to 26 μ s for an initial bubble radius of 30 μ m. For the purposes of the present study, these coalescence times can be considered to be virtually instantaneous .

The above data would seem to indicate that bubble coalescence in a turbulent flow where many bubble to bubble contacts could be expected would be a very rapid process. However, observations of air bubbles in water have shown this not to be the case. Kirkpatrick and Lockett (1974) observed air bubble clouds held stationary in a column of down flowing water and found little or no coalescence to take place. They found that the majority of the bubbles which contact each other in a turbulent flow have a significant approach velocity, and tend to rebound and separate before coalescence can take place.

Calderbank et al. (1965) observed bubble coalescence in a cloud of air bubbles rising freely through a column of various aqueous solutions, and found coalescence to follow a first order rate dependence on bubble number density. Following Calderbank, Miller (1983) assumed a first order rate law to obtain a correlation for

bubble density in an column containing upward flowing aqueous solutions and air bubbles:

$$N_{BM} = N_{BO} \exp(-f_c \theta) \quad (97)$$

where:

N_{BM} - bubble number density based on mean bubble diameter

(bubbles/m³)

N_{BO} - bubble number density based on original bubble

diameter (bubbles/m³)

f_c - bubble coalescence frequency, evaluated from

experimental data (s⁻¹)

θ - two phase holdup time (s)

= (volume occupied by two phase mixture at time t)/
(volumetric flow rate of two phase mixture)

Experimentally determined values of f_c are presented in Figure 68 for air in a variety of aqueous solutions, and are seen to be strong functions of liquid properties. Unfortunately no theory is provided whereby calculation of f_c could be extended to liquid metals.

To summarize, no data on bubble coalescence in liquid metals was found. Experiments with air in aqueous solutions indicate that bubbles can be expected to coalesce in a pure liquid, but rebound effects in a turbulent flow slow this rate of coalescence. Coalescence has been experimentally determined to follow a first order rate law based on bubble number density for air bubbles in aqueous solutions.

Figure 68 - Bubble Coalescence Frequency
(Miller, 1983)

3.5.4 Maximum Stable Bubble Size

Given that bubbles may coalesce, the question arises as to what size they will grow. Researchers in this area have observed that bubbles tend to coalesce until reaching a certain maximum stable drop size. Bubbles in a turbulent flow field with radii larger than this maximum stable radii tend to break apart, presumedly as a result of turbulent shear forces in the fluid. Bubbles smaller than the maximum stable size coalesce. Several correlations are available to predict the maximum stable bubble size, four of which are discussed below.

Hinze (1955) developed one of the first correlations which predicts the maximum stable bubble size in a turbulent flow:

$$D_{\max} = 0.725(\sigma/\rho_L)^{0.6}(\epsilon)^{-0.4} \quad (98)$$

where:

$D_{\max}$ - maximum stable bubble diameter

σ - liquid surface tension (N/m)

ρ_L - liquid density (kg/m^3)

ϵ - power dissipated per unit volume of liquid (m^2/s^3)

Now the power dissipated in a length of pipe is:

$$W = \Delta p A_c V \quad (99)$$

where:

W - power dissipated (W)

Δp - pressure loss in pipe (Pa)

A_c - pipe cross sectional area (m^2)

V - fluid velocity in the pipe (m/s)

The pressure loss (Δp) can be found from the Darcy-Weisbach equation:

$$\Delta p = (f L \rho_L V^2)/(2D) \quad (100)$$

where:

f - Darcy friction factor (dimensionless)

L - pipe length (m)

ρ_L - fluid density (kg/m^3)

V - fluid velocity (m/s)

D - pipe diameter (m)

The total mass of fluid in a pipe is:

$$M = \rho_L L A_c \quad (101)$$

Therefore

$$\epsilon = W/M = fV^3/2D \quad (102)$$

For turbulent flow in a smooth pipe f can be approximated by

$$f = 0.176/(\text{Re})^{0.2} \quad (103)$$

where:

Re - Reynolds Number (dimensionless)

$$= \rho_L D V / \mu_L$$

μ_L - fluid viscosity (kg/m-s)

As a result, Hinze's equation (98) can be expressed for turbulent internal flow as:

$$D_{\max} = (1.92 \sigma^{0.6} D^{0.48}) / (\rho_L^{0.52} V^{1.18} \mu_L^{0.08}) \quad (104)$$

Hinze obtained good agreement with results from bubble size experiments performed with counterrotating cylinders (Hinze, 1955).

Sleicher (1962) reanalyzed the problem of maximum stable drop size in turbulent pipe flow and proposed the following relationship for

drops with bubble viscosity much lower than the continuous phase viscosity:

$$D_{\max} = (38 \sigma^{1.5}) / (\rho_L^{2.5} V^{0.5} \mu_L) \quad (105)$$

The predictions of this equation compared well with experiments performed using two immiscible liquids in turbulent pipe flow. Sleicher found that drop breakup occurred primarily near the pipe walls, where shear forces were at or near a maximum. A subsequent study by Paul and Sleicher (1965) showed the above relationship to be only weakly dependent on pipe diameter. A study by Collins and Knudsen (1970) of drop size distributions in turbulently flowing immiscible liquids also supports the above relationship.

Hughmark (1971) proposed a modification of the Hinze equation (98), which was specific to turbulent pipe flow:

$$D_{\max} = (26.9 \sigma D^{0.2}) / (\rho_L^{0.8} V^{1.8} \mu_L^{0.2}) \quad (106)$$

This predictions of this equation compared favorably with the data of Sleicher (1962).

Sevik and Park (1973) proposed a modification of Hinze's equation (98) based on experiments with air bubble breakup in the adjustment region of a water jet. The correlation is identical to (98) except that the constant 1.92 is replaced with 3.04:

$$D_{\max} = (3.04 \sigma^{0.6} D^{0.48}) / (\rho_L^{0.52} V^{1.18} \mu_L^{0.08}) \quad (107)$$

The predictions of the above equation were in good agreement with the experimental data of Sevik and Park (1973).

The fluid properties and flow parameters used in the four maximum bubble size relationships discussed above are available from the PHTS computational model for the SP-100. Figure 69 presents the calculated maximum stable bubble sizes for the various components of the PHTS at

full power. As can be seen in the figure, there is wide disagreement between the predictions of the four correlations with variations as large as 2 orders of magnitude not uncommon. This primary reason for this is that the correlations do not agree on the dependence of $D_{\max}$ on the physical properties of the continuous phase fluid. Liquid lithium at 1300 K has a surface tension ≈ 4 times that of room temperature water, a density of ≈ 0.5 times that of water, and a viscosity ≈ 0.1 times that of water. The four correlations produce results of the same order of magnitude for the fluid properties of water, but when the correlations are extended to lithium, large discrepancies result.

The wide disagreement between the four correlations above emphasizes the uncertainties associated with extending correlations developed for room temperature water to a liquid metal at 1300 K. Such an extension is generally unreliable. Only experiment can produce a definite resolution of this matter. However, if a choice must be made, there is some reason to prefer the correlation of Sleicher (105) over the others for the present purposes. Sleicher is the only one of the four to have experimented with turbulent pipe flows. Also, his results are corroborated by other experiments with turbulent pipe flow (Paul and Sleicher, 1965), (Collins and Knudsen, 1970). Finally, Sleicher observed that the primary location of bubble breakup in turbulent pipe flow was near the pipe wall, in a region of high shear, rather than at the pipe center where isotropic turbulence is highest. The Hinze equation (98) and its derivatives are based on bubble breakup by uniform isotropic turbulence, and therefore may not be applicable to bubble breakup in pipe flow.

Figure 69 - Maximum Stable Bubble Size in PHTS

SP-100 PRIMARY COOLANT LOOP MAXIMUM STABLE BUBBLE SIZE

3.5.5 Summary

Mechanical temperature and pressure effects will not cause large bubble growth as bubbles are transported through the SP-100 PHTS. Bubbles with radii smaller than $\approx 40 \mu\text{m}$ at full power and $\approx 150 \mu\text{m}$ at standby power will ultimately collapse due to mass diffusion effects. Larger bubbles will grow, but will not grow significantly in the time required for coolant to make a circuit of the PHTS and return to the gas separator.

Although little work appears to have been reported regarding gas bubble behavior in liquid metal flow, some generalizations may be possible from chemical engineering experiments involving air and immiscible liquids in aqueous solution. The results of these studies indicate that bubbles will likely tend to coalesce following a first order rate law based on bubble number density. However, the rate coefficient is undetermined. It appears likely that the bubbles will grow to some maximum size and then be broken apart by shear forces in the flow, but it is not clear what this size may be. In short, the possibility that bubbles may grow through coalescence to reach a large size may not be excluded. However, if a low bubble number density is assumed, then the probability of rapid growth leading to problems before normal separation occurs seems to be low.

3.6 Bubble Distributions

A brief discussion of bubble distributions in internal two phase flow is presented in Appendix H. Several mechanisms which were thought to have potential effects on bubble distributions in the SP-100 system are considered, including microgravity effects, magnetic field effects, and inertially induced buoyancy effects. Unfortunately, no

definite conclusions could be drawn from this phase of the study. Bubble distributions appear to be a strong function of channel geometry and inlet conditions and are not easily predicted even for earth based systems. Extension of earth based results to a space based system is speculative at best. Further research is required in this area.

3.7 Overall Bubble Behavior Estimate

The results of the preceeding sections are in many cases uncertain and based on laboratory results at one g in idealized flow channels with fluids other than liquid metals. Very little experimental data exists on which to base a sound model of bubble behavior in the SP-100 PHTS. Quantitative predictions of bubble distributions within a pumped loop are difficult to make, even for earthbound systems. Nevertheless, with these uncertainties in mind, a best estimate summary will be attempted based on the available information.

Assuming a coolant Li^6 content of ≈ 0.1 a/o, radiologic production of helium gas can be expected to saturate the coolant approximately 24 hours after the start of full power operation. The concentration of gas will continue to increase until reaching a value of approximately 2% over the saturation concentration at the accumulator exit. At this point, gas diffusion from the coolant into the accumulator bubbles will match the radiologic production rate, and the supersaturation will remain constant. It appears unlikely that this supersaturation will be large enough to drive nucleation processes elsewhere in the loop. At standby power, a similar situation exists, but the stable supersaturation is $\approx 30\%$, measured at the accumulator exit. It appears that small changes in an individual loop flow rate could markedly affect the amount of gas removed in that loop's accumulator.

Design criteria for the gas separators allow the release of gas bubbles of radii 15 μm or less. Under the assumption that bubbles of this size will be released with a low number density and will not be held up or concentrated in the PHTS, it appears that there is no likely means of rapid bubble growth that would lead to large bubbles which could create problems prior to normal separation. Temperature, pressure, and diffusion effects in the loop in fact lead to the reduction in size of any bubbles released from the separators with a radius of less than 15 μm . Coalescence may lead to bubble growth, but this growth should not be rapid, assuming a low bubble number density.

4. SUMMARY AND CONCLUSIONS

A significant amount of hydrogen and helium gas can be radiologically generated in the coolant of small lithium cooled reactors such as the SP-100. Hydrogen and its isotopes can be expected to dissolve readily into the coolant and then to diffuse out of the system. Helium gas will remain trapped in the coolant. Three major helium production reactions ($\text{Li}^6(n,t)$, $\text{Li}^7(n,nt)$, and $\text{Li}^6(n,nd)$) were identified in reactors using natural lithium coolant. A significant fraction of the total system helium production ($\approx 50\%$ for natural Li) occurs outside the reactor core, primarily in the lower plenum and core inlet/outlet piping regions. The amount of helium gas produced can be reduced by minimizing the amount of coolant in high production areas such as the reactor core and lower plenum, or by reducing the coolant Li^6 content. System total helium production can be reduced to approximately 10% of the production calculated for natural lithium by utilizing coolant with a Li^6 enrichment below 0.1 atom %. However, a certain amount of gas production is inevitable due to $\text{Li}^7(n,nt)$ reactions.

Assuming a coolant Li^6 content of ≈ 0.1 a/o, the coolant of a SP-100 class reactor is expected to become saturated with helium one day or less after the start of full power operations. Helium will then begin to come out of solution, most likely through a mass diffusion process at the accumulator bubbles in the loop gas separators. Results suggest that this mass diffusion process in the loop gas separators could be sufficient to keep the coolant helium supersaturation low enough to prevent bubble nucleation elsewhere in loop.

It is possible that small differences in mass flow rates from one loop to another could lead to large differences in He removal rates between separators. This implies that separators should be sufficiently oversized to accept extra gas without saturating, as it is uncertain whether or not each separator will accumulate the same amount of gas over the life of the system.

Should any small bubbles ($<15\mu\text{m}$ in radius) escape the gas separators, they will not experience rapid growth as they circulate in the PHTS. Both mass diffusion and mechanical temperature and pressure effects will tend to collapse small bubbles. Bubble coalescence may cause some growth, but this growth should not be rapid if bubble number density is kept low.

The issues of bubble distribution and gas trapping in the PHTS remain unresolved. Further research is required in these areas.

5. RECOMMENDATIONS FOR FUTURE RESEARCH

Based on the information obtained during this study, the following topics are suggested for future research:

1) An investigation into the contact angle of lithium on niobium at SP-100 operating conditions, both on smooth surfaces and in microcavities. This basic data is required for the determination of nucleation supersaturations and for the determination of forces holding a bubble to a wall surface.

2) An experimental determination of the diffusion coefficient of He in Li. This data is required to verify the predicted gas removal rates at the gas separators.

3) An experimental determination of the values of ψ and α for flowing lithium systems. This information is required to determine nucleation supersaturation requirements in the PHTS.

4) An experiment in a helium saturated, flowing Li loop to verify that helium gas removal by diffusion into an existing gas bubble will preempt helium bubble nucleation at loop wall surfaces.

5) A determination of the maximum stable bubble size in two phase liquid metal turbulent flow. This data would be useful to determine whether or not bubbles are capable of growing to a large size in the PHTS.

6) Analysis of forces acting on a bubble attached to a wall in a flow field, with application to liquid metals. This data would be useful to determine whether or not bubbles will attach themselves to PHTS wall surfaces.

7) Development of liquid metal, two phase, microgravity flow distribution maps at various void fractions and flow channel

geometries. This information would be useful to confirm bubble distributions and flow regimes in the PHTS.

8) A study of bubble coalescence rates in liquid metal systems. This information would enable prediction of the time required for a bubble to reach its maximum stable size in the PHTS.

9) Experimental validation of predicted gas generation rates.

Information obtained from these investigations would serve to confirm the conclusions reached herein, and to further the general understanding of gas bubble behavior in liquid metal coolants.

It is possible that some of the practical issues of bubble behavior in the SP-100 PHTS addressed by this study can be confirmed by the Nuclear Assembly Test (NAT), a full scale test of the SP-100 nuclear subsystem, scheduled for the early 1990's. Although data from the NAT regarding such questions as bubble attachment to PHTS wall surfaces would be valuable from a system design standpoint, a complete understanding of such phenomena will probably require additional basic research.

6. CLOSING

The results of this study provide several items of information which are useful in the design of lithium cooled nuclear power systems in general. The first of these is a capability to estimate the amount of gas radiologically produced in the reactor coolant. This information is needed to properly size the system gas accumulators, predict system end of life pressures, and minimize system mass. Second, the concept of controlling the location of He nucleation by selectively placing a gas bubble in the PHTS is an important principle that could be used to develop a simple, effective gas removal system for the PHTS. Third, the calculation of a minimum bubble size stable against collapse by mass diffusion can be used to determine a lower limit on bubble filter pore sizes (if such a filter is used), minimizing loop pressure drops and pump mass, and reducing concerns regarding filter plugging. Fourth, the identification of areas requiring further investigation provides a starting point for future researchers.

Lithium is a promising coolant for space nuclear power applications. These results obtained herein constitute valuable contributions to the design of lithium cooled reactors in general, and to the SP-100 program in particular. Resolution of outstanding design issues such as the one addressed in this study are important to the application of advanced reactor coolants and the continued development of advanced space power systems.

APPENDIX A - NOMENCLATURE

- a_r - radial acceleration (m/s^2)
- A - energy of gas cluster/liquid solution system (J)
- A_0 - energy of the liquid solution with gas cluster radius equal to 0 (J)
- ΔA - $A - A_0$ (J)
- A_c - cross sectional area (m^2)
- A_s - mass transfer surface area (m^2)
- B - magnetic flux density in the coolant (Tesla)
- C_0 - actual or analytic gas concentration in liquid (kg/m^3)
- C_s - saturation gas concentration in the liquid (kg/m^3)
- ΔC_0 - supersaturation - $C_0 - C_s$ (kg/m^3)
- $\Delta C_0/C_s$ - relative supersaturation (dimensionless)
- C_d - Dimensionless drag coefficient, composed of sum of friction drag coefficient and pressure drag coefficient
- d - bubble diameter (m)
- D - pipe diameter (m)
- D_{eq} - channel equivalent hydraulic diameter (m)
- D_h - channel hydraulic diameter (m)
- D_i - annulus inner diameter (m)
- D_o - annulus outer diameter (m)
- D_{max} - maximum stable bubble diameter (m)

D_v - pressure vessel diameter (m)
 D_{AB} - Diffusion coefficient (m^2/s)
 E_d - activation energy for diffusion of a molecule of gas
 through the liquid (J)
 E_{nom} - base case core average uranium enrichment
 = 0.855 (U^{235} atom % as a decimal fraction)
 E_x - energy of formation for a gas nucleus (J)
 E_o - dimensionless Eotvos Number = $g\Delta\rho d^2/\sigma$
 f_c - bubble coalescence frequency (s^{-1})
 f - Darcy friction factor (dimensionless)
 $f_{con} = (E_{nom})/(W_{nom} \times M_{nom} \times N_A) = 3.523 \times 10^{-32}$ (mole/W-s-kg-atom)
 F_d - drag force (N)
 g - acceleration (m/s^2)
 Ha - dimensionless Hartmann Number = $(x^2 B^2 \sigma_e / \mu)^{0.5}$
 h - Boltzmann's Constant = 6.26×10^{-34} (J/s)
 h_i - initial contact film thickness (m)
 h_f - final contact film thickness at time of rupture (m)
 Ja - dimensionless Jacob Number = $(C_o - C_s)/\rho_g$
 J - nucleation rate (nucleations/s)
 K - Henry's Law Constant (note that $P_L = KC_s$) (m^2/s^2)
 K_L - pressure loss coefficient (dimensionless)
 k - Planck's Constant = 1.38×10^{-23} (J/K)

L - length of channel (m)
 L_{arc} - arc length (m)
 L_v - pressure vessel length (m)
 m - mass (kg)
 m_c - mass of coolant in core (kg)
 m_{lp} - mass of coolant in lower plenum (kg)
 m_p - mass of coolant in piping (kg)
 m_{rp} - mass of coolant in rod plenum (kg)
 m_{up} - mass of coolant in upper plenum (kg)
 M_{nom} - base case total coolant mass = 16.117 (kg)
 n - amount of inert gas (moles)
 n_t - number density of gas molecules (atoms/m³)
 N_A - Avagadro's Number = 0.602252×10^{24} (atoms/mole)
 N_{He} - total helium produced in system (moles)
 Nu_{AB} - dimensionless mass transfer Nusselt number
 (also known as the Sherwood Number)
 N_{BM} - bubble number density based on mean bubble diameter
 (bubbles/m³)
 N_{BO} - bubble number density based on original bubble
 diameter (bubbles/m³)
 Δp - pressure loss in pipe (Pa)
 P - pressure (Pa)
 P_L - liquid pressure (Pa)

P_B - pressure inside gas bubble (Pa)

$P_G = P_g$ - pressure due to inert gas (Pa)

P_v - liquid vapor pressure (Pa)

P_w - flow channel wetted perimeter (m)

P_T - production rate of H^3 (kg/s)

P_{He^3} - production rate of He^3 (kg/s)

P_{He^4} - production rate of He^4 (kg/s)

Pr - dimensionless Prandtl Number

r' - radius of liquid meniscus in the equivalent cavity at
surface pre-conditioning temperature and pressure (m)

r - radius (m)

r_{curv} - radius of curvature (m)

r_{cr} - critical gas cluster radius (m)

r_c - cavity radius (m)

r_a - atomic radius of He (m)

$r(t)$ - bubble radius as a function of time (m)

R_s - radius of surface of contact between the bubbles (m)

R - Universal Gas Constant = 8.31441 (J/mole-K)

Re - dimensionless Reynolds Number = $\rho D_h V / \mu$ for pipes,

= $\rho d V / \mu$ for bubbles

R_0 - initial bubble radius (m)

SS - relative supersaturation = $(C_0 - C_s) / C_s$

Sc - Schmidt Number = $\mu/(\rho D_{AB})$ (dimensionless)

t - time (s)

T - temperature (K)

U_{LS} - superficial liquid velocity (m/s)

U_{GS} - superficial gas velocity (m/s)

V - liquid velocity in the channel (m/s)

V_b - gas bubble volume (m^3)

V_c - coolant volume (m^3)

V_r - radial velocity (m/s)

V_{rt} - radial terminal velocity (m/s)

V_t - tangential velocity (m/s)

w_6 - Li^6 zone production weighted coolant mass (kg)

For coolant enrichment similar to natural lithium:

$$= .6603 \times m_p + 1.1731 \times m_{lp} + 2.5392 \times m_c + .4704 \times m_{rp} + .0792 \times m_{up}$$

For coolant enrichment similar to 0.1 Atom % Li^6 :

$$= .7733 \times m_p + 1.7789 \times m_{lp} + 1.7089 \times m_c + .3112 \times m_{rp} + .0535 \times m_{up}$$

w_7 - Li^7 zone production weighted coolant mass (kg)

For coolant enrichment similar to natural lithium:

$$= .1621 \times m_p + .5974 \times m_{lp} + 3.7442 \times m_c + .4611 \times m_{rp} + .0857 \times m_{up}$$

For coolant enrichment similar 0.1 Atom % Li^6 :

$$= .1680 \times m_p + .6156 \times m_{lp} + 3.7177 \times m_c + .4561 \times m_{rp} + .0849 \times m_{up}$$

W_A - mass transfer rate (kg of He/sec)

W - power (W)

- W_{nom} - base case power level = 2.5×10^6 (W)
 x - characteristic length (m)
 - for an EM pump, commonly the half channel length in the direction of the magnetic field
 x_{max} - maximum radial distance traveled by a bubble (m)
 z_6 - coolant atom % Li^6 (decimal fraction)
 z_{235} - core average uranium enrichment (U^{235} atom % as a decimal fraction)

Greek Letters:

- α - r/r' (dimensionless)
 β - coolant void fraction (decimal fraction)
 γ_6 - system He production rate due to Li^6
 $= 3.777 \times 10^{16} \times z_6^{0.9216}$ (atoms/sec)
 γ_7 - system He production rate due to Li^7
 $= 3.473 \times 10^{14} \times (1 - z_6)^{0.9800}$ (atoms/sec)
 δ - a dimensionless constant in the range of 10 to 30.
 ϵ - power dissipated per unit volume of liquid (m^2/s^3)
 ϵ_s - flow channel surface roughness (m)
 ξ - dimensionless trapped gas parameter = $P_g r_c / 2\sigma$
 θ - two phase holdup time (s)
 - (volume occupied by two phase mixture at time t) /
 (volumetric flow rate of two phase mixture)

λ - half life (s^{-1})
 μ - dynamic viscosity (kg/m-s)
 μ_L - liquid dynamic viscosity (kg/m-s)
 ρ - density (kg/m^3)
 ρ_g - gas density in bubble (kg/m^3)
 ρ_L - liquid density (kg/m^3)
 $\Delta\rho = \rho_L - \rho_g$ (kg/m^3)
 σ - liquid surface tension (N/m),
 σ_e - coolant electrical conductivity ($ohm^{-1} m^{-1}$)
 ψ - a measure of inert gas concentration in the
nucleation cavity, a function of cavity geometry,
contact angle, and moles of gas in the cavity (m-N/K)

APPENDIX B - TWODANT CALCULATION DETAILS

B.1 Calculation Flow Path

A total of 17 separate TWODANT runs were executed in support of the program of analysis. Table 23 summarizes these runs.

The first run was a master flux calculation to converge the flux for the base case. The flux from this run was used in the following six runs to analyze the base case results, producing He production, fission power, and flux edits in various formats.

Each of the following ten runs was a complete calculation, with a separate flux converged for each case. These runs included a two case sensitivity analysis involving S_n order and mesh spacing, a five case parametric analysis involving coolant void fraction and Li^6 content, a run utilizing un-selfshielded cross sections, and finally, two runs using collapsed cross section sets.

Table 23 - TWODANT Run Identification Chart

Case# 1 Run ID 013210 031388

Master flux run for base case. 80 group flux and fission spectrum at one representative edit point in each of the 5 edit zones.

Case# 1a Run ID 153524 032488

80 group helium production spectrum by reaction, at one representative point in each of the 5 edit zones. Case 1 flux, edit run only.

Case# 1b Run ID 163024 032488

1 group radial fission and flux profile at core center. Case 1 flux, edit run only.

Case #1c Run ID 171753 032488

1 group axial fission and flux profile at core center. Case 1 flux, edit run only.

Case# 1d Run ID 170646 032588, and Run ID 202716 032388

1 group helium production edits. Zone total production for 5 edit zones. Case 1 flux, edit run only. Run ID 202716 032388 is a repeat calculation which includes edits for total system only.

Case# 1e Run ID 175044 032588

1 group helium production edits. Zone total production for 270 edit zones defined by coarse mesh. Case 1 flux, edit run only.

Case# 2 Run ID 200436 032388

Sensitivity study. Identical to Case 1 except that S_n order was increased from S_4 to S_8 .

Case# 3 Run ID 232950 032388

Sensitivity study. Identical to Case 1 except that radial fine mesh spacing was decreased from 1 cm to 0.5 cm.

Case# 4 Run ID 173153 032488

Parametric study. Identical to Case 1 except that coolant composition was changed to 1.0 a/o Li^6 .

Case #5 Run ID 173211 032488

Parametric study. Identical to Case 1 except that coolant composition was changed to 0.1 a/o Li^6 .

Case #6 Run ID 184006 032488

Parametric study. Identical to Case 1 except that coolant composition was changed to 0.01 a/o Li^6 .

Case #7 Run ID 223855 032488

Parametric study. Identical to Case 1 except that coolant atom density was reduced by 10%.

Case #8 Run ID 224827 032488

Parametric study. Identical to Case 1 except that coolant atom density was reduced by 30%.

Case #9 Run ID 133439 060388

Identical to Case #5 except that a 80 group unselfshielded cross section set was used.

Case #10 Run ID 161637 053188

Identical to Case #4 except that a 20 group cross section set developed using Case #4 fluxes was used.

Case #11 Run ID 101922 060788

Identical to Case #4 except that a 1 group unselfshielded cross section set developed using Case #4 fluxes was used.

B.2 TWODANT Sample Input Deck

```

      2      0      0
SP-100 COOLANT GAS GENERATION MODEL - VERSION 1.1   7 MAR 88
NATURAL LITHIUM, 0% VOID FRACTION, BASE CASE, CASE# 1
/
/ ***** block 1 - controls and dimensions *****
/
igeom=    7           / r/z geometry
ngroup=   80          / # of neutron groups
isn=      4           / Sn order
niso=    19           / # of isotopes on xslib
mt=       9           / # of materials
nzone=   44           / # of model zones
im=      15           / # of r coarse mesh intervals
it=      44           / # of r fine mesh intervals
jm=      18           / # of z coarse mesh intervals
jt=     102           / # of z fine mesh intervals
maxlcm= 2500000       / maximum large core memory
maxscm=  70000        / maximum small core memory
nogeod=   0           / 1 - suppress geometry input
nomix=    0           / 1 - suppress mixing input
noasg=    0           / 1 - suppress material zone asnmnt input
nomacr=   0           / 1 - suppress generation of macroscopic xs
noslnp=   0           / 1 - suppress generation of solver input
nosolv=   0           / 1 - suppress solver module
noedit=   0           / 1 - suppress edit module
t
/
/ ***** block 2 - geometry details *****
/
/ r coarse mesh point coordinates (cm):
xmesh=  0 3 8 14 15 17 18 21 23 26 28 30 33 35 40 44
/ z coarse mesh point coordinates (cm):
ymesh=  0 7 9 12 16 20 21 23 28 40 52 57 60 70 77 79 80 94 95
/ fine intervals per r coarse interval:
xints=  3 5 6 1 2 1 3 2 3 2 2 3 2 5 4
/ fine intervals per z coarse interval:
yints=  7 2 3 4 4 1 2 5 12 12 5 3 10 14 2 1 14 1
/ zone number for each coarse mesh interval:
zones=
      1  1  1  1  1  1  1  1  1  1  1  1  1  1  9 ;
      2  3  4  5  5  5  0  0  0  0  0  0  0  0  9 ;
      2 10 13 11 11 12 0  0  0  0  0  0  0  0  9 ;
      2 10 13 11 11 12 6  6  6  7  7  7  8  8  9 ;
      2 10 13 11 11 12 0  0  0  0  0  0  0  0  0 ;
      15 10 14 11 11 12 0  0  0  0  0  0  0  0  0 ;
      15 16 17 18 18 18 0  0  0  0  27 27 27 0  0 ;
      15 19 22 25 25 26 0  0  0  0  27 27 27 0  0 ;
      15 20 23 25 25 26 0  0  0  28 28 28 0  0  0 ;
      15 20 23 25 25 26 0  0  29 29 29 0  0  0  0 ;
      15 21 24 25 25 26 0  30 30 30 0  0  0  0  0 ;
      15 32 33 35 35 26 0  30 30 30 0  0  0  0  0 ;
      31 32 34 35 35 26 0  0  0  0  0  0  0  0  0 ;
      31 32 34 35 35 26 0  0  0  0  0  0  0  0  0 ;
      31 36 37 38 38 38 0  0  0  0  0  0  0  0  0 ;
      31 39 40 41 41 41 0  0  0  0  0  0  0  0  0 ;

```

```

      31 39 40 41 0 0 0 0 0 0 0 0 0 0 0 ;
      42 43 44 41 0 0 0 0 0 0 0 0 0 0 0 ;

t
/
/ ***** block 3 - nuclear data *****
/
lib=      bxslib      / cross section source
maxord=   1           / highest legendre order
ihm=      93          / rows in xs table
iht=      13          / total xs row #
lhs=      14          / self scatter xs row #
ifido=    1           / fixed field fido xs format
ititl=    1           / title cards included
i2lpl=    0           / no 2l + 1 term
savbxs=   0           / 1 = save binary xs file
kwikrd=   0           / 1 = no error checking
/ names for each isotope:
names=    he4 li6 li7 be9 b10 b11 cnatc cnatr nnat onat zrnat nb93
          ta181 renat u235 u238 lecore beo b4c
/ names for each edit xs:
edname=    n2na6 nnd6 nt6 np6 n2na7 ncnt7 ng7 nd7 n-fiss chivec
/ chi vector:
chivec=
          4.748e-06  1.981e-05  5.387e-05  2.042e-04  1.009e-03
          5.859e-03  1.874e-02  4.195e-02  7.127e-02  9.790e-02
          1.143e-01  1.178e-01  1.104e-01  5.018e-02  4.625e-02
          4.207e-02  3.791e-02  3.383e-02  2.980e-02  2.619e-02
          2.287e-02  1.980e-02  1.707e-02  2.717e-02  1.973e-02
          1.416e-02  1.013e-02  7.130e-03  5.016e-03  3.493e-03
          2.412e-03  1.662e-03  1.144e-03  4.299e-04  2.218e-04
          1.346e-04  2.955e-04  2.449e-04  2.030e-04  1.682e-04
          1.394e-04  1.155e-04  9.572e-05  7.932e-05  6.572e-05
          5.446e-05  4.512e-05  3.739e-05  3.098e-05  2.567e-05
          2.127e-05  1.762e-05  1.460e-05  1.210e-05  1.003e-05
          8.308e-06  6.884e-06  5.705e-06  4.727e-06  3.917e-06
          3.246e-06  2.690e-06  4.076e-06  2.799e-06  1.922e-06
          1.320e-06  9.064e-07  1.049e-06  4.943e-07  2.326e-07
          1.108e-07  5.233e-08  2.471e-08  1.167e-08  5.503e-09
          2.481e-09  1.722e-09  3.841e-10  8.570e-11  2.461e-11

t
/
/ ***** block 4 - mixing details *****
/
/ atom densities (atoms/b-cm) for each material:
matls=
          fuel97      u238 .0009564 u235 .0309236 nnat .0318800;
          fuel73      u238 .0086080 u235 .0232720 nnat .0318800;
          liner       renat .0659500
          ;
          pwcll       nb93 .0525700 zrnat .0005414 cnatc .0004112;
          b4c         b10 .0684000 b11 .0076000 cnatr .0190000;
          beo         be9 .0676600 onat .0676600
          ;
          be          be9 .1196000
          ;
          baffle      ta181 .0540800
          ;
          clnt        li6 .0027500 li7 .0343100
          ;
/ material volume fractions in each zone:
assign=
          1 be      1.00;

```

```

2  pwc11 .190;
3  pwc11 1.00;
4  pwc11 .838;
5  pwc11 1.00;
6  pwc11 .028 clnt .245;
7  pwc11 .020 clnt .183;
8  pwc11 .016 clnt .148;
9  pwc11 .012 clnt .128;
10 clnt 1.0;
11 clnt 1.0;
12 pwc11 .350 clnt .650;
13 pwc11 .038 clnt .800;
14 pwc11 .038 clnt .800 beo .134;
15 pwc11 .190 beo .670;
16 pwc11 .800 clnt .200;
17 pwc11 .678 clnt .160 beo .134;
18 pwc11 .800 clnt .200;
19 pwc11 .129 clnt .176 fuel97 .594 liner .066;
20 pwc11 .129 clnt .176 fuel73 .594 liner .066;
21 pwc11 .129 clnt .176 fuel97 .594 liner .066;
22 pwc11 .141 clnt .141 fuel97 .475 liner .053 beo .134;
23 pwc11 .141 clnt .141 fuel73 .475 liner .053 beo .134;
24 pwc11 .141 clnt .141 fuel97 .475 liner .053 beo .134;
25 pwc11 .129 clnt .176 fuel97 .594 liner .066;
26 pwc11 .250 clnt .650 baffle .100;
27 beo .683;
28 beo .729;
29 beo .811;
30 beo .878;
31 pwc11 .190 b4c .670;
32 pwc11 .129 clnt .176 liner .107;
33 pwc11 .141 clnt .141 liner .086 beo .134;
34 pwc11 .141 clnt .141 liner .086 b4c .134;
35 pwc11 .129 clnt .176 liner .107;
36 pwc11 .200 clnt .800;
37 pwc11 .678 clnt .160 b4c .134;
38 pwc11 .200 clnt .800;
39 clnt 1.0;
40 pwc11 .038 clnt .800 b4c .134;
41 pwc11 .250 clnt .750;
42 pwc11 .500 clnt .500;
43 pwc11 .250 clnt .750;
44 pwc11 .500 clnt .500;

```

```

/
/  ***** block 5 - solver details *****
/
ievt=    1          / eigenvalue search
isct=    1          / scattering legendre order
ith=     0          / direct calculation
ibl=     1          / left boundary reflective
ibr=     0          / right boundary vacuum
ibt=     0          / top boundary vacuum
ibb=     0          / bottom boundary vacuum
epsi=    .0001      / convergence precision
fluxp=   0          / 1 = print isotropic flux
norm=    0          / fission source normalized to this value

```

```

influx= 0 / 1 - read starting flux from rtflux file
iitm= 150 / inner iteration limit
t
/
/ ***** block 6 - edit input details *****
/
/ spectrum data
/
/pted= 1 / edit by mesh point
/zned= 0 / no zone edits
/
/ edit location: lwr plnum, lecore, hecore, rod plenum, upr plnum
/ fine intr (r,z): (11,14), (6,40), (16,40), (11,70), (9,94)
/ edit points = r+44(z-1):
/points= 583 1722 1732 3047 4101
/
/edxs= n-fiss / edit cross section
/resdnt= 1 / use edit xs resident macro xs
/igrped= 3 / print broad groups and totals
/power= 2.5 / normalize output to this value (mw)
/mevper= 200.0 / mev per fission
/rsfe= 80r 1.0 ; / response function multiplier
/rsfnam= nflux / response function identifier
/
/ ***** he gas production data
/
pted= 0 / no point edits
zned= 1 / edit by zone
/ collapse results to six zones:
/ zone 1= piping, zone 2= lower plenum, zone 3= core
/ zone 4=rod plenum, zone 5=upper plenum, zone 6=non-coolant zones
edzone=
40r 6 4r 1 ; 8y 1 ;
3r 6 15r 2 22r 6 4r 1 ; 2y 1 ;
3r 6 15r 2 26r 1 ; 3y 1 ;
3r 6 15r 2 26r 6 ; 6y 1 ;
3r 6 15r 3 26r 6 ; 33y 1 ;
3r 6 15r 4 26r 6 ; 28y 1 ;
3r 6 15r 5 26r 6 ;
3r 6 12r 5 29r 6 ; 13y 1 ;
15r 5 29r 6 ;
rsfe= 80r 1.0 ; / response function multiplier
rsfnam= nflux / response function identifier
icoll= 80 / collapse output to 1 group
power= 2.5 / normalize power to this value (mw)
mevper= 200.0 / mev per fission
edxs= n2na6 nnd6 nt6 n2na7 ncnt7 ng7 / edit xs for output
edcons= li6 li7 / isotopes for output
/byvolp= 1 /multiply reaction rates by zone volume
/micsum= li6 0 n2na6 nnd6 nt6
/ li7 0 n2na7 ncnt7 ng7 0
/ li6 li7 0 n2na6 nnd6 nt6 n2na7 ncnt7 ng7
t

```

APPENDIX C - TRANSX CALCULATION DETAILS

C.1 Description of TRANSX Calculations

For purposes of analysis of the SP-100 system, a cross section set with the following characteristics was desired:

- 1) Fast reactor group structure,
 - a. fast flux weighting function,
 - b. group boundaries appropriate for fast reactor calculations,
- 2) approximately 50 to 100 groups,
- 3) resonance self shielding data available,
- 4) doppler broadening data available,
- 5) cross sections available for materials of interest, and,
- 6) Li gas production cross sections available.

A review of several cross section sets revealed that a multigroup cross section library known as MATXS6s met the majority of the above requirements. MATXS6s was created from ENDF/B-V data at Los Alamos National Laboratory in February, 1985, using the MATXS module of the NJOY cross section processing system. It is a coupled neutron-gamma 80x24 group library intended for fast reactor analysis, containing cross sections for 104 separate materials. Cross section data for these materials are tabulated at temperatures ranging from 0 to 4,000 K. Additionally, self shielded cross sections computed using the Bondarenko method are tabulated for many of the 104 materials, with background cross sections ranging from 10^{10} to 0.1 barns. These materials include all of the major isotopes composing the SP-100 system. Also, all significant He production reactions are available for both Li^7 and Li^6 .

Although MATXS6s contains all of the appropriate data for the required calculation, the format of this library is in general not acceptable for use by transport codes. To produce a cross section library with an appropriate format, the MATXS6s master library was modified with the cross section processing code TRANSX. These modifications are discussed below.

Processing with TRANSX had a three-fold purpose:

- 1) Reduction of the general cross section data in the MATXS6s library to a SP-100 specific cross section set,
- 2) Production of Li gas production edit cross sections, and,
- 3) Ordering of the resulting data in a format suitable for use by the S_n code TWODANT.

Regarding the production of a SP-100 specific cross section set, three operations were performed with TRANSX. First, gamma groups were stripped away, as these were judged to have a negligible effect on the gas production calculations. Second, 18 isotopes identified as the major constituents of the SP-100 system were selected for further processing. These 18 isotopes are listed in Table 24. The remaining 86 materials were eliminated. Third, data on the remaining 18 isotopes was further reduced by interpolating the MATXS6s data to the expected SP-100 operating temperatures and self shielding parameters.

For temperature (Doppler Broadening) interpolation TRANSX requires only the operating temperature of the region in which the isotope will reside. However, interpolation of self shielding parameters requires the input of the geometry and composition of the region surrounding the isotope in question. Strictly speaking, a different cross section set could be defined for each point in the reactor, since geometry and composition are generally constantly changing functions of position.

In order to reduce the number of cross sections to be handled to a reasonable number, the reactor was assumed to consist of three independent, homogeneous regions: 1) a cylindrical 73% enriched core region with a 18cm radius, 2) a cylindrical BeO reflector region with a 7cm radius, and, 3) a cylindrical B₄C rod region with a 3cm radius. Using these geometries and compositions, TRANSX calculated background cross sections for each isotope in each region and selected appropriately self shielded cross section values by interpolating MATXS6s tabular data. A summary of the region composition calculations is presented in Table 26. With these operations completed, the initial cross section set was reduced to one appropriate specifically to the SP-100 system.

The second purpose of the TRANSX calculations was to produce edit cross sections to extract fission power and He gas production data. Nine edit cross sections and a chi vector were produced. Chi, the group-wise fission spectrum, was produced for direct input into TWODANT. A fission edit cross section containing all tabulated fission modes for both U²³⁵ and U²³⁸ was created to enable flux normalization to desired power levels. Finally eight edit cross sections for He gas production for Li⁶ and Li⁷ were created. Six of these required no special treatment, and were extracted directly from the MATXS6s file. However, the Li⁶ (n,nd) reaction resided on the MATXS6s file as cross sections for the excitation of 30 separate energy levels. These 30 separate reactions were summed to produce a total Li⁶ (n,nd) edit cross section. Also, since the Li⁷ (n,γ) reaction produces two He atoms per reaction, this cross section was doubled for output as a He production edit.

Finally, TRANSX was utilized to format the SP-100 specific cross sections generated as outlined above in a format suitable for use by TWODANT. Cross section angular data is stored in MATXS6s in the form of Legendre tables. TWODANT is a discrete ordinates code that requires a "transport correction" to the Legendre data. Several formulas are available; all converge to the same answer if enough tables are retained. However, studies have shown that some corrections produce better results than others when only a few tables are utilized (Bell, et al., 1967). One of the more popular corrections, the diagonal transport correction, was applied by TRANSX and the Legendre tables were terminated at P_1 . The resulting cross section set was then written to an output file in a fixed field FIDO format acceptable to TWODANT.

Although TRANSX has the capability to collapse multigroup cross section data into larger groups, this capability was not utilized in this calculation. The original 80 group structure of the MATXS6s file was retained to minimize the errors resulting from group collapse using a model flux. The flux model used in the preparation of the MATXS6s file is presented in Figure 70. MATXS6s energy group bounds are presented in Table 25.

The resulting cross sections are considered to be reliable for analysis of a fast system like the SP-100. However, the combination of fast flux weighting, Bondarenko self shielding, and coarse, survey type thermal group structure makes results for the thermal groups somewhat questionable. Results from the thermal groups should be considered order of magnitude estimates only. The cross sections developed are definitely inappropriate for a reactor with a significant thermal flux component.

Figure 70 - MATXS6s Model Flux

(MacFarlane, 1978)

MATXS6s Library Weighting Flux

Table 24 - Selected Isotopes from MATXS6s

Isotope	MATXS6s Material Number	Number of Submaterials Available	Temperature Range - K	Range of Background Cross Section-b
He ⁴	5	1	300	1.0x10 ¹⁰
Li ⁶	6	1	300	1.0x10 ¹⁰
Li ⁷	7	1	300	1.0x10 ¹⁰
Be ⁹	9	1	300	1.0x10 ¹⁰
B ¹⁰	10	1	300	1.0x10 ¹⁰
B ¹¹	11	1	300	1.0x10 ¹⁰
C ¹²	12	6	300	1.0x10 ¹⁰ - 0.1
N ¹⁴	13	6	300	1.0x10 ¹⁰ - 1.0
N ¹⁵	14	1	300	1.0x10 ¹⁰
O ¹⁶	15	6	300	1.0x10 ¹⁰ - 0.1
O ¹⁷	16	1	300	1.0x10 ¹⁰
Zr	36	1	300	1.0x10 ¹⁰
Nb ⁹³	37	20	300 - 1200	1.0x10 ¹⁰ - 10.0
Ta ¹⁸¹	46	20	300 - 1200	1.0x10 ¹⁰ - 10.0
Re ¹⁸⁵	51	20	300 - 2100	1.0x10 ¹⁰ - 100.0
Re ¹⁸⁷	52	20	300 - 2100	1.0x10 ¹⁰ - 100.0
U ²³⁵	62	36	300 - 4000	1.0x10 ¹⁰ - 10.0
U ²³⁸	64	42	300 - 4000	1.0x10 ¹⁰ - 1.0

Note: Isotopes with no superscript are natural composition.

Table 25 - MATXS6s Energy Group Structure
(Group Number - Lower Energy (eV))

1	1.690E+07	17	8.208E+05	33	3.183E+04	49	4.881E+03	65	4.540E+02
2	1.492E+07	18	7.244E+05	34	2.809E+04	50	4.307E+03	66	3.536E+02
3	1.350E+07	19	6.393E+05	35	2.606E+04	51	3.801E+03	67	2.754E+02
4	1.191E+07	20	5.642E+05	36	2.479E+04	52	3.355E+03	68	1.670E+02
5	1.000E+07	21	4.979E+05	37	2.188E+04	53	2.960E+03	69	1.013E+02
6	7.788E+06	22	4.394E+05	38	1.930E+04	54	2.613E+03	70	6.144E+01
7	6.065E+06	23	3.877E+05	39	1.704E+04	55	2.306E+03	71	3.727E+01
8	4.724E+06	24	3.020E+05	40	1.503E+04	56	2.035E+03	72	2.260E+01
9	3.679E+06	25	2.352E+05	41	1.237E+04	57	1.796E+03	73	1.371E+01
10	2.865E+06	26	1.832E+05	42	1.171E+04	58	1.585E+03	74	8.315E+00
11	2.231E+06	27	1.426E+05	43	1.033E+04	59	1.398E+03	75	5.044E+00
12	1.738E+06	28	1.111E+05	44	9.119E+03	60	1.234E+03	76	3.059E+00
13	1.353E+06	29	8.652E+04	45	8.047E+03	61	1.089E+03	77	1.125E+00
14	1.194E+06	30	6.738E+04	46	7.102E+03	62	9.611E+02	78	4.140E-01
15	1.054E+06	31	5.248E+04	47	6.267E+03	63	7.485E+02	79	1.523E-01
16	9.301E+05	32	4.087E+04	48	5.531E+03	64	5.830E+02	80	1.389E-04

Table 26 - TRANSX Self-Shielding Region Compositions

Isotope	Natural Atom Density (Atoms/b-cm)	Region Volume Fraction	Region Atom Density (Atoms/b-cm)
<u>1. Core Region</u>			
73 % Enriched UN Fuel		0.590	
²³⁵ U	0.0232700		0.01373000
²³⁸ U	0.0086100		0.00508000
¹⁴ N	0.0317600		0.01874000
¹⁵ N	0.0001200		0.00007080
Natural Li Coolant		0.180	
⁶ Li	0.0027800		0.00050040
⁷ Li	0.0342800		0.00617000
PWC-11 Clad		0.130	
⁹³ Nb	0.0525700		0.00683400
Zr	0.0005414		0.00007038
C	0.0004112		0.00005346
Rhenium Liner		0.065	
¹⁸⁵ Re	0.0244500		0.00158900
¹⁸⁷ Re	0.0415000		0.00269800
Void Gap	0.0000000	0.035	0.00000000
<u>2. BeO Reflector Region</u>			
BeO		1.000	
⁹ Be	0.067660		0.06766000
¹⁶ O	0.067640		0.06764000
¹⁷ O	0.000023		0.00002300
<u>3. B₄C Rod Region</u>			
B ₄ C		1.000	
¹⁰ B	0.068400		0.06840000
¹¹ B	0.007600		0.00760000
C	0.019000		0.01900000

Note: See Appendix A for derivation of natural atom densities and material volume fractions.

C.2 Sample TRANSX Input Deck

```

sp-100 - 80 grp lib, li gas prod edits, 1400k, homo self shielding
*matxs6* *xslib*/
0 3 0 1 1 1 1 2 0 0
80 0 2 93 0 19 4 36 10 39
*he4* *li6* *li7* *be9* *b10* *b11* *cnat* *cnatr* *nnat* *onat*
*zrnat* *nb93* *ta181* *renat* *u235* *u238* *lecore* *beo* *b4c*
*core* 1400.0 1.0 1 0.0 36.00/
*reflc* 1000.0 1.0 1 0.0 14.00/
*rod* 1300.0 1.0 1 0.0 6.00/
* * 1300.0/
17 1 *li6* 0.50040e-03 0/
17 1 *li7* 6.17000e-03 0/
17 1 *cnat* 0.05346e-03 0/
17 1 *nl4* 18.7400e-03 0/
17 1 *nl5* 0.07080e-03 0/
17 1 *zrnat* 0.07038e-03 0/
17 1 *nb93* 6.83400e-03 0/
17 1 *re185* 1.58900e-03 0/
17 1 *re187* 2.69800e-03 0/
17 1 *u235* 13.7300e-03 0/
17 1 *u238* 5.08000e-03 0/
18 2 *be9* 67.6600e-03 0/
18 2 *ol6* 67.6400e-03 0/
18 2 *ol7* 0.02300e-03 0/
19 3 *b10* 68.4000e-03 0/
19 3 *b11* 7.60000e-03 0/
19 3 *cnat* 19.0000e-03 0/
1 4 *he4*/
2 4 *li6*/
3 4 *li7*/
4 4 *be9*/
5 4 *b10*/
6 4 *b11*/
7 1 *cnat* 1.0 -3/
8 3 *cnat* 1.0 -17/
9 1 *nl4* .9963 -4/
9 1 *nl5* .0037 -5/
10 2 *ol6* .99963 -13/
10 2 *ol7* .00037 -14/
11 4 *zrnat*/
12 1 *nb93* 1.0 -7/
13 4 *ta181*/
14 1 *re185* 0.3707 -8/
14 1 *re187* 0.6293 -9/
15 1 *u235* 1.0 -10/
16 1 *u238* 1.0 -11/
*n2na6* *nnd6* *nt6* *np6* *n2na7* *ncnt7* *ng7* *nd7* *nfiss* *chi*/
1 *n2na* 1.0 *li6*/
2 *n51d* 1.0 *li6*/
2 *n52d* 1.0 *li6*/
2 *n53d* 1.0 *li6*/
2 *n54d* 1.0 *li6*/
2 *n55d* 1.0 *li6*/
2 *n56d* 1.0 *li6*/
2 *n58d* 1.0 *li6*/

```

```

2  *n59d* 1.0 *li6*/
2  *n60d* 1.0 *li6*/
2  *n61d* 1.0 *li6*/
2  *n62d* 1.0 *li6*/
2  *n63d* 1.0 *li6*/
2  *n64d* 1.0 *li6*/
2  *n65d* 1.0 *li6*/
2  *n66d* 1.0 *li6*/
2  *n67d* 1.0 *li6*/
2  *n68d* 1.0 *li6*/
2  *n69d* 1.0 *li6*/
2  *n70d* 1.0 *li6*/
2  *n71d* 1.0 *li6*/
2  *n72d* 1.0 *li6*/
2  *n73d* 1.0 *li6*/
2  *n74d* 1.0 *li6*/
2  *n75d* 1.0 *li6*/
2  *n76d* 1.0 *li6*/
2  *n77d* 1.0 *li6*/
2  *n78d* 1.0 *li6*/
2  *n79d* 1.0 *li6*/
2  *n80d* 1.0 *li6*/
2  *n81d* 1.0 *li6*/
3  *nt* 1.0 *li6*/
4  *np* 1.0 *li6*/
5  *n2na* 1.0 *li7*/
6  *ncnt* 1.0 *li7*/
7  *ng* 2.0 *li7*/
8  *nd* 1.0 *li7*/
9  *nftot*/
10 *chi*/
*stop*/

```

APPENDIX D - FORTRAN HELIUM PRODUCTION CORRELATION

Program CORR Listing:

C234567

```
C*****
C
C   CORR -   SP-100 COOLANT HELIUM PRODUCTION CORRELATION
C
C*****
C
C   DECLARATIONS
C
C   IMPLICIT REAL(A-H,O-Z)
C   CHARACTER*40 CASE
C   OPEN(UNIT=7,FILE='CORR.OUT',STATUS='NEW')
C   OPEN(UNIT=8,FILE='CORR.DAT',STATUS='OLD')
C
C
C   USER INPUT DATA
C
C   READ(8,*)CASE
C   READ(8,*)ALPHA
C   READ(8,*)Z
C   READ(8,*)IWT
C   READ(8,*)XMP,XMLP,XMC,XMRP,XMUP
C   READ(8,*)ZU235
C   READ(8,*)POW
C   READ(8,*)TIME
C   READ(8,*)TEMP
C   READ(8,*)PRES
C
C
C   CALCULATE HELIUM PRODUCTION
C
C   CALL HECORR(ALPHA,Z,IWT,XMP,XMLP,XMC,XMRP,XMUP,ZU235,POW,
C   $           TIME,HE6,HE7,HETOT)
C
C   CALCULATE VOLUME OCCUPIED BY HELIUM
C
C   CALL VOL(TEMP,PRES,HETOT,V)
C
C   CALCULATE TOTAL COOLANT MASS
C
C   XMT=XMP+XMLP+XMC+XMRP+XMUP
C
C   WRITE RESULTS TO OUTPUT FILE
C
C   WRITE(7,*)
C   WRITE(7,*)
C   WRITE(7,*)
C   WRITE(7,*)'   ***** SP-100 COOLANT HELIUM PRODUCTION *****'
C   WRITE(7,*)
C   WRITE(7,*)
C   WRITE(7,*)'           CASE TITLE: ',CASE
C   WRITE(7,*)
C   WRITE(7,*)'           ** INPUT DATA **'
C   WRITE(7,*)
```



```

C
C   FOR FURTHER INFORMATION CONTACT: INSTITUTE FOR SPACE NUCLEAR
C   POWER STUDIES, DEPARTMENT OF CHEMICAL AND NUCLEAR
C   ENGINEERING, UNIVERSITY OF NEW MEXICO, ALBUQUERQUE, NM,
C   87131; REFERENCE: PHD DISSERTATION, JOHN MCGHEE, MAY 1989.
C
C   INPUT DATA:
C
C   ALPHA - COOLANT VOID FRACTION (DECIMAL PERCENT)
C   Z      - COOLANT LI6 ENRICHMENT (DECIMAL ATOM PERCENT)
C   IWT    - FLAG TO SELECT COOLANT WEIGHTING STRATEGY
C           - 1 FOR HIGH ENRICHMENT LI6 COOLANT (> 1.0 a/o)
C           - 2 FOR LOW ENRICHMENT LI6 COOLANT (< 1.0 a/o)
C   XMP    - COOLANT MASS IN REACTOR INLET/OUTLET PIPING ZONE
C           (KG)
C   XMLP   - COOLANT MASS IN REACTOR LOWER PLENUM ZONE (KG)
C   XMC    - COOLANT MASS IN REACTOR CORE ZONE (KG)
C   XMRP   - COOLANT MASS IN FUEL ROD FISSION GAS PLENUM ZONE
C           (KG)
C   XMUP   - COOLANT MASS IN REACTOR UPPER PLENUM ZONE (KG)
C   Z235   - CORE AVERAGE U235 ENRICHMENT (DECIMAL ATOM PERCENT)
C   POW    - REACTOR THERMAL OPERATING POWER (MW)
C   TIME   - REACTOR OPERATING TIME (YEARS)
C
C   OUTPUT DATA:
C
C   HE6    - ON RETURN, HELIUM PRODUCED BY LI6 (MOLES)
C   HE7    - ON RETURN, HELIUM PRODUCED BY LI7 (MOLES)
C   HETOT  - ON RETURN, TOTAL HELIUM PRODUCED (MOLES)
C
C   SUBROUTINE HECORR(ALPHA,Z,IWT,XMP,XMLP,XMC,XMRP,XMUP,ZU235,
$   POW,TIME,HE6,HE7,HETOT)
C
C   DECLARATIONS
C
C   IMPLICIT REAL(A-H,O-Z)
C
C   CONSTANTS
C
C   PNOM=2.5
C   XMTNOM=16.117
C   Z25NOM=0.855
C   XNA=.602252E24
C   SPY=3.1557E7
C   F=(SPY*Z25NOM)/(XNA*PNOM*XMTNOM)
C
C   WEIGHT COOLANT MASSES FOR LOCATION IN SYSTEM
C
C   IF(IWT.EQ.2)THEN
C     W6=.7733*XMP+1.7789*XMLP+1.7089*XMC+.3112*XMRP+.0535*XMUP
C     W7=.1680*XMP+0.6156*XMLP+3.7177*XMC+.4561*XMRP+.0849*XMUP
C   ELSE
C     W6=.6603*XMP+1.1731*XMLP+2.5392*XMC+.4704*XMRP+.0792*XMUP
C     W7=.1621*XMP+0.5974*XMLP+3.7442*XMC+.4611*XMRP+.0857*XMUP
C   ENDIF
C
C   CALCULATE PRODUCTION RATES BY ISOTOPE

```



```

C
    PLI6=W6*3.77670E16*Z**.92161
    PLI7=W7*3.47321E14*(1.0-Z)**.98001
    VFAC=(1-ALPHA)**.90458
C
C    CALCULATE TOTAL HELIUM PRODUCED
C
    HE6=F*TIME*POW*VFAC*PLI6/ZU235
    HE7=F*TIME*POW*VFAC*PLI7/ZU235
    HETOT=HE6+HE7
C
C    GO BACK
C
    RETURN
    END
C
C*****
C
C    VOL - A SUBROUTINE TO CALCULATE THE VOLUME OCCUPIED BY A
C    GAS.
C    IDEAL GAS BEHAVIOR IS ASSUMED.
C
C    INPUT DATA:
C
C    TEMP - TEMPERATURE OF THE GAS (K)
C    PRES - PRESSURE OF THE GAS (PA)
C    X    - QUANTITY OF GAS (MOLES)
C
C    OUTPUT DATA:
C
C    V - ON RETURN, VOLUME THAT X MOLES OF GAS OCCUPIES AT
C    TEMPERATURE TEMP AND PRESSURE PRES (LITERS)
C
C
C    SUBROUTINE VOL(TEMP,PRES,X,V)
C
C    DECLARATIONS
C
C    IMPLICIT REAL(A-H,O-Z)
C
C    CONSTANTS
C
C    R=8.3144
C
C    CALCULATE GAS VOLUME
C
C    V=1.0E03*X*R*TEMP/PRES
C
C    GO BACK
C
C    RETURN
C    END
C

```

Program CORR - Sample Input Deck:

'BASE CASE GEOMETRY'

0.0

.001

2

2.197,4.608,3.106,2.258,3.948

0.855

2.5

7.3

1375.0

151.7e03

Program CORR - Sample Output:

***** SP-100 COOLANT HELIUM PRODUCTION *****

CASE TITLE: BASE CASE GEOMETRY

** INPUT DATA **

REACTOR:

POWER	:	2.500000	MW
OPERATING TIME	:	7.300000	YEARS
AVG U235 ENRICHMENT	:	85.50000	ATOM %

PRIMARY SYSTEM:

PRESSURE	:	151700.0	PA
TEMPERATURE	:	1375.000	K

COOLANT:

WEIGHT STRATEGY	:	LOW ENRICHMENT	LI6
LI 6 ENRICHMENT	:	0.1000000	ATOM %
VOID FRACTION	:	0.0000000E+00	%
MASS IN PIPING	:	2.197000	KG
MASS IN LOWER PLENUM	:	4.608000	KG
MASS IN CORE	:	3.106000	KG
MASS IN ROD PLENUM	:	2.258000	KG
MASS IN UPPER PLENUM	:	3.948000	KG
TOTAL MASS	:	16.11700	KG

** OUTPUT DATA **

HELIUM PRODUCTION:

FROM LI6	:	2.4828250E-02	MOLES
	:	9.9387474E-02	GRAMS

FROM LI7	:	0.1327312	MOLES
	:	0.5313229	GRAMS

TOTAL	:	0.1575594	MOLES
	:	0.6307104	GRAMS

TOTAL HELIUM VOLUME
AT INPUT SYSTEM

PRESSURE AND TEMP	:	11.87387	LITERS
-------------------	---	----------	--------

APPENDIX E - He^3 PRODUCTION FROM DISSOLVED TRITIUM

E.1 Simplified Diffusion Analysis

Tritium radiologically produced in the coolant of a SP-100 class lithium cooled reactor can be expected to decay into He^3 with a 11 year half life. The amount of He^3 produced will be directly proportional to the amount of H^3 in the coolant. Tritium is known to have a high diffusion coefficient in most metals and is expected to diffuse out of the coolant rapidly. The steady state amount of tritium present in the coolant will be a function of a balance between radiologic tritium production and tritium losses through radioactive decay and diffusion through the coolant loop walls. Many factors, such as surface contaminants, interstitial impurities and alloys, and chemical reaction between the medium and the diffusing species may impact the diffusion loss rate. However, under the simplifying assumption that diffusion is the limiting, or rate controlling step in this process, an estimate can be made of the equilibrium amount of tritium that will remain in the coolant.

To this end, approximate the reactor vessel and primary piping as a thin slab of Nb, with Li containing dissolved H^3 on one side, and the vacuum of space on the other.

Let:

C_0 - concentration of H^3 in Li (kg/m^3)

P_T - rate of production of H^3 in Li (kg/sec)

D_{AB} - diffusion coefficient of H^3 in Nb (m^2/sec)

A_s - surface area of the slab (m^2)

x - slab thickness (m)

V_c - volume of Li coolant (m^3)

m - $C_0 V$ - amount of H^3 in Li (kg)

W_A - rate of H^3 diffusion loss from Li (kg/sec)

t - time (sec)

λ - decay constant for H^3 (s^{-1})

P_{He^3} - production rate of He^3 in Li (kg/sec)

P_{He^4} - production rate of He^4 in Li (kg/sec)

D_v - pressure vessel diameter (m)

L_v - pressure vessel length (m)

It can be shown (see any transport text) that for the thin slab under consideration here that:

$$\begin{aligned} W_A &= (D_{AB} C_0 A_s) / x \\ &= (D_{AB} m A_s) / (x V_c) \quad (\text{kg/s}) \end{aligned} \quad (108)$$

Now:

$$\begin{aligned} dm/dt &= \text{rate of production} - \text{rate of loss} \\ &= P_T - W_A - \lambda m \\ &= P_T - [(D_{AB} A_s) / (x V_c)] m - \lambda m \\ &= P_T - b m \quad (\text{kg/s}) \end{aligned} \quad (109)$$

where:

$$b = \{ (D_{AB} A_s) / (x V_c) \} + \lambda \quad (s^{-1})$$

This differential equation can be solved with the aid of the integrating factor $\exp(bt)$ to result in a solution as follows:

$$m(t) = (P_T/b)[1 - \exp(-bt)], \text{ for } m(0) = 0 \quad (110)$$

After $5/b$ seconds $m(t)$ has increased to within 1% of its asymptotic value of P_T/b . Assuming $t \gg 5/b$, the amount of He^3 generated from H^3 dissolved in the coolant will be:

$$\begin{aligned} P_{\text{He}^3} &= \lambda m \\ &\approx \lambda P_T/b \quad (\text{kg/s}) \end{aligned} \quad (111)$$

Now under the assumption that one He^4 atom is generated per H^3 atom (as in the $\text{Li}^6(n,t)\alpha$ reaction) the direct production of He^4 over the same unit time will be:

$$P_{\text{He}^4} = P_T \quad (112)$$

The ratio of the two production rates is then found to be:

$$\begin{aligned} P_{\text{He}^3}/P_{\text{He}^4} &= (\lambda P_T)/(b P_T) \\ &= \lambda/b \quad (t \gg 5/b) \end{aligned} \quad (113)$$

E.2 Application to SP-100 Class Systems

For a typical SP-100 class system, the following values can be estimated for the constants:

$$\lambda = 1.78 \times 10^{-9} \text{ s}^{-1}$$

$$x = 0.0025 \text{ m}$$

$$V_c = 300 \text{ liters} = 0.3 \text{ m}^3$$

$$D_{AB} = 2.0 \times 10^{-8} \text{ m}^2/\text{sec} \text{ (@ } 1000 \text{ K)}$$

$$A_s = \pi D_v L_v \text{ (considering the pressure vessel only)}$$

$$= \pi 0.35 \times 1.00$$

$$= 1.1 \text{ m}^2$$

$$b = \{(D_{AB} A_s)/(xV)\} + \lambda = 2.9 \times 10^{-5} \text{ s}^{-1}$$

Using these values, it is seen that $5/b$ seconds, the amount of time to reach equilibrium H^3 concentration, equals approximately 48 hours. For t greater than 48 hours, the ratio of He^3 produced to He^4 produced is found to be:

$$\begin{aligned} P_{\text{He}^3}/P_{\text{He}^4} &= \lambda/b \\ &= 1.78 \times 10^{-9} / 2.9 \times 10^{-5} \\ &= 6.13 \times 10^{-5} \end{aligned}$$

Thus the amount of He^3 produced from the decay of retained H^3 will be less than 1/10,000 of the He^4 produced directly, and will not add significantly to total helium production. It should be noted that this analysis neglects the effects of $\text{He}^3(n,p)\text{H}^3$ reactions which could be expected to occur in the core of a operating nuclear reactor. These reactions could be expected to convert some He^3 back into H^3 , which could further reduce the coolant He^3 inventory.

APPENDIX F - CORR CALCULATIONS FOR GE RFS UD3 DESIGN

***** SP-100 COOLANT HELIUM PRODUCTION *****

CASE TITLE: GE ORIGEN2 COMPARISON

** INPUT DATA **

REACTOR:

POWER	:	2.500000	MW
OPERATING TIME	:	7.300000	YEARS
AVG U235 ENRICHMENT	:	96.20000	ATOM %

PRIMARY SYSTEM:

PRESSURE	:	151700.0	PA
TEMPERATURE	:	1375.000	K

COOLANT:

WEIGHT STRATEGY	:	LOW ENRICHMENT LI6	
LI 6 ENRICHMENT	:	0.1170000	ATOM %
VOID FRACTION	:	0.0000000E+00	%
MASS IN PIPING	:	0.0000000E+00	KG
MASS IN LOWER PLENUM	:	5.492000	KG
MASS IN CORE	:	4.715000	KG
MASS IN ROD PLENUM	:	8.988000	KG
MASS IN UPPER PLENUM	:	4.239000	KG
TOTAL MASS	:	23.43400	KG

** OUTPUT DATA **

HELIUM PRODUCTION:

FROM LI6	:	3.2991193E-02	MOLES
	:	0.1320637	GRAMS
FROM LI7	:	0.1856461	MOLES
	:	0.7431414	GRAMS
TOTAL	:	0.2186373	MOLES
	:	0.8752052	GRAMS

TOTAL HELIUM VOLUME
AT INPUT SYSTEM

PRESSURE AND TEMP	:	16.47678	LITERS
-------------------	---	----------	--------

***** SP-100 COOLANT HELIUM PRODUCTION *****

CASE TITLE: GE GASP COMPARISON

** INPUT DATA **

REACTOR:

POWER	:	2.500000	MW
OPERATING TIME	:	7.300000	YEARS
AVG U235 ENRICHMENT	:	96.20000	ATOM %

PRIMARY SYSTEM:

PRESSURE	:	151700.0	PA
TEMPERATURE	:	1375.000	K

COOLANT:

WEIGHT STRATEGY	:	LOW ENRICHMENT LI6	
LI 6 ENRICHMENT	:	0.1000000	ATOM %
VOID FRACTION	:	0.0000000E+00	%
MASS IN PIPING	:	0.0000000E+00	KG
MASS IN LOWER PLENUM	:	6.577000	KG
MASS IN CORE	:	4.341000	KG
MASS IN ROD PLENUM	:	10.57300	KG
MASS IN UPPER PLENUM	:	3.555000	KG
TOTAL MASS	:	25.04600	KG

** OUTPUT DATA **

HELIUM PRODUCTION:

FROM LI6	:	3.0939450E-02	MOLES
	:	0.1238506	GRAMS
FROM LI7	:	0.1852552	MOLES
	:	0.7415764	GRAMS
TOTAL	:	0.2161946	MOLES
	:	0.8654270	GRAMS

TOTAL HELIUM VOLUME
AT INPUT SYSTEM

PRESSURE AND TEMP	:	16.29269	LITERS
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APPENDIX G - PHTS COMPUTATIONAL MODEL LISTINGS

G.1 PHTS Hydraulic Model

Program HYDRA Listing:

C234567

C *****

C

C HYDRA - A PROGRAM TO CALCULATE VARIOUS THERMAL-HYDRAULIC
C PARAMETERS IN THE PRIMARY COOLANT LOOP OF A MODEL SP-100
C CLASS SPACE NUCLEAR POWER SYSTEM.

C

C WRITTEN BY JOHN MCGHEE - 18 AUGUST 1988

C

C ALL UNITS ARE SI. NODE 1 IS ASSUMED TO BE THE ACCUMULATOR
C EXIT. MODEL CONSISTS OF DISCRETE FLOW SEGMENTS DEFINED
C BETWEEN NODES. EXPANSION/CONTRACTION LOSSES ARE ASSUMED TO
C OCCUR IMMEDIATELY AFTER A NODE AND ARE BASED ON FLOW AREA
C RATIOS WITH NO ALLOWANCE FOR ROUNDING OF ENTRANCE/EXITS.
C PUMP PRESSURE IS SET EQUAL TO TOTAL UNRECOVERABLE
C EXTERNAL LOSSES COMPUTED FOR THE LOOP.

C

C TWO INPUT FILES ARE USED: HYDGEO.INP CONTAINS GEOMETRY
C INFORMATION, AND HYDRA.INP CONTAINS THE PROBLEM INPUT
C PARAMETERS. FIVE OUTPUT FILES ARE WRITTEN: HYDRA.OUT
C CONTAINS COMPLETE HYDRAULIC DATA ON EACH NODE, SOL.PLT
C CONTAINS A TELLAGRAF COMPATIBLE LISTING OF HE SOLUBILITY AT
C EACH NODE, SBUB.PLT CONTAINS A TELL-A-GRAF COMPATIBLE
C LISTING OF MAXIMUM STABLE BUBBLE SIZES AT EACH NODE,
C BRAD.DAT CONTAINS INPUT DATA FOR THE BUBBLE RADIUS PROGRAM
C BRAD, AND REN.PLT CONTAINS A TELL-A-GRAF COMPATIBLE PLOT
C FILE OF LOOP REYNOLDS NUMBERS.

C

C LINKING INSTRUCTIONS: LINK HYDRA,PROP

C

C REFERENCES:

C

- C 1. JEPSON, D.W., ET AL., "LITHIUM LITERATURE REVIEW:
C LITHIUM'S PROPERTIES AND INTERACTIONS", HEDL-TME 78-15,
C HANFORD ENGINEERING DEVELOPMENT LABORATORY, RICHLAND
C WA., APRIL 1978.
- C 2. SLOTNICK, H., ET AL., "THE SOLUBILITY OF HELIUM IN .
C LITHIUM AND POTASSIUM", PWAC-380, PRATT AND WHITNEY
C AIRCRAFT CORPORATION, MIDDLETOWN, CT., FEBRUARY, 1965.
- C 3. WALTER A. E., AND A. B. REYNOLDS, FAST BREEDER REACTORS,
C PERGAMON, NY, 1981.
- C 4. WHITE, F. M., FLUID MECHANICS, 2ND EDITION, MCGRAW HILL,
C NY, 1986.
- C 5. SLEICHER, C.A., "MAXIMUM STABLE DROP SIZE IN TURBULENT
C FLOW", AICHE JOURNAL, VOL 8, NO 4, P471-477, SEP 1962.
- C 6. HINZE, J.O., "FUNDAMENTALS OF THE HYDRODYNAMIC MECHANISM
C OF SPLITTING IN THE DISPERSION PROCESS", AICHE JOURNAL,
C VOL 1, NO 3, P289-295, SEP 1955.
- C 7. HUGHMARK, G.A., "DROP BREAKUP IN TURBULENT PIPE FLOW",
C AICHE JOURNAL VOL 17, NO 4, P1000, JUL 1971.
- C 8. SEVIK, M., AND S.H. PARK, 'THE SPLITTING OF DROPS AND

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C      BUBBLES IN TURBULENT FLOW', TRANSACTIONS OF THE ASME,
C      JOURNAL OF FLUIDS ENGINEERING, VOL. 95, NO 1, MARCH 1973.
C
C      HYDGEO.INP FILE FORMAT:
C
C      LINE 1:  GEOMETRY TITLE (<40 CHARACTERS)
C      LINE 2:  DUMMY HEADER FOR NEXT LINE
C      LINE 3:  STANDARD REACTOR EXIT TEMPERATURE (K)
C              STANDARD TOTAL ACCUMULATOR VOID (M**3)
C              STANDARD COOLANT TOTAL MASS (KG)
C              STANDARD ACCUMULATOR PRESSURE (PA)
C              TOTAL NUMBER OF NODES
C      LINE 4:  DUMMY HEADER FOR NODE DATA
C      LINE 5:  NODE 1 TITLE (<40 CHARACTERS)
C      LINE 6:  SURFACE ROUGHNESS TO NEXT NODE (M)
C              MASS FLOW RATE REDUCTION FACTOR TO NEXT NODE
C              DISTANCE TO NEXT NODE (M)
C              TEMPERATURE CHANGE FLAG
C              NUMBER OF PIPE BENDS TO NEXT NODE
C              GEOMETRY TO NEXT NODE
C              DIMENSION 1 (M)
C              DIMENSION 2 (M)
C              DIMENSION 3 (M)
C              DIMENSION 4 (M)
C      REPEAT LINES 5 AND 6 UNTIL ALL NODES DESCRIBED
C
C      GEOMETRY-0=PUMP NODE FLAG (ASSUMED TO BE RECTANGULAR PIPE)
C      GEOMETRY-1=CIRCULAR PIPE, D1=DIMETER, D2,D3,D4=NOT USED
C      GEOMETRY-2=RECTANGULAR PIPE, D1=HEIGHT, D2=WIDTH, D3,D4=NOT
C              USED
C      GEOMETRY-3=ANNULUS, D1=INNER DIA, D2=OUTER DIA, D3,D4=NOT
C              USED
C      GEOMETRY-4=TRIANGULAR PITCH WIRE WRAP FUEL ROD CHANNEL
C              D1=ROD PITCH, D2=ROD DIA, D3=WIRE WRAP DIA,
C              D4=WIRE WRAP LEAD
C      GEOMETRY-5=ACCUMULATOR NODE FLAG (ASSUMED TO BE ANNULUS,
C              D2=OUTER DIAMETER, D1=COMPUTED BY PROGRAM FROM
C              AS A FUNCTION OF COLANT OPERATING TEMPERATURE.
C
C      HYDRA.INP FILE FORMAT:
C
C      LINE 1:  PROBLEM TITLE (<40 CHARACTERS)
C      LINE 2:  ACCUMULATOR TEMPERATURE (K)
C              SYSTEM MASS FLOW RATE (KG/SEC)
C              REACTOR THERMAL POWER (W)
C              NUMBER OF ADDITIONAL MOLES OF GAS IN ACCUMULATORS
C              (MOLES)
C
C      *****
C
C      INITIALIZE FILES AND VARIABLES
C
C      *****
C
C      DIMENSION MDOT1(50), VEL1(0:50), DH1(50), AREA1(0:50),
C      ?SOL1(50), RE1(50), RHO1(0:50), MU1(50), BERN1(50), FF1(50),
C      ?EPS1(50), X1(50), T1(50), BNDS1(50), D1(50), D2(50), D3(50),

```

```

?P1(50), XPN1(50), BNDL1(50), KS1(50), DP1(50), PL1(50), D4(50),
?FRL1(50), MDOTF1(50), ELR1(50), HUGH1(50), SLEIC1(50),
?HINZE1(50), SIG1(50), SEVIK1(50), TIM1(50)
INTEGER IGEO1(50), IT1(50)
CHARACTER*40 TITLE, NODE1(50), GEOTIT
CHARACTER*80 HEADR
REAL MDOTS, MU1, MISC, KS, KS1, MDOT1, MDOTF1, MU
OPEN(UNIT=7, FILE='HYDRA.OUT', STATUS='NEW')
OPEN(UNIT=8, FILE='HYDGeo.INP', STATUS='OLD')
OPEN(UNIT=9, FILE='HYDRA.INP', STATUS='OLD')
OPEN(UNIT=10, FILE='SOL.PLT', STATUS='NEW')
OPEN(UNIT=11, FILE='BRAD.DAT', STATUS='NEW')
OPEN(UNIT=12, FILE='SBUB.PLT', STATUS='NEW')
OPEN(UNIT=13, FILE='REN.PLT', STATUS='NEW')

C
C      **** READ PROBLEM PARAMETERS ****
C
READ(9,*)TITLE
READ(9,*)T1(1), MDOTS, POW, HMOLES

C
C      **** READ SYSTEM GEOMETRY ****
C
READ(8,*)GEOTIT
READ(8,*)HEADR
READ(8,*)TS, VS, CM, PS, NNODES
READ(8,*)HEADR
DO 10 I=1,NNODES
    READ(8,*)NODE1(I)
    READ(8,*)EPS1(I), MDOTF1(I), X1(I), IT1(I), BND1(I),
?      IGEO1(I), D1(I), D2(I), D3(I), D4(I)
    IF(IGEO1(I).EQ.0)IPMP=I
    IF(IGEO1(I).EQ.5)IACC=I
10 CONTINUE

C
C      **** PHYSICAL CONSTANTS ****
C
C      ** PI
PI=3.1415926535
C      ** UNIVERSAL GAS CONSTANT (JOULE/KG-MOLE-K)
R=8.31434
C      ** SPECIFIC HEAT OF LITHIUM (JOULES/KG-K)
CP=4163.0
C
C *****
C
C      CALCULATE ACCUMULATOR PRESSURE AND INNER DIAMETER
C
C      ** COOLANT VOLUME CHANGE
VOL=VS/(VS+CM*(1.0/RHO(TS)-1/RHO(T1(1))))
C      ** TEMPERATURE CHANGE
TEMP=T1(1)/TS
C      ** GAS CONTENT CHANGE
GAS= 1+HMOLES*R*TS/(PS*VS)
C      ** NET EFFECT ON PRESSURE
P1(1)=PS*TEMP*VOL*GAS
C      ** INNER DIAMETER AND SURFACE AREA
VNEW=VS/(VOL*MDOTF1(IACC))

```

```

D1(IACC)=SQRT(4.0*VNEW/(PI*X1(IACC)))
ACCSUR=PI*D1(IACC)*X1(IACC)
C
C *****
C
C   CALCULATE DELTA T IN CORE, EM PUMP, AND PCA
C   ASSUMPTIONS: 10% HEAT LOST IN EM PUMP
C                 90% HEAT LOST IN PCA
C                 HEAT LOSSES ELSEWHERE IN LOOP NEGLIGABLE.
C
C   DELTC=POW/(CP*MDOTS)
C   DELTEM=0.10*DELT
C   DELTPCA=DELT-DELTEM
C
C *****
C
C   CALCULATE LOOP TEMPERATURE PROFILE
C
C   DO 20 I=2,NNODES
C     IF(IT1(I).EQ.1)THEN
C       T1(I)=T1(1)
C     ELSEIF(IT1(I).EQ.2)THEN
C       T1(I)=T1(1)-DELTEM
C     ELSEIF(IT1(I).EQ.3)THEN
C       T1(I)=T1(1)-DELT
C     ENDIF
20  CONTINUE
C   T1(NNODES+1)=T1(1)
C
C *****
C
C   CALCULATE FLOW PARAMETERS
C
C   DO 30 I=1,NNODES
C     AREA1(I)=AREA(IGEO1(I),D1(I),D2(I),D3(I))
C     MDOT1(I)=MDOTS/MDOTF1(I)
C     RHO1(I)=RHO((T1(I)+T1(I+1))/2.0)
C     MU1(I)=MU((T1(I)+T1(I+1))/2.0)
C     SIG1(I)=SIG((T1(I)+T1(I+1))/2.0)
C     VEL1(I)=VEL(RHO1(I),MDOT1(I),AREA1(I))
C     DH1(I)=DH(IGEO1(I),D1(I),D2(I),D3(I))
C     RE1(I)=RE(DH1(I),RHO1(I),VEL1(I),MU1(I))
C     FF1(I)=FF(EPS1(I),RE1(I),DH1(I),IGEO1(I))
C     HUGH1(I)=HUGH(RHO1(I),SIG1(I),VEL1(I),FF1(I))
C     HINZE1(I)=HINZE(RHO1(I),VEL1(I),DH1(I),SIG1(I),FF1(I),1)
C     SEVIK1(I)=HINZE(RHO1(I),VEL1(I),DH1(I),SIG1(I),FF1(I),2)
C     SLEIC1(I)=SLEIC(RHO1(I),VEL1(I),MU1(I),SIG1(I))
C     ELR1(I)=ENTRL(RE1(I),DH1(I))/X1(I)
30  CONTINUE
C   VEL1(0)=VEL1(NNODES)
C   RHO1(0)=RHO1(NNODES)
C   AREA1(0)=AREA1(NNODES)
C
C *****
C
C   CALCULATE TIME REQUIRED FOR FLOW THROUGH LOOP

```

```

C      TIM1(1)=0.0
      DO 35 I=2,NNODES+1
        TIM1(I)=TIM1(I-1)+X1(I-1)/VEL1(I-1)
35    CONTINUE
C
C *****
C
C      CALCULATE PRESSURE LOSSES
C
      DO 40 I=1,NNODES
C
C          ** VELOCITY PRESSURE CHANGE **
          BERN1(I)=BERN(VEL1(I-1),VEL1(I),RH01(I-1),RH01(I))
C
C          ** SUDDEN EXPANSION/CONTRACTION LOSS **
          KS1(I)=KS(AREA1(I-1),AREA1(I))
          IF(AREA1(I).LE.AREA1(I-1))THEN
            XPN1(I)=MISC(KS1(I),RH01(I),VEL1(I))
          ELSE
            XPN1(I)=MISC(KS1(I),RH01(I-1),VEL1(I-1))
          ENDIF
C
C          ** BENDS LOSS **
          BNDL1(I)=BNDL1(I)*MISC(0.15,RH01(I),VEL1(I))
C
C          ** FRICTION LOSS **
          IF(IGEO1(I).EQ.4)THEN
            FRL1(I)=BUNDL(X1(I),DH1(I),FF1(I),RH01(I),VEL1(I),
              $              D1(I),D2(I),D3(I),D4(I))
          ELSE
            FRL1(I)=FR(FF1(I),X1(I),RH01(I),VEL1(I),DH1(I))
          ENDIF
C
C          ** TOTAL UNRECOVERABLE LOSSES **
          PL1(I)=XPN1(I)+BNDL1(I)+FRL1(I)
C
C          ** NET PRESSURE CHANGE **
          DP1(I)=BERN1(I)-PL1(I)
40    CONTINUE
C
C          ** REQUIRED PUMP PRESSURE **
          PLT=0.0
          PLPINT=PL1(IPMP)
          DO 50 I=1,NNODES
            IF(I.NE.IPMP)PLT=PLT+PL1(I)
50    CONTINUE
          PL1(IPMP)=-PLT
          DP1(IPMP)=BERN1(IPMP)-PL1(IPMP)
C
C *****
C
C      CALCULATE PRESSURE AND SOLUBILITY
C
          SOL1(1)=SOL(T1(1),P1(1))
          DO 60 I=2,NNODES

```

```

        P1(I)=P1(I-1)+DPL(I-1)
        SOL1(I)=SOL(T1(I),P1(I))
60    CONTINUE
C
C *****
C
C    CALCULATE SUMMARIES
C
        BT=0.0
        FT=0.0
        ST=0.0
        DO 70 I=1,NNODES
            BT=BT+BNDL1(I)
            FT=FT+FRL1(I)
            ST=ST+XPN1(I)
70    CONTINUE
        TPLOSS=BT+FT+ST
C *****
C
C    WRITE RESULTS TO OUTPUT FILE 'HYDRA.OUT'
C
C *****
C
        WRITE(7,*)'***** PROGRAM HYDRA OUTPUT *****'
        WRITE(7,*)
        WRITE(7,*)'PROBLEM ID: ',TITLE
        WRITE(7,*)'GEOMETRY ID: ',GEOTIT
        WRITE(7,*)
        WRITE(7,*)'PROBLEM INPUT PARAMETERS:'
        WRITE(7,*)'REACTOR THERMAL POWER:          ',POW,' WATTS'
        WRITE(7,*)'REACTOR OUTLET TEMPERATURE:      ',T1(1),' K'
        WRITE(7,*)'SYSTEM MASS FLOW RATE:           ',MDOTS,' KG/SEC'
        WRITE(7,*)'ADDITIONAL GAS PRESENT IN LOOP: ',HMOLES,' MOLES'
        WRITE(7,*)
        WRITE(7,*)'SUMMARY OUTPUT DATA: '
        WRITE(7,*)
        WRITE(7,*)'TOTAL LOSSES IN BENDS:              ',BT,' PA'
        WRITE(7,*)'TOTAL LOSSES IN EXPANSION/CONTRACTION: ',ST,' PA'
        WRITE(7,*)'TOTAL MOODY FRICTION LOSSES:        ',FT,' PA'
        WRITE(7,*)'TOTAL LOSSES:                      ',TPLOSS,
$      ' PA'
        WRITE(7,*)'PUMP INTERNAL LOSSES:                ',PLPINT,
$      ' PA'
        WRITE(7,*)'LOSSES EXTERNAL TO PUMP:              ',
$      TPLOSS-PLPINT,' PA'
        WRITE(7,*)
        WRITE(7,*)'ACCUMULATOR DATA -'
        WRITE(7,*)'INNER DIAMETER:          ',D1(IACC),' M'
        WRITE(7,*)'OUTER DIAMETER:         ',D2(IACC),' M'
        WRITE(7,*)'ID SURFACE AREA:        ',ACCSUR,' M**2'
        WRITE(7,*)
        WRITE(7,*)'***** FOR THIS NODE *****'
$***** TO THE NEXT NODE *****'
        WRITE(7,*)'PRESS(PA) TEMP(K) SOL (KG/M**3) DELT P(PA)
$ LOSS(PA) RE # VEL(M/S)'
        WRITE(7,*)
        DO 80 I=1,NNODES

```

```

        WRITE(7,*)NODE1(I)
        WRITE(7,1000)P1(I),T1(I),SOL1(I),DP1(I),
$          PL1(I),RE1(I),VEL1(I)
    80  WRITE(7,*)
    1000 CONTINUE
    1000 FORMAT(1X,F8.1,3X,F6.1,3X,E9.3,7X,
$          F8.1,2X,F8.1,3X,F8.1,2X,F5.2)
    WRITE(7,*)
    WRITE(7,*)'NODE INPUT AND OUTPUT DATA:'
    DO 90 I=1,NNODES
        WRITE(7,*)
        WRITE(7,*)' **** ',NODE1(I),' ****'
        WRITE(7,*)
        WRITE(7,*)'INPUT DATA FOR FLOW TO NEXT NODE: '
        WRITE(7,*)'SURFACE ROUGHNESS: ',EPS1(I),' M'
        WRITE(7,*)'MASS FLOW RATE FACTOR: ',MDOTF1(I),'
$ DIMENSIONLESS'
        WRITE(7,*)'LENGTH TO NEXT NODE: ',X1(I),' M'
        WRITE(7,*)'TEMPERATURE FLAG: ',IT1(I)
        WRITE(7,*)'# BENDS TO NEXT NODE: ',BNDS1(I)
        WRITE(7,*)'GEOMETRY FLAG: ',IGE01(I)
        WRITE(7,*)'D1: ',D1(I),' M'
        WRITE(7,*)'D2: ',D2(I),' M'
        WRITE(7,*)'D3: ',D3(I),' M'
        WRITE(7,*)'D4: ',D4(I),' M'
        WRITE(7,*)
        WRITE(7,*)'OUTPUT DATA:'
        WRITE(7,*)
        WRITE(7,*)' ** AT THIS NODE:'
        WRITE(7,*)'PRESSURE: ',P1(I),' PA'
        WRITE(7,*)'TEMPERATURE: ',T1(I),' K'
        WRITE(7,*)'SOLUBILITY: ',SOL1(I),' KG/M**3'
        WRITE(7,*)'TOTAL TIME: ',TIM1(I),' SEC'
        WRITE(7,*)
        WRITE(7,*)' ** TO THE NEXT NODE: '
        WRITE(7,*)'MDOT: ',MDOT1(I),' KG/SEC'
        WRITE(7,*)'TIME: ',TIM1(I+1)-TIM1(I),'
$ SEC'
        WRITE(7,*)'AVERAGE TEMPERATURE: ',(T1(I)+T1(I+1))/2.0,
$ ' K'
        WRITE(7,*)'AVG DENSITY: ',RH01(I),' KG/M**3'
        WRITE(7,*)'AVG DYNAMIC VISCOSITY: ',MU1(I),' PA-SEC'
        WRITE(7,*)'AVG SURFACE TENSION: ',SIG1(I),' N/M'
        WRITE(7,*)'FLOW AREA: ',AREA1(I),' M**2'
        WRITE(7,*)'AVG VELOCITY: ',VEL1(I),' M/SEC'
        WRITE(7,*)'HYDRAULIC DIAMETER: ',DH1(I),' M'
        WRITE(7,*)'AVG REYNOLDS #: ',RE1(I),'
$ DIMENSIONLESS'
        WRITE(7,*)'FRICTION FACTOR: ',FF1(I),'
$ DIMENSIONLESS'
        WRITE(7,*)'ENTRANCE/LENGTH RATIO: ',ELR1(I),'
$ DIMENSIONLESS'
        WRITE(7,*)'EXP/CONT LOSS FACTOR: ',KS1(I),'
$ DIMENSIONLESS'
        WRITE(7,*)'VELOCITY PRES CHANGE: ',BERN1(I),' PA'
        WRITE(7,*)'EXPAN/CONTR LOSSES: ',XPN1(I),' PA'
        WRITE(7,*)'LOSSES IN BENDS: ',BNDL1(I),' PA'

```



```

WRITE(7,*)'FRICTION LOSSES:      ',FRL1(I),' PA'
WRITE(7,*)'NET LOSSES TO NEXT NODE: ',PL1(I),' PA'
WRITE(7,*)'NET DELTA PRES TO NEXT NODE: ',DP1(I),' PA'
WRITE(7,*)'MAXIMUM STABLE BUBBLE SIZES'
WRITE(7,*)'  HINZE:              ',HINZE1(I), ' M'
WRITE(7,*)'  SEVIK:              ',SEVIK1(I), ' M'
WRITE(7,*)'  HUGHMARK:           ',HUGH1(I), ' M'
WRITE(7,*)'  SLEICHER:           ',SLEIC1(I), ' M'
WRITE(7,*)
90  CONTINUE
C
C *****
C
C  WRITE TELL-A-GRAF PLOT FILES
C
C  SOLUBILITY PLOT
C
  SOL1(NNODES+1)=SOL1(1)
  WRITE(10,*)'GENERATE A PLOT.'
  WRITE(10,*)'X AXIS LABEL IS "TIME - SEC".'
  WRITE(10,*)'Y AXIS LABEL IS "SOLUBILITY - KG/M**3".'
  WRITE(10,*)'INPUT DATA.'
  DO 100 I=1,NNODES+1
    WRITE(10,*) TIM1(I), SOL1(I)
100  CONTINUE
  WRITE(10,*)'END OF DATA.'
C
C  MAXIMUM STABLE BUBBLE SIZE PLOT
C
  WRITE(12,*)'GENERATE A PLOT.'
  WRITE(12,*)'X AXIS LABEL IS "TIME -SEC".'
  WRITE(12,*)'Y AXIS LABEL IS "MAX STABLE BUBBLE SIZE - M".'
  WRITE(12,*)'INPUT DATA.'
  WRITE(12,*)'"HINZE"'
  HINZE1(NNODES+1)=HINZE1(1)
  DO 101 I=1,NNODES+1
    WRITE(12,*) TIM1(I),HINZE1(I)
101  CONTINUE
  WRITE(12,*)'"HUGHMARK"'
  HUGH1(NNODES+1)=HUGH1(1)
  DO 102 I=1,NNODES+1
    WRITE(12,*)TIM1(I),HUGH1(I)
102  CONTINUE
  WRITE(12,*)'"SLEICHER"'
  SLEIC1(NNODES+1)=SLEIC1(1)
  DO 103 I=1,NNODES+1
    WRITE(12,*)TIM1(I),SLEIC1(I)
103  CONTINUE
  WRITE(12,*)'"SEVIK"'
  SEVIK1(NNODES+1)=SEVIK1(1)
  DO 104 I=1,NNODES+1
    WRITE(12,*)TIM1(I),SEVIK1(I)
104  CONTINUE
  WRITE(12,*)'END OF DATA.'
C
C  REYNOLDS NUMBER PLOT
C

```

```

      RE1(NNODES+1)=RE1(1)
      WRITE(13,*)'GENERATE A PLOT.'
      WRITE(13,*)'X AXIS LABEL IS "TIME - SEC".'
      WRITE(13,*)'Y AXIS LABEL IS "REYNOLDS NUMBER".'
      WRITE(13,*)'INPUT DATA.'
      DO 105 I=1,NNODES+1
        WRITE(13,*)TIM1(I), RE1(I)
105   CONTINUE
      WRITE(13,*)'END OF DATA.'
C
C *****
C
C   WRITE INPUT DATA FILE FOR BUBBLE RADIUS PROGRAM BRAD
C
C *****
C
      WRITE(11,*)TITLE
      WRITE(11,*)NNODES
      DO 110 I=1,NNODES
        WRITE(11,*)T1(I),P1(I),VEL1(I),X1(I),TIM1(I)
110   CONTINUE
      WRITE(11,*)TIM1(NNODES+1)
C
C *****
C
C   QUIT
C
C *****
C
      STOP
      END
C
C
C *****
C
      FUNCTION BUNDL - GIVEN BUNDLE LENGTH (X) IN MTRS, HYDRAULIC
      DIAMETER (DH) IN METERS, FRICTION FACTOR (FF) DIMENSIONLESS,
      DENSITY (RHO) IN KG/M**3, VELOCITY (VEL) IN M/SEC, ROD PITCH
      (P) IN METERS, WIRE WRAP LEAD (XL) IN METERS, ROD DIAMETER
      (D) IN METERS, AND WIRE WRAP DIAMETER (S) IN METERS RETURNS
      THE BUNDLE PRESSURE LOSS IN PA FOR A TRIANGULAR PITCH WIRE
      WRAPPED FUEL BUNDLE (REF 3, CHAPTER 10 - CRT MODEL).
C
      REAL FUNCTION BUNDL(X,DH,FF,RHO,VEL,P,D,S,XL)
      REAL X,DH,FF,RHO,VEL,P,XL,AR,A,D,S,PI,C
      C=2200.0
      PI=3.141593
      A=SQRT(3.0)*P**2/4.0-PI*D**2/8.0
      AR=PI*((D+2.0*S)**2-D**2)/24.0
      DUM1=X*FF*RHO*VEL**2/(DH*2.0)
      DUM2=1+C*(AR*DH*P**2)/(A*XL*(PI*P**2+XL**2))
      BUNDL=DUM1*DUM2
      RETURN
      END
C
C *****
C

```

```

C      FUNCTION VEL - GIVEN DENSITY (RHO) IN KG/M**3, MASS FLOW
C      RATE (MDOT) IN KG/SEC, AND FLOW AREA (AREA) IN M**2, RETURNS
C      THE VELOCITY IN M/SEC.
C
C      REAL FUNCTION VEL(RHO,MDOT,AREA)
C      REAL RHO, MDOT, AREA
C      VEL=MDOT/(AREA*RHO)
C      RETURN
C      END
C
C *****
C
C      FUNCTION MISC - GIVEN LOSS COEFFICIENT (CX) DIMENSIONLESS,
C      DENSITY (RHO) IN KG/M**3, AND VELOCITY (VEL) IN M/SEC,
C      RETURNS THE PRESSURE LOSS IN PA.
C
C      REAL FUNCTION MISC(CX,RHO,VEL)
C      REAL CX,RHO,VEL
C      MISC=CX*RHO*VEL**2/2.0
C      RETURN
C      END
C
C *****
C
C      FUNCTION AREA - GIVEN THE CHANNEL GEOMETRY (IGEO=1-CIRCULAR,
C      IGEO=0 OR 2-RECTANGULAR, IGEO=3-ANNULAR, IGEO=4-TRIANGULAR
C      PITCH CORE WITH WIRE WRAP), AND THE CHANNEL DIMENSIONS IN
C      METERS (D1=DIAMETER FOR CIRCULAR, D1 AND D2=HEIGHT AND WIDTH
C      FOR RECTANGULAR, D1 AND D2=INNER AND OUTER DIAMETER FOR
C      ANNULAR, D1, D2, AND D3=ROD PITCH, ROD DIAMETER, WIRE WRAP
C      DIAMETER FOR TRIANGULAR PITCH CORE WITH WIRE WRAP), RETURNS
C      THE FLOW AREA IN M**2.
C
C      REAL FUNCTION AREA(IGEO,D1,D2,D3)
C      REAL D1,D2,PI
C      INTEGER IGEO
C      PI=3.141593
C      IF(IGEO.EQ.1)THEN
C        AREA=PI*D1**2/4.0
C      ELSEIF((IGEO.EQ.2).OR.(IGEO.EQ.0))THEN
C        AREA=D1*D2
C      ELSEIF((IGEO.EQ.3).OR.(IGEO.EQ.5))THEN
C        AREA=PI*ABS(D1**2-D2**2)/4.0
C      ELSEIF(IGEO.EQ.4)THEN
C        AREA=SQRT(3.0)*D1**2/4.0-PI*(D2**2+D3**2)/8.0
C      ENDIF
C      RETURN
C      END
C
C *****
C
C      FUNCTION BERN - GIVEN INITIAL VELOCITY (V1) IN M/SEC,
C      FINAL VELOCITY (V2) IN M/SEC, INITIAL DENSITY (RHO1) IN
C      KG/M**3, AND FINAL DENSITY (RHO2) IN KG/M**3, RETURNS THE
C      RETURNS THE BERNOULLI VELOCITY PRESSURE CHANGE IN PA.
C
C      REAL FUNCTION BERN(V1,V2,RHO1,RHO2)

```

```

REAL V1,V2,RHO1,RHO2
BERN=(RHO1*V1**2-RHO2*V2**2)/2.0
RETURN
END

C
C *****
C
C FUNCTION KS - GIVEN THE INITIAL FLOW AREA (AI) IN M AND THE
C FINAL FLOW AREA (AF) IN M, RETURNS THE SUDDEN
C EXPANSION/CONTRACTION DIMENSIONLESS PRESSURE LOSS
C COEFFICIENT (REF 4, EQ 6.112,113).
C VALID FOR TURBULENT FLOW ONLY.
C
REAL FUNCTION KS(AI,AF)
REAL AI,AF
IF(AF.GT.AI)THEN
  KS=(1-AI/AF)**2
ELSEIF(AF/AI.GT..58)THEN
  KS=(1-AF/AI)**2
ELSE
  KS=0.42*(1-AF/AI)
ENDIF
RETURN
END

C
C *****
C
C FUNCTION FR - GIVEN THE FRICTION FACTOR (F) DIMENSIONLESS,
C PIPE LENGTH (X) M, DENSITY (RHO) KG/M**3, VELCTY (V) M/SEC,
C AND THE HYDRAULIC DIAMETER (DE) M, RETURNS THE FRICTION
C PRESSURE LOSS IN PA.
C
REAL FUNCTION FR(F,X,RHO,V,DE)
REAL F,X,RHO,V,DE
FR=F*X*RHO*V**2/(2.0*DE)
RETURN
END

C
C *****
C
C FUNCTION DH - GIVEN THE CHANNEL GEOMETRY
C (IGEO=1-CIRCULAR,IGEO= 0 OR 2-RECTANGULAR, IGEO=3-ANNULAR,
C IGEO=4-TRIANGULAR PITCH CORE WITH WIRE WRAP), AND THE
C CHANNEL DIMENSIONS IN METERS (D1=DIAMETER FOR CIRCULAR, D1
C AND D2=HEIGHT AND WIDTH FOR RECTANGULAR, D1 AND D2=INNER AND
C OUTER DIAMETER FOR ANNULAR,D1, D2, AND D3= ROD PITCH, ROD
C DIAMETER, AND WIRE WRAP DIAMETER FOR TRIANGULAR PITCH CORE
C WITH WIRE WRAP), RETURNS THE HYDRAULIC DIAMETER IN METERS.
C
REAL FUNCTION DH(IGEO,D1,D2,D3)
REAL D1,D2,PI
INTEGER IGEO
PI=3.141593
IF(IGEO.EQ.1)THEN
  DH=D1
ELSEIF((IGEO.EQ.2).OR.(IGEO.EQ.0))THEN
  DH=2.0*D1*D2/(D1+D2)

```

```

ELSEIF((IGEO.EQ.3).OR.(IGEO.EQ.5))THEN
  DH=ABS(D1-D2)
ELSEIF(IGEO.EQ.4)THEN
  DH=2.0*SQRT(3.0)*D1**2-PI*(D2**2+D3**2)
  DH=DH/(PI*(D2+D3))
ENDIF
RETURN
END

C
C *****
C
C   FUNCTION FF - GIVEN THE SURFACE ROUGHNESS (EPS) IN M, THE
C   REYNOLDS NUMBER (RE) DIMENSIONLESS, AND THE EQUIVALENT
C   HYDRAULIC DIAMETER (DE) IN M, RETURNS THE DIMENSIONLESS
C   DARCY FRICTION FACTOR. RESULTS UNRELIABLE IN CRITICAL ZONE
C   (2000<RE<4000). HYDRAULIC DIAMETER ADJUSTED TO EFFECTIVE
C   HYDRAULIC DIAMETER FOR NON-CIRCULAR GEOMETRY (IGEO NOT EQUAL
C   TO 1) (REF 4, EQ 6.64A, AND SECTION 6.6, HAALAND EQUATION)
C
REAL FUNCTION FF(EPS,RE,DE,IGEO)
REAL EPS,REX,DEX,RE,DE,DUM
INTEGER IGEO
IF((IGEO.NE.1).AND.(IGEO.NE.4))THEN
  DEX=DE*0.6667
  REX=RE*0.6667
ELSE
  DEX=DE
  REX=RE
ENDIF
IF(RE.LE.2000.0)THEN
  FF=64.0/REX
ELSE
  DUM=-1.8*LOG10((6.9/REX)+((EPS/DEX)/3.7)**1.11)
  FF=(1/DUM)**2
ENDIF
RETURN
END

C
C *****
C
C   FUNCTION RE - GIVEN THE HYDRAULIC DIAMETER (DE) IN M,
C   DENSITY (RHO) IN KG/M**3, VELOCITY (V) IN M/SEC, AND DYNAMIC
C   VISCOSITY (MU) IN PA-SEC, RETURNS THE DIMNSNLESS REYNOLDS #.
C
REAL FUNCTION RE(DE,RHO,V,MU)
REAL DE,RHO,V,MU
RE=DE*RHO*V/MU
RETURN
END

C
C *****
C
C   FUNCTION ENTRL - GIVEN THE DIMENSIONLESS REYNOLDS NUMBER
C   (RE) AND THE HYDRAULIC DIAMETER (DH) IN METERS, RETURNS THE
C   ENTRANCE LENGTH IN METERS (REF. 4, EQ 6.6).
C
REAL FUNCTION ENTRL(RE,DH)

```

```

REAL RE, DH
IF(RE.LE.2300.0)THEN
  ENTRL=0.06*DH*RE
ELSE
  ENTRL=4.4*DH*RE**0.16667
ENDIF
RETURN
END

C
C *****
C
C FUNCTION SLEIC - GIVEN GIVEN DENSITY (KG/M**3), VELOCITY
C (M/S), VISCOSITY (KG/M-S), AND SURFACE TENSION (N/M) OF THE
C CONTINUOUS PHASE, RETURNS THE MAXIMUM STABLE BUBBLE SIZE IN
C TURBULENT FLOW REFERENCE 5.
C
REAL FUNCTION SLEIC(RHO,VEL,MU,SIG)
REAL RHO, VEL, MU, SIG
SLEIC = 38.0*SIG**1.5/(VEL**2.5*RHO*SQRT(MU))
RETURN
END

C
C *****
C
C FUNCTION HINZE - GIVEN DENSITY (KG/M**3), VELOCITY (M/S),
C EQ DIAMETER (M), SURFACE TENSION (N/M), AND DARCY FRICTION
C FACTOR (DIMENSIONLESS), RETURNS THE MAXIMUM STABLE BUBBLE
C SIZE IN TURBULENT FLOW (M). REFERENCE 5,6 AND 8. IF
C IFLAG=1, HINZE ORIGINAL CORRELATION IS USED. IF IFLAG=2
C THEN THE SEVIK MODIFICATION OF HINZE'S ORIGINAL FORMULA
C IS USED.
C
REAL FUNCTION HINZE(RHO,VEL,DEQ,SIG,FF,IFLAG)
REAL RHO, VEL, DEQ, SIG, FF, EPS
INTEGER IFLAG
IF(IFLAG.EQ.1)C=0.725
IF(IFLAG.EQ.2)C=1.150
EPS=FF*VEL**3/(2.0*DEQ)
HINZE=C*(SIG/RHO)**0.6/(EPS**0.4)
RETURN
END

C
C *****
C
C FUNCTION HUGH - GIVEN DENSITY (KG/M**3), SURFACE TENSION
C (N/M), DARCY FRICTION FACTOR (DIMENSIONLESS), AND VELOCITY
C (M/S), RETURNS THE MAXIMUM STABLE BUBBLE SIZE (M).
C REFERENCE 7.
C
REAL FUNCTION HUGH(RHO,SIG,VEL,FF)
REAL RHO, SIG, VEL, FF
HUGH= SIG*8/(1.69*FF*VEL**2*RHO)
RETURN
END
C

```

PHTS Hydraulic Model Input Files

HYDGEO.INP:

```
'SP-100 RFS UD3, VER 1.1, 28 NOV 88'
'TS    VS    CM    PS    NNODES'
1300   0.035 110.0 35000.0 14
'EPS    MODTF  X    IT  BNDS  IGEO PITCH  DIA    S    H'
'NODE 1 - ACCUMULATOR EXIT'
1.5E-06 12.0    0.5  1    1    1    0.04    0    0    0
'NODE 2 - TE-EM PUMP SUCTION'
1.5E-06 12.0    0.75 1    1    0    0.0178  0.0268 0    0
'NODE 3 - TE-EM PUMP DISCHARGE'
1.5E-06 12.0    1.0  2    2    1    0.04    0    0    0
'NODE 4 - PCA INLET '
1.5E-06 72.0    0.10 2    1    1    0.02    0    0    0
'NODE 5 - PCA HEAT EXCHANGER INLET'
1.5E-06 432.0   0.28 2    1    2    0.027   0.001  0    0
'NODE 6 - PCA HEADER INLET'
1.5E-06 72.0    0.10 3    1    1    0.02    0    0    0
'NODE 7 - PCA EXIT'
1.5E-06 12.0    1.5  3    3    1    0.04    0    0    0
'NODE 8 - PIPE JUNCTION'
1.5E-06 6.0     1.5  3    3    1    0.0425  0    0    0
'NODE 9 - REACTOR VESSEL INLET'
1.5E-06 1.0     0.8  3    2    3    0.350   0.320  0    0
'NODE 10 - UPPER PLENUM INLET'
1.5E-06 1.0     0.30 3    1    1    0.280   0    0    0
'NODE 11 - CORE INLET'
1.5E-06 2700.0  0.560 3    0    4    .00813  .00775 .00038 .1372
'NODE 12 - LOWER PLENUM INLET'
1.5E-06 1.0     0.10 1    1    1    0.320   0    0    0
'NODE 13 - REACTOR VESSEL EXIT'
1.5E-06 6.0     2.00 1    3    1    0.0425  0    0    0
'NODE 14 - ACCUMULATOR INLET'
1.5E-06 6.0     0.60 1    0    5    0.0     0.220  0    0
```

HYDRA.INP:

```
'EOL STBY POWER'
680.0 2.4 0.28E06 0.25
```

Sample PHTS Hydraulic Model Output

***** PROGRAM HYDRA OUTPUT *****

PROBLEM ID: BOL FULL POWER
GEOMETRY ID: SP-100 RFS UD3, VER 1.1, 28 NOV 88

PROBLEM INPUT PARAMETERS:
REACTOR THERMAL POWER: 2400000. WATTS
REACTOR OUTLET TEMPERATURE: 1300.000 K
SYSTEM MASS FLOW RATE: 11.00000 KG/SEC
ADDITIONAL GAS PRESENT IN LOOP: 0.0000000E+00 MOLES

SUMMARY OUTPUT DATA:

TOTAL LOSSES IN BENDS:	3334.667	PA
TOTAL LOSSES IN EXPANSION/CONTRACTION:	10033.21	PA
TOTAL MOODY FRICTION LOSSES:	20978.00	PA
TOTAL LOSSES:	34345.87	PA
PUMP INTERNAL LOSSES:	4266.245	PA
TOTAL LOSSES EXTERNAL TO PUMP:	30079.63	PA

ACCUMULATOR DATA -

INNER DIAMETER:	0.1112597	M
OUTER DIAMETER:	0.2200000	M
ID SURFACE AREA:	0.2097196	M**2 PER ACCUMULATOR

***** FOR THIS NODE ***** TO THE NEXT NODE *****
PRESS(PA) TEMP(K) SOL(KG/M**3) DELT P(PA) LOSS(PA) RE# VEL(M/S)

NODE 1 - ACCUMULATOR EXIT						
35000.0	1300.0	0.933E-06	-1076.0	464.2	175608.7	1.69
NODE 2 - TE-EM PUMP SUCTION						
33924.0	1300.0	0.905E-06	26420.1	-30079.6	246753.3	4.45
NODE 3 - TE-EM PUMP DISCHARGE						
60344.2	1294.8	0.160E-05	1581.2	2079.0	174696.8	1.69
NODE 4 - PCA INLET						
61925.4	1294.8	0.164E-05	187.2	155.0	58232.3	1.13
NODE 5 - PCA HEAT EXCHANGER INLET						
62112.6	1294.8	0.165E-05	-6541.7	5791.7	10631.6	2.17
NODE 6 - PCA HEADER INLET						
55570.8	1247.6	0.140E-05	-170.6	923.6	55457.8	1.11
NODE 7 - PCA EXIT						
55400.2	1247.6	0.140E-05	-1136.8	798.4	166373.3	1.67
NODE 8 - PIPE JUNCTION						
54263.4	1247.6	0.137E-05	-3152.1	1849.4	313173.2	2.96
NODE 9 - REACTOR VESSEL INLET						
51111.3	1247.6	0.129E-05	-675.8	2031.9	119192.8	1.60

NODE 10 - UPPER PLENUM INLET						
50435.4	1247.6	0.127E-05	205.9	313.4	285211.3	0.41
NODE 11 - CORE INLET						
50641.3	1247.6	0.128E-05	-10904.8	10169.7	7479.5	1.89
NODE 12 - LOWER PLENUM INLET						
39736.6	1300.0	0.106E-05	-24.9	774.8	263413.1	0.32
NODE 13 - REACTOR VESSEL EXIT						
39711.6	1300.0	0.106E-05	-4895.3	2981.7	330557.6	3.00
NODE 14 - ACCUMULATOR INLET						
34816.3	1300.0	0.928E-06	183.7	1746.7	42409.9	0.15

NODE INPUT AND OUTPUT DATA:

**** NODE 1 - ACCUMULATOR EXIT

INPUT DATA FOR FLOW TO NEXT NODE:

SURFACE ROUGHNESS:	1.5000001E-06	M
MASS FLOW RATE FACTOR:	12.00000	DIMENSIONLESS
LENGTH TO NEXT NODE:	0.5000000	M
TEMPERATURE FLAG:	1	
# BENDS TO NEXT NODE:	1.000000	
GEOMETRY FLAG:	1	
D1:	3.9999999E-02	M
D2:	0.0000000E+00	M
D3:	0.0000000E+00	M
D4:	0.0000000E+00	M

OUTPUT DATA:

** AT THIS NODE:

PRESSURE:	35000.00	PA
TEMPERATURE:	1300.000	K
SOLUBILITY:	9.3325332E-07	KG/M**3
TOTAL TIME:	0.0000000E+00	SEC

** TO THE NEXT NODE:

MDOT:	0.9166667	KG/SEC
TIME:	0.2957593	SEC
AVERAGE TEMPERATURE:	1300.000	K
AVG DENSITY:	431.4892	KG/M**3
AVG DYNAMIC VISCOSITY:	1.6615579E-04	PA-SEC
AVG SURFACE TENSION:	0.2650000	N/M
FLOW AREA:	1.2566372E-03	M**2
AVG VELOCITY:	1.690564	M/SEC
HYDRAULIC DIAMETER:	3.9999999E-02	M
AVG REYNOLDS #:	175608.7	DIMENSIONLESS
FRICTION FACTOR:	1.6123677E-02	DIMENSIONLESS
ENTRANCE/LENGTH RATIO:	2.634214	DIMENSIONLESS
EXP/CONT LOSS FACTOR:	0.4013444	DIMENSIONLESS
VELOCITY PRES CHANGE:	-611.7334	PA
EXPAN/CONTR LOSSES:	247.4688	PA
LOSSES IN BENDS:	92.48994	PA

FRICTION LOSSES: 124.2732 PA
 NET LOSSES TO NEXT NODE: 464.2319 PA
 NET DELTA PRES TO NEXT NODE: -1075.965 PA
 MAXIMUM STABLE BUBBLE SIZES
 HINZE: 8.6679393E-03 M
 SEVIK: 1.3749145E-02 M
 HUGHMARK: 6.3088737E-02 M
 SLEICHER: 0.2508104 M

**** NODE 2 - TE-EM PUMP SUCTION

INPUT DATA FOR FLOW TO NEXT NODE:
 SURFACE ROUGHNESS: 1.5000001E-06 M
 MASS FLOW RATE FACTOR: 12.00000 DIMENSIONLESS
 LENGTH TO NEXT NODE: 0.7500000 M
 TEMPERATURE FLAG: 1
 # BENDS TO NEXT NODE: 1.000000
 GEOMETRY FLAG: 0
 D1: 1.7800000E-02 M
 D2: 2.6799999E-02 M
 D3: 0.0000000E+00 M
 D4: 0.0000000E+00 M

OUTPUT DATA:

** AT THIS NODE:

PRESSURE: 33924.04 PA
 TEMPERATURE: 1300.000 K
 SOLUBILITY: 9.0456336E-07 KG/M**3
 TOTAL TIME: 0.2957593 SEC

** TO THE NEXT NODE:

MDOT: 0.9166667 KG/SEC
 TIME: 0.1685159 SEC
 AVERAGE TEMPERATURE: 1297.380 K
 AVG DENSITY: 431.7538 KG/M**3
 AVG DYNAMIC VISCOSITY: 1.6658798E-04 PA-SEC
 AVG SURFACE TENSION: 0.2654193 N/M
 FLOW AREA: 4.7703998E-04 M**2
 AVG VELOCITY: 4.450620 M/SEC
 HYDRAULIC DIAMETER: 2.1391928E-02 M
 AVG REYNOLDS #: 246753.3 DIMENSIONLESS
 FRICTION FACTOR: 1.6746623E-02 DIMENSIONLESS
 ENTRANCE/LENGTH RATIO: 0.9939620 DIMENSIONLESS
 EXP/CONT LOSS FACTOR: 0.2605611 DIMENSIONLESS
 VELOCITY PRES CHANGE: -3659.494 PA
 EXPAN/CONTR LOSSES: 1114.184 PA
 LOSSES IN BENDS: 641.4140 PA
 FRICTION LOSSES: 2510.647 PA
 NET LOSSES TO NEXT NODE: -30079.62 PA
 NET DELTA PRES TO NEXT NODE: 26420.13 PA
 MAXIMUM STABLE BUBBLE SIZES
 HINZE: 2.0815758E-03 M
 SEVIK: 3.3018095E-03 M
 HUGHMARK: 8.7726610E-03 M
 SLEICHER: 2.2313775E-02 M

*** A detailed listing of all nodes is provided in the format above for normal program output.
Only the first two nodes are presented above in the interest of brevity.

G.2 Material Properties Subroutines

C234567

C *****

C

C A COLLECTION OF SUBPROGRAMS AND FUNCTIONS TO RETURN VARIOUS
C MATERIAL PROPERTIES OF LI AND HE.

C

C WRITTEN BY JOHN MCGHEE 19 SEP 88.

C

C REFERENCES:

C

C 1. SLOTNICK, H., ET AL., "THE SOLUBILITY OF HELIUM IN
C LITHIUM AND POTASSIUM", PWAC-380, PRATT AND WHITNEY
C AIRCRAFT, MIDDLETOWN, CONN., 24 FEB 1965.

C 2. JEPSON, D.W., ET AL., LITHIUM LITERATURE REVIEW:
C LITHIUM'S PROPERTIES AND INTERACTIONS, HEDL-TME 78-15,
C HANFORD ENGINEERING DEVELOPMENT LAB, RICHLAND, WA, APRIL
C 1978.

C 3. REED, E.L., AND D.H. DROHER, "SOLUBILITY AND DIFFUSIVITY
C OF INERT GASES IN LIQUID SODIUM, POTASSIUM, AND NAK",
C LMEC-69-36, AEC R&D REPORT, LIQUID METAL ENGINEERING
C CENTER, 1969.

C

C *****

C

C FUNCTION DIFF - GIVEN THE TEMPERATURE (T) IN K, AND THE
C VISCOSITY (MU) IN KG/M-SEC, RETURNS THE MASS DIFFUSION
C COEFFICIENT OF HE IN LI IN M**2/SEC, USING THE STOKES
C -EINSTEIN RELATION (REF 3).

C

REAL FUNCTION DIFF(T,MU)

REAL T, MU

DIFF=7.8753E-15*T/MU

RETURN

END

C

C *****

C

C FUNCTION RHO - GIVEN T IN DEGREES K RETURNS THE DENSITY OF
C LITHIUM IN KG/M**3 (REF 2).

C

REAL FUNCTION RHO(T)

REAL T

RHO=515.0-0.101*(T-473.16)

RETURN

END

C

C *****

C

C FUNCTION MU - GIVEN T IN DEGREES K RETURNS THE DYNAMIC
C VISCOSITY OF LITHIUM IN KG/M-SEC (REF 2).

C

REAL FUNCTION MU(T)

REAL T

IF(T.GT.983.5)THEN

MU=10.0**(726.07/T-4.338)

```

      ELSE
        MU=10.0**(-1.5064-0.7368*LOG10(T)+109.95/T)
      ENDIF
      RETURN
    END

C
C
C *****
C
C   FUNCTION SOL - GIVEN TEMPERATURE (T) IN K, AND PRESSURE
C   (P) IN PA, RETURNS THE SOLUBILITY OF HE IN LITHIUM IN
C   KG/M**3 (REF 1).
C
    REAL FUNCTION SOL(T,P)
    REAL T, P
    SOL=P*EXP(-1752.0/T-23.00)
    RETURN
    END

C
C *****
C
C   FUNCTION SIG - GIVEN TEMPERATURE (T) IN K, RETURNS THE
C   SURFACE TENSION OF LITHIUM IN N/M (REF 2).
C
    REAL FUNCTION SIG(T)
    REAL T
    SIG= 0.473-1.60E-04*T
    RETURN
    END

C
C *****
C
C   FUNCTION PV - GIVEN THE TEMPERATURE (T) IN K RETURNS THE
C   VAPOR PRESSURE OF LITHIUM IN PA (REF 2).
C
    REAL FUNCTION PV(T)
    REAL T
    PV=EXP(23.31-18750.0/T)
    RETURN
    END

```

G.3 Supersaturation for Nucleation at Surface Cavities

WALLNUC Program Listing:

C234567

```
C *****
C
C WALLNUC - A PROGRAM TO ESTIMATE HE SUPERSATURATION
C REQUIRED FOR NUCLEATION ON SMOOTH SURFACES COVERED
C WITH LITHIUM. "EQUIVALENT CAVITY, MBS MODEL"
C
C WRITTEN BY JOHN MCGHEE - 21 DEC 88
C
C REFERENCE: DWYER, O.E. "BOILING LIQUID METAL HEAT
C TRANSFER", 1976, AMERICAN NUCLEAR SOCIETY,
C PP 24-28.
C
C LINKING INSTRUCTIONS: LINK WALLNUC,PROP,IMSL/LIB/INCLUDE=
C ZPLRC,ZREAL
C
C ALL UNITS ARE SI. INPUT FILE FORMAT:
C
C PRECONDITION TEMPERATURE (K), PRECONDITION PRESSURE (PA)
C PSI, R/R'
C OPERATING TEMPERATURE (K), OPERATING PRESSURE (PA)
C
C *****
C
C DECLARATIONS, INITIALIZE FILES
C
C REAL CF(4)
C COMPLEX ROOT(3)
C
C OPEN(UNIT=7,FILE='WALLNUC.INP',STATUS='OLD')
C OPEN(UNIT=8,FILE='WALLNUC.OUT',STATUS='NEW')
C
C *****
C
C READ INPUT DECK
C
C READ(7,*)TLP,PLP
C READ(7,*)PSI,RATIO
C READ(7,*)TL,PL
C
C *****
C
C COMPUTE R PRIME FOR SPECIFIED PRE-CONDITIONS
C
C T=TLP
C P=PLP
C DEN=P-PV(T)
C CF(4)=1.0
C CF(3)=-2.0*SIG(T)/DEN
C CF(2)=0.0
C CF(1)=-PSI*T/DEN
C
C CALL ZPLRC(3,CF,ROOT)
```

```

C      DO 10 I=1,3
          IF (REAL(ROOT(I)).GT.0.0) THEN
              RP=ROOT(I)
          ENDIF
10      CONTINUE
C
C      *****
C      COMPUTE HE SUPERSATURATION REQUIRED FOR NUCLEATION
C      AT OPERATING CONDITIONS
C
          T=TL
          P=PL
          HK=EXP(1752/T+23)
          CS=SOL(T,P)
          R=RP*RATIO
C
          DELP=2.0*SIG(T)/R - PSI*T/(RP**3) - PV(T) + PL
          DELC=DELP/HK
          SS=100*(DELC-CS)/CS
C
C      *****
C      COMPUTE SUPERHEAT REQUIRED FOR NUCLEATION
C      USING BISECTION METHOD
C
C      FIND ENDPOINTS OF INTERVAL CONTAINING REQUIRED SUPERHEAT
C
          ICOUNT=0
          IMAX=5000
          TSAT=18750/(23.31-LOG(P))
          TA=TSAT
          FA=2.0*SIG(TA)/R-PSI*TA/(RP**3)-PV(TA)+P
          IF (FA.GT.0.0) THEN
              TCHNG=10.0
          ELSE
              TCHNG=-10.0
          ENDIF
          TB=TA
110      TB=TB+TCHNG
          ICOUNT=ICOUNT+1
          FB=2.0*SIG(TB)/R-PSI*TB/(RP**3)-PV(TB)+P
          IF ((FA*FB).GT.0.0) THEN
              IF (ICOUNT.LT.IMAX) THEN
                  GOTO 110
              ELSE
                  IERROR=1
                  GOTO 200
              ENDIF
          ENDIF
C
C      FIND NEW INTERVAL ENDPOINTS
C
          ICOUNT=0
115      TM=(TA+TB)/2.0
          ICOUNT=ICOUNT+1

```

```

FM=2.0*SIG(TM)/R-PSI*TM/(RP**3)-PV(TM)+P
IF((FA*FM).GT.0)THEN
  TA=TM
  FA=FM
ELSE
  TB=TM
ENDIF
C
C CHECK FOR CONVERGENCE
C
IF(ABS(TA-TB).GT.0.001)THEN
  IF(ICOUNT.LT.IMAX)THEN
    GOTO 115
  ELSE
    IERROR=1
    GOTO 200
  ENDIF
ENDIF
T=(TA+TB)/2.0
200 CONTINUE
C
C *****
C
C WRITE OUTPUT FILES
C
WRITE(8,*)'**** WALLNUC OUTPUT ****'
WRITE(8,*)
WRITE(8,*)'- INPUT DATA - '
WRITE(8,*)
WRITE(8,*)'PRECONDITION TEMPERATURE:' ,TLP,' K'
WRITE(8,*)'PRECONDITION PRESSURE : ' ,PLP,' PA'
WRITE(8,*)'PSI, INERT GAS FACTOR : ' ,PSI,' M*N/K'
WRITE(8,*)'R/R PRIME RATIO : ' ,RATIO
WRITE(8,*)'OPERATING TEMPERATURE : ' ,TL,' K'
WRITE(8,*)'OPERATING PRESSURE : ' ,PL,' PA'
WRITE(8,*)
C
WRITE(8,*)'- OUTPUT DATA - '
WRITE(8,*)
WRITE(8,*)'ROOTS OF R PRIME EQUATION: '
DO 20 I=1,3
  WRITE(8,*)ROOT(I)
20 CONTINUE
WRITE(8,*)
WRITE(8,*)'R PRIME AT PRECONDITIONS:' ,RP,' M'
WRITE(8,*)
WRITE(8,*)'FOR NUCLEATION AT OPERATING CONDITIONS'
WRITE(8,*)'BY HE SUPERSATURATION ..... '
WRITE(8,*)
WRITE(8,*)'SATURATION CONCENTRATION:' ,CS,' KG/M**3'
WRITE(8,*)'HE SUPERSATURATION'
WRITE(8,*)'REQUIRED FOR NUCLEATION : ' ,SS,' %'
C
WRITE(8,*)
WRITE(8,*)'FOR NUCLEATION AT OPERATING CONDITIONS'
WRITE(8,*)'BY SUPERHEAT..... '
WRITE(8,*)

```



```

      IF(IERROR.EQ.1)THEN
        WRITE(8,*)'**  UNABLE TO CONVERGE ON SUPERHEAT **'
      ELSE
        WRITE(8,*)'SATURATION TEMPERATURE   : ' ,TSAT,' K'
        WRITE(8,*)'T LIQUID - T SAT          : ' ,T-TSAT,' K'
      ENDIF
C
      STOP
      END
C

```

WALLNUC Sample Input:

```

1300.0 62000.0
6.1E-16 0.72
1300.0 62000.0

```

WALLNUC Sample Output:

**** WALLNUC OUTPUT ****

- INPUT DATA -

```

PRECONDITION TEMPERATURE: 1300.000      K
PRECONDITION PRESSURE   : 62000.00      PA
PSI, INERT GAS FACTOR   : 6.0999999E-16 M*N/K
R/R PRIME RATIO         : 0.7200000
OPERATING TEMPERATURE   : 1300.000      K
OPERATING PRESSURE      : 62000.00      PA

```

- OUTPUT DATA -

```

ROOTS OF R PRIME EQUATION:
(9.8279379E-06,0.0000000E+00)
(-7.4959885E-08,1.2115211E-06)
(-7.4959885E-08,-1.2115211E-06)

```

R PRIME AT PRECONDITIONS: 9.8279379E-06 M

FOR NUCLEATION AT OPERATING CONDITIONS
BY HE SUPERSATURATION

```

SATURATION CONCENTRATION: 1.6531916E-06 KG/M**3
HE SUPERSATURATION
REQUIRED FOR NUCLEATION : 107.7867      %

```

FOR NUCLEATION AT OPERATING CONDITIONS
BY SUPERHEAT.....

```

SATURATION TEMPERATURE : 1527.481      K
T WALL - T SAT          : 88.19434      K

```

APPENDIX H - BUBBLE DISTRIBUTIONS IN THE SP-100 PHTS

It appears that any small bubbles which may become entrained in the SP-100 Primary Heat Transport System (PHTS) coolant are likely to eventually collapse through a mass diffusion process. However, the time before collapse can be arbitrarily long depending on the coolant supersaturation and bubble size. Also, the possibility exists that bubbles may become trapped or held at some location outside the system gas separators. Under these conditions, the possibility arises that a source of bubbles from surface nucleation sites and/or the gas separators could balance collapse by mass diffusion leading to a quasi-steady state bubble population in the coolant. The spatial distribution of this bubble population within the PHTS components is of interest in order to better understand any effects that the bubbles may have on normal system operations. The following sections present a discussion of several mechanisms which were identified as having a potential impact on PHTS bubble distributions.

H.1 Bubble Traps in the PHTS

The possibility that gas bubbles may become trapped in the PHTS outside the gas separators is of concern for two reasons. First, bubbles may become trapped in a sensitive area and grow there, impacting system operation. Second, bubbles may become trapped in a non-sensitive area, grow over a period of time, and subsequently dislodge and relocate into a sensitive area.

Two mechanisms of bubble entrapment are envisioned: First, gas bubbles can become hydraulically trapped in flow eddies, stagnation points, or backwaters. Second, gas bubbles may attach themselves to

wall surfaces in the PHTS with a force sufficient to resist being swept away by the coolant flow.

A detailed treatment of the method and dynamics of hydraulic entrapment of bubbles will not be attempted, but, in general, entrapment could be expected to occur at points where a no flow or circular flow pattern is found. An example of a potential hydraulic trap is found in the PCA's entrance and exit headers. These components have a dead end zone past the exit for the last heat exchanger passage that could be expected to have little or no flow (See Figure 71). Bubbles entering this area could possibly remain for some time before being swept out by the passing coolant. Potential problem areas similar to these can be controlled by designing the loop to eliminate or minimize such points.

Turning now to the phenomena of bubble attachment to the PHTS wall surfaces, this problem can be formulated as a balance between surface tension forces holding the bubble to the wall, and drag forces tending to sweep the bubble away from the wall. As a bubble grows, increasing drag forces overcome surface tension forces, resulting in the release of the bubble from the wall. Several researchers have investigated this phenomena, primarily in the case of boiling nucleation on the walls of pipes containing flowing water. Unfortunately, typical correlations (eg. Levy, 1967) include the use of experimentally determined coefficients, making extension to liquid metals uncertain at best.

In an attempt to quantify the forces in question, consider first the forces which act to remove a bubble from a wall surface. Assuming the growing bubbles to be a smooth sphere, the drag forces exerted on a

spherical bubble by a uniform fluid flow can be calculated from the following formula (White, 1986):

$$F_d = C_d \rho V^2 A_c / 2 \quad (114)$$

where:

F_d - drag force (N)

C_d - Dimensionless drag coefficient, composed of sum of friction drag coefficient and pressure drag coefficient, a function of bubble Reynolds number ($Re_b = \rho 2rV/\mu$)

ρ - liquid density (kg/m^3)

V - liquid velocity (m/s)

A_c - bubble frontal area (m^2)

$$= \pi r^2$$

r - bubble radius (m)

μ - liquid dynamic viscosity (kg/m-s)

C_d and A_c are strong functions of the actual shape of the bubble, so if the bubble is deformed from the spherical by the fluid flow, F_d can be expected to change significantly. Values of C_d are tabulated by Clift et al. (1978) for rigid and fluid spheres.

Calculation of the drag forces by the above formula depends on a knowledge of the flow velocity near the bubble. Except for the PCA and reactor core at standby power, all PHTS components are predicted to operate in the turbulent flow regime. The velocity profile in this flow regime is characterized by a laminar boundary sublayer near the wall and a turbulent core region in the center of the flow area.

Liquid velocity is zero at the wall, and increases rapidly to a nearly constant value at the edge of the boundary layer. Small bubbles which attach themselves to the PHTS walls will be located in the laminar sublayer. The thickness of this sublayer can be estimated via the following formula (Bejan, 1984):

$$y = (\delta\mu/\rho V) \{(8/f)^{0.5}\} \quad (115)$$

where:

y - laminar sublayer thickness (m)

δ - a dimensionless constant in the range of 10 to 30.

ρ - fluid density (kg/m^3)

V - fluid velocity (m/s)

f - Darcy friction factor (dimensionless)

The calculated sublayer thickness ($\delta=30$) in each of the components of the SP-100 PHTS is presented in Table 27 for both standby and full power flow conditions. It appears that in most loop components, the laminar sublayer is relatively thin, on the order of 0.1 to 1 mm, although there are exceptions, primarily in the plenums at standby power, where the sublayer is calculated to be 5 to 7 mm thick. Thus the fluid velocity in most loop components becomes relatively large within less than a millimeter of the wall surface. For the two components in the laminar flow regime, the velocity profile as a function of radius is parabolic, changing smoothly from 0 at the wall to a maximum in the pipe center. In any case, the flow velocity changes significantly as a function of r , and is not uniform as required by (114).

In summary, the facts that the bubble shape may not be spherical, and that the flow field near the bubble is not uniform, considerably complicate the analysis bubble drag forces. It appears that an

accurate assesment of the drag forces on a small bubble resting on a PHTS wall surface requires a detailed hydrodynamic calculation, which will not be undertaken herein.

Consider next the forces which act to hold a bubble to a wall. A search of the literature reveals that the forces which cause bubbles attached to a wall to resist lateral movement are not well understood. However, a few qualitative results are available from studies of droplets sliding on inclined surfaces. These studies indicate that the forces opposing lateral motion are a function of contact angle and the base area of the bubble which is in contact with the surface (Bikerman, 1970). As either contact angle or base surface area is decreased, the forces preventing lateral movement of the bubble are reduced. Considering that liquid metals normally have exceedingly low contact angles (Addison, 1984) it appears likely that any forces tending to attach a bubble to a wall would be relatively small. If it is further assumed that the bubble base area is approximately the size of a microscopic surface cavity, this also serves to limit the magnitude of the attaching forces.

In summary, due to uncertainties in the calculation of bubble drag coefficients, and uncertainties in the calculation of the forces opposing lateral motion of a bubble attached to a surface, it is not possible to accurately quantify the number and size of bubbles that would remain attached to the PHTS walls at the various flow conditions present throughout the system life. However, a qualitative argument can be made that the maximum size of a bubble attached to a PHTS wall surface must be fairly small due to: 1) a low contact angle and base surface area, and; 2) large drag forces close to the wall resulting from the highly turbulent flow present in the majority of the loop.

Exceptions to this argument could be made in the case of components with laminar flow profiles (PCA's and core at standby power) and components with thick boundary layers (primarily reactor inlet and outlet plenums).

Figure 71 - Hydraulic Trap in Power Conversion Assembly

Table 27 - Estimated Laminar Sublayer Thickness

<u>Component Modeled</u>	<u>Sublayer Thickness (mm)</u>	
	<u>Full Power</u>	<u>Standby Power</u>
Piping #1	0.15	1.21
TE-EM Pump	0.06	0.45
Piping #2	0.15	1.21
PCA Inlet Header	0.20	1.59
PCA Heat Exchanger	0.08	Laminar
PCA Exit Header	0.21	1.58
Piping #3	0.15	1.21
Large Piping #1	0.09	0.73
Reactor Annulus	0.15	1.13
Upper Plenum	0.66	5.16
Reactor Core	0.09	Laminar
Lower Plenum	0.84	6.81
Large Piping #2	0.09	0.74
Accumulator	1.42	6.96

H.2 Normal Gravity Bubble Distributions

Several type of flow patterns are observed in two phase flow systems. These are extensively described in the literature (eg. Hsu and Graham, 1976) and will not be repeated here. Gas distributions in two phase pipe flow can be described in terms of flow maps which define different flow regimes as a function of coolant void fraction and flow velocity. A typical flow map proposed by Barnea et al. (1980) is presented in Figure 72. The flow is divided into dispersed bubble, slug, annular or stratified flow regimes. Assuming that a low void fraction ($<0.1\%$) will be maintained in the SP-100 PHTS by the gas separators, a low superficial gas velocity (gas volumetric flow rate (m^3/s) divided by the total channel cross sectional flow area (m^2)) can be expected in the loop. Under these conditions, the flow map predicts either a stratified, elongated bubble, or dispersed bubble flow regime depending on the superficial liquid velocity. However, the stratified flow regime is a result of gravitational effects which will not be present in SP-100 during system operations in space. As a result, the flow may be expected to be exclusively in the dispersed bubble regime. This effect is in fact confirmed by observation. Microgravity effects on flow regime are discussed further in the following section.

Several researchers have investigated void fraction distributions in air/water mixtures. At void fractions over 50% a parabolic distribution is usually found both in void fraction and bubble size (van der Welle, 1985) (see Figure 73). At lower void fractions and flow rates below 2 m/s a "saddle shaped" void fraction distribution is sometimes observed (Herringe and Davis, 1976) (see Figure 74). The type of distribution which is found appears to be a function of inlet

geometry, bubble size distribution, and liquid velocity (Sekoguchi et al. 1981).

Figure 72 - One G Two Phase Flow Map

(Barnea et al. 1980)

□	STRATIFIED SMOOTH (SS)	} STRATIFIED (S)
■	STRATIFIED WAVY (SW)	
○	ELONGATED BUBBLE (EB)	} INTERMITTENT (I)
●	SLUG (SL)	
△	ANNULAR (A)	} ANNULAR (A)
▲	WAVY ANNULAR (AW)	
▼	DISPERSED BUBBLE (DB)	

Figure 73 - Void Fraction Distributions: High Void Fraction
(van der Welle, 1985)

10 cm Dia Circular Pipe
Upward Vertical Flow
Air/Water
Superficial Liquid
Velocity: 1.7 m/s

Figure 74 - Void Fraction Distributions: Low Void Fraction
(Herringe and Davis, 1976)

5.08 cm Circular Pipe, Air/Water
Upward Vertical Flow
Superficial Water Velocity: 1.88 m/s

H.3 Micro-Gravity Effects

As mentioned in the previous section, it appears that the stratified flow regime is eliminated in a microgravity environment. This is demonstrated in an air/water microgravity flow map proposed by Dukler et al. (1988) and confirmed in drop tower and Learjet experiments (Figure 75). The transition between bubble and slug flow is described by the equation:

$$U_{LS} = 1.22 U_{GS} \quad (116)$$

where:

U_{LS} = superficial liquid velocity (m/s)

U_{GS} = superficial gas velocity (m/s)

Now considering the SP-100 system at normal operating conditions, if a slip ratio of 1.0 is assumed and a void fraction of 0.1%, then:

$$U_{LS} = 1000 U_{GS} \quad (117)$$

which is clearly well above the line given by (116), and well into the bubbly flow regime.

Figure 75 - Microgravity Flow Map

(Dukler et al. 1988)

H.4 Electro-Magnetic Field Effects

The design of the SP-100 includes electromagnetic pumps to circulate the primary and secondary coolant. As a result, the coolant will be subjected to a magnetic field as it passes through the pumps. It has been shown in studies relating to the design of magnetohydrodynamic generators that strong magnetic fields can influence the void distribution in a two phase liquid metal flow. This is of concern in that this effect may act to accelerate bubble coalescence.

In experiments with mercury and nitrogen, Reed and Lykoudis (1978) observed that a strong magnetic field acts to suppress the turbulent dispersion of bubbles by reducing the turbulence intensity in the flow. Gerbeth (1987), has approximated the results of Reed and Lykoudis in a manner which relates the turbulence intensity reduction to a ratio of two dimensionless numbers: the Hartmann number, and the Reynolds number (see Figure 76).

The Hartmann Number (Ha) can be thought of as expressing the ratio of magnetic body forces to viscous forces. It is defined by the following relationship:

$$Ha = (x^2 B^2 \sigma_e / \mu)^{0.5} \quad (118)$$

where:

x - characteristic length (m)

- for an EM pump, commonly the half channel length in the direction of the magnetic field ≈ 0.0089 m for the SP-100

B - magnetic flux density in the coolant (Tesla)

≈ 0.06 T for SP-100 at full power

σ_e = coolant electrical conductivity ($\text{ohm}^{-1} \text{m}^{-1}$)

$\approx 2.13 \times 10^6 \text{ ohm}^{-1} \text{m}^{-1}$ for Li at 1300 K

μ = coolant dynamic viscosity (kg/m-s)

$\approx 1.66 \times 10^{-4} \text{ kg/m-s}$ for Li at 1300 K

Using the values given above, Ha for the SP-100 at full power is found to be ≈ 60.5

The Reynolds number in the TE-EM pump at full flow is calculated by the PHTS computational model as $\approx 247,000$. Therefore,

$$\text{Ha/Re} = 62/247,000 = 0.25 \times 10^{-3} \quad (119)$$

Consulting Figure 76, it is clear that the effects of the magnetic field will be small. It can be concluded that the void distribution will be essentially unaffected as it passes through the TE-EM pumps.

Figure 76 - Turbulence Intensity in a Magnetic Field
(Gerbeth, 1987)

H.5 Inertially Induced Buoyancy Effects

As coolant circulates in the PHTS flow channels, it will experience radial accelerations upon encountering channel bends or swirl inducing devices. Under the influence of such accelerations, buoyancy effects could affect the distribution of any bubbles present in the coolant. Assuming that the bubbles are small enough not to be affected by the walls of the flow channel, the terminal velocity of the bubbles in the acceleration field can be calculated by a force balance between drag and buoyancy. The drag force can be calculated as:

$$F_D = C_D \rho V_r^2 A_c / 2 \quad (120)$$

where:

F_D - drag force on the bubble (N)

C_d - dimensionless total drag coefficient

ρ - liquid density (kg/m^3)

V_r - radial velocity of bubble (m/s)

A_c - cross sectional area of the bubble

The buoyancy force can be calculated as:

$$F_B = \Delta \rho g V_b \quad (121)$$

where:

F_B - buoyancy force on the bubble (N)

$\Delta \rho$ - difference between bubble density and liquid
density (kg/m^3)

g - acceleration (m/s^2)

V_b - volume of the bubble (m^3)

At terminal velocity, $F_D = F_B$ and, for a spherical bubble:

$$V_{rt}^2 = g\Delta\rho 4d / (3\rho C_D) \quad (122)$$

where:

d = bubble diameter (m)

V_{rt} = terminal bubble radial velocity (m/s)

Now, if C_D were known, V_{rt} could be calculated.

In general C_D is a function both of the shape and the liquid flow velocity. For all bubbles at low Reynolds numbers ($Re < 1$) it is observed that bubbles are spherical. Bubbles may retain their spherical shape at even higher Reynolds numbers ($Re \approx 1000$ or more) if the corresponding Eotvos number (Eo) is low ($< 10^{-1}$). Figure 77 presents a chart of bubble shapes as a function of Reynolds number and Eotvos number.

The drag coefficient of smooth rigid spherical particles is well known and can be expressed in the form of a standard drag curve (see Figure 78). This curve gives reasonably accurate results for rigid spheres. In the case of a bubble, in which the viscosity of the bubble is comparable to or less than the viscosity of the fluid through which it is moving, drag coefficients are observed to be lower than those given for a rigid sphere. This phenomena is attributed to internal circulation patterns which are set up within the bubble as it moves through the surrounding liquid. Spherical bubble drag coefficients can be estimated by the following formula (Clift et al. 1978):

$$C_D = 14.9 Re^{-0.78} \quad (123)$$

This formula represents the lowest possible drag coefficient for a bubble. In practical systems, the presence of even small amounts of surface active agents can increase the drag to that of a rigid sphere

by inhibiting internal circulation within the bubble. Any significant departure from a spherical shape can increase C_D even beyond that of a rigid sphere (Clift et al. 1978). Equation (123) is useful therefore as a lower limit on C_D .

Substituting (123) into (122) yields an expression for the upper limit on V_{rt} :

$$V_{rt} = 0.13829 \mu^{-0.6393} d^{1.459} (g\Delta\rho)^{-0.8197} \rho^{-0.1803} \quad (124)$$

where:

μ = liquid viscosity (kg/m-s)

ρ = liquid density (kg/m^3)

Considering a helium bubble at full power in the SP-100 PHTS, $\rho \approx \Delta\rho \approx 431 \text{ (kg/m}^3\text{)}$, $\mu \approx 1.66 \times 10^{-4} \text{ (kg/m-s)}$, and the upper limit on V_{rt} can be reduced to:

$$V_{rt} = 1755 d^{1.459} g^{0.8197} \quad (125)$$

At standby power, ($\rho \approx \Delta\rho \approx 494 \text{ (kg/m}^3\text{)}$, $\mu \approx 3.7 \times 10^{-4} \text{ (kg/m-s)}$):

$$V_{rt} = 1141 d^{1.459} g^{0.8197} \quad (126)$$

Now in order to determine V_{rt} , g must be known.

In the SP-100 PHTS, g will arise from centrifugal forces encountered when the coolant is forced to follow a curved trajectory. The radial acceleration in such a situation can be calculated as:

$$a_r = V_t^2 / r_{\text{curv}} \quad (127)$$

where:

a_r = radial acceleration (m/s^2)

V_t = liquid tangential velocity (m/s)

r_{curv} = radius of curvature (m)

The time the coolant spends in the g field is:

$$t = L_{\text{arc}} / V_t \quad (128)$$

where:

t = time spent traversing the arc (s)

L_{arc} = arc length (m)

Under the assumption that inertial effects are negligible, and that the bubble attains its terminal velocity instantaneously, the total radial distance the bubble could be expected to travel while in the arc can be computed as:

$$x_{\text{max}} = V_{rt} \quad (129)$$

where:

x_{max} = maximum bubble radial travel distance (m)

Table 28 presents bubble travel distances and other data for three representative SP-100 PHTS bend geometries: a core channel where the coolant is assumed to follow a swirl path defined by the wire wrap, a entrance to a PCA heat exchanger channel, and an entrance to the reactor flow annulus. Full power and a bubble radius of 15 μm are assumed to produce maximum bubble travel.

As can be seen in the Table, maximum bubble travel is only a small fraction of the flow channel diameter for the non-core components, and radial acceleration is therefore not expected to produce a significant bubble redistribution in these components. In the core the maximum travel distance is more than the diameter of the flow channel, indicating that centrifugal forces could be a major factor in determining the bubble distribution in the core, possibly causing

bubbles to move radially inward to cling along the fuel pins. This could be of some concern as a mechanism for a reduction in heat transfer from the pins. However, under the assumption of a low void fraction and an even bubble distribution between fuel pins, a total blanketing of any particular pin surface seems unlikely.

Figure 77 - Bubble Shape Regimes

(Clift et al. 1978)

Figure 78 - Standard Drag Curve for Smooth Rigid Spheres
(Clift et al. 1978)

Table 28 - Bubble Maximum Radial Travel Distances

<u>Data ID</u>	<u>Units</u>	<u>Core</u>	<u>PCA Hx Entrance</u>	<u>Entrance to Reactor Annulus</u>
Flow				
Channel Dia	(m)	3.8×10^{-4}	0.027	0.0425
r_{curv}	(m)	3.875×10^{-3}	0.0135	0.020
Axial Velocity	(m/s)	1.89	2.17	2.96
V_t	(m/s)	0.335	2.17	2.96
a_r	(m/s ²)	29.0	348.8	438.1
L_{arc}	(m)	0.09926	0.0212	0.0314
t	(s)	0.296	9.77×10^{-3}	0.0106
V_{rt}	(m/s)	6.94×10^{-3}	0.0533	0.0643
Re	dimensionless	0.54	4.15	5.00
Eo	dimensionless	4.25×10^{-5}	5.1×10^{-4}	6.4×10^{-4}
x_{max}	(m)	2.05×10^{-3}	5.2×10^{-4}	6.8×10^{-4}
x_{max}/D	%	540%	1.92	1.60

Notes:

- 1) Bubble radius = 15 μm
- 2) Full Power temperature and flow rate (1300 K, 11 kg/s,
 $\rho = 431 \text{ (kg/m}^3\text{)}, \mu = 1.66 \times 10^{-4} \text{ (kg/m}^3\text{))}$
- 3) Core wire wrap makes complete rotation of fuel pin
in 0.137 m, fuel pin length = 0.56 m, coolant assumed
to rotate with wrap.
- 4) Re and Eo based on bubble diameter and V_{rt}

H.6 Summary

Little data is available on two phase flow distributions in liquid metals subjected to a microgravity environment. Only tentative conclusions can be drawn from the available information. Both normal gravity and microgravity air/water flow maps indicate that a dispersed bubbly flow regime is likely in the SP-100 PHTS at low void fractions. Normal gravity air/water experiments in the bubbly flow regime indicate that the void fraction will be lowest near the walls and increase parabolically toward the center of the flow channel, but this result is highly dependent on entrance geometry and void fraction. Magnetic field effects in the pumps will not significantly alter the void distribution. Inertially induced buoyancy effects will not affect void distributions, except in the core, where any bubbles that enter are likely to travel to the fuel pin outer diameter prior to leaving the core.

Bubble entrapment in hydraulic eddies or at PHTS wall surfaces appears to be unlikely, but cannot be ruled out, and could be of concern as a potential mechanism of bubble blanketing of heat transfer surfaces, especially at the low flow rates associated with standby power operation. However, there is reason to believe that forces attaching a bubble to a wall would be small and ineffective at trapping bubbles in significant number or size.

Further research is required before reliable predictions of bubble distributions in the SP-100 PHTS can be made.

SELECTED BIBLIOGRAPHY

1. Addison, C. (1984), The Chemistry of Liquid Alkali Metals, John Wiley and Sons, New York, 1984, Chapter 16.
2. Alcouffe, R., et al (1984), User's Guide for TWODANT: A Code for Two Dimensional Diffusion Accelerated, Neutral Particle Transport, Los Alamos National Laboratory, Report No. LA-10049-M, March 1984.
3. Alefeld, G. and Volkl, J., Ed. (1978), Hydrogen in Metals I, Springer-Verlag, N.Y., 1978, pp329-330.
4. Angelo, J.A., Jr., and Buden, D. (1985), Space Nuclear Power, Orbit, Malabar, Fla., 1985, p ix.
5. Barnea, D., Shoham, O., Taitel, Y., and Duckler, A.E. (1980), "Flow Pattern Transition for Gas Liquid Flow in Horizontal and Inclined Pipes", International Journal of Multiphase Flow, Vol. 6, pp217-225, 1980.
6. Bejan, A. (1984), Convection Heat Transfer, John Wiley and Sons, New York, 1984, Chapter 7.
7. Bell, G.I., et al (1967), "Multitable Treatments of Anisotropic Scattering in S_n Multigroup Transport Calculations", Nuclear Science and Engineering, Vol 28, pp376-383, 1967.

8. Bell, G.I., and Glasstone, S. (1985), Nuclear Reactor Theory, Krieger, Malabar, Fla., 1985.
9. Bikerman, J.J. (1970), Physical Surfaces, Academic, New York, 1970, p278.
10. Bird, R.B., W.E. Stewart, and E.N. Lightfoot (1960), Transport Phenomena, John Wiley and Sons, New York, 1960, p646.
11. Blackledge, J.P. (1968), in Metal Hydrides, Mueller, W.M., et al., Eds., Academic Press, N.Y., 1968, p7.
12. Bondarenko, I.I., Ed. (1964), Group Constants for Nuclear Reactor Calculations, Consultants Bureau, N.Y., 1964.
13. Borgstedt, H.G., Ed. (1982), Material Behavior and Physical Chemistry in Liquid Metal Systems, Plenum, 1982.
14. Brandes, E.A., Ed. (1983), Smithells Metals Reference Handbook, Edition 6, Butterworth, London 1983.
15. Brandon, D. (1988), Personal communication, Group N12, Los Alamos National Laboratory, reference letter from R. Protsik of General Electric Company, XL-T22 -88087, 26 April 1988.
16. Calderbank, P.H., Moo-Young, M.B., and Bibby, R. (1964), "Coalescence in Bubble Reactors and Absorbers", Chemical Reaction Engineering, a Supplement to Chemical Engineering Science, Proceedings

of the Third European Symposium, held in Amsterdam, 15-17 September, 1964, pp91-110, Pergamon, New York, 1965.

17. Chase, M.W., Jr., et al. (1985), JANAF Thermochemical Tables, Third Edition, Part II, Journal of Physical and Chemical Reference Data, Vol 14, Supplement 1, American Chemical Society and American Institute of Physics for the National Bureau of Standards, 1985, p1225.

18. Chen, J.C. (1968), "Incipient Boiling Superheats in Liquid Metals", Transactions of the ASME, Series C, Journal of Heat Transfer, Vol. 90:303, 1968.

19. Chen, J.D., Hahn, P.S., and Slattery, J.C. (1984), "Coalescence Time for a Small Drop or Bubble at a Fluid-Fluid Interface", AIChE Journal, Vol. 30, No.4, pp622-630, July, 1984.

20. Clift, R., Grace, J.R., and Weber, M.E. (1978), Bubbles, Drops, and Particles, Academic Press, New York, 1978, Chapters 2 and 5.

21. Cole, R. (1974), "Boiling Nucleation", Advances in Heat Transfer, Vol. 10, pp 85-166, 1974, Academic Press, New York.

22. Collins, S.B., and Knudsen, J.G. (1970), "Drop-Size Distributions Produced by Turbulent Pipe Flow of Immiscible Liquids", AIChE Journal, Vol. 16, No. 6, pp1072-1080, November, 1970.

23. Cooper, R.H., and Hoffman, E.E., Eds. (1983), Proceedings of the Symposium on Refractory Alloy Technology for Space Power Applications, CONF-8308130, Oak Ridge, Tenn., 10-11 August 1983.
24. Dukler, A., Fabre, J., McQuillen J., and Vernon, R. (1988), "Gas-Liquid Flow at Microgravity Conditions: Flow Patterns and Their Transitions", International Journal of Multiphase Flow, Vol.14, No.4, pp389-400, 1988.
25. Dwyer, O.E. (1969), "On Incipient Boiling Wall Superheats in Liquid Metals", International Journal of Heat and Mass Transfer, Vol. 12, p 1403, 1969.
26. Dwyer, O.E. (1976), Boiling Liquid Metal Heat Transfer, American Nuclear Society, 1976, Chapter 1.
27. El-Genk, M.S., and Seo, J.T. (1987), "SNPSAM - Space Nuclear Power Systems Analysis Model", in Space Nuclear Power Systems 1986, El-Genk M.S., and Hoover, M., Eds., CONF-860102, Orbit Book Company, Malabar, Fl., Vol 5, 1987, pp187-198.
28. El-Genk, M.S., and Seo, J.T. (1988), Space Nuclear Power System Analysis Model (SNPSAM), Final Report, AFWL-TR-87-36, Air Force Weapons Laboratory, Albuquerque, New Mexico, April 1988.
29. El-Wakil, M.M. (1971), Nuclear Heat Transfer, American Nuclear Society, 1971.

30. Fast, J.D. (1971), Interaction of Metals and Gases - Vol 2, Macmillan, London, 1971.
31. Garber, D.I., and Kinsey R.R. (1976), Neutron Cross Sections - Vol 2- Curves, 3rd Ed., Brookhaven National Laboratory, Report No. BNL-325, Jan 1976.
32. General Electric Company (1985), SP-100 Ground Engineering System Baseline Definition and Characterization Study - Final Report. Vol I, General Electric Company, Doc No. 85SDS4268, Aug 1985.
33. General Electric Company (1988a), Materials presented at SP-100 Project Integration Meeting (PIM), Long Beach, California, 19-21 July 1988.
34. General Electric Company (1988b), "Draft SP-100 Reference Flight System Design Description", DRD SE004, Astro Space Division, Philadelphia, PA, 19 July 88.
35. Gerbeth, G., and Hamann, D. (1987), "Influence of a Transverse Magnetic Field on Phase Distribution in Liquid Metal Two-Phase Flows", Kernenergie, Vol.30, No.4, pp149-153, 1987.
36. Gherson, P. and Lykoudis, P. (1984), "Local Measurements in Two-Phase Liquid-Metal Magneto-Fluid-Mechanic Flow", Journal of Fluid Mechanics, Vol. 147, pp81-104, 1984.

37. Ginoux, J.J., Ed. (1978), Two-Phase Flows and Heat Transfer, Hemisphere, Washington, 1978.
38. Habip, L. (1965), "On the Mechanics of Liquids in Subgravity", Astronautica Acta, Vol. 11, No. 6, pp401-409, 1965.
39. Hahn, P.S., Chen, J.D., and Slattery, J.C. (1985), "Effects of London-van Der Waals Forces on the Thinning and Rupture of a Dimpled Liquid Film as a Small Drop or Bubble Approaches a Fluid-Fluid Interface", AIChE Journal, Vol. 31, No.12, pp2026-2038, December, 1985.
40. Hahn, P.S., and Slattery, J.C. (1985), "Effects of Surface Viscosities on the Stability of a Draining Plane Parallel Liquid Film as a Small Bubble Approaches a Liquid-Gas Interface", AIChE Journal, Vol. 31, No. 6, pp950-956, June, 1985.
41. Herringe, R. and Davis, M. (1976), "Structural Development of Gas Liquid Mixture Flows", Journal of Fluid Mechanics, Vol. 73, pp97-123, 1976.
42. Ho, C.Y., and Pouloukian (1970), Thermophysical Properties of Matter, TPRC Data Series, Purdue University, Lafayette, Ind., IFI/Plenum, New York, NY, 1970.
43. Hinze, J.O. (1955), "Fundamentals of the Hydrodynamic Mechanism of Splitting in Dispersion Processes", AIChE Journal, Vol. 1, No. 3, pp289-295, September, 1955.

44. Hirth, J.P., and Pound, G.M. (1963), "Condensation and Evaporation, Nucleation and Growth Kinetics", Progress in Materials Science Vol. 11, Chalmers, B., Ed., Macmillan, New York, 1963.
45. Hsu, Y.Y., Graham, R.W. (1976), Transport Processes in Boiling and Two-Phase Systems, Hemisphere, Washington, 1976.
46. Hughmark, G.A. (1971), "Drop Breakup in Turbulent Pipe Flow", AIChE Journal, Vol. 17, No.4, p1000, July, 1971.
47. Hughes, W., and McNab, I. (1981), "A Quasi-One-Dimensional Analysis of an Electromagnetic Pump Including End Effects", presented at the Third Beer-Sheva International Seminar on Magnetohydrodynamic Flows and Turbulence, Ben-Gurion University of the Negev, Beer-Sheva, Israel, March 23-27, 1981.
48. Jackson, S.V. (1987), "SP-100 Nuclear Subsystems - Systems Engineering Progress Report", Los Alamos National Laboratory unpublished internal memorandum, symbol A-4/87-998U, 24 November 1987.
49. Jeppson, D.W., et al. (1978), Lithium Literature Review: Lithium's Properties and Interactions, HEDL-TME:78-15, Hanford Engineering Development Lab, Richland, Wa., April, 1978.
50. Keesom, W.H. (1942), Helium, Elsevier, Amsterdam, 1942, p132.

51. Keshock, E. and Siegel, R. (1964), Forces Acting on Bubbles in Nucleate Boiling Under Normal and Reduced Gravity Conditions, NASA TN D-2299, April, 1964.
52. Kinsey, R., Ed. (1983), ENDF-102 Data Formats and Procedures for the Evaluated Nuclear Data File ENDF/B-V, BNL-NCS-50496, Nov 1983.
53. Kirkpatrick, R.D., and Lockett, M.J. (1974), "Influence of Approach Velocity on Bubble Coalescence", Chemical Engineering Science, Vol. 29, pp2363-2373, 1974.
54. Kruger, G.B., et al (1987), "SP-100 Reactor Design", 22nd Intersociety Energy Conversion Engineering Conference, Philadelphia, Penn., August 10-14, 1987, Vol 1, pp419-423.
55. Lamarsh, J.R. (1975), Introduction to Nuclear Engineering, Addison Wesley, Reading Mass., 1975.
56. Lee, D., and Best, F. (1988), "Microgravity Two Phase Flow Pattern Modeling", Transactions of Fifth Symposium on Space Nuclear Power Systems, CONF-880122-Summs., pp451-455, Albuquerque, New Mexico, 11-14 January, 1988.
57. Levy, S. (1967), "Forced Convection Subcooled Boiling- Prediction of Vapor Volumetric Fraction", International Journal of Heat and Mass Transfer, Vol.19, pp951-965, 1967.

58. Ma, B.M. (1983), Nuclear Power Materials and Applications, Van Nostrand Reinhold, New York, NY, 1983.
59. MacFarlane, R. (1978), The NJOY Nuclear Data Processing System: User's Manual, Los Alamos National Laboratory, Report No. LA-7584-M, 1978.
60. MacFarlane, R. (1984), TRANSX - A Code for Interfacing MATXS Cross Section Libraries to Nuclear Transport Codes, Los Alamos National Laboratory, Report No LA-9863-MS, Feb 1984.
61. Magee, C.B. (1968), in Metal Hydrides, Mueller, W.M., et al, Eds., Academic, New York, 1968, pp 165-240.
62. Margulis, M.A., and Dmitrieva (1983), "Dynamics of Coalescence of Bubbles in Gas-Liquid Systems", Teoreticheskie Osnovy Khimicheskoi Tekhnologii, (English translation), Vol. 12, No. 2, March-April, 1983.
63. Magurno, B.A., et al. (1982), Guidebook for the ENDF/B-V Nuclear Data Files, Brookhaven National Laboratory, Report No. BNL-NCS-31451, Jul 1982.
64. Marrucci, G. (1969), "A Theory of Coalescence", Chemical Engineering Science, Vol. 24, pp975-985, 1969.
65. Marrucci, G., Nicodemo, L., and Acierno, D. (1969), "Bubble Coalescence Under Controlled Conditions", Cocurrent Gas-Liquid Flow, Rhodes, E., and Scott, D.S. Eds., Proceedings of an International

Symposium on Research in Cocurrent Gas-Liquid Flow held at the University of Waterloo, Canada, September 18-19, 1968, Plenum, New York, 1969.

66. Miller, D.N. (1983), "Interfacial Area, Bubble Coalescence, and Mass Transfer in Bubble Column Reactors", AIChE Journal, Vol. 29, No.2, pp312-319, March, 1983.

67. Minkowycz, W.J., France, D.M., and Singer, R.M. (1976), "Behavior of Inert Gas Bubbles in Forced Convective Liquid Metal Circuits", Journal of Heat Transfer, Transactions of ASME, Vol 98, No.1, pp 5-11, February, 1976.

68. Mukherjee, H., and Brill, J. (1985), "Empirical Equations to Predict Flow Patterns in Two-Phase Inclined Flow", International Journal of Multiphase Flow, Vol. 11, No. 3, pp229-315, 1985.

69. O'dell, R.D. (1987), Transport Calculations for Nuclear Analysis: Theory and Guidelines for Effective Use of Transport Codes, Los Alamos National Laboratory, Report No. LA-10983-MS, Sep, 1987.

70. Paul, H.I., and Sleicher, C.A., Jr. (1965), "The Maximum Stable Drop Size in Turbulent Flow: Effect of Pipe Diameter", Chemical Engineering Science, Vol. 20, pp57-59, 1965.

71. Petrick, M., et al. (1979), Experimental Two-Phase Liquid-Metal Magnetohydrodynamic Generator Program - Final Report, ANL/MHD-79-1, Argonne National Lab, April, 1979.

72. Rapperport, E.J., and Hartley, C.S. (1962), "A Review of Diffusion in Refractory Metal Systems", in Refractory Metals and Alloys II, Semchyshen, M. and Perlmutter, I., Eds., Interscience, N.Y., 1962.
73. Reed, C., and Lykoudis, P. (1978), "The Effect of a Transverse Magnetic Field on Shear Turbulence", Journal of Fluid Mechanics, Vol. 89, part 1, pp147-171, 1978.
74. Reed, E., and Droher, D. (1969), Solubility and Diffusivity of Inert Gases in Liquid Sodium Potassium and NaK, LMEC-69-36, AEC R&D Report, Liquid Metal Engineering Center, 1969.
75. Reith, I.T., and Beek, I.W. (1970), "Bubble Coalescence Rates in a Stirred Tank Contactor", Transactions of the Institute of Chemical Engineers, Vol. 48, pp T63-T68, 1970.
76. Rezkallah, K. (1988), "Two-Phase Flow and Heat Transfer at Reduced Gravity: A Literature Survey", ANS Proceedings of the National Heat Transfer Conference, Vol. 3, pp435-444, Houston, Texas, 24-27 July 1988.
77. Ronen, Yigal, Ed. (1986), CRC Handbook of Nuclear Reactors Calculations, CRC Press, Boca Raton, Fla., 1986.
78. Sagert, N.H., and Quinn, M.J. (1978), "The Coalescence of Gas Bubbles in Dilute Aqueous Solutions", Chemical Engineering Science, Vol. 33, pp1087-1095, 1978.

79. Sato, Y., Sadatomi, M., and Sekoguchi, K. (1981), "Momentum and Heat Transfer in Two-Phase Bubble Flow", International Journal of Multiphase Flow, Vol.7 , pp167-177, 1981.
80. Schlunder, E.U., et al., Eds. (1983), Heat Exchanger Design Handbook, Hemisphere Publishing Corporation, 1983, Section 2.5.1.
81. Sekoguchi, K. , Fukuki, H., and Sato,Y. (1981), "Flow Characteristics and Heat Transfer in Vertical Bubbly Flow", in Two-Phase Flow Dynamics, Bergles, A. and Ishigai, S., Eds., pp59-74, Hemisphere, 1981.
82. Seo, J.T. (1986), "Space Nuclear Power Systems Analysis Model", M.S. Thesis, University of New Mexico, Albuquerque, NM, 1986.
83. Seo, J.T., and El-Genk, M.S. (1988), "Preliminary Analysis of He Generation and Its Effects on SP-100 System Performance", Transactions of the 5th Symposium on Space Nuclear Power Systems, CONF-880122, Albuquerque, New Mexico, January 11-14, 1988, pp153-158.
84. Serizawa, A., and Michiyoshi, I. (1973), "Void Fraction and Pressure Drop in Liquid Metal Two-Phase Flow", Journal of Nuclear Science and Technology, Vol.10, no. 7, pp435-445, July 1973.
85. Serizawa, A., Katoka, I., and Michiyoshi, I. (1975), "Turbulence Structure of Air-Water Bubbly Flow", International Journal of Multiphase Flow, Vol. 2, No.3, pp221-259, 1975.

86. Sevik, M., and Park, S.H. (1973), "The Splitting of Drops and Bubbles by Turbulent Fluid Flow", Journal of Fluids Engineering, Transactions of the ASME, Vol. 95, No.1, pp53-60, March, 1973.
87. Siegel, R. (1967), "Effects of Reduced Gravity on Heat Transfer", in Advances in Heat Transfer Vol 4, Hartnett, J. and Irvine, T., Eds., pp143-228, Academic, New York, 1967.
88. Singer, R.M., France, D.M., and Minkowycz, W.J. (1977), "A Simplified Model of Inert Gas Dynamics in Liquid Metal Circuits", International Journal of Heat and Mass Transfer, Vol 20, No. 1, pp 87-88, January 1977.
89. Sleicher, C.A., Jr. (1962), "Maximum Stable Drop Size in Turbulent Flow", AIChE Journal, Vol.8, No.4, pp471-477, September, 1962.
90. Slotnick H.S., et al. (1965), The Solubility of Helium in Lithium and Potassium, PWAC-380, Pratt and Whitney Aircraft Co., Middletown, Conn., 24 February, 1965.
91. Smith, F.J., et al. (1975), "Current Tritium Chemical Studies at Oak Ridge National Laboratory", in Proceedings of the Symposium on Tritium Technology Related to Fusion Energy Systems, Smith, W.H., et al., Eds., ERDA-50, CONF-741050, June 1975, p43.
92. Spalding, D. (1980), "Multiphase Flow Prediction in Power-System Equipment and Components", International Journal of Multiphase Flow, Vol. 6, pp157-168, 1980.

93. Stern, S.A. (1961), in Argon, Helium, and the Rare Gases, Cook, G.A., Ed., Interscience, New York, 1961, p216.
94. Stralen, Sjoerd van, and R. Cole (1979), Boiling Phenomena Vol I and II, Hemisphere, 1979, Washington, D.C., p259-260, 425.
95. Swartz, J.E., Kessler, D.P. (1970), "Single Drop Breakup in Developing Turbulent Pipe Flow", AIChE Journal, Vol. 16, No. 2, pp254-260, March, 1970.
96. Thomas, R.M. (1981), "Bubble Coalescence in Turbulent Flows", International Journal of Multiphase Flow, Vol. 7, No. 6, pp709-711, 1981.
97. Tipton, J.R., Ed. (1960), Reactor Handbook, 2nd Edition, Vol 1 - Materials, Interscience, NY, 1960.
98. Touloukian, Y., Saxena, S., and Hestermans, P. (1975), Viscosity - Vol. 11 of The Thermophysical Properties of Matter, Plenum, New York, 1975.
99. Truscello, V.S., Davis, H.S. (1984), "Nuclear Electric Power in Space", IEEE Spectrum, Dec 1984, pp58-65.
100. Walter, A.E., and Reynolds, A.B. (1981), Fast Breeder Reactors, Pergamon, N.Y., 1981, p354.

101. Welle, R. van der (1985), "Void Fraction, Bubble Velocity, and Bubble Size in Two-Phase Flow", International Journal of Multiphase Flow, Vol. 11, No.3, pp317-345, 1985.
102. Westwater, J.W. (1958), "Boiling of Liquids", in Advances in Chemical Engineering Vol. II, Drew, T.B., and Hoopes, J.W., Jr., Eds., Academic, New York, 1958.
103. White, Frank M. (1986), Fluid Mechanics. 2nd Edition McGraw-Hill, New York, 1986, Chapter 6.
104. Zettlemoyer, A.C., Ed. (1969), Nucleation, Marcel Dekker, New York, 1969.

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