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**"Local Measurement of Fuel Energy Deposition and Heat Transfer Environment
During Fuel Lifetime Using Controlled Calorimetry"**

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1.0 INRODUCTION

1.1 Report Overview

This report provides a comprehensive description of the accomplishments of the DOE Grant titled, "Local Measurement of Fuel Energy Deposition and Heat Transfer Environment During Fuel Lifetime using Controlled Calorimetry" . The remainder of the first chapter has three sections, a summary of program objectives, a brief description of the constant temperature power sensor (CPS) and a summary of accomplishments. Included in the summary of accomplishments is an overview of the last four chapters of the report, which address the following topics, sensor development, numerical modeling of the sensor, the in-reactor test facility and sensor control and temperature compensation.

1.2 Program Objectives

The following objectives were established in the proposal for the research program described in this report:

- Develop an incore sensor that is capable of direct measurement of the parameters most closely related to basic reactor safety limits; fuel melt and core coolability.
- Provide the basis for incorporation of this incore sensor into the fuel in next generation reactors
- Develop a comprehensive numerical model of the sensor for use in developing experimental methods a comparison with experimental measurements
- Design, construct and test an in-reactor sensor test facility that provides a synergistic neutronic/ high temperature environment comparable to the environment expected in next generation reactor designs.
- Design, construct and test sensor electronics.
- Fabricate sensor prototypes.
- Verify operation of the sensor electronics when used with operational prototype in a neutron flux.
- Test sensor prototypes in the in-reactor facility.
- Compare test results with numerical model.
- Refine numerical model based on test results with the goal of providing a design basis for application in next generation reactors.

1.3 A Brief Description of In-Core Power Instrumentation and the Constant-Power Temperature Sensor

Current-generation operating reactors are provided with safety-grade power sensors to protect thermal limits to fuel melt and cladding surface critical heat flux. These thermal limits are monitored indirectly. Quantum-principle sensors, such as fission chambers, ion chambers and self-powered neutron detectors, are used to measure in-core or ex-core neutron flux. These flux measurements must be related to the local fuel energy deposition, often requiring calculation of a peak-to-average power ratio that must bound all fuel cycle conditions. Critical heat flux limits are protected through a correlation limit that depends on global measurements of coolant temperature, pressure and flow rate as well as the estimated local power as described above. Although calculation of the

relationship of global-to-local parameters has been greatly advanced by the use of on-line core simulations, *a priori* knowledge is still the goal. Direct local measurements of deposited power and coolant thermal-hydraulic conditions would support increased operating margin and allow collection of detailed fuel operational history records, which could improve prediction of fuel performance over time.

A calorimetric instrument that shows promise of attaining these goals has been under development at The Ohio State University and The University of Akron since 1996. This concept, which we denote the constant temperature power sensor (CTPS), is based on the energy balance about a fuel pellet. Energy (Q) is deposited in the fuel pellet through several different nuclear interactions while energy is removed through heat transfer to the reactor coolant. Since heat transfer is a function of temperature, the pellet will be hotter than the coolant and, given constant energy deposition, will maintain an equilibrium temperature somewhat above the surrounding coolant temperature. Placing thermocouples in the pellet and coolant to measure the equilibrium temperature difference will give a direct measurement of the energy deposition in the fuel if the effective heat transfer coefficient UA is known. At steady-state,

$$Q_{\text{deposited}} = Q_{\text{removed}} = UA (T_{\text{fuel average}} - T_{\text{bulk coolant}}) .$$

A classical thermal sensor of this type is called a fission pile and was considered obsolete 25 years ago (Boland, 1972) because it is slow and changes calibration as the fissile material depletes.

Now consider an electric heater element placed inside the fuel pellet as shown in Fig. 1-1. Introducing a current through this element will cause energy deposition in the pellet through resistive dissipation in addition to any nuclear energy deposited. Controlling the added electrical energy will allow us to set the fuel pellet temperature to a value above that resulting from the deposited nuclear energy. Also, note that the resistance of the heater element is directly related to its temperature by a coefficient of resistivity. The heater element, then, allows both control and measurement of the local fuel pellet temperature. Given a known coolant bulk temperature and effective heat transfer coefficient, measurement of the fuel temperature thus becomes a measurement of the power deposition in the fuel, a variation on the fission pile concept.

We can improve on this by placing the element resistance in a feedback control loop and controlling the energy addition to the heater to maintain a constant heater resistance, and therefore constant heater average temperature, regardless of the nuclear energy deposition in the fuel pellet. If UA and T_{coolant} remain in equilibrium, the electrical power added to the pellet becomes an inverse measure of the deposited nuclear energy,

$$Q_{\text{dep, nuclear}} + Q_{\text{dep, electrical}} = UA (\Delta T) = \text{Constant} ,$$

or

$$Q_{\text{dep, nuclear}} = \text{Constant} - Q_{\text{dep, electrical}} .$$

This inverse signal is unique in reactor power sensing; the signal will be large at zero reactor power, and must be designed to go to zero at a sensor power limit well above the highest expected local reactor power.

To effect constant temperature control, the conductor is placed in a Wheatstone bridge also shown in Fig. 1-1. Here, the current supplied to the bridge is controlled by any imbalance resulting from change in the sensor heated element resistance. By setting the variable bridge reference resistance R_S to a value that corresponds with the element resistance R_W at a desired temperature, this circuit will provide the current necessary to maintain the element temperature constant. This type of constant temperature control has been well-developed in the field of hot-wire anemometry (Lomas, 1986).

There are many advantages to this sensor design. Foremost, this sensor can provide a truly direct measurement of the local deposited energy. The sensor can literally be a fuel pellet if desired. All of the components of the signal, whether from fission fragments, neutron, beta or gamma interactions, are in the correct proportion. An array of these sensors would provide an accurate map of core power distribution, not neutron flux, as needed to evaluate thermal margin.

The overall performance of this sensor should also be good. Given the same fissile loading, a CTPS will have a significantly greater sensitivity than a fission chamber simply because all the fission fragments contribute to the measured energy balance while more than half of the energy released in a fission chamber ineffectively heats the fissile lining or exterior wall. In addition, the loading of fissile material in the CTPS is not restricted to a thin film as in the fission chamber but can be spread over the entire sensor volume, so the fissile mass in the sensor can be an order of magnitude higher than that in an in-core fission chamber, giving proportionally higher sensitivity. The ultimate limit to sensitivity is the amount of heat that can be dissipated to the reactor coolant. Increased sensitivity, along with the fact that the non-depleting gamma signal is a desirable component of the sensor output and not "noise" as in the case of a fission chamber, means that the sensor should also have a much longer useful lifetime than a fission chamber.

These benefits could also be attributed to a fission pile sensor, but this sensor has been categorically rejected as too slow for safety-related system use. This is the first advantage of constant temperature control. To measure a change in power with other classical sensors such as fission piles or a gamma thermometers, it is necessary that the energy deposition in the heated element and the heat transfer to the environment come into equilibrium through change in the element temperature. This is a slow process. An example is the gamma thermometer. In this instrument, the sensing element must have a relatively high mass and the heat transfer path to the coolant must be restrictive. If the element is, say, 10°C above ambient at full power, then measuring a reactor power reduction of 50 percent will require the sensor element temperature to drop to 5°C, with the excess energy slowly bleeding off through a restrictive thermal path with little driving temperature differential. In the CTPS, the thermal inertia of the sensor is much less

relevant because the temperature of the element is held nearly constant. As reactor power drops, the element temperature will begin to drop. This will occur at a faster rate than in the gamma thermometer simply because the driving temperature differential can be hundreds of degrees, regardless of the reactor power level. When the temperature of the heated element begins to drop, the coincident change in element resistance unbalances the Wheatstone bridge and current is added to restore the temperature, providing a fast measurement of the change in reactor power. Temperature variations of a fraction of a degree are sufficient for this control. The combination of higher temperature differential and nearly constant temperature allow a sensor bandwidth orders of magnitude higher than other classical sensors, giving the CTPS design potential for use as safety-grade instrumentation.

The second advantage of constant temperature control is simply that the two-wire sensor is in a feedback control loop. This allows control manipulations to extract information about the instrument dynamic response and the heat transfer characteristics of the surrounding region *in situ*. Consider the earlier case where the $UA\Delta T$ is constant and the fuel temperature is controlled. A change in $Q_{dep, nuclear}$ causes an inversely proportional change in $Q_{dep, electrical}$. If we then disable the constant temperature control and assume that the $Q_{dep, nuclear}$ stays momentarily constant, the removal of this known amount of power through the heater element will cause a decrease in the temperature of the sensor measurable through the heater resistance. The sensor temperature will decay, with characteristic dynamics, to the steady-state temperature the sensor would achieve with only nuclear heating. The measured time constant of this temperature decay is related to the effective heat transfer coefficient UA , and this value contains information about the local convective heat transfer coefficient h . On the other hand, the steady-state temperature of the sensor can be directly related to the bulk coolant temperature given UA and $Q_{dep, nuclear}$. Once this dynamic data is sampled the temperature control can be reinstated to continue constant-temperature power deposition measurements. This "mode-switching" operation, then, should allow effectively simultaneous measurement of nuclear power deposition, the convection heat transfer coefficient and the bulk fluid temperature.

Mode switching of the sensor could be performed often, perhaps once a minute, to compensate the sensor signal for changes in external thermal environment *i.e.* when $UA\Delta T$ is not constant. This is necessary if a two-wire sensor is desired and thus other means of compensation are unavailable. If other compensation is included in the in-core sensor through use of a three-wire design, then dynamic thermal measurements can be taken whenever desired. Comparison of the dynamic responses with expected as-built responses could also be used as a sensor diagnostic. Significant variations in the response might be characteristic of cracking of the insulator or ingress of coolant. We anticipate that this mode-switching capability would allow the sensor to provide meaningful measurements with substantial degradation such as might be expected in a post-accident environment. Other useful information such as core coolability or coolant level could also be inferred from a CTPS array.

The CTPS design can complement new reactor designs in which its specific characteristics can be applied to best advantage. The mode-switching design could be easily accommodated by the digital safety systems currently being developed. In addition, a reactor design that incorporates fixed sensors into the fuel assemblies would result in sensors that deplete along with the fuel, obviating the need to compensate for depletion of the sensor and making the issue of radioactive sensor disposal moot. The application of controlled calorimetric sensors to Generation IV reactors is being studied under the auspices of a Department of Energy NERI grant.

There is only one clear disadvantage for this design. The advantage of increased sensitivity comes at the price of increased heat dissipation. A string of ten sensors in a fuel assembly might add 100 watts of energy to the instrument coolant channel, so channel heat removal capability must be evaluated for specific reactor applications.

Figure 1.1 The basic constant-temperature power sensor and its associated controller circuitry.

1.4 Program Accomplishments

A task we have found to be substantially more challenging than expected was the design and fabrication of sensor prototypes that will reliably operate in environments expected in High Temperature Gas Reactors. The difficulty in sensor fabrication lead to a substantial increase in effort and resources directed to the sensor program. At the end of the first year of this program we initiated a NERI program titled “An In-Core Power Deposition and Fuel Thermal Environment Monitor for Long-Lived Reactor Cores.” This program also included a sensor development program, albeit with different environmental requirements. As a consequence we combined the two programs. Chapter 2 is a comprehensive report of the research completed in this combined program. Since research completed on sensor development was funded primarily by NEER, the sensor report is included as Chapter in this report and was included as an Appendix in the NERI program annual report.

We initially attempted to utilize a method comparable the used to construct the first prototype. Following many attempts we realized that due to the high operational temperature we would not be able to fabricate a sensor with the required performance characteristics. As shown in subsection 2.1, material properties are the limiting factors

and that the maximum temperature a sensor would be able to operate with this geometry is approximately 750C.t

As is often the case finding a solution for this problem has lead to an innovative approach using printed circuit methods. We are optimistic that this approach, which is described subsection 2.2, will operate at the required operational temperature and exhibit the specified performance characteristics.

A comprehensive numerical model of the sensor and related control system was developed. The model, which is documented in Chapter 3, has been used for the design of the next generation CTPS, which is described in subsection 3.2 and a new but related sensor, the constant heat flux power sensor, which has been designed and will be fabricated and tested for NERI program.

An in-reactor sensor test facility was designed, constructed and tested. This facility provides a synergistic neutronic/ high temperature environment comparable to the environment expected in next generation reactor designs. Currently the facility provides an environment comparable to a high temperature gas reactor. In the next year it will be modified to also provide an environment comparable to the environment expected in the Westinghouse IRIS.

This facility, which is documented in Chapter 4 of this report, is a significant contribution of the DOE NEER program that may be as important s the primary objective of the research program. It provides a facility, which is truly unique among research reactors world wide. It is currently the key experimental facility for a NERI Program, "In-Core, Self-Powered Flux and Temperature Probe for HTGRs" and an I-NERI Program "Development of Enhanced Reactor Operation Strategy Through Improved Sensing And Control at Nuclear Power Plants", which are just beginning at Oak Ridge National Laboratory with Ohio State as a subcontractor. These programs put Ohio State and ORNL in a leadership position to design, construct and test many of the next generation measurement systems for Generation IV nuclear power plants. We expect additional research related to Generation IV NPPs will be conducted at the OSU NRL will use this facility.

Chapter 5 has two sections, "Control and Data Acquisition" and "Dynamic Thermal Environment Measurements and Mode-Switching Compensation". These two topics were included in this chapter because they both relate to control and data acquisition from the constant temperature power sensor (CTPS).

The control system and the temperature compensation methodology were used as the basis in the program sponsored NERI, where a PID controller was incorporated into the control system and a second order model was developed for temperature compensation. The two systems wee integrated and tested using a variety of sensor surrogates.

2.0 Development of Solid State Sensors for Use in Nuclear Reactor Cores to Monitor Fuel Status

2.1 Introduction

Two prototype designs for “in core” fuel sensors have been developed and are being pursued. The cylindrical design is shown in Figure 2.1 its intended use is in reactors, which operate at temperatures below 700°C. The planar design is shown in Figure 2.2 and it is intended for use in reactors operating at temperatures up to 1400°C. The cylindrical design has been challenging to implement, because of the small-scale mechanical operations required and because of several problems with compatibility of the materials used. Most of the problems have been solved or will soon be solved. Except for the outer package, completed cylindrical sensors should be available within the next six weeks. The planar sensor uses screen printing technology similar to that found in the microelectronics industry. The main innovation required is development of special screen printing inks based on the high temperature materials needed in this design. We also have to formulate suitable inks containing high concentrations of U^{235} and U^{238} to form the sensor body itself. Much of the materials technology developed for the cylindrical sensor will carry over to the planar sensor. In the long term, it will probably be much simpler and cheaper to manufacture the planar design than the cylindrical design. Progress on these two designs is reported in separate sections of this report.

2.2 Cylindrical Sensor

The cylindrical design is shown in Figure 2.1. It calls for production of ceramic cores ~0.7mm dia. x 12.0mm long containing U^{235} . We have chosen UO_2 as the source of uranium for several reasons. The more common oxide U_3O_8 is volatile in air at high temperatures and cannot be sintered to form a coherent structure. On the other hand, UO_2 rapidly oxidizes to U_3O_8 when heated in air, as shown in the thermogravimetric analysis in Figure 3. A 40 mg sample of pure UO_2 was heated in flowing air at a rate of 5°C per minute and the weight change of the sample was recorded. As Fig. 2.3 shows oxidation occurs in two stages. The first stage begins at ~140°C and ends at ~300°C, the product of this stage has the composition U_3O_7 . The second stage begins at ~340°C and is complete at 380°C to give the final product U_3O_8 . Figure 2.3 clearly shows that UO_2 must be fired in oxygen free environments. Firings up to about 1400°C were done in an atmosphere of flowing forming gas (5% H_2 and 95% N_2) in a tube furnace. Firing at higher temperature (up to 1750°C) was done in a vacuum furnace.

2.2.1 Core Composition

Several ceramic compositions based on UO_2 and other oxides were prepared and fired under various conditions to find a material suitable for making the sensor cores. The compositions studied are listed in Table 2.1. The UA-75 and UA-97 bodies consist only of UO_2 and Al_2O_3 . Their compositions differ because they are formulated assuming UO_2

with 75% U^{235} or 97% U^{235} . To get equivalent weights of U^{235} in a core of given size requires different proportions of UO_2 and Al_2O_3 . These bodies produced variable results when fired under the conditions shown in Fig. 2.4.

These micrographs are polished cross sections through the sintered bodies. The bright (white) grains are UO_2 and the dark gray grains are Al_2O_3 . The UA-97 body was fired at 1700° for 3 hr. in vacuum, and at $1600^\circ C$ for 3 hrs. in vacuum. This material has considerable porosity, and when made into 1.0mm dia. rods is not very strong despite the high sintering temperature. The UA-75 body also exhibits a lot of porosity even when fired at $1750^\circ C$ in vacuum. If the body is first fired in air at $1500^\circ C$ and then fired in vacuum at $1750^\circ C$, it has a surface skin completely depleted of UO_2 . The surface skin is about 0.010 mm thick and as shown in Fig. 2.4d it is very porous. The skin forms after air firing at $1500^\circ C$ due to vaporization of U_3O_8 . In summary, none of the UA formulations were very promising. Despite the high firing temperatures they retained a lot of porosity and were mechanically weak. These bodies were also difficult to extrude into rods.

Since the UA bodies were unsatisfactory, we developed the UFC (UO_2 , Feldspar, Clay) formulation. The objectives were to: decrease the firing T, increase the strength, and make extrusion easier. Although the UFC body was easy to extrude, it had a heavily "mud cracked" surface as shown in Fig. 2.5a. A cross section of the body (Fig. 2.5b) shows that these cracks extended through the bulk of the ceramic as well. These rods are very weak and the rough, cracked surfaces were unsuitable for application of thin film platinum heaters. At first we thought that the clay was responsible for the cracking. To test this we made the CF (clay feldspar) body having the same weight ratio of feldspar to clay as in the UFC body. When fired under the same conditions as the UFC body, the surface of the CF body was free of cracks, as shown in Fig. 2.5a. Therefore it is not the clay which causes the cracking, but the combinations of clay, feldspar and UO_2 .

The UF bodies consist of only UO_2 and feldspar. These bodies produced smooth crack free surfaces when fired at $1325^\circ C$ in forming gas as shown in Fig. 6a. The interior of the body is also free of cracks, Fig. 6b. The bright areas in Fig. 6b consist of an intimate mixture of rounded UO_2 grains in a matrix of feldspar glass, as shown in Fig. 2.6c. Micro-chemical analysis, Fig. 2.7a, confirms this interpretation since x-ray fluorescence lines characteristic of both UO_2 (U, O) and feldspar (Al, Si, K, O) were observed in the bright regions. The large dark spots are pockets of feldspar glass. The glass apparently has a small amount of UO_2 dissolved in it. The UF bodies are considerably stronger than all the other bodies. They do not contain cracks and porosity is limited to about 12% of the total volume.*

2.2.2 Extrusion of Cores

Cylindrical rods of ceramic materials are usually made by extrusion, and this method was adopted for this project. As shown in Fig. 2.1, the ceramic core should be a cylindrical

* Based on the density of a large pellet used for thermal expansion measurements.

rod having finished dimensions of ~0.7mm diameter and 12.0mm long. This is not an unusual size for extrusion methods. However the small amount of $U^{235}O_2$ material available (~2.0 grams) required that a special miniature extrusion apparatus be built. The device is shown schematically in Fig. 2.8. Basically it consists of a piston and cylinder arrangement with a small hole at one end through which the extruded body flows. The piston and cylinder are mounted on a stand in a Carver laboratory press and pressure is applied through the hydraulically driven rams of the press. The material to be extruded was loaded into the die. The die was closed by inserting the piston, and a vacuum was pulled on the open orifice. This is necessary to eliminate air pockets within the batch which, if not removed, will lead to voids in the extruded body. After evacuation for one minute the body was compacted by driving the piston into it at relatively low pressure. The vacuum hose was removed, the loaded extruder was placed in the Carver press and the body was extruded. A long rod of extruded material is produced which can be cut to the desired lengths with a razor blade. Since all ceramics shrink when they are fired, it is necessary to make the parts oversize. The extrusion orifice is therefore 0.8mm in diameter and the rods are cut to a length of 13.2mm to produce fired cores 0.7mm dia. x 12.0mm long. The linear firing shrinkage of the UF body is 9.8%.

To be successful, the feed material or “paste” used in a ceramic extrusion process must have a carefully controlled consistency. The paste must flow through the extrusion die but not be so soft that the extruded parts deform when handled. The paste consists of finely divided particles of the ceramic components mixed with a suitable binder. The binders chosen for evaluation were two grades of polyethylene glycol manufactured by Union Carbide Co. and marketed as “Carbowax Sentry.” Carbowax (CW) 4600 is a hard waxy solid and must be dissolved in water or some other solvent to provide sufficient fluidity to the paste. It has the advantage that after the solvent evaporates, the shaped body has high strength due to the presence of the solid binder. The second grade, CW 540, has the consistency of a heavy grease and can be mixed with the ceramic powders to obtain a suitable extrusion paste. Parts made with CW 540 remain soft and pliable after extrusion, but with care they can be handled without excessive deformation.

Four Carbowax solution formulations and clay plus water were evaluated as extrusion binders. The clay in the UFC body becomes plastic when water is added and this gives a suitable consistency for extrusion. In essence, the clay + water mixture can be viewed as the binder in this case. The Carbowax binders used are listed in Table 2.2.

Binder #1 was used only in the $UO_2 - Al_2O_3$ cores and it worked, but not very well. The water tended to evaporate while the paste was being prepared and loaded into the extrusion die. Because of the variable water content the extrusion pressure and the quality of the extruded parts was unpredictable. Binder #2 (pure CW-540) was used to formulate a few bodies, but it was difficult to weigh precisely and difficult to mix into the powder batch. It too gave unpredictable results. Binders #3 and #4 were prepared by weighing a known amount of CW 540 into a clean beaker. The polymer was dissolved with a small amount of methyl alcohol (MeOH), the solution was carefully transferred

into a 50 ml volumetric flask, and additional alcohol was added to bring the total volume of the binder solution to exactly 50 ml. The first UO_2 -feldspar batch was made using Binder #3, but because the binder had a high concentration, a very small volume of binder solution was required and it was difficult to control this volume adequately.

Consequently a more dilute solution was prepared (Binder #4) and this was used to prepare all subsequent batches of the UO_2 -feldspar body. The binder solution was measured out in a 2.0ml hypodermic syringe and dispensed into the powder from the syringe.

The powder and binder were mixed by hand in a small alumina mortar and pestle until all the methanol had evaporated. The consistency of the resulting paste depended upon the relative amounts of CW 540 and ceramic powder used. The desirable feature of this binder system is that once the alcohol has evaporated the paste maintains a consistent viscosity during all handling operations, including evacuation of entrapped air from the paste. CW 540 has a low vapor pressure and does not evaporate under vacuum at room temperature. The consistency of the UF bodies was controlled, by using enough CW 540 binder to fill the void space in the dry compacted powder.

The packing fraction and void fraction of UO_2 , feldspar and the UF mixture were determined by compressing known weights of the powders in a 1.0cm dia. pellet mold at 9050 psi and measuring the diameter and thickness of the resulting pellet to determine the bulk volume. The true volume of the solid material was obtained by dividing the weights of each powder present in the formulation by the density of each solid phase present. The ratio of the solid volume (V_s) to the bulk volume (V_{bulk}) is defined as the packing fraction ϕ .

The void fraction is then $1 - \phi$.

In order to get a ceramic powder to flow in an extruder it is necessary to fill or slightly overfill the void space with binder. This separates the particles slightly and provides lubrication so that they can slide over each other. The ratio of binder volume (V_b) to void volume (V_v) is the saturation (S) of the body.

$$S = \frac{V_b}{V_v}$$

The packing fractions of UO_2 , feldspar, and the mixed UF body ($\text{UO}_2 + \text{feldspar}$) are given in Table III. The formulation of several UF extrusion pastes is given in Table IV. The saturation (S) was varied from 0.924 to 1.262 in these bodies to find a binder content suitable for extrusion. In each lot of UF extrusion paste the total amount of solid material was 1.3362g (1.1367g UO_2 and 0.1995g feldspar). The calculations needed to formulate each lot of paste are indicated below.

The solid volume (V_s) in Table 2.4 was calculated from the relationship

$$V_s = \sum_i \frac{W_i}{\rho_i}$$

where W_i is the weight in grams of the i th powder and ρ_i is the theoretical density in gram/cc of the i th powder. The void volume (V_v) was calculated from

which can be rearranged to $V_v = \frac{\Phi V_s}{1 - \Phi}$.

where ϕ is the packing fraction. The volume of binder solution used in the formulation was converted to the volume of dry CW 540 binder in the batch (V_b) using

$$V_b = \frac{V_{sol} C_b}{\rho_b}$$

where V_{sol} is the volume of binder solution used in the formulation, C_b is the concentration of the binder in solution (g dry binder/cc of solution) and ρ_b is the density of the pure CW 540 binder at 22°C. As noted earlier, the saturation (S) is the volume ratio of dry binder to void space

$$S = \frac{V_b}{V_v}$$

The saturation by and large determines the pressure needed to extrude the material through the extrusion die. The extrusion pressures P_e in Table IV were calculated from the total load applied to the extrusion piston (L) using the cross sectional area (A_e) of the

extruder. The values of P_e in Table IV represent the average pressures applied during extrusion of the lots. The relationship of extrusion pressure with saturation in the UF bodies is shown in Figure 2.9. The quality of the extruded rods was as follows. The parts made with $S = 0.924$ had excellent surface finish but the paste required very high extrusion pressure and resulted in damage to the extruder. The parts with $S = 1.262$ extruded easily but they were too soft and were easily deformed during handling. The parts made with $S = 1.036$ required a moderate extrusion pressure, had acceptable surface finish, and could be handled without deformation. Therefore, the lot #3 formulation ($S = 1.036$) was selected as the standard extrusion paste.

All of the bodies listed in Table 4.1 (UA, UFC, UF) were formulated into extrusion pastes using the methods just described for the UF body. However, since the UA and UFC bodies were rejected for reasons given earlier, the details of their formulations are not included in this report.

2.2.3 Firing of Cores

After extrusion, the rods were dried at 105-110°C for about 2.0 hours. They were then loaded into a rectangular alumina tray and fired in a flowing atmosphere of forming gas (95% N₂ and 5% H₂). The standard firing schedule is shown in Figure 2.10. The plateau at 500°C was to allow time for complete removal of the CW 540 binder. The final firing temperature was 1315-1325°C and the hold time at this temperature was 4-6 hours. The hold (or soak) time was not critical so long as it was above 3 hours. In early firings, the UF cores were supported on a bed of zirconia powder. This was unsatisfactory because grains of the zirconia powder became fused to the surface the rods and they could not be removed in later cleaning steps. The embedded particles gave the cores a rough surface which could lead to difficulty in subsequent stages of sensor construction. To eliminate this problem, the alumina trays were lined with Grafoil (a graphite paper) and the zirconia bed was eliminated. The UF cores did not stick to the graphite paper and clean smooth surfaces resulted.

As noted earlier care must be taken to exclude oxygen during the firing of any ceramic containing UO₂. If oxygen is present the UO₂ oxidizes in two stages to give a final product of U₃O₈ as shown in Figure 2.3. Oxidation cannot be tolerated because; a) U₃O₈ is volatile at high temperatures and does not produce a dense strong body and b) a large, destructive expansion in volume occurs upon conversion of UO₂ into U₃O₈. The destructive expansion can be observed in Figure 2.11, which is the thermal expansion for a large cylindrical sample made from the UO₂-feldspar (UF) mixture. This pellet fired to high density in forming gas using the schedule shown in Fig. 2.10. The sample was heated in air during the thermal expansion measurement, and had a normal thermal expansion curve up to about 720°C. At 720°C rapid oxidation begins and the body expands abruptly. Eventually the body crumbles into a fine black powder. Oxidation of the UF body is delayed in comparison to that of pure UO₂ powder (Fig. 2.3). The reason is that the body has low porosity and at low temperatures oxygen cannot penetrate into it and cause oxidation of the UO₂. At about 720°C penetration of oxygen into the body becomes rapid and destructive oxidation occurs.

2.2.4 Sensor Assembly

After firing, the UF rods were ultrasonically cleaned in distilled water, containing a small amount of detergent, they were then rinsed several times with distilled water and ultrasonically cleaned a second time in methyl alcohol. After air drying the UF rods were ready to be made into sensors. As may be inferred from Fig. 2.1, sensor construction requires the following steps:

1. Application of a Pt heater onto the cores.
2. Attachment of suitable end terminations (leads) to the heater.
3. Coating the core with a layer of electrical insulation.
4. Application of a thermally conducting silver sheath over the electrical insulation.
5. Application of a layer of thermal insulation over the silver sheath and finally
6. Sealing the sensor into a metal sheath of platinum or stainless steel.

Initial plans were to make the heater by winding 1.0 mil platinum wire directly onto the UF cores. Several difficulties were encountered in early attempts to do this. The main problem was that the UA and UFC cores available at that time, were too weak to withstand the stresses imposed when the wire was wound onto them. It was also found that the platinum wire, due to elastic spring-back, did not stay in place after it had been wound. We also experienced problems with kinking and breaking of the wire as it was being handled. As a result of these problems we decided to try a thin film approach to making the heating elements.

The most efficient way to put a thin film of platinum on the surface of a small cylindrical core is by use of platinum resins. Platinum resins are chemical compounds involving large organic molecules (resins) and platinum atoms. The resins can be dissolved in organic solvents to make solutions suitable for painting onto smooth surfaces. The resins are commercially available and have been used for many years to put decorative gold and platinum bands on fine china. After coating onto a substrate, the resin is dried to remove solvents and then heated to a fairly high temperature to remove the organic resin. A thin layer of platinum or gold remains behind. These layers are electrically conducting and have been used as a way to lay down conductors in electronic circuits. The resins are usually fired in air, but in our case this was not possible due to oxidation of UO_2 . We used Engelhard, Hanovia #05 platinum resin. The cores were coated by dipping them into the resin solution. They were dried for 15 hours in air at 135°C on a sheet of Teflon, and then fired in a forming gas atmosphere. The firing schedule consisted of heating from 23°C to 700°C at 7°C per minute, holding the temperature constant at 700°C for one hour and then shutting off the furnace power. The coated cores were allowed to cool to room temperature while still in the furnace and the flow of forming gas through the furnace was maintained for the entire time. In all a total of 45 UF cores were coated with platinum in this way. The resistance of 12 of the cores was monitored using a Fluke digital multimeter. Air curing silver paint no. 1481 from Ernest F. Fullam, Inc. was applied to the ends of the cores to provide good electrical contact. The average resistance values at room temperature are shown in Table V. The resistance of uncoated UF cores was 1300-1500 $\text{k}\Omega$ depending upon core length. After the second coating the average resistance was $\sim 200 \text{ k}\Omega$. It dropped to 120-160 $\text{k}\Omega$ after three coatings and remained constant after that. The idea behind use of multiple coatings was that we could tailor the resistance of the Pt films by controlling the number of coatings and thus the film thickness. However, since the resistance did not continue to fall after three coatings, this approach will not work. Our target resistance of 10Ω could not be reached with any practical number of coatings.

Since thin film platinum heaters are not feasible, we returned to the idea of winding wire onto the cores. A small device was constructed for this purpose. The cores were attached to brass mandrels using a tiny drop of Crystalbond 509* thermoplastic cement, the

* Aremco Products, Inc.

mandrel was in turn attached to a #2-56 screw which, when turned advanced the UF core past a fixed wire guide. Each full turn of the screw advanced the core by 0.018" and after a few tries we succeeded in routinely producing tightly wound coils on 0.7mm cores as shown in Fig. 12. To achieve a room temperature resistance of 10Ω required a winding having 22 turns. After the coil was wound, a small spot of Fullam* no. 1481 air curing silver paint was placed over the outer most turns of the coil. This kept the coils in place by "gluing" the Pt wire to the UF core. The remainder of the coil was coated with Aremco† Ceramabond 503 using a small brush. Ceramabond 503 is a phosphate bonded alumina cement which can be cured at temperatures below 400°C. After curing the cement can be used at temperatures up to 1650°C. After the Aremco cement had air dried for several hours, the ends of the cores were coated with additional silver to serve as contact pads to the heater wire. The cores were removed from the brass mandrels by heating to ~120°C to melt the Crystalbond cement. The coated cores were cured using the heating steps recommended by Aremco; air dry at room temperature for 1- 4 hours and then heat for 1 - 2 hours at each of the following temperatures 93°C (200°F), 204°C (400°F), and 371°C (700°F). Normally these steps would be done in air but in our case the last two were done in forming gas. To densify the silver contact pads, the cores were also heated in forming gas at 680°C for 40 minutes.

Although we successfully completed the mechanical operations of winding and coating the UF cores about 85% of the time, 75% of the cores in the first lot failed after heating to cure the coating. The failure mode was that the silver contact pads flaked off the ends of the cores and contact with the heater wire was lost. In addition the Pt wire underneath the silver pads had broken at several places and much of the wire fell off at the same time the silver did.

Cross sections through two of the failed cores are shown in Fig. 13 which are optical micrographs at low magnification (~28x). In these micrographs the brown material is the UF ceramic, the adjacent white material is the Aremco cement. In the regions near the silver pads, the Aremco cement is stained by some contaminant and is dark gray in color. Fragments of the damaged Pt wire are still clinging to the core shown in the lower micrograph. These same cores were examined at higher magnification in a scanning electron microscope. Figure 2.14 shows one of these images. In the micrograph the embedded Pt wire is clearly seen in cross section as bright circular dots, the Aremco cement is the medium gray material and the thin light gray bands are the remnants of the silver pads. The black material to the right and left of the sensor is the epoxy material into which the sensor was mounted for sectioning and polishing. X-ray micro-chemical analyses were conducted on each of the features to confirm their identities as stated above. We analyzed various areas in the Aremco cement, which appeared dark gray in the optical micrographs (Fig. 2.13), to determine what contaminants might be present. One such analysis is shown in Fig. 2.15a. The location of the analyzed area was between

* Ernest F. Fullam, Inc.

† Aremco Products, Inc.

the silver layer and the middle Pt wire on the right side of Fig. 2.14. The analyzed area was a square measuring 20 x 20 microns, which contained only the Aremco cement. The analysis shows that only C, O, Al, Si, and P were detected. Except for carbon none of these elements can explain the gray discoloration seen in the optical micrographs. The elements Al, Si, and P are all present as the oxides (Al_2O_3 , SiO_2 , and P_2O_5) and all of these are white. The discoloration is probably due to elemental carbon present as black graphite particles. However, for this sample, carbon will always be detected in the x-ray analyses obtained in the scanning electron microscope. The reason for this is that the samples have to be coated with a very thin film of carbon to make them electrically conducting. Without the carbon film the samples would become negatively charged in the electron beam, the beam would then be deflected away from the sample and imaging would be impossible. It should be noted that the optical micrographs were made before the samples were carbon coated. It takes only a small amount of carbon to give a visible discoloration. Therefore it is likely that at least part of the carbon detected was present in the sample before it was coated. There are several potential sources of carbon in the materials used to coat the sensor. Carbon is present in the silver paint, in the crystal bond cement, and probably also in the Aremco cement. When these materials are fired to high temperatures ($>600^\circ\text{C}$) in air, carbon is completely removed as gaseous carbon oxides due to one or both of the reactions

In our case the samples were fired in forming gas which provides a strongly reducing environment. Under these conditions graphitic carbon is stable to very high ($>1600^\circ\text{C}$) temperatures.

The problem with graphite contamination is that it may short out some turns of the heating coil and drastically alter the resistance of the heater. Carbon also reacts with platinum to form brittle platinum carbides. This could explain the breaks observed in the wire. It is therefore highly desirable to prevent the formation of graphite deposits in the sensor body. A micro-chemical analysis of the silver layer immediately to the right of the area analyzed in Fig. 2.15a is shown in Fig. 2.15b. This analysis was most surprising. Large amounts of C, O, Al, and Si were found in addition to the expected silver. Most of the carbon is from epoxy, which penetrated into the silver layer during the sample mounting operation.* The presence of large amounts of O, Al, and Si in the silver layer was unexpected. A small amount of Pt was found in the silver layer and this might indicate a chemical interaction between the silver layer and the nearby Pt wire. However, micro-chemical analysis of the Pt wire showed that only Pt and a trace of C were present. The source of the Mg, Al, Si, and O found in the silver layer is from the Fullam silver paint. Figure 2.16a is a back scattered electron image of a sample of the Fullam paint which had been fired on an alumina substrate at 930°C for 13 hours in air. The bright

* The mounting operation involves use of liquid epoxy which surrounds the sample and is forced into the open pores by the application of 10psi pressure.

white spots are due to elemental silver and the light gray areas are due to silver combined with other elements. The rounded black spots are areas where the alumina substrate shows through the silver coating. An x-ray analysis of the entire imaged area is shown in Fig. 2.16b. Silicon, aluminum and oxygen are present in large amounts in this silver paint. Bismuth was also detected. The manufacturer of this paint apparently used an oxide flux to promote adherence of the silver to oxide substrates. The flux is most probably a glass containing Bi_2O_3 , Al_2O_3 , and SiO_2 . The gold signal is due to a thin gold film applied to this sample to prevent charging in the electron beam.

We suspect that the oxide flux may be responsible for the failure of the silver contact pads. Consequently we have examined several commercial gold, silver, and platinum paints to find a more suitable material for the contact pads. Micrographs and X-ray analyses of these paints are shown in Figs. 2.17 and 2.18 respectively. All of them except Pt sinter to dense metal layers when fired at 800 to 930°C. None of these formulations contain oxide fluxes as is evident from the x-ray analyses in Fig. 2.18. The identities of these materials are shown on the relevant micrographs and X-ray analyses. Du Pont silver 7095 gives an excellent, dense silver coating which adheres strongly to the alumina substrate (Fig. 2.17a). This material is essentially pure silver, but also contains small amounts of lead, silicon, aluminum, magnesium and oxygen (Fig. 2.18a). Du Pont silver 7167 sinters well at 930°C, but this coating was too thin when applied and the alumina substrate (dark gray) shows through open areas in the silver film (Fig. 17b, 18b). This material is pure silver. The Al and O are from the exposed substrate and the gold is from an anti-charging layer applied to the surface of the sample.

Results of SEM analysis of an Engelhard gold paste (A3338) are shown in Figures 2.17c and 2.18c. This material provides an excellent dense gold coating which adheres strongly to the alumina substrate. This material is pure gold. Finally, we examined a Heraeus platinum paste used to screen print Pt conductors onto alumina substrates. Although the film adhered well to the P substrate after firing at 930°C, it had a porous micro-structure and the size of the metal grains was much smaller than those found in the silver and gold films. This material is pure platinum (Figures 2.17d and 2.18d).

All of the four materials shown in Figs. 2.17 and 2.18 should be superior to the Fullam silver paint we used (Fig. 2.16). Use of the Fullam silver has been discontinued and we are preparing cores with these four alternative materials as contact pads. The best of these will be adopted for future use.

Although the silver contact pads failed, there is much encouragement to be found in Fig. 2.14. The interface between the UF ceramic and the Aremco cement is sharp and clean which is a good indication that these two materials are chemically compatible and do not react with each other. The platinum wire embedded in the Aremco cement remains pure after firing which indicates that the cement and wire are compatible. Although the Aremco cement is porous, silver paint can be applied to it without silver particles penetrating into the pores of the cement and causing electrical shorts. Except for the

silver pads the assembly maintained its mechanical integrity after firing. There is no evidence of cracking within the UF ceramic or within the Aremco cement. Nor is there any evidence of cracking at the UF-Aremco, Aremco-Pt, or Ag-Aremco interfaces. The chemical and mechanical compatibilities noted are essential if the sensors are to have long service life in a nuclear reactor core.

The main problems uncovered are carbon deposition within the Aremco cement, failure of the silver pads to adhere to the UF ceramic, and a possible chemical incompatibility between silver and platinum. We are confident that all these problems can be solved.

The easiest way to prevent carbon deposition is to fire the cores in air at low temperatures to burn out the organic binders used in the metal paints and Aremco cement, before they carbonize to graphite. We know from Figure 2.3 that pure UO_2 begins to oxidize in air at $\sim 120^\circ\text{C}$. However when combined in a UF body there is no evidence of oxidation below 720°C during thermal expansion measurements in air (Fig. 2.11). On the other hand several instances have been noted where cores have been destroyed by oxidation when fired in air at temperatures near 600°C . The question then, is what is the maximum safe temperature when UF bodies are heated in air? Since thermal expansion is not a good indicator of the onset of oxidation we have studied this question using a much more sensitive thermogravimetric balance. The results are shown in Figure 2.19 for 0.7mm UF cores sintered to high density. The heating rate was 5°C per minute and the atmosphere consisted of 10volume % oxygen in argon. This gas mixture reasonably approximates the oxidizing potential of air. As Fig. 2.19 shows, oxidation of the cores does not occur at a detectable rate until the temperature exceeds 400°C . When burning organic binders out of ceramic bodies a temperature of 250 to 350°C for ~ 1 hour is generally sufficient to oxidize the binders and prevent formation of graphite. Our TGA results indicate that the cores should be able to withstand oxidizing atmospheres at temperatures up to $\sim 350^\circ\text{C}$. This will be sufficient to remove all the carbon. Therefore we use a one- hour hold in air at 350°C in current lots of the UF sensor. It is noted from Fig. 19 that catastrophic oxidation of the cores begins at $\sim 600^\circ\text{C}$, whereas oxidation was not detected below 720°C in the thermal expansion measurement (Fig. 2.11). The real onset of oxidation, however, begins at about 400°C , and this is the maximum safe temperature for the UF cores in air.

We believe that use of one or more of the "paints" shown in Figures 2.17 and 2.18 will solve the problem of poor adherence between the contact pads and the UF ceramic. The reasons are that the thermal expansion coefficient of Al_2O_3 and UF ceramic are virtually identical and since both are oxides, the surface chemistries should be very similar. If silver or gold interact destructively with platinum at the temperatures of interest, we can use Pt paints to make the contact pads.

At this point we are confident that steps 1 - 3 have been or soon will be mastered. The observation that silver particles do not enter the pores of the Aremco cement when it is coated with silver paint is important. To complete step 4, the incorporation of a heat sink

layer having high thermal conductivity, we plan to carry out the following process. The Aremco cement will be coated with a thin layer of silver paint. The thin layer of silver paint will be densified by heating it to $\sim 900^{\circ}\text{C}$. The layer will then be brought to the desired 0.25mm thickness by electroplating additional silver onto it. If necessary, the electroplated silver will be densified by firing at $\sim 900^{\circ}\text{C}$ in forming gas.

The contact pad will be laser welded to either a stainless steel or platinum can, the space between the silver sheath and the can will be filled with fine Al_2O_3 powder to serve as thermal insulation thus completing step 5. Finally, an alumina washer will be slipped over the lead wire and into the metal can, as shown in Figure 2.1. This will be covered with a layer of low melting seal glass powder, the whole assembly will be placed in a vacuum furnace and sealed by heating to 850°C . The seal glass has yet to be identified, but many such compositions are available commercially.

2.2.5 Future Work

To conserve extremely expensive U^{235}O_2 we have based all of the development work thus far on natural UO_2 . As soon as all steps in sensor construction have been verified, we will replace the natural UO_2 with 97% enriched U^{235}O_2 . This conversion to U^{235}O_2 will require only a few working days.

Since 1.0 mil platinum wire is fragile we will go to a design using 2.0 mil platinum or 2.0 mil platinum 10% rhodium wires in future sensors. A device is under construction which will allow wire to be wound onto the UF cores at pitches other than 0.018" (the pitch of a #56 screw). As Figure 12 shows, there is plenty of room to reduce the pitch. The coil design with 2.0 mil platinum wire will require 87 turns to obtain a resistance of 10Ω . With 2.0 mil Pt - 10% Rh wire 48 turns of wire will be required. The pitches needed to wind these two types of wire are 0.0045" and 0.0081" respectively.

Sensors having 2.0 mil platinum windings, U^{235} cores, and complete through step 4 should be available by the end of February 2002. Fully encapsulated sensors should be available shortly thereafter.

Since the phosphorus in the Ceramabond cement may cause degradation of platinum at high temperatures, and lead to decreased sensor lifetime, we will attempt to apply the electrical insulation to the wire wound cores by electrophoretic deposition of pure alumina. If successful, this coating will require higher sintering temperatures than the Ceramabond cement. In turn this will require use of platinum rather than silver for the contact pads and the heat sink. If these developments are successful, the operating temperature of the cylindrical sensor can be raised to about 1200°C . The seal glass around the platinum lead would have to be replaced with feldspar.

2.3 Planar Sensor

The design of the planar sensor is shown in Fig. 2.2. This sensor is intended for use at high temperatures, that is, temperatures up to $\sim 1200^{\circ}\text{C}$. Screen printing technology will be used to make these sensors. The design of these sensors has been completed, and the compatibility of the proposed materials has been verified.

2.3.1 Materials Selection

The design of the sensor, particularly the platinum heater, depends strongly on the materials selected for its construction. The most critical choice is the type of platinum ink selected for the heater. We have examined two commercial platinum pastes - Engelhard #36280 and Heraeus OS2 (CL11-5100). Samples of these materials were painted onto an alumina substrate and fired at 930°C for 13 hours. Micrographs of the two materials are compared in Figure 2.20 at the same magnification. Both materials are porous after firing at 930°C . The Heraeus material, however, has less porosity and smaller grain size than the Engelhard ink. In addition, complete engineering data were supplied with the Heraeus ink. We therefore have chosen the Heraeus ink for our design. The chemical compositions of the two inks were identical after firing to 930°C . The x-ray emission spectrum for the Heraeus ink is shown in Fig. 2.18d. The material is pure platinum. The small amount of carbon detected was due to a thin carbon film evaporated onto the sample. This was done to make uncoated portions of the alumina substrate electrically conducting and thus prevent charging in the electron beam.

The UO_2 containing material will initially be the UF body developed for the cylindrical sensor. This material contains 85 wt % UO_2 and 15 wt % feldspar. A small amount of this body was mixed with Carbowax binder #4 (Table 2.2), painted onto an alumina substrate, and then fired in forming gas at 1325°C for 5 hours. The material adheres strongly to the alumina substrate and cannot be removed by vigorous scraping with a steel spatula. The microstructure of the coating is shown in Figure 2.21, at two different magnifications. At lower magnification the body contains a network of micro-cracks. These are caused by shrinkage of the layer during drying and firing of the coating. These cracks do not affect the adherence of the UF body to the substrate. Figures 2.21a & b are back scattered electron images and therefore the bright areas of the images correspond to regions containing UO_2 . These images show that, except for the micro-cracks, the UO_2 is uniformly distributed over the painted area of the substrate. To screen print the UO_2 layer, it will be necessary to formulate the UF material into a screen printing ink. We will use the composition of the Heraeus platinum ink as a starting point for this formulation. The Heraeus ink consists of 85 weight % platinum mixed with 15 weight % binder and solvent. Using the densities of platinum (21.45g/cc) and the binder ($\sim 1.3\text{g/cc}$) these values convert to 26 volume % platinum and 74 volume % binder. Since volumetric proportions control the rheological properties of printing inks, we will substitute the UF body for platinum to obtain the same volumetric proportions of solids to binder as in the platinum ink. The weight percentages and volume percentages for such an ink are shown below.

Material	Density (g/cc)	Wt. %	Volume %
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UO ₂	10.96	54.54	13.72
Feldspar	2.58	9.62	10.28
Binder	1.30	35.84	76.00

This will serve as a starting formulation and if necessary, adjustments will be made to the binder content to obtain the flow characteristics necessary for screen printing. We have purchased some of the Heraeus binder to use in these formulations. It will also be necessary to formulate a feldspar ink to make the seals required in the sensor. The initial formulation, which corresponds to the Heraeus ink in volumetric proportions, is shown below.

Material	Density (g/cc)	Weight %	Volume %
Feldspar	2.58	38.53	24.00
Binder	1.30	61.47	76.00

If necessary, the proportions of feldspar and binder will be adjusted to fine tune the flow properties of the ink.

The material used to seal the platinum leads into the substrate and to seal the top cover onto the sensor will initially be feldspar. We have confirmed as shown in Fig. 2.22a that this material wets platinum wire and that it adheres strongly to both the wire and the alumina substrate. Fig.2.22a shows the point where a 6mil platinum wire emerges from a “pool” of the feldspar glass. The dark spot on the platinum wire is a small droplet of glass. Careful examination reveals that the contact angle between the glass and wire is less than 90° so wetting does occur. Furthermore, there are no cracks within the glass around either the perimeter of the platinum wire or at the contact surface between the glass and the alumina substrate. This seal was made by firing at 1325°C in a forming gas atmosphere. All indicators are that this material will form a crack-free, hermetic seal where the wire enters the alumina substrate. This is necessary to prevent air from reaching the UO₂ in the sensor.

The remaining structures in the sensor will be made using various commercial alumina ceramics. The base of the sensor, which also serves as the substrate for screen printing the platinum heater and the UF body, is a standard grade substrate used in the micro-electronics industry. The top cover is a 0.50mm thick sapphire optical window supplied by Edmund Scientific Company. These windows will be used to monitor the internal condition of the sensor during development and can be replaced with the alumina substrate material later on. The sides of the sensor are made by sealing a 1.0mm thick ring of alumina to both the sensor base and the cover using the feldspar ink as the sealing agent.

With the selection of materials indicated above, the upper limit of temperature is set by the melting point of feldspar. As shows in Fig. 2.22b the feldspar is primarily KAlSi₃O₈

which, when pure, has a melting point of 1150°C. Figure 2.22b shows that the feldspar we have used contains some sodium. This could reduce the melting point to 1070°C. When feldspar “melts” at 1150°C it produces a composite material consisting of solid leucite (KAlSi_2O_6) and a very viscous liquid. This composite body maintains its shape and integrity to much higher temperatures. Complete melting of feldspar does not occur until the temperature exceeds 1540°C. The melting behavior of feldspar is summarized in the phase diagram shown in Fig. 2.23a. It is anticipated that sensors sealed with feldspar glass will be able to operate in the 1100-1200°C temperature range. Initially planar sensors will be made with feldspar seals. In the long term a seal material capable of operating up to ~1400-1450°C will be developed. This material will be based on the $\text{CaO-Al}_2\text{O}_3$ system shown in Fig. 2.23b. Compositions near the compound $12\text{CaO} \cdot 7\text{Al}_2\text{O}_3$ are known to form gas tight seals with alumina ceramics.

2.3.2 Sensor Construction

The design of a screen-printed platinum heater requires specification of the resistance and dimensions. The planar sensor has an overall diameter of 13mm which means that the heater must fit within a circular area 10mm in diameter. To match the instrumentation used to drive the sensor, the room temperature resistance of the heater should be about 10Ω . The resistance of the Heraeus ink is about 35 milliohms per square at 10 micron fired film thickness. Therefore 286 squares are needed to make a 10Ω resistor.* The problem is then to pack 286 squares into a 10mm circle.

The approach taken here was to assume a geometry for the heater as shown in Fig. 2. Let n = the number of Pt bands; and $n-1$ is the number of gaps between the bands. If we assume that the bands and gaps are equal in width, the width of the bands (W_b) can be calculated from

$$W_b = \frac{R_h}{n + n - 1} .$$

where R_h is the outside radius of the largest heater band. By assuming a value of n , one can then calculate the total length of the heater band and its resistance. Figure 2.24 shows a plot of length and resistance of printed platinum heaters as a function of the number of bands used. To achieve a 10Ω resistor with the Heraeus ink will require a heater with 7 bands (Fig. 2.24). The width of these bands (i.e., the size of the squares) is then 0.385mm. The length of the heater is 110.1mm (or 286 squares) and its room temperature resistance is 10.0Ω . The dimensions and exact design for this heater are shown in

Fig. 2.25. This pattern will be screen-printed onto the alumina substrates shown in Fig. 2.26. These substrates are 20mil thick and contain two 10mil vias to accept the platinum leads. They were purchased from Laser Tech Inc.

Prior to screen printing the platinum heater onto the substrates, the two 6mil leads will be threaded through the vias and sealed with feldspar glass. The sealing operation will be

* Number of squares = $10.0\Omega / 0.035\Omega \approx 286$ squares.

done at 1325°C in air. After inserting and sealing the leads, the platinum heater will be screen printed onto the substrate. The printed heater will be dried and then fired at 1325°C in air. During this operation, the platinum ink will sinter onto the platinum leads and establish electrical connections to the heater. The nature of the connection is shown in Figure 2.27, in which Engelhard platinum ink was used to make the connection. Although the connection appears to be quite tenuous, the contact resistance between the wire and the sintered ink was only 0.2Ω. Thus the connection was quite good. We expect that the resistance will be even lower with the Heraeus ink, since it sinters to higher density than the Engelhard ink.

The next step will be to print the UO₂-feldspar (UF) layer on top of the platinum heater using the pattern shown in Fig. 28. After firing this coating, a band of feldspar will be screen-printed onto both the substrate and the sapphire cover using the pattern in Fig. 2.29. Organic materials in the feldspar ink will be burned out at 350°C* in air. The printed substrate, alumina ring, and cover will be stacked in the proper sequence, placed in a vacuum furnace and fired at 1325°C to seal the entire structure.

2.3.3 Compatibility of Materials

If the completed and sealed sensor is to maintain its structural integrity, several conditions must be met. The materials must be chemically compatible at high temperatures, their thermal expansions must be well matched, and the materials must strongly adhere to each other. The chemical compatibility and adherence at all interfaces have been confirmed by test firing spots of Pt ink, UF paint, and feldspar paint onto alumina and platinum substrates. All the observed interfaces were free of cracking and signs of chemical attack.

The thermal expansion curves of all the materials to be used in the planar sensor are compared in Fig. 2.30. The expansion curves are well matched up to 1400°C. This means that minimal thermal strains will develop when sensors are cycled between low and high temperatures. We can therefore expect the sensors to withstand thermal cycling.

2.3.4 Future

We believe that the planar printed sensors will be easier to manufacture than the cylindrical type, and that they will be capable of higher operating temperatures. Most of the materials needed to construct these sensors are on hand. The screens needed for printing the various layers and the alumina rings needed to form the cylindrical walls of the sensors are on order. As soon as these materials arrive, screen-printing trials of the UF and feldspar inks will begin. Depending upon the rate of progress in these trials (i.e., if no unusual difficulties are encountered) printed sensors should be available in January 2002. These sensors are hermetically sealed in a “container” made of Al₂O₃, a highly inert material. Therefore, it will not be necessary to encapsulate them in a metal can.

* It has been confirmed that this can be done without oxidizing the UF ceramic and thus destroying it.

2.4 Tables

Table 2.1. Compositions evaluated as sensor bodies.

Body Number	Vol. % UO ₂	Weight percentage of each component				
		UO ₂	Al ₂ O ₃	Feldspar	EPK Clay	Ky. Clay
UA-75	26.43	49.82	50.18	-	-	-
UA-97	20.38	41.44	58.56	-	-	-
UFC	20.55	52.18	-	17.90	14.96	14.96
CF	-	-	-	37.43	31.28	31.29
UF	57.29	85.07	-	14.93	-	-

Table 2.2. Formulations containing Carbowax binders.

Binder Number	Amounts of components used				Density* (g/cc)	Concent. [†] (C _b /ml)
	CW 4600	Water	CW 540	MeOH		
1	42.86%	57.14	-	-	1.067	-
2	-	-	100.00 %	-	1.126	-
3	-	-	4.363 g	10.0 ml	-	0.4363
4	-	-	5.005 g	50.0 ml	-	0.1001

* Density of dry binder.

[†] Concentration of binder solution in grams of dry binder (Gb) per ml solution

Table 2.3. Packing fractions and void fractions of powders used to prepare UF bodies.

Powder Material	Packing Fraction (ϕ)	Void Fraction ($1 - \phi$)	Saturation Weight (g/g)*
UO ₂	0.6528	0.3472	
Feldspar	0.6243	0.3757	
UF Powder	0.6249	0.3751	
CW 540 binder density = 1.126 g/cc			

* Weight of dry binder needed to just saturate pores in one gram of dry powder. That is, weight of powder needed to obtain $S = 1.00$.

Table 2.4. Formulation of UF extrusion pastes.

Lot No.	Solid Volume V _s (cc) [†]	Void Volume V _v (cc)	Volume Binder Solution V _{sol} (cc)	Volume Dry Binder V _b (cc)	Saturation (S)	Extrusion Pressure P _e (psi)
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1	0.1810	0.1086	0.259*	0.1003	0.924	47,000
2	0.1810	0.1086	1.542	0.1371	1.262	4,500
3	0.1810	0.1086	1.265	0.1125	1.036	20,500
Theoretical (true) density of natural $\text{UO}_2 = 10.96 \text{ g/cc}$.						
Theoretical (true) density of feldspar = 2.58 g/cc .						
Theoretical (true) density of dry CW 540 binder at $22^\circ\text{C} = 1.126 \text{ g/cc}$.						

* The binder solution, used in lot 1, contained 0.4363g dry CW 540 per cc of solution, lots 2 and 3 contained 0.1001g dry binder per cc of solution.

† All three lots contained 1.1367g UO_2 and 0.1995g feldspar.

Table 2.5. Resistance of UF cores having thin films of platinum on their surfaces.

Total Number of Platinum Coatings	Electrical Resistance of Cores ($\text{K}\Omega$)	
	Core Length = 10 mm	Core Length = 12 mm
0	1300*	1500*
1	-	-
2	$218 \pm 16^\dagger$	$225 \pm 30^\dagger$
3	$123 \pm 11^\dagger$	$161 \pm 6^\dagger$
4	$122 \pm 8^\dagger$	$151 \pm 4^\dagger$
5	$121 \pm 3^\dagger$	$153 \pm 5^\dagger$

* Single measurements of these two cores.

† These values are the mean $\pm$ standard deviation for six cores.

Figures

3.0 Numerical Model

3.1 Numerical Modeling of the CTPS Instrument

3.1.1 Scaling Analysis

To design and optimize a sensor prototype, typical practice is to develop a numerical model and perform a parameter study. The instrument is quite complicated, however, so some guidance in determining the most important parameters to study is useful. A scaling analysis of the fundamental equations governing sensor sensitivity and bandwidth can provide this information (Radcliff et al. 2000b).

The CTPS is governed by the equations for sensor energy deposition (including energy deposition by nuclear reactions and resistive dissipation), heat transfer, the Wheatstone bridge and proportional control. From these equations, we can scale the instrument sensitivity and bandwidth. The sensor power deposition from all particle and photon interactions can be defined locally by

$$Q_{dep, nuclear} = \chi_i \Sigma_i \phi + I_\gamma e \mu_a ,$$

where χ_i is energy per interaction i , Σ_i is the material macroscopic cross-section for each interaction, ϕ is the particle flux, I and e are the photon intensity and energy, and μ_a is the material mass absorption coefficient.

Given a specific reactor environment, the principal parameters affecting sensor power sensitivity are seen to be the fission macroscopic cross-section, a function of fissile atom density, and the gamma absorption coefficient, a strong function of material density. Assuming a uniform distribution of energy, the power sensitivity must vary linearly with these parameters.

Scaling of bandwidth for a convectively cooled wire under constant-temperature control has been the subject of considerable analysis because of applications of the hot-wire anemometer (Wood, 1975; Watmuff, 1995; Freymuth, 1997). For an optimally adjusted anemometer with proportional control, the cutoff frequency that defines the sensor bandwidth is given by

$$f_{cutoff} = \frac{1}{2\pi(MM''/K)^{1/3}} ,$$

where

$$M = \frac{4c}{h} \left(\frac{R_0}{\alpha_T(T - T_0)} + 1 \right) ,$$

M'' = second-order time constant of the controller,

T = heated sensor temperature,

T_0 = cold sensor temperature,

R_0 = cold sensor resistance,
 α_T = temperature coefficient of resistivity
 c = specific heat, and
 h = external convection coefficient.

The difference between the CTPS and the hot-wire anemometer is a layer of material between the heated wire and the convection boundary condition. The first fraction in the equation for M is an expression for the thermal time scale of a sensor where convection dominates conduction heat transfer, a reasonable approximation for the very thin metal wire of an anemometer. In the CTPS, time scales for both the heated element and the insulator become important, and convection no longer dominates conduction. This can be accounted for by defining a conductivity time scale for both wire and insulator, represented by a Fourier number, as well as a relationship between the conduction and convection time scales; the Biot number.

$$Fo = \frac{kt}{\rho c L^2} \quad Bi = \frac{hL}{k}$$

where ρ is density, k is thermal conductivity, and L is the material characteristic length scale. These dimensionless groups define the nondimensional temperature (θ) solution to the transient lumped-parameter heat conduction equation (Bayazitoglu and Ozisik, 1988), in which

$$\theta = \frac{T - T_\infty}{T_i - T_\infty} = f(Fo, Bi)$$

Now, the assumption of a lumped-parameter system is fine for a hot-wire anemometer and is useful for identifying scaling parameters; however, it may only be qualitatively appropriate for detailed sensor modeling. The actual temperature distribution in a sensor will depend on the spatial distribution of source terms, which may be different for fission, photon or electrical energy in a sensor that has segregated material regions (Radcliff et al. 2000b). Transient variations in sensor temperature distribution as the source changes from nuclear to electrical energy may affect bandwidth significantly.

From this analysis we expect bandwidth to increase with the sensor ΔT , the product $Fo \cdot Bi$ for the outer insulative material, the controller proportional gain, and α_T of the heated element, while it should decrease with M'' , all with a 1/3-power dependency. Of these, M'' may be neglected because this time constant is very small compared to the thermal time constants. This leaves the proportional gain, sensor ΔT , α_T , Bi and insulator Fo as the pertinent bandwidth scaling parameters.

In addition to scaling the sensor performance, we considered inherent and operational sources of nonlinearity in this device before developing sensor prototypes and the testing protocol. A Wheatstone bridge is inherently nonlinear but approaches linearity as the ratio of resistances in the bridge legs become greater than ten (Doebelin, 1996), *e.g.* $R_2 = 10R_1$. In addition, we noted an offset voltage E_{qi} must be superimposed on the bridge error signal to prevent the instrument from achieving the unconditionally stable point where bridge input and output are both zero.

This can introduce substantial nonlinearity, but only a very small offset voltage is needed and this may be reduced to zero once bridge current is established.

A region of operational nonlinearity exists because the sensor output is inversely related to power. If nuclear energy deposition causes the heated element temperature to increase above the controlled constant-temperature setpoint, the controller will receive a negative error signal from the bridge and will try to "cool" the sensor by delivering current of the opposite polarity. This condition is defined as saturation of the controller. To prevent thermal runaway and destruction of the sensor, the output of the controller must be clamped such that a negative error signal gives zero output. If nuclear energy deposition increases beyond saturation, the controller will become ineffective and the sensor temperature will increase above the setpoint temperature. This does not indicate failure of the sensor, which will occur only if the heated element temperature climbs high enough to cause damage. In this saturated region the sensor could continue to indicate reactor power because the element resistance would change linearly with deposited power; however, the response time would drop substantially. When the energy deposition is subsequently reduced, the element temperature will decrease at the relatively slow natural time constant of the sensor until the setpoint temperature is reached and controller action is reintroduced.

3.2 Transient Model

The principal tool being applied to develop the CTPS is a numerical model of the sensor and associated electronics in the MATLAB (1998) computational environment. This model, shown in Fig. 3-1 and developed by Fluhrer (1997) and Radcliff et al. (2000a), includes several function modules to handle the diverse calculations needed to simulate the instrument. The modules, in the order in which they are updated in the dynamic model, include: the boundary condition definition module; the neutron and photon absorption module; the decay heat module; the thermal module; the Wheatstone bridge module; and the controller module. This model can simulate instrument sensitivity, bandwidth, and stability. Detailed development of each of the modules follows.

3.2.1 Boundary Condition Definition Module

The neutron and gamma fields that interact with the sensor depend on conditions within the reactor, which will be steady or only slowly varying most of the time. To analyze the frequency response, sensitivity and stability of the sensor, however, forcing functions such as sinusoidally varying neutron or gamma flux, ideal step changes in flux or white noise superimposed on the steady flux must be generated. The boundary condition module generates the time-dependent incident thermal neutron and one-energy-group gamma flux forcing functions that we use to represent in-reactor conditions for the simulation. This module also defines the thermal environment through a convection coefficient (h) and an ambient coolant temperature (T_{∞}).

3.2.2 Neutron Absorption Module

This module calculates the inventory of fissile material in the sensor nodes and evaluates the energy deposited by fission capture. The capability to model fertile material that can be transmuted into fissile material is also included so that fissile inventory regeneration may be

considered. The input to this module is neutron flux from the boundary condition module; while the outputs are the energy deposited in each sensor node by U^{235} fission capture, to be used in the thermal module, and U^{235} depletion to be used in the decay heat module. The number of U^{234} to U^{235} conversions are also evaluated and included in the fissile material inventory for calculations at later times in this module. The number of fissions in any node per unit time step is (Lamarsh, 1983)

$$N_f = \frac{\sigma_f^{235} \phi_n \rho^{235} N_A V}{X^{235}} ,$$

where σ_f is the microscopic fission cross section while V and X are the volume and atomic weight of the fissile material, respectively.

The number of conversions from U^{234} to U^{235} is modeled in a similar manner given the appropriate cross-section. The heat generation rate in the sensor from fission of U^{235} is then

$$Q_f = \chi N_f .$$

The nuclear cross-sections are evaluated at the node average temperature, where

$$\sigma_f = \sigma_{fo} \sqrt{\frac{T_o}{T}} .$$

The energy contributions from fission in the fertile material of the fissile element and from non-fission neutron capture are not currently modeled. Neutron self-shielding is also not considered in this simple neutronics model.

3.2.3 Photon Absorption Module

This module calculates the energy deposition from photon interactions in each sensor node. Photon intensity and energy from the boundary condition module are input to this module, while the outputs are nodal heat generation rates which are used in the thermal module. The photon energy is discretized into energy groups, with only one group currently used. As before, we assume that the sensor does not shield itself from the incident photon flux. The energy deposition in each node is then

$$Q_\gamma = V \sum_i I_{\gamma i} e_i \mu_i .$$

3.2.4 Decay Heat Module

Although not currently applied, a decay heat module is provided to calculate the energy deposited in the heated element by absorption of β -particles emitted during the decay of fission products. Decay photons are assumed to contribute to the external photon field rather than interacting locally. The input to this module is the number of nodal fission captures from the

neutron absorption module while the output of the module is the nodal decay heat generation rate for use in the thermal module.

Decay heat calculation is a substantial problem because of the time scales involved. The quantity of fission products in the sensor is related to the neutron irradiation history. The historic time scale of interest may be on the order of years while the time increment for a simulation may be less than a millisecond if flux is changing rapidly. Clearly, data storage could become unwieldy. Compressing the information using Run-Length Encoding (RLE), a method typically used for compressing graphics files, solves this problem. This algorithm identifies when the fission capture rate changes, and then stores values for this rate between changes and the time at which the values change rather than values at each time increment. This information is organized into bins in a data table. The heat generation is estimated by the following empirical expression (Todreas and Kazimi, 1993) for times 2 seconds after fission to 100 days after fission. The decay heat value at 2 seconds is assumed to apply for the 0-2 second interval.

$$Q_p = 1.4 t^{-1.2} \frac{\text{MeV}}{\text{fis} - \text{s}}.$$

Thus, given the stored fission rate history, it is possible to determine the amount of heat generation at any time by integrating this equation to find

$$e(t_1, t_2) = N_f \int_{t_1}^{t_2} 1.4 t^{-1.2} dt = 7 N_f (t_1^{-0.2} - t_2^{-0.2}).$$

3.2.5 Thermal Module

This module calculates the transient temperature distribution in the sensor and the electrical resistance of the heated element. Inputs for this module are: nuclear energy deposition from the neutron absorption, photon absorption and decay heat modules; electrical energy dissipation from the controller module; convection parameters from the boundary condition module; and the dimensional parameters of the conductive element. The output resistance is used in the Wheatstone bridge module.

The three-dimensional heterogeneous sensor is modeled by a finite difference representation of the sensor radial and axial coordinates, with nodes that represent azimuthally uniform annular sections. Only the helical heated element has azimuthal dependence, which is handled using anisotropic material properties where necessary. The energy balance in all nodes may be represented by the matrix equation

$$\overline{A}^m \overline{T}^m = \overline{B}^m,$$

where A is a matrix describing thermal connectivity of the nodal temperatures and B is a vector of thermal sources independent of current nodal temperatures, as described by Patankar (1990). The terms in the A -matrix include stored energy and node-to-node conduction, while the source vector includes nuclear and electrical energy deposition and sensor surface convection and

conduction losses. Zero-width nodes are used at the surface to evaluate the surface terms. Material properties are assigned to each node by creating property sets and using flags to tie these sets to appropriate nodes. The matrix equation is solved explicitly such that

$$\bar{T}^m = \bar{A}^{m-1} \bar{B}^{m-1}.$$

The material properties in the A -matrix are assumed independent of temperature, so the matrix is inverted only once at the beginning of the transient. The source terms are continuously updated.

Once the temperature distribution in the sensor is known, the resistance of the heated element can be determined by summing the resistances of all nodes flagged as part of the element at their respective temperatures such that

$$R_w = \frac{1}{A_w} \sum_i L_i (\xi_i (T_0) + \alpha_i (T_i - T_0)),$$

where L_i and A_w are the length and cross-sectional area of each heater element segment and ξ is the resistivity of the heater material.

3.2.6 Wheatstone Bridge Module

This module calculates the imbalance about a Wheatstone bridge that includes the sensor heated element. Inputs to this module are the element resistance from the thermal module, the value of the setpoint resistor R_S opposing the sensor in the bridge, and the bridge current i_b from the controller module. The output is the bridge voltage E_b that is supplied to the controller module. These terms are defined in the schematic of the instrument presented in Fig. 1-1. The output voltage of the bridge is given by

$$E_b = i_b \frac{R_w (R_1 + R_2) + R_2 (R_w + R_S)}{R_1 + R_2 + R_w + R_S}$$

One can see that E_b depends on R_w in a non-linear fashion. If $R_1 + R_2$ is much larger than R_w though, the non-linearity is relatively small. The setpoint resistance R_S determines the expected resistance, and thus the temperature, of the sensor heated element. Varying this resistance can set the operational ΔT across the sensor.

3.2.7 Controller Module

The proportional controller module consists of three sub-devices: a linear differential amplifier, a voltage-to-current converter and an offset voltage supply. The input to this module is the bridge output voltage resulting from bridge imbalance while the output is current supplied back to the bridge. The proportional controller is modeled by

$$i_b = K(E_b + E_{\text{offset}}).$$

The parameters in this model may be adjusted over a wide range, but two undesirable conditions are encountered. Increasing proportional gain (K) to a high value will increase the aggressiveness of the controller but can cause underdamped control oscillations. On the other hand, dropping the offset voltage to zero will allow the controller to find the unconditionally stable point at zero bridge current. The instrument output signal, total sensor electrical energy dissipation, is then found from

$$q_w = i_b^2 \frac{R_w (R_1 + R_2)^2}{(R_1 + R_2 + R_w + R_s)^2} .$$

3.3 Model Verification

Before any predictive results were obtained, the numerical model was subject to a range of tests to ensure its veracity. An initial design study led to a cylindrical foamed alumina sensor, 5 mm in diameter and 15 mm long. The heater, fabricated from highly enriched uranium nitride, was spirally wrapped about a central core 1 mm in diameter and 10 mm long (Fluhrer, 1997).

Overall behavior was verified by observing the response of the model to a 1% sinusoidal fluctuation in neutron flux generated by the boundary condition module, as shown in Fig. 3-2. This flux perturbation produces a sinusoidal response in sensor current of equal frequency and nearly opposite phase, demonstrating that the instrument, although nonlinear in nature, gives a linear output in response to small changes in neutron flux. Because of this, frequency response analysis of the instrument is expected to give meaningful results (Khalil, 1994).

Our sensor is expected to behave as a second-order coupled, lumped-parameter systems. One system, the heated element, has a small but finite energy capacity. The second system is the bulk fissile/insulator ceramic, which has a much larger energy capacity and is coupled to the first system through conduction heat transfer and to the environment through convection heat transfer. This coupled system, then, may be modeled as a type zero, second order system (Doebelin, 1996). A type zero system with proportional control is expected to exhibit steady state error, so the resistance of the sensor will always be less than the setpoint resistance, with the difference related to the proportional gain (Franklin, 1994). The steady state error of the system is expected to be

$$e_{ss} = \frac{1}{1 + KC} .$$

where C is the open-loop DC gain of the sensor.

The steady state error for the model, taken as the difference between the heated element and setpoint resistance, has been compared to this analytical error over a range of proportional gain. The comparison is excellent except at very low gains, as shown in Fig. 3-3, which gives a high level of confidence that the sensor behaves very nearly as a type zero linear system.

The thermal model was further verified at steady state by evaluating conservation of energy in the sensor and comparing the predicted radial temperature distribution to analytical results. The

results demonstrate that steady-state sensor current accuracy of three significant digits is obtained by using 30 radial and 9 axial nodes in the modeled sensor quadrant. A time discretization of 1000 points per highest resolved wavelength is needed to reproduce transient response with similar accuracy, although two-digit accuracy is obtained with as few as 20 increments per cycle.

3.4 Detailed Steady-State Neutronics Model

The MATLAB model of the instrument has limited ability to model the deposition of energy from nuclear interactions in the sensor. Proportions of energy deposited by different interactions and in different regions of the detector may be misrepresented by this model, causing discrepancies in benchmarking the model to experimental results and limiting its usefulness as a design tool for a production detector. In particular, the ability to predict how closely the detector tracks local power is jeopardized by the unrefined neutronics model.

To minimize the risk introduced by the simple MATLAB neutronics model, the neutronics code MCNP4B (Briesmeister, 1997) has been applied to calculate the detailed deposition of energy by nuclear reactions in the sensor and immediately surrounding fuel rods. MCNP is based on a Monte-Carlo representation of the Boltzmann transport equation, and is widely used to calculate neutron and photon interactions in problems with strongly absorbing materials in complex multidimensional configurations. The MCNP model can be used to verify or refine nuclear energy deposition in the MATLAB model. For the highest calculational integrity, it is necessary to iterate between the MATLAB and MCNP models to calibrate energy deposition. A particular instrument design can be analyzed with the MATLAB model to determine the temperature distribution within the sensor based on the crude neutronics. Given these temperatures, which affect the sensor sensitivity and bandwidth, a MCNP calculation can subsequently determine a more accurate energy distribution in the sensor as well as any effects on energy deposition in the surrounding fuel rods. Correction factors could then be applied to adjust the energy deposition in the MATLAB model, and the process continued until the models converge on energy deposition and temperature.

A local reactor environment has been modeled in MCNP. Sensor insulator material was segmented into four shells of equal radial width, while the heated element was modeled by a series of 20 parallel cylindrical wires aligned with the axis of the insulator cylinder as shown in Fig. 3-4. These conductors occupy the same volume at the same radius and elevation as the helical heater proposed in the initial design. Surrounding the sensor are four homogenized fuel rods with composition representative of commercial PWR reactor fuel (Todreas and Kazimi, 1993) in a square configuration centered about the detector as illustrated in Fig. 3-5. One of these symmetric fuel rods is also segmented into four radial sections like the sensor to reveal more detail about the energy deposition in the fuel if desired.

The environment of the detector/fuel rod model is a cube, 10 cm. on a side, intended to represent enough of the reactor environment to accurately calculate multidimensional effects at the sensor scale. Water fills the free spaces within this cube. Neutrons are initiated randomly throughout the water moderator within this cube while gamma photons are initiated at the boundary of the cube to simulate operational conditions. These particles are generated singly by a subroutine in

MCNP with random direction and energy chosen stochastically from spectrum probability density functions representative of a light water reactor.

Generated neutrons and photons are tracked one at a time through the environment, interacting with the modeled materials in a random walk. The probability of each interaction with each material depends on the cross-section evaluated from libraries incorporated into MCNP. As they are tracked, MCNP evaluates the average path length between interactions to provide an estimate of the deposited energy for each type of material and particle. The error associated with this type of calculation is dependent on the number of particles tracked, which is also continuously evaluated by MCNP.

The MCNP model has been qualitatively compared against the MATLAB model prediction. MCNP results, referred to as tallies, were created for three different regions of the model: the heated element, the sensor insulating material and a 10 cm. long section of one fuel rod. The energy deposition from neutrons and photons were tallied separately. The overall power deposition predicted by MCNP agrees reasonably with the MATLAB model results. Because of this, further iteration between MATLAB and MCNP has not been carried out at this time.

Figure 3-1: MATLAB module layout.

Figure 3-2: Sensor current response to variation in neutron flux

Figure 3-3: Comparison of measured control error and predicted steady-state error

Figure 3-4: MCNP model of the CTPS internal components

Figure 3-5: The entire MCNP model environment

3.2 Sensor Testing and Model Validation

3.2.1 Sensor Prototype Testing

Radcliff et al. (2000b) have demonstrated the CTPS concept experimentally. Five sensors have been fabricated and tested in-reactor. The reference prototype has a platinum wire 0.025 mm in diameter and 5 cm in length coiled in a helix around a ceramic core about 0.7 mm in radius and 10 mm long containing 21 mg of 85% enriched fissile material. The heater is placed in a machined sleeve of silica-based low conductivity ceramic. The full assembly, shown in Fig. 3-6, is about 6 mm in diameter and 15 mm in length. Other prototypes were designed to test sensitivity to sensor material parameters identified by the scaling analysis. The second prototype was loaded with 25% of the base sensor fissile material to vary macroscopic fission cross-section. The third prototype had a heated element of chromel wire 0.05 mm in diameter and 2 cm in length. This nickel-chromium alloy has a temperature coefficient 8 times smaller than platinum and thus demonstrates the scaling of α_T . The fourth prototype uses an alumina-based ceramic, testing sensitivity to insulator Fourier number. Finally a reference sensor was fabricated without fissile content to compensate for gamma heating and thermal leakage effects. Without this reference it would be impossible to compare the prototype sensors to quantum sensors that are not sensitive to photon deposition. This sensor is similar to the fourth prototype in configuration.

Sensor linearity and sensitivity were measured by taking data during reactor excursions from 10 kW to 500 kW and back to decay heat power. An excursion of this sort, as measured by the reference fission chamber and by CTPS #4 is shown in Fig. 3-7. Both of the signals are related to reactor power as measured by the reactor facility instrumentation. The transient begins with an 8 second period, slows as temperature feedback builds in, and is then terminated with a rod runback. The CTPS follows the transient well, although a small lag is noted during the fastest power changes from 30-40 and 190-230 s.

Linearity and sensitivity of the instrument are illustrated by Fig. 3-8. This plot is generated from the Fig. 3-7 excursion by plotting the CTPS signal against the fission chamber signal at each time they are measured. The raw data are shown at top left of the figure. Good sensor linearity is observed during the reactor power increase (the top line), while some hysteresis is noted on the power runback (the curved bottom line). There are two identifiable reasons for this: coolant temperature change during the excursion and the lack of gamma response from the fission chamber.

Applying temperature compensation accounts for about half of the hysteresis, as shown at top right of Fig 3-8. Removing the signal generated by the non-fissile reference CTPS, which compensates for gamma deposition and thermal losses such that the CTPS signal can be compared with the fission chamber, also reduces hysteresis as shown at bottom left. Here, the active CTPS signal is subtracted from the non-fissile sensor output, so the sign of the plot reverses. Combining the two corrections in the figure at bottom right gives the final comparison. Noting the 10 kW to 490 kW power range from Fig. 3-7, a power sensitivity of about $-8.3 \times 10^{-7} W_{\text{sensor}}/W_{\text{reactor}}$ is found. The full-power thermal neutron flux at the sensor head location was $3.1 \times 10^{12} \text{ n/cm}^2\text{-s}$ according to the reference fission chamber, giving a CTPS neutron flux sensitivity of $-1.29 \times 10^{-13} \text{ W/nv}$. Nonlinearity and hysteresis of the compensated plot are both

less than 3%. Much of the remaining nonlinearity is thought to result from small physical differences in the gamma deposition and thermal leakage between the hand-built active and reference sensors. Note that in actual use the gamma contribution would not be compensated.

Bandwidth of the sensor was determined from its response to a reactor scram. Figure 3-9 shows a reactor scram transient again measured by both the CTPS and fission chamber. A close-up view of the transient shows that the CTPS exhibited a time lag of 0.2 s during the initial 20% drop in reactor power. If we assume that the scram corresponds to a ramp change, then this relates to a sensor bandwidth of about 1 Hz.

Another notable characteristic of the prototype sensors was drift in the indicated power. Short-term drift is indicated in Fig. 3-9, particularly before and after the scram, while Fig. 3-10 shows the sensor drift over a period of several days. Both of these drift records are compensated for the steady-state coolant temperature.

3.6 Validation of the CTPS Instrument Model

3.6.1 Sensitivity and Linearity

The numerical model of the sensor and the associated electronics has been validated with experimental data (Radcliff et al. 2000a). Prototype sensor neutron sensitivity was measured and found to be -1.29×10^{-13} W/nv with good linearity. The comparable prediction from the numerical model is presented in Fig. 3-11. Sensitivity is shown to be -1.32×10^{-13} w/n/cm²-s, with near-perfect linearity.

This prediction depends on the fission cross-section chosen for the fissile material. Although the cross-section is known at specific neutron energies, the effective cross-section for our sensor must be found by neutron energy weighting based on the reactor energy spectrum. This weighting has not yet been performed, so the cross-section was adjusted from the recognized thermal value to predict the sensor fission power measured in the experiment at the averaged neutron flux measured by the fission chamber. A cross-section of 100 b was found. This means that the model does not predict the precise amplitude of the sensitivity, but does predict the linearity. The measured and predicted fission power deposition is about 0.4 W.

3.6.2 Bandwidth and Time Constant

The transient response of the prototype sensors was measured using a deterministic evaluation of the sensor response to a reactor scram. The same general technique was initially applied using the numerical model. First, a steady-state condition was modeled, typically using 200 s of simulation time with a 0.001 s time increment. Step decreases in external reactor neutron and photon flux boundary conditions, from full-power flux to zero flux, were then simulated. A time increment of 0.0005 s was applied for the transients. Resulting predictions for sensor input electrical power (the actual sensor signal) and total sensor power dissipation including the nuclear components are shown in Figs. 3-12 and 3-13 for the neutron flux and photon intensity steps, respectively. The observed time constants are on the order of 0.2 s in response to the neutron flux and about 8 s in response to the gamma flux. Of course, the individual flux

components could not be separated in the experiment, but the overall response time compares reasonably well.

Unlike the experiment, noise in the numerical model is under our control, and therefore bandwidth can be determined from noise filtering techniques. Random noise was added to the modeled neutron flux by adding a random input component, with amplitude of up to one percent of the steady value, at each model time increment. Response of the sensor input current to this random flux perturbation was predicted for 200 s with a time step of 0.0005 s. Taking the Fourier transform (FFT) of the simulated current data then shows how the sensor filters the input signal. The predicted sensor frequency spectrum is shown in Fig. 3-14. As expected from the step change response, this neutron flux spectrum shows a relatively flat response until a breakpoint is qualitatively observed at about 2 Hz. The sensor then filters at approximately 20 dB/dec until about 70 Hz, where filtering increases to about 100 dB/dec.

Additional analyses were performed to determine the effects of insulator Fourier number, heated element temperature coefficient of resistivity, proportional gain, and sensor total power dissipation (and thus temperature differential) on the predicted bandwidth. Measurement of the time constant for the prototype sensors that incorporated these parametric differences showed little effect, although the results were not conclusive. The predicted bandwidth for a case in which the insulator Fourier number was increased by a factor of 2 increased approximately 50%. In all other parameter studies, the first breakpoint was virtually unaffected, while the second breakpoint was reduced.

The accuracy of the predicted sensor frequency response beyond the first breakpoint is somewhat uncertain. We noted that a discretization of less than 1000 time increments per cycle, which corresponds to frequencies above 2 Hz in Figure 3-14, could result in significant error in the predicted amplitude. Simulations with time steps as small as 0.00003 s have been performed. Qualitatively, the first breakpoint is unchanged in frequency or amplitude, as is the slope following that break; however, the frequency of the second breakpoint tends to increase slightly with smaller time steps. Also, a resonance peak is resolved at the second breakpoint when time steps are less than 0.00008 s. Although it is clear that the spectrum in Fig. 3-14 is not fully resolved at higher frequencies, we believe that the first frequency breakpoint is accurate and compares well to the experimental measurement.

3.6.3 Sensor Drift and Losses to the Environment

A general downward drift in sensor electrical power (analogous to a predicted increase of reactor power) has been observed when the sensor is heated, while the opposite trend was noted when the sensor was cooled. More complicated trends were observed when the nuclear power was varied, such as those seen in Figs. 3-9 and 3-10. Study of this phenomenon led to the hypothesis that the drift could be caused by heat losses to the electrical lead connecting the sensor to the controller, or to long-term (meaning much longer than the measured sensor time constant of about 0.4 s) redistribution of temperature within the sensor.

To further understand these effects, a numerical model of the conduction boundary condition presented by the central power leads, as seen in Fig. 3-6, was incorporated into the overall instrument model. This lead, represented as a one-dimensional thermal conductor, is modeled

and solved using the same technique applied in the sensor thermal module. The power lead is interfaced with the copper wire radial nodes at the top centerline of the sensor through a heat transfer term, where the lead temperature distribution is solved explicitly after the sensor temperature distribution is solved.

Figure 3-15 shows the overall response of the combined sensor and power lead model to a step increase and subsequent decrease in reactor power initiated after a 600 s approach to steady state. Total sensor power, sensor electrical power, and heat loss to the conductor are shown. The corresponding predicted reactor power measurement, comparable to that shown in Fig. 3-9, would be related to the inverse of the electrical input. From the response we can see that there is a continuously changing power leakage, both to and from the wire, during the transient. The initial magnitude of the leakage is determined by the difference in sensor and power lead temperatures at the start of a transient, and the leakage then decays as the two components come into thermal equilibrium. This effect explains the presence of a slow downward drift in sensor electrical power, and the subsequent upward drift predicted in reactor power, observed in Fig. 3-9. The decay in leakage causes an equivalent decay in the input power needed to maintain the heater temperature constant, which appears as a measured increase in deposited power.

An effect that does not correspond to simple conductor leakage is the overshoot in predicted reactor power observed after both the initial power ramp and the scram in Figure 3-9. The transient effect of energy transfer to the wire tends to create an undershooting prediction, as observed in Fig. 3-15, rather than an overshoot. Testing has shown that the drift correlates to changes in the circulating coolant temperature as shown in Fig. 3-10. The sensor power leads in the experiment were routed along the coolant lines, so we now suspect that there was substantial coupling between nuclear heating of the coolant and heat leakage from the sensor. Direct nuclear heating of the power leads could also be significant. These effects could logically cause the measured overshoot. Transient power lead heating during a reactor power increase would tend to reduce or even reverse sensor heat leakage, which would then reduce the electrical power demanded by the sensor, an apparent overshoot.

3.7 Numerical Parameter Study

The first-generation sensor prototype testing and subsequent modeling illustrated a couple of design shortcomings. Figures 3-12 and 3-13 show the predicted response of one the first-generation sensors to a step removal of about 0.4 watts of neutron and 0.1 watts of gamma power deposition, respectively. The results illustrate a change in total sensor power dissipation that occurs when nuclear power is replaced by electrical power. This effect is particularly severe in response to a change in gamma deposition. The total sensor power will ideally not change with deposited energy because the sensor temperature, at least as measured by the heated element resistance, is maintained constant. Also, we note that the relationship between the electrical power response and the total power response is not the same when the deposited nuclear energy comes from neutrons or photons. This clearly indicates that the sensor deposited power signal, the inverse of the electrical power, is not equal for equal quantities of fission or photon absorption power. To accurately measure all components of reactor power, the relationship of electrical to total sensor power must remain equal for all components of nuclear power deposition.

Variations in either flux component can cause changes in sensor temperature distribution, and therefore total power dissipation, while maintaining a constant temperature at the radius of the helical heated element. This effect is most pronounced when photon intensity changes because most of the photon energy deposition occurs at radii outside of the heater radius. Energy deposited uniformly in the insulator generates half the temperature difference of a similar amount of energy concentrated in the central heated element. This reduced ΔT and resulting variation in sensor total power dissipation causes the reduction in apparent photon sensitivity noted in Fig. 3-13. A significant change in total power was not anticipated when only neutron flux was changed.

Three basic sensor configurations were studied to evaluate sensor linearity, neutron-photon power proportionality and bandwidth characteristics for the next generation of prototypes (Liu et al. 2000). Two sensor designs evaluate the effects of energy redistribution, while the third determines the effect of reducing sensor mass. All are shown in Fig. 3-16. These designs have led to a final design that greatly improves the predicted sensor performance.

The first design study is based on a uniform distribution of fuel material, and thus nuclear energy deposition, over the entire sensor. One may note that this design is not very practical, because nuclear fuels have high thermal conductivity and thus the sensor temperature difference will be very small compared to the first-generation prototypes with their insulator region. The temperature distribution in one r-z quadrant of the sensor is shown in Fig. 3-17. The first picture shows the distribution when the sensor is operating in temperature-control mode with approximately 0.5 watts of nuclear power and 3.5 watts of electrical power, comparable with the test conditions, while the second shows the distribution given 4 watts of nuclear power only. The location of the heated wire is easily seen. It is apparent from this figure that a transition from nuclear to electrical heating, which is expected during the operation of the sensor, will cause significant, and slow, redistribution of heat within the sensor. If we recall that it is the heated element temperature that is controlled, it is also clear that the total sensor power must change to achieve the same heater temperature as the source of power deposition changes. This is the reason for the change in total sensor power noted in the first-generation sensors.

The predicted response of this sensor design to a step-removal of the neutron or photon flux is shown in Figs. 3-18 and 19, respectively. About 0.4 watts of fission power and 0.1 watt of photon power are removed. As one may expect, the change in total power is about half of the change in electrical power in both neutron and photon step cases. This occurs because the heat escaping the sensor from the heater element must traverse most of the sensor radius, while heat from fission or photon absorption must traverse only a fraction of the sensor radius. The power response of this design is thus very proportional, with equal sensitivity to fission or photon deposition, but the bandwidth, shown in Fig. 3-20, clearly suffers when the total sensor power must change so greatly as nuclear power varies. The observed value of about 0.07 Hz is much worse than that of the first-generation prototype.

The second design studied, also shown in Fig. 3-16, spaces the heated wire throughout the fissile material in an attempt to achieve the same sensor temperature distribution from an equal quantity of fission, photon, or electrical energy deposition. This is not trivial because it is the wire

average temperature that is maintained by feedback control and local wire temperature can vary significantly from the average. Several possible physical heater configurations were studied, with the best heater distribution shown in Fig. 3-21. The temperature redistribution for this case is shown in Fig. 3-22. One can see that the distributions are nearly equivalent with the exception of higher temperatures near the center of the electrically heated sensor. The step change and bandwidth results are shown in Figs. 3-23 to 3-25. This sensor, because the electrical energy source is now distributed with radius just like the fissile material, exhibits a somewhat improved bandwidth.

A third design has been modeled to evaluate the effect of simply reducing the radius of the first design. One might expect that the reduced radius would result in reduced sensor temperature difference also, but ΔT in fact increases with volumetric heat generation if the total sensor power dissipation is maintained while radius is decreased. In addition, the uniformity of nuclear vs. electrical distribution improves as size is decreased. Figures 3-26 to 29 show that sensor bandwidth improves given a smaller overall radius while proportionality is maintained for the step change.

It is clear from this study that we need a uniform distribution of energy from all sources along the thermal path to the environment, while keeping the total stored heat small, to optimize both proportionality and bandwidth. In the proposed second-generation CTPS design, a fuel/heater kernel of small radius is thermally connected to the surrounding environment by thin metal axial supports rather than the high-area annular radial ceramic insulator applied in the first prototype. Most of the heat then travels through the sensor in an axial rather than radial direction. We have modeled several different variations of this sensor prototype, shown in Fig. 3-30.

Because neutron, photon and electrical energy are distributed evenly in the axial direction along the center element, the optimized second-generation design gives improved control over temperature redistribution, as shown in Figure 3-31. The step-change responses for fission and photon absorption are shown in Figs. 3-32 and 3-33, respectively. The ratio of electrical to total sensor power is about 8:1 for fission energy deposition, but less than 2:1 for photon deposition. This result indicates some loss of proportionality, which is thought to be caused by the increased photon energy deposited in the ends of the heated axial element where neither electrical nor fission energy is present. The bandwidth of this sensor, predicted to be nearly 30 Hz as shown in Fig. 3-34, is greatly improved over previous designs, principally because of the reduced mass of the active axial element. An additional advantage to this design is that the metal thermal conduction path can be better controlled than ceramic construction, with more accurate dimensions and welded connections, contributing to minimized drift.

The parameter study thus leads us to a prototype in which a thin metal axial enclosure, containing the fissile material and surrounded by the heated element, can route heat to the sensor outer metal skin with good control of thermal losses and temperature distribution. A foamed insulator component with very low thermal conductivity will occupy the region between the axial element and outer skin to provide support and minimize radiation heat transfer effects.

3-4-the first one used to be

Figure 3-6: Design of the first-generation sensor prototypes

Figure 3-7: Response of the CTPS and fission chamber to a reactor transient

3.8 Dynamic Thermal Environment Measurements and Mode-Switching Compensation

The constant temperature sensor is based on a simple lumped-parameter energy balance; if the sensor is maintained at a constant temperature above the ambient, then the sum of electrical and nuclear power dissipated by the sensor will remain constant. When this condition holds, the input electrical power becomes an inverse measurement of the deposited nuclear power. There are two inherent assumptions in the stated energy balance. First, it is assumed that the parameter we are controlling, the heated element average temperature, accurately represents the lumped temperature of the sensor. We have noted that both the distribution of power deposition within the sensor and thermal leakage from the sensor can affect the total power dissipation, and therefore the predicted nuclear power, of the sensor. The second assumption is that the heat loss from the sensor to the environment remains constant. If the effective heat transfer coefficient (UA) or the bulk coolant temperature (T_∞) change during operation of the sensor, the electrical power input to the sensor will change to maintain a constant element temperature rather than maintaining the sensor-to-environment temperature difference. Because reactor coolant conditions change, this effect must be compensated.

Traditionally, separate measurements of the coolant conditions, such as a Reynolds number and bulk temperature, would be required to compensate our calorimetric measurement; however, the fact that the CTPS is in a feedback control loop provides another possibility. Because the electrical power input to the sensor is under external control, we may interrogate the sensor and measure its dynamic response. Kerlin et al. (1981) demonstrated the use of a loop-current step response method to effectively and efficiently measure the thermal resistance between plant resistance temperature detectors (RTDs) and their thermowell.

This concept can be extended to independently evaluate UA and T_∞ given a known internal sensor heat generation from nuclear processes. The method begins with the transient lumped-parameter expression for the sensor and its environment,

$$\rho V c \frac{dT}{dt} = UA(T - T_\infty) + q_{NUC} + q_{ELEC},$$

where ρ , V and c are the sensor density, volume and specific heat; T and T_∞ indicate sensor and coolant bulk temperatures; and q_{ELEC} and q_{NUC} indicate electrical and nuclear heating rates, respectively. If the electric power to the sensor is turned off, the sensor average temperature will experience a simple exponential decay from its initial temperature with an asymptotic approach to a final temperature that depends on the coolant temperature and the nuclear heating rate q_{NUC} . The solution to this equation when $q_{ELEC} = q_{ELEC}(0)$ at $t < 0$ and $q_{ELEC} = 0$ for $t > 0$ is

$$T(t) = T_\infty + \frac{q_{NUC}}{UA} + \frac{q_{ELEC}(0)}{UA} e^{(-\frac{UA}{\rho V c} t)},$$

If we assume that q_{NUC} remains constant during this transient, this equation can be represented by the form

$$T(t) = a_0 + a_1 e^{-a_2 t}.$$

This is the basis for mode-switching operation of the CTPS instrument. A constant-temperature controller feeds a known current through the heated element, thus a voltage measurement across the element gives its resistance. The power provided by the controller has been shown to be inversely proportional to the deposited nuclear power, q_{NUC} , if the losses to the environment remain constant. At some time, the controller can execute a step-decrease in supplied current to a small but finite constant value. Sampling the sensor voltage drop over time then gives the dynamic response of the heated element resistance, and thus average temperature, which begins an asymptotic approach to the a_0 value. Regression of these data will then allow determination of a_0 , a_1 , and a_2 . If q_{NUC} is known from constant-temperature operation immediately before the step-change, then both UA and T_∞ may be evaluated from the regression, as illustrated in Fig. 3-30. These values may subsequently be applied to compensate the sensor constant-temperature signal for any changes in the environmental loss term.

There are several problems with this seemingly straightforward technique. First, the data must be reduced without detailed knowledge of the sensor conditions. Because the sensor is not truly lumped, but is heterogeneous in composition, the order of the dynamic response is not clear. Linear approximation tells us that a sensor of our design is likely to behave as at least a second-order system, with two discrete exponential decay constants. These decay constants must be estimated to determine the appropriate sampling rate and sample time needed to produce an accurate regression. Also, accuracy of the regression algorithm must not be affected by small changes in sensor properties that may alter the decay constants in operation. In addition, the data to be reduced are likely to be quite noisy, so the regression algorithm must be able to tolerate this.

Second, a controller must be designed to support mode-switching operation. The device must perform the following functions:

- provide feedback control of supplied current to maintain constant resistance to measure nuclear power,
- produce step-changes in the current at prescribed time intervals,
- store the sensor resistance data for some amount of time,
- re-establish feedback control, and then
- perform the regression needed to evaluate the thermal resistance and ambient temperature.

Furthermore, it must be capable of averaging several cycles of mode switching to ensure that the heat loss is not in fact changing before using the calculated thermal information to compensate the measured nuclear power. Clearly, a microprocessor-based digital controller is required to perform these functions.

3.9 First-Order Least Squares Regression Algorithm

For the initial sensor model we assume a system of first-order step response to reduce collected sensor data. The sensor temperature response can be described by

$$T(t) = A_0 + A_1 e^{-t/A_2},$$

where A_0 , A_1 and A_2 are constants. Specifically, A_2 is the sensor time constant, A_0 is the temperature at infinity, and the sum of A_0 and A_1 is the initial temperature. To determine the constants from experimental data, the least square method (LSM) is used. From the definition of LSM, we have the total square error as

$$\begin{aligned} E &= \frac{1}{2} \sum_{i=1}^N (T(t_i) - T_i)^2 \\ &= \frac{1}{2} \sum_{i=1}^N (A_0 + A_1 e^{-t_i/A_2} - T_i)^2 \end{aligned}$$

or

$$\begin{aligned} 2E &= N A_0^2 + \sum_{i=1}^N T_i^2 + A_1^2 \sum_{i=1}^N e^{-2t_i/A_2} - 2 A_0 \sum_{i=1}^N T_i \\ &\quad + 2 A_0 A_1 \sum_{i=1}^N e^{-t_i/A_2} - 2 A_1 \sum_{i=1}^N T_i e^{-t_i/A_2} \end{aligned}$$

where T_i is the experimental temperature at time t_i , $T(t_i)$ is the temperature obtained at time t_i , and N is the number of experimental data used to evaluate the constants. The next step is to minimize the total square error E . A minimum of the function E is achieved when the following conditions are satisfied:

$$\frac{\partial E}{\partial A_0} = 0, \quad \frac{\partial E}{\partial A_1} = 0, \quad \frac{\partial E}{\partial A_2} = 0$$

The equivalent equations are then

$$N A_0 - \sum_{i=1}^N T_i + A_1 \sum_{i=1}^N e^{-t_i/A_2} = 0$$

$$A_1 \sum_{i=1}^N e^{-2t_i/A_2} + A_0 \sum_{i=1}^N e^{-t_i/A_2} - \sum_{i=1}^N T_i e^{-t_i/A_2} = 0$$

$$A_1 \sum_{i=1}^N t_i e^{-2t_i/A_2} + A_0 \sum_{i=1}^N t_i e^{-t_i/A_2} - \sum_{i=1}^N T_i t_i e^{-t_i/A_2} = 0$$

Clearly, these are non-linear equations. The procedure followed is to iteratively solve the last equation for A_2 , and then directly solve for A_0 and A_1 from the other two equations. A bisection method was chosen to solve for A_2 . Other methods such as Newton's method proved to be less successful.

To test this algorithm, simulated temperature data for a first-order sensor system with a time constant A_2 of 10 seconds, an initial temperature of 200°C, and an ambient temperature A_0 of 40°C were produced. Random noise with amplitude of 100% of the ambient value was superimposed on the root exponential. We then applied the LSM algorithm to regenerate the time constant and asymptote of these data. Figure 3-35 shows the effect of total sample time on the accuracy on the regression of the data given a fixed sampling rate of 100 samples/sec. Even with this noisy signal, a sample equal to the time constant of the decay appears to give adequate accuracy. Sampling rate is varied in Fig. 3-37. Here, the convergence problems characteristic of least-squares regressions are apparent. The time constant is well predicted by all of the sampling rates evaluated; however, prediction of the asymptote does not converge monotonically on the correct value. Overall, small time steps on the order of 0.001-0.002 s converged acceptably, but time steps of 0.01 s gave better convergence than when either 0.05 or 0.005 s steps were applied. This effect is studied further in Ning (2001).

3.10 Regression of Transient Test Data

To evaluate the regression technique with real data, a resistance temperature detector (RTD) was used to simulate a CTPS without nuclear heat. A RTD consists of a thin platinum wire surrounded by a metal/insulator structure that isolates the wire from the ambient conditions and has a heat capacity much greater than the active wire. This gives it dynamics very similar to the planned CTPS device.

A stepped power supply, shown in Fig. 3-38, was used to heat the RTD with a steady current, simulating constant-temperature operation when ambient conditions are held constant. On a trigger signal, the power supply was switched to a much lower voltage, inducing a temperature transient in the RTD. Temperature decay would occur according to the dynamics of the sensor until reaching an asymptote at a temperature somewhat higher than the bulk surrounding fluid. The steady-state temperature was dictated by the energy still being delivered to the sensor, analogous to a constant nuclear heat component in a CTPS. These tests were run with the sensor immersed in a wind tunnel and in still water, demonstrating forced and natural convection conditions, respectively.

The dynamic response of the sensor resistance, and thus temperature, was sampled for 200 seconds at a frequency of 100 Hz. These transient data present a predominantly first-order decay, with a noise level of 5-30% of the signal. The higher noise levels occur at the lower steady-state RTD initial temperatures. These data were reduced using the first-order LSM algorithm. The fit was reasonable when all or half of the data was used, corresponding to about 5 or 2.5 time constants of the system, respectively. When one time constant of data is used, the fit fails to accurately predict the asymptotic approach to an equilibrium temperature. This is poorer performance than that observed in Fig. 3-31 when fitting simulated data, where a good fit

was often achieved in less than one time constant. The behavior of the prediction suggests that the measured system is in fact higher-order. A prediction of the system parameters from the first part of the transient captures the higher-order time constants, causing an underprediction of the asymptote. Use of more data reduces the influence of faster-decaying components of the system. A second order algorithm was not applied to reduce these data because the RTD displayed only weak second-order decay.

Seven step-response tests were performed under natural convection conditions in water, while 19 tests were performed in a wind tunnel. The test details are described in Ning (2001). Two air velocities: 3.5 and 5 m/s, and a small range of temperatures from 21 to 27°C were studied. The other tests represent variations in the initial and final temperatures of the sensor when electrical heating is accounted for.

Predicted sensor steady-state temperatures are compared with experimental values in Fig. 3-39. Note that this temperature is not the temperature of the environmental fluid because a low voltage is still imposed on the RTD when the steady state has been achieved. The measured temperatures were obtained from the RTD resistance at steady state, while the predicted values are simply A_0 from the first order regression of the transient data. The comparisons are quite good, with all but Cases 9 and 11 within the $\pm 1^\circ\text{C}$ accuracy expected from the RTD ambient measurement.

To convert the transient asymptote A_0 from the LSM regression to bulk fluid temperature, and the transient time constants predicted by A_2 to an external convection coefficient h , we must apply the expressions

$$T_\infty = A_0 - \frac{P * A_2}{MC} ,$$

and

$$\frac{1}{hA} = \frac{1}{UA} - R_{INT} = \frac{A_2}{MC} - R_{INT} ,$$

respectively, where M is the mass of the RTD affected by heat transfer, C is the sensor average specific heat, P is the input electrical power, UA is the overall heat transfer coefficient referenced to the outside surface area A , and R_{INT} is the internal sensor thermal resistance.

Generally, for a given sensor, MC and internal thermal resistance are considered known constants because we know the structure and materials of the sensor. However, in this research, we used a commercial temperature sensor whose structure and materials are not specifically known. In addition, the whole length of the RTD does not take part in the measured heat transfer process, and conduction heat transfer in the axial direction of the RTD may be significant. For these reasons, values of MC and R_{INT} were evaluated from the test data. Note that in the case of a production sensor, these would be known parameters stored in the digital controller software, and thus these quantities would not have to be evaluated when performing mode-switching operation.

At steady state, the initial sensor temperature and ambient temperature are related by

$$T_0 = T_\infty + \frac{V}{UA} (q_e''' + q_n''').$$

Because there is no nuclear power in the RTD, and T_0 is actually A_0 in first order system, the above equation can be rewritten as

$$A_0 = T_\infty + \frac{P}{UA},$$

Again, from definition of the time constant we have

$$MC = \gamma * UA = A_2 * UA$$

Combining these last two equations, a formulation for MC is obtained

$$MC = \frac{P * \gamma}{A_0 - T_\infty} = \frac{P * A_2}{A_0 - T_\infty}.$$

To evaluate MC , A_0 and A_2 were predicted using the first-order algorithm while T_∞ was the measured fluid temperature. The 26 transient tests were evaluated for MC based on this model, giving an average MC of 0.5562 J/°C. This single value is used in the subsequent evaluations of other parameters, although we should note that MC s evaluated in water were slightly larger than those from the wind tunnel, most likely because of the increased importance of axial conduction.

Given measured sensor input power input, the bulk fluid temperature can be calculated using the A_0 regressed from the transient data. Figure 3-40 compares this prediction with the measured bulk fluid temperature from both natural and forced convection cases. With the notable exception of cases 9 and 11, the comparison is quite good.

Also, given measured input power and bulk fluid temperature, the sensor overall thermal resistance UA could be calculated at steady state without introducing any approximations. Well-known correlations for the external convection coefficients of a vertical cylinder in natural and forced convection were then applied to calculate $1/hA$ and subsequently approximate the sensor internal thermal resistance, which should be the same in all cases. The results of this study are shown in Fig. 3-41 for both natural and forced convection cases. Although the calculated external resistance is greatly different in the two cases, the evaluation of the internal resistance agrees quite well. The R_{INT} value for the appropriate sensor temperature was therefore also applied to the final evaluation of h from A_2 .

A direct comparison of the sensor time constant A_2 predicted from the LSM regression of transient data with that found from the MC and steady-state UA values is shown in Fig. 3-42. For the short time-constant condition of natural convection in water, the accuracy is on the order of 10%. The longer time constants from the wind tunnel experiments are also predicted well,

with only Case 11 falling out of the 10% range. The variation caused by changes in air velocity is quite clear. For instance, cases 1-4 at 3.5 m/s may be compared to cases 5-8, which were performed at 5 m/s.

Figure 3-38 shows the pertinent comparison of the external convection coefficient predicted from the regression of the transient data and that given by the empirical correlations. Given the need to estimate several parameters and recognizing the fundamental uncertainty in the correlations, these comparisons, although not perfect, appear perfectly reasonable.

Recognizing the inherent convergence issues of the LSM algorithm, and given a degree of care to ensure that chosen data sampling rates do indeed give convergence of the algorithm, this method should be suitable for implementation into a mode-switching controller. Deriving changes in UA and T_{∞} from dynamic data can then be applied directly to correct the power output from the constant-temperature power-sensing mode.

Figure 3-8: Sensitivity and linearity of the sensor prototype

Figure 3-9: CTPS bandwidth indicated through response to a reactor scram

Figure 3-10: CTPS drift observed over several days

Figure 3-11: CTPS sensitivity as predicted using the numerical model

Figure 3-12: Power response of the sensor to a step-reduction in neutron flux

Figure 3-13: Power response of the sensor to a step-reduction in gamma flux

Figure 3-14: Predicted sensor response to neutron flux white noise input

Figure 3-15: Response of the CTPS to flux steps with power lead conduction modeled

Figure 3-16: Physical configuration of the first three design studies

Figure 3-17: Sensor r-z quadrant temperature redistribution as nuclear/electrical power varies

Figure 3-18: Predicted power response of the sensor to a step-reduction in neutron flux

Figure 3-19: Predicted power response of the sensor to a step-reduction in photon flux

Figure 3-20: Predicted sensor response to neutron flux white noise input

Figure 3-21: R-Z distribution of the heated element that best simulates nuclear heating

Figure 3-22: Sensor r-z quadrant temperature redistribution as nuclear/electrical power varies

Figure 3-23: Predicted power response of the sensor to a step-reduction in neutron flux
Figure 3-24: Predicted power response of the sensor to a step-reduction in photon flux
Figure 3-25: Predicted sensor response to neutron flux white noise input
Figure 3-26: Sensor r-z quadrant temperature redistribution as nuclear/electrical power varies
Figure 3-27: Predicted power response of the sensor to a step-reduction in neutron flux
Figure 3-28: Predicted power response of the sensor to a step-reduction in photon flux
Figure 3-29: Predicted sensor response to neutron flux white noise input
Figure 3-30: Design of the optimized second-generation prototype
Figure 3-31: Sensor r-z quadrant temperature redistribution as nuclear/electrical power varies
Figure 3-32: Predicted power response of the sensor to a step-reduction in neutron flux
Figure 3-33: Predicted power response of the sensor to a step-reduction in photon flux
Figure 3-34: Predicted sensor response to neutron flux white noise input

Figure 3-35: Representation of the CTPS mode-switching process

Figure 3-36: Simulated noisy sensor data and the result of the LSM regressions as a function of total sample time

Figure 3-37: Simulated noisy sensor data and the result of the LSM regressions as a function of sampling rate

Figure 3-38: Circuit used to perform the transient tests. Channel 0 gives loop current, and Channel 1 gives voltage drop across the RTD.

Figure 3-39: Measured and regressed sensor steady-state temperature

Figure 3-40: Measured and regressed bulk fluid steady-state temperature

Figure 3-41: Steady-state measurements of total thermal resistance, combined with convection resistance from empirical correlations, to give the sensor internal thermal resistance

Figure 3-42: Regressed sensor time constant compared with value derived from MC estimation and steady-state UA measurements

Figure 3-43: Regressed external convection coefficient, based on measured UA values and evaluation of internal thermal resistance, compared with correlation predictions

Figure 3-6: Design of the first-generation sensor prototypes

Figure 3-7: Response of the CTPS and fission chamber to a reactor transient

Figure 3-8: Sensitivity and linearity of the sensor prototype

Figure 3-9: CTPS bandwidth indicated through response to a reactor scram

Figure 3-10: CTPS drift observed over several days

Figure 3-11: CTPS sensitivity as predicted using the numerical model

Figure 3-12: Power response of the sensor to a step-reduction in neutron flux

Figure 3-13: Power response of the sensor to a step-reduction in gamma flux

Figure 3-14: Predicted sensor response to neutron flux white noise input

Figure 3-15: Response of the CTPS to flux steps with power lead conduction modeled

Figure 3-16: Physical configuration of the first three design studies

Figure 3-17: Sensor r-z quadrant temperature redistribution as nuclear/electrical power varies

Figure 3-18: Predicted power response of the sensor to a step-reduction in neutron flux

Figure 3-19: Predicted power response of the sensor to a step-reduction in photon flux

Figure 3-20: Predicted sensor response to neutron flux white noise input

Figure 3-21: R-Z distribution of the heated element that best simulates nuclear heating

Figure 3-22: Sensor r-z quadrant temperature redistribution as nuclear/electrical power varies

Figure 3-23: Predicted power response of the sensor to a step-reduction in neutron flux

Figure 3-24: Predicted power response of the sensor to a step-reduction in photon flux

Figure 3-25: Predicted sensor response to neutron flux white noise input

Figure 3-26: Sensor r-z quadrant temperature redistribution as nuclear/electrical power varies

Figure 3-27: Predicted power response of the sensor to a step-reduction in neutron flux

Figure 3-28: Predicted power response of the sensor to a step-reduction in photon flux

Figure 3-29: Predicted sensor response to neutron flux white noise input

Figure 3-30: Design of the optimized second-generation prototype

Figure 3-31: Sensor r-z quadrant temperature redistribution as nuclear/electrical power varies

Figure 3-32: Predicted power response of the sensor to a step-reduction in neutron flux

Figure 3-33: Predicted power response of the sensor to a step-reduction in photon flux

Figure 3-34: Predicted sensor response to neutron flux white noise input

4. In-Reactor Test Facility

4.1 Introduction

We have designed and built an in-reactor test facility to evaluate the static and dynamic performance of the prototype CTPS in environmental and neutronic conditions similar to those expected in a high-temperature gas reactor. The facility has been designed to provide the following capabilities:

- heat test sensors to 800° C
- maintain a constant gas flow rate of approximately 350 liters per minute in a closed cooling flow loop
- provide the maximum neutron flux level available from the OSU Research Reactor
- sinusoidally modulate by a few percent the neutron flux at the sensor location over a frequency range of 1 Hz to 100 Hz with negligible effect on reactor power and reactivity
- independently monitor the neutron flux, the gamma flux, and the temperature at selected locations
- minimize component activation and shield users from radiation

The Large Volume External Cavity (LVEC) dry-tube was chosen to house this test facility. It is a cylindrical 6061-T6 aluminum dry tube with a 9.5” inner diameter and 0.5” thick wall that is secured with a bracket next to the reactor when being used as shown in Figure 4.1. The LVEC facility was selected because it has sufficient space to house both the neutron-flux oscillator and the high-temperature facility and is located in a high neutron flux. An MCNP simulation predicted a total neutron flux of $\sim 1 \times 10^{12}$ nv at full reactor power at the position of the test sensors. The tube is counterweighted with iron plates to provide near-neutral (slightly negative) buoyancy. The plates fill the bottom three feet of the tube, which makes the usable length of the tube about 17’ and positions the bottom of the usable space at the bottom of the core.

To maximize the flux seen from the reactor core, an aluminum box that can be evacuated of water with air is positioned on the front of the LVEC at the midline height of the core. A gold wire flux analysis was performed at 1% reactor power to characterize the horizontal and vertical neutron flux distributions in the LVEC. This analysis estimated a full-reactor-power neutron flux at the test sensor position of 4.5×10^{11} nv with the box filled with air and a flux of 2.1×10^{11} nv with a water-filled box. Above the top of the core, the neutron flux fell off as about $1/x^2$.

The design of the assembly to go in the LVEC was subject to a number of constraints. The high-temperature facility and flux oscillator must be positioned near the bottom of the tube so that they are aligned with the peak flux at the center of the reactor core. This means that a support structure for the flow-loop pipes, wire conduit, high-temperature facility, neutron flux oscillator, and oscillator drive mechanism must be an integral part of the design. This support structure must be in sections to accommodate the limited space between the surface of the reactor pool and the ceiling. The structure must prevent the drive mechanism for the oscillator from translational movement, and it must not

shake any components loose with vibrations emanating from the neutron flux oscillator and shaft.

The facility must be designed to minimize activation of construction materials. Aluminum proves to be the best material for its combined advantages of low cost, low activation, good strength and machinability. To minimize personnel exposure to radiation, the parts of the facility that will become activated must be designed so that they can be quickly removed if they need to be replaced. In addition, the facility must be designed such that sensors can be introduced into and removed from the coolant loop without removing the entire assembly from the LVEC.

Since aluminum components and polyethylene neutron shielding materials have a lower melting point than the maximum temperature to be reached at the sensor-testing position, high temperatures must be confined to the high-temperature facility. Finally, the LVEC must not be heated enough to cause the reactor pool water near the tube to approach boiling.

The support structure is an aluminum skeleton, which has three six foot sections consisting of aluminum plates attached to threaded aluminum rod. The locking nuts used to hold the plates to the threaded rods have all had aluminum wire run through them and the rods to ensure that they cannot back away from the plates from vibration. Components of the test facility are attached to this support structure. The bottom section houses the oscillator and the high-temperature facility. The middle section holds flow-loop pipes and wire conduit, and the top section holds pipes, conduit, neutron and gamma shielding, a helical copper cooling coil, and the oscillator motor. The support structure attaches to the top of the LVEC tube to secure everything in place. Sections are attached together with couplings on the threaded rods (locked in place with nuts) as they are lowered into the LVEC facility. Both wire conduit and flow-loop pipes are schedule-40 aluminum pipes with threading on the ends. Threaded couplings with locking nuts are used to attach the pipe sections together.

A fission chamber is used to monitor the neutron flux level in the region of the test sensors, and a gamma sensor is used to monitor the gamma flux level. The fission chamber is positioned in a plane parallel to the edge of the reactor at the same distance as the prototype sensors, and the gamma chamber is positioned right behind it. They are positioned as close as possible to the prototype sensors without being in the high-temperature region. This allows them to monitor flux levels similar to those seen by the CTPS sensors without being unnecessarily exposed to high temperatures. Type-K thermocouples, which are suitable for radiation environments, monitor temperatures at a number of locations within and outside the high-temperature loop. To monitor facility shaking from the oscillator, two tri-axial accelerometers have been installed in the LVEC facility. The first is in the bottom section, not far above the oscillator, but at its position the neutron flux has dropped off considerably. A second accelerometer was added to the facility at the top of the middle section in case the lower accelerometer is damaged from excessive radiation. It is over nine feet above the top of the core, so it will be exposed to much less radiation.

The assembly's radiation shielding consists of two cylindrical plugs in the top section of the assembly with through holes for pipes. The first is a 1' long section of polyethylene and the second is a 1' foot long section of 80% lead, 1% boron, and 19% polyethylene. Although the through holes allow some streaming, the shielding cuts down significantly the radiation streaming from the LVEC tube.

See Figure 4.2 for pictures of the LVEC and assembly.

- a. LVEC dry tube. The box that can be evacuated can be seen near the bottom.
- b. View of the assembly where it was constructed and tested in the ladder cage of the reactor building bay.
- c. Radiation shielding and the copper cooling coil on the flow loop hot leg.
- d. Connections between assembly sections. There are connections for the threaded rod, the flow loop and conduit pipes, and the oscillator shaft (hidden). Connections for the thermocouples and accelerometer leads can also be seen.

4.2 High-Temperature Facility

Because the high-temperature facility is sharing space in the LVEC with the neutron flux oscillator, it could only be allocated a little more than half of the LVEC inside diameter. A 3 kW alumina and nichrome commercial heat gun element was chosen to heat the airflow based on its small size and ability to output air at 800 °C. A quartz tube houses the heating element, and both sit atop a machined graphite base, chosen for its good thermal characteristics and low activation. The quartz tube holds the heater in place, and the quartz tube is held in position by a coupling above it.. Kaowool, a ceramic fiber blanket made primarily of alumina and silica, insulates the heater assembly, and a thin aluminum cylindrical shell encloses the furnace assembly. This shell holds the fiber blanket in place, and together the shell and fiber blanket protect the quartz tube.

Immediately below the graphite, an aluminum elbow holds a perforated plate, designed to even the velocity profile of the air as it rounds two square elbows. This measure is taken to ensure an even flow across the entrance of the heating element, preventing premature burnout. A pressure feed-through that carries the lead wires for the heater is screwed into the back of the elbow. The lead wires are connected to a 0-240 VAC voltage supply that controls the power to the element.

A tapered neck at the top of the quartz fits into a tapered aluminum slip coupling. This slip coupling consists of two pieces. The top piece can move up or down as the aluminum pipe above expands or contracts or with heating or cooling, and the bottom piece is secured to the outer aluminum cylinder to prevent it from moving and breaking the quartz tube. The coupling contains high-temperature fabric inside and outside that seals the flow loop but still allows for pipe expansion. The furnace facility is suspended a small distance above the bottom plate of the assembly to allow for expansion of the facility (expansion below the slip coupling). It has been designed so that it is secure when it is in place but can be removed quickly to minimize personnel exposure during maintenance.

The complete furnace assembly can be seen in Figure 4.3. Part A is the 1" aluminum down pipe that screws into Elbow #1 (B). Connecting Elbow #1 to Elbow #2 (D) is a short section of threaded pipe (C). E denotes the threaded hole that holds the brass feedthrough for the heater wires. Above Custom Elbow #2, sits the Aluminum Plate (G), with the Pressure Plate (F) at the center. Screwed to the Aluminum Plate is the Graphite Fixture (H), which holds the commercial heater (I). Surrounding the heater is the Quartz Tube (J), which sits inside the Bottom Coupling (N). This coupling is fixed in place by three aluminum bolts (L), which are fed through the Aluminum Ring (M) and tightened against the outer Aluminum Canister (K). The Bottom Coupling is fixed to the Top Coupling (O) with the aid of Fabriseal high temperature fabric (P), held in place with two steel hose clamps. The Top Coupling connects to the exit, or up pipe, by screwing into a custom aluminum nut used to connect the rest of the pipe sections.

Figure 4.4 shows:

- a. Components of the furnace facility.
- b. The assembled furnace facility.
- c. The furnace facility installed in the assembly without the insulation and aluminum shell. This was done to measure the length from the top of the hot leg to the position of the test sensors.

Slip couplings similar to that above the furnace have also been installed in the middle and upper sections of the flow loop hot leg to allow for expansion from heating. To slow down the rate at which the entire LVEC facility heats up, fiber blanket insulation is wrapped around the hot leg of the flow loop in the lower two sections of the facility. A helical copper cooling coil surrounds a 2.5-foot length of the hot leg where it penetrates the shielding in the top section (see Figure 4.2c). This coil removes heat from the furnace flow loop and prevents the shielding from being exposed to high temperatures. Deionized water is pumped from a reservoir through the tube and back into the reservoir, and an ice bath can be used to cool the reservoir. In addition, ceramic inserts were put between the hot leg locking nuts and the structure plates and between the oscillator-shaft bearing blocks and structure plates. In addition, ceramic inserts were put between the hot leg locking nuts and the structure plates and between the oscillator-shaft bearing blocks and structure plates. These ceramic inserts slow the rate at which the LVEC tube heats up as well as prevent conduction of heat to the oscillator shaft, which would speed up degradation of the lubricant in the bearings.

The flow loop feed and return pipes connect to a blower outside of the test facility. This blower is capable of moving about 480 liters per minute (17 cfm) of air through the flow loop, and a valved bypass controls the flow rate. The maximum temperature of 800 °C can be reached by running the blower at 350 lpm with the heater at its maximum voltage. A lower flow rate would allow higher temperatures, but according to the manufacturer would severely degrade the lifespan of the element. To measure the flow rate, a laminar flow element is located between the blower outlet and LVEC facility after a straight pipe section long enough to develop laminar flow. Type-K thermocouples are located along the length of the flow loop hot leg and at the entrance of the cool leg to get an accurate profile of temperatures during operation.

Figure 4.2 Assembly Pictures

Figure 4.3 Schematic of the Furnace Facility

Because parts of the furnace facility will become significantly activated, it was designed so that it can be removed as one piece, allowing it to be put in shielding quickly. Also, parts for a replacement furnace facility have been machined so a new furnace facility can be installed while the original furnace is allowed to decay to acceptable exposure levels.

4.3 Neutron Flux Oscillator

In a previous study the use of noise analysis was shown to be insufficient for determining the frequency response of test sensors. As a consequence a local neutron flux oscillator has been included in the LVEC test facility to give a sinusoidal flux oscillation. The design of the oscillator had to consider available space and global reactivity. It must be reasonably compact to allow enough space for the high-temperature facility, and since the sensors need to be tested at full reactor power, to comply with reactor technical specifications the oscillator must modulate the neutron flux near the sensors without changing the reactor power.

Before the oscillator was constructed, an analytical and experimental assessment of the design was completed to evaluate whether it would meet the performance requirements. MCNP was used to model the use of both polyethylene and graphite as the body. Both gave a five to seven per cent change in flux at the sensor position. Next a mock-up was tested at low reactor power in the LVEC. This test used a Teflon cylinder (similar to polyethylene) with cadmium on one side suspended at the correct height, and a fission chamber in place of the CTPS. The fission chamber response was recorded with the cadmium facing towards and away from it. A flux difference of approximately five per cent was measured, and no reactor control rod movement was necessary to compensate for the change in orientation of the oscillator. This indicated a negligible impact on core reactivity, and the agreement between the computer models and mock-up test indicated that the proposed design would perform as needed.

4.4 Testing Results

Before testing was performed on the LVEC facility, a detailed safety analysis was completed. This analysis looked into possible safety concerns due to the furnace facility, the oscillator, electrical wiring, and reactivity effects, as well as an analysis of Argon-41 production, an activation analysis, and worst-case scenario analysis. Following the completion of this safety analysis, testing was performed in three stages: testing the assembly in the ladder cage of the reactor bay where it was constructed, testing it in the LVEC tube following installation, and testing it with the reactor.

Figure 4.4 Furnace Facility Pictures

The oscillator is comprised of a cylindrical body of moderator that has an absorber on one side and spins along its axis, connected by a shaft to a motor. Graphite is used as the moderator since it retains physical integrity at high neutron fluence, and cadmium was chosen as the absorber since it can be easily shaped. Because of the required rotational speed and wide speed range, a DC motor was selected over other motor types, and a straight 5/8" diameter aluminum shaft consisting of three sections (one for each assembly section) coupled together was chosen over designs using gears. Due to internal bearings that would degrade in a radiation environment, the motor is mounted at the top of the facility. The oscillator is encased in a sturdy aluminum enclosure to prevent it from coming apart at high speeds. This enclosure is welded to aluminum rods running through the graphite to prevent it from rotating within. Aluminum has been machined out of the ends of the oscillator to counterbalance the cadmium sheet on one side. Aluminum end caps are welded to the casing to provide strength and reduce air drag that would be caused by the machined ends.

The oscillator is positioned on the far side of the sensors from the reactor to maximize the flux at the sensor location and to minimize the reactivity effect of the oscillator on the core. A thrust bearing supports the weight of the oscillator, and ten more bearings are spaced along the length of the assembly to prevent the shaft from whipping or bowing. An appropriate combination of sealed and needle-roller bearings was determined experimentally to strike a balance between stability and required motor power. Two sealed bearings, which firmly attach to the oscillator shaft with setscrew collars, have significant drag but provide high stability. Because they have iron blocks, both have been positioned above the high neutron flux area in the bottom few feet of the LVEC. The remaining needle-roller bearings allow slight movement of the shaft, but they cause very little drag for the motor to overcome. These bearings have been mounted in aluminum blocks, so minimal activation will occur. Coupling sleeves with screws through them are used to connect the drive shaft sections together. An optical encoder provides a measurement of the oscillator speed and phase. It reflects light off of a rotating disk that is half white and half black to output one square wave cycle per shaft rotation. See Figure 5 for pictures of the oscillator.

- a. Interior of the oscillator. Aluminum rods penetrate the graphite core, and a cadmium sheet sits to the right of the graphite.
- b. Assembled oscillator before machining to counterbalance the cadmium.
- c. Completed oscillator with welded end caps.
- d. DC motor at the top of the assembly.
- e. Optical encoder with reflecting disk.

Figure 4.5 Oscillator Pictures

4.4.1 Testing in ladder cage

Testing was performed in the ladder cage, where the assembly was constructed and modified, to assure the oscillator and oven operate correctly and safely. Because the assembly is out of sight once it is in the LVEC tube, this testing was critical in that it allowed us to visually verify safe operation. To test in the ladder cage, the facility was bracketed to the ladder at seven different positions. Two brackets at the top of the ladder attached to the top plate of the facility and supported its weight. Five brackets were clamped to facility plates along its length to prevent it from whipping. This was deemed a reasonable approximation of how the assembly would be constrained along its length in the LVEC tube.

The oscillator was tested across the frequency range achievable with the DC motor of 0 Hz to 79 Hz in steps of 5 Hz. The DC motor used for this testing is marginal for its purpose of spinning the oscillator. A replacement DC motor will be identified to achieve the desired 100 Hz range. Accelerations were measured along the x, y, and z axes with both lower and upper accelerometers. Between zero and 50 Hz, the oscillator runs quietly, and accelerations were fairly small. The RMS accelerations measured in all directions by both accelerometers were 3 g's or less. However, as evidenced by audible noise, RMS acceleration measurements, and acceleration power spectral density (PSD) plots, the facility attached to the ladder cage had an excitation frequency at 60 Hz. Measurements were taken without the oscillator running to verify that the 60 Hz peak seen in the PSDs was not 60 Hz noise being picked up by cabling. At 60 Hz, the highest RMS acceleration was seen to be 10 g's along the z-axis of the lower accelerometer. If this RMS acceleration were entirely from one sinusoidal oscillation at 60 Hz, this would correspond to a maximum acceleration (sine wave amplitude) of 14 g's. This acceleration would correspond to a positional sine wave with amplitude of about 1/32". The maximum acceleration measured while at 60 Hz was 35 g's, which mean that periodically different frequencies of vibration move into phase with one another to cause large short-lived accelerations. Above 70 Hz, the accelerations dropped off to RMS levels below 5 g's. See Figures 6 and 7 for plots of acceleration as a function of oscillator frequency. The upper accelerometer measures smaller accelerations, as would be expected since it is farther away from the oscillator. However, it still shows the same excitation at 60 Hz. Figure 8 shows PSD plots of the voltage output of the lower accelerometer X-axis with the oscillator off and with the oscillator running at 30 Hz. In the lower graph, an excitation at 60 Hz can clearly be seen, and the upper graph shows that this 60 Hz peak is not the result of electrical noise. Data was not taken for a PSD at 60 Hz, as we did not want to run the oscillator at this speed for an extended period of time.

Because the anticipated bandwidth of the CTPS sensors is predicted to be about 30 Hz, extended testing of the oscillator was performed at 30 Hz. The oscillator was run at this speed for 30 minutes, and the RMS accelerations were monitored. During this period, the amount of acceleration did not increase, indicating that no parts were shaking loose. From this testing in the ladder cage, the oscillator appeared safe to run between 0-50 Hz and above 70 Hz. Between 50 and 70 Hz, the shaking was not so great that this range

must be avoided entirely, but great enough that operation in this range should be minimized to prolong the useful life of the facility.

The temperature of the furnace facility at the test sensor position was raised in steps to its maximum temperature of 800 °C, and then the LVEC facility was allowed to come into thermal equilibrium to be tested at this temperature. No part of the assembly, including the aluminum expansion coupling right above the furnace, got too hot. The ceramic inserts that were put between the hot leg locking nuts and the structure plates and between the bearing blocks and structure plates prevented the structure from warming up. The plates and oscillator shaft both stayed cool to the touch. Ambient temperatures within the structure were measured, and all stayed low, but since it was not enclosed in a tube, these were fairly meaningless. The section of flow-loop hot leg pipe connected to the helical copper cooling tube remained cool to the touch, and the shielding never got hotter than the temperature of this coil. Figure 9 shows temperatures from this test. While the temperature at the test-sensor position remained at 800 °C, the aluminum coupling stayed below 400 °C, and the shielding stayed well below 80 °C. If ice was used to cool the water used for the cooling coil, shielding temperatures would be even lower.

With the furnace facility at thermal equilibrium at the maximum temperature, the oscillator was run at 50 Hz. Since the hot leg is expanded the most in this state, this is the worst-case scenario for the oscillator shaking the hot leg and facility. The hot leg and facility were both visually verified to not be shaking. The measured accelerations were unchanged from those taken without the furnace running.

4.4.2 Testing in the LVEC

After the facility was installed into the LVEC, tests were run to verify safe operation of the facility in its dry tube. The oscillator was tested across the frequency range achievable with the DC motor of 0 Hz to 50 Hz in steps of 5 Hz. Above 50 Hz, the fuse in the motor controller was blown because the oscillator and shaft require a DC motor with more available torque. Most likely, the assembly was not perfectly straight over its 18' length when it was secured in the ladder cage, but the oscillator rod was very straight within it. When the assembly was loaded into the LVEC, which forced it to be straight, the oscillator shaft was forced into a slight misalignment. Even so, the shaft moves freely when turned by hand, indicating that it is fairly well aligned and working properly.

Figure 4.6 Measured Accelerations from the Oscillator - Lower Accelerometer

Figure 4.7 Measured Accelerations from the Oscillator - Upper Accelerometer

Figure 4.8 PSD Plots of Accelerations from the Oscillator

Figure 4.9 Furnace Facility Test in the Ladder Cage

Accelerations were measured along the x, y, and z axes with both accelerometers. Between zero and 45 Hz, accelerations were fairly small. The RMS accelerations measured in all directions by both accelerometers were 3 g's or less, just like when the facility was in the ladder cage. At 50 Hz, the RMS accelerations began to rise, just as they did at 55 Hz when the facility was secured to the ladder cage. However, all RMS values were still 5 g's or less. This rise may indicate that the resonant frequency has been shifted down to 55 Hz from 60 Hz, but that cannot be confirmed until a more powerful DC motor is installed so that we can test at higher speeds.

The furnace was tested at 800 °C, and all ambient and component temperatures remained reasonable. The temperature of the aluminum coupling above the furnace remained below 400 °C, and all ambient temperatures within the LVEC tube remained below 60 °C. The temperature at the bottom of the shielding remained below 40 °C, which is well below its manufacturer's suggested limit of 82 °C. With the furnace facility at thermal equilibrium at the maximum temperature, the oscillator was run at 50 Hz. The furnace and oscillator had no apparent effect on each other's operation. The RMS acceleration measurements were slightly lower than those discussed in the previous paragraph, but this can be attributed to a minor adjustment made to the motor bracket between these tests.

4.4.3 Testing Next to Reactor

The first test that was run measured Argon-41 production. The postulated worst-case scenario for Ar-41 production and release into the reactor bay was: the reactor at full power for maximum Ar-41 production; the furnace at maximum temperature to release heat into the LVEC and make the air more buoyant; the oscillator running to stir up the air in the bottom of the LVEC; and the LVEC box filled with air, which allows bubbles containing Ar-41 to occasionally rise to the pool surface. The Ar-41 concentration measured by the reactor building's effluent monitor was 1.4 times the derived air concentration (DAC) for Ar-41, which limits the LVEC's time of operation to 1411 full-power-hours/year (ignoring other contributors to occupational dose limits). This is well above the amount of time the facility will be operated in a year, and is therefore not a problem.

While the Ar-41 test was run, exposure rates were measured in the reactor building. The highest rates (excluding right above the LVEC) were measured at the wall at the pool top close to the LVEC tube. The gamma exposure rate was about 20 mR/hr and the neutron exposure rate was about 3 mR/hr, so no posting for high radiation areas is necessary. In addition, the gamma exposure rate in the region of the test sensor position of the LVEC facility was measured. It was 8.9 MR/hr right after reaching full power and 9.4 MR/hr following fission fragment buildup from 45 minutes of full-power operation. A result of this high exposure rate was noticed while the experiment was running. Even though the oscillator was not running, the lower accelerometer was measuring unusually high accelerations. The only possible source of acceleration was the reactor primary coolant pump, which was an unlikely suspect. Following shutdown and turning the pump off, the lower accelerometer was still reading high, indicating that it is affected by the high radiation field. The upper accelerometer's readings with and without the oscillator

running are consistent with its readings previous to operation next to the reactor, so it remains a valid indicator of the facility's integrity.

To test the oscillator in the neutron flux field and to measure the neutron flux in the hot leg and compare it to the flux measured by the mounted fission chamber, a fission chamber was put in the test-sensor position. Since the fission chamber is not intended for operation in high temperature airflow, the furnace was off for all such testing. Most measurements were made with the LVEC box filled with air, but measurements were taken at 5 kW with a water-filled box to determine the difference in neutron flux.

As shown below, the fission chamber mounted in LVEC facility and the fission chamber in the flow-loop hot leg measured neutron fluxes very close to one another. Flooding the box with water instead of air reduces the flux measured by the fission chambers by a factor of about 3.7. Compared to reactor power measured by the control-room instrumentation, the flux in the LVEC is not entirely linear with reactor power, but this nonlinearity is less than 5% across the reactor power range of 1% to 100%.

Table 4.1 – Neutron Flux Measurements in the LVEC Facility

Reactor Power (kW)	LVEC Box Filled with	Mounted Fission Chamber (nv)	Hot-Leg Fission Chamber (nv)	Percent Difference in Reading
5	water	7.94×10^8	8.06×10^8	1.5%
5	air	2.98×10^9	2.98×10^9	< 1%
50	air	2.92×10^{10}	2.90×10^{10}	< 1%
500	air	2.86×10^{11}	2.87×10^{11}	< 1%

At low frequencies of oscillator rotation (≤ 10 Hz), the fission chamber in the hot leg measured a peak-to-peak oscillation of 6.6% at full reactor power. Above this, the percentage oscillation falls off with frequency. This falloff is most certainly a result of the measurement system, not the oscillator. More specifically, the mineral-insulated cable that carries the current from the fission chamber to the electrometer acts as a low-pass filter. Figure 4.10 shows the frequency response magnitude of the fission chamber, and Figure 4.11 shows the determination of the 1st-order dynamic parameters of the measurement system with the oscillator. Figure 4.12 shows that flux oscillation seen at the test sensor position follows the shape of a sine wave reasonable well.

Figure 4.10 Hot-Leg Fission Chamber Frequency Response Magnitude

Figure 4.11 Determination of Hot-Leg Fission Chamber Frequency Response Parameters

4.8 Summary of Results and Conclusion

Table 4.2 shows a summary of the important results and data for the in-reactor test facility.

Table 4.2 – Summary of Results for In-Reactor Test Facility

Parameter	Value
Maximum temperature the test sensor position	800 °C
Maximum air flow rate in the flow loop	480 lpm
Maximum neutron flux on the test sensor position	2.87×10^{11} nv
Maximum gamma exposure rate at the test sensor position	$9-10 \times 10^6$ R/hr
Difference between hot-leg neutron flux and mounted fission chamber neutron flux	< 2%
Nonlinearity in flux measurement vs reactor power	< 5%
Peak-to-peak flux oscillation	6.6%
Maximum oscillation speed	50 Hz
Allowed full-power hours of operation per year for the LVEC facility	< 1400 hours

The furnace facility and oscillator are both working properly, but currently the oscillator does not have the full operational range desired. When an appropriate replacement motor has been identified and purchased, this will be rectified.

Figure 4.13 shows pictures of the completed facility.

- Oscillator and furnace facility mounted in the assembly before installation in the LVEC. The oscillator is in the foreground and the furnace is behind. The bracket holding the fission chamber and gamma sensor is to the left.
- The completed facility secured next to the reactor core.
- LVEC facility connected to the blower for a test of the furnace facility.

Figure 4.13 Completed LVEC Facility

A computer-based control and DAQ system was selected, and two computers running Windows NT with National Instruments (NI) DAQ cards have been purchased and tested. One computer controls the sensors and performs algorithms to test sensor dynamics, and the other streams data to disk. A custom interface box was designed and built to interface the sensors with the computers.

5.2.1 Interface Box

The interface provides the voltage dividers to condition the sensor signals for input to the computer DAQ card, and it provides the power gain necessary for converting a DAQ card output to operating currents for the sensors. In addition, the interface box filters the input sensor signals with 6th-order 500 Hz Bessel filters to allow for creation of PSD plots from sampled data without aliasing.

Figure 5.1 shows a simple schematic one channel of the interface box. Differential voltages proportional to the voltage drop across the sensor and the current through the sensor (voltage drop across a 10-Ohm resistor in series with the sensor) are passed through low-pass filters and sent to the control computer. The control computer uses these voltage and current measurements in some algorithm such as PID control to generate a control signal. Then the control signal is used to set the current through the sensor by adjusting the gate voltage of a FET transistor in a feedback configuration. The source current from the FET, which is controlled via the non-inverting feedback loop seen at the bottom of the figure, is the same current that is run through the sensor.

The design has been completed; a local company that specializes in the fabrication of electronic systems has created a detailed schematic and circuit board layout and has fabricated the electronics board; and the box has been assembled and tested. Figure 5.2 shows the circuit board of the interface box. The white cables on the left connect the interface box to the control and DAQ computers using NI 68-pin connectors, and the sensors and DC supply voltages connect to the interface box via jacks on the front. Power MOSFETs and resistors are mounted separate from the board in the interface box for better cooling, and are not seen in the picture.

Figure 5.1 Simple Schematic of Single Channel of the Interface Box

Figure 5.2 Control and DAQ Interface Box

5.2.2 Control Computer

The control computer contains two DAQ boards, each of which has eight differential analog inputs and two analog outputs. The first board is used to sample the sensors' voltage and current signals and to output the control signals. The second board outputs signals proportional to compensated power estimates to be recorded by the DAQ computer.

To verify that the control computer with its NI DAQ card could provide reliable control at 2 kHz, it was first tested in a simple control configuration. A 60 Hz sine wave was connected to the input, the Labview virtual instrument software fed the input to the output, and the two signals were captured and compared on a digital oscilloscope. Figure 5.3 shows this comparison. The control computer accurately tracks the input with a short delay, except in a few instances. In these instances, the output voltage latches for an extra time step or two because the operating system has taken enough processor resources that the controller cannot update. Further testing has shown that this control system works well for controlling dummy sensors.

Figure 5.3 Control Computer Test with a 60 Hz Sine wave

5.2.3 DAQ Computer

The DAQ computer monitors a number of parameters, which include:

- Test sensor measurements
 - Test sensor voltage and current for two sensors
 - Test sensor control signal for two sensors
 - Compensated power signal for two sensors
- Baselines for comparison to test sensor measurements
 - Neutron flux measurement from fission chamber
 - Gamma exposure rate measurement from gamma ionization chamber
 - Temperature in high-temperature facility
 - Oscillator rotation measurement
- Flow loop measurements
 - Flow loop temperature profile measurements
 - Gas flow rate
- Facility measurements
 - Ambient and component temperatures in the LVEC
 - Structure acceleration measurements

The test sensor voltages, currents, and control signals are acquired through a DAQ card connected to the interface box, and the remaining signals are acquired through a DAQ card connected to a signal-conditioning chassis that multiplexes the signals to allow more to be captured by a single card. All of the signals are sampled at 2 kHz and averaged once per second to be written to a tab-delimited text file. If data for PSD plots is desired, the data for all or a select number of signals can be streamed to disk at the sampling speed.

5.2.4 Instrument Chassis

Much of the equipment that comprises the control and DAQ system is mounted in an instrumentation chassis, seen in Fig. 5.4. This chassis contains:

- DC Power supplies
- Control and DAQ interface box
- Pressure transducers that connect to the flow element in the flow loop
- Instrumentation for control and measurement of oscillator speed
- DAQ signal-conditioning chassis
- DAQ signal breakout boards
- Networking hub to allow DAQ and control computers to exchange data

The interface box is the silver box at the front of the chassis, and the control and DAQ computers can be seen to either side of the chassis.

Figure 5.4 Instrumentation Chassis

5.3 Dynamic Thermal Environment Measurements and Mode-Switching Compensation

5.3.1 Introduction

The constant temperature sensor is based on a simple lumped-parameter energy balance; if the sensor is maintained at a constant temperature above the ambient, then the sum of electrical and nuclear power dissipated by the sensor will remain constant. When this condition holds, the input electrical power becomes an inverse measurement of the deposited nuclear power. There are two inherent assumptions in the stated energy balance. First, it is assumed that the parameter we are controlling, the heated element average temperature, accurately represents the lumped temperature of the sensor. We have noted that both the distribution of power deposition within the sensor and thermal leakage from the sensor can affect the total power dissipation, and therefore the predicted nuclear power, of the sensor. The second assumption is that the heat loss from the sensor to the environment remains constant. If the effective heat transfer coefficient (UA) or the bulk coolant temperature (T_∞) change during operation of the sensor, the electrical power input to the sensor will change to maintain a constant element temperature rather than maintaining the sensor-to-environment temperature difference. Because reactor coolant conditions change, this effect must be compensated.

Traditionally, separate measurements of the coolant conditions, such as a Reynolds number and bulk temperature, would be required to compensate our calorimetric measurement; however, the fact that the CTPS is in a feedback control loop provides another possibility. Because the electrical power input to the sensor is under external control, we may interrogate the sensor and measure its dynamic response. Kerlin et al. (1981) demonstrated the use of a loop-current step response method to effectively and efficiently measure the thermal resistance between plant resistance temperature detectors (RTDs) and their thermowell.

This concept can be extended to independently evaluate UA and T_∞ given a known internal sensor heat generation from nuclear processes. The method begins with the transient lumped-parameter expression for the sensor and its environment,

$$\rho V c \frac{dT}{dt} = UA(T - T_\infty) + q_{NUC} + q_{ELEC},$$

where ρ , V and c are the sensor density, volume and specific heat; T and T_∞ indicate sensor and coolant bulk temperatures; and q_{ELEC} and q_{NUC} indicate electrical and nuclear heating rates, respectively. If the electric power to the sensor is turned off, the sensor average temperature will experience a simple exponential decay from its initial temperature with an asymptotic approach to a final temperature that depends on the coolant temperature and the nuclear heating rate q_{NUC} . The solution to this equation when $q_{ELEC} = q_{ELEC}(0)$ at $t < 0$ and $q_{ELEC} = 0$ for $t > 0$ is

$$T(t) = T_\infty + \frac{q_{NUC}}{UA} + \frac{q_{ELEC}(0)}{UA} e^{(-\frac{UA}{\rho V c} t)},$$

If we assume that q_{NUC} remains constant during this transient, this equation can be represented by the form

$$T(t) = a_0 + a_1 e^{-a_2 t}.$$

This is the basis for mode-switching operation of the CTPS instrument as shown in Figure 5.5. A constant-temperature controller feeds a known current through the heated element, thus a voltage measurement across the element gives its resistance. The power provided by the controller has been shown to be inversely proportional to the deposited nuclear power, q_{NUC} , if the losses to the environment remain constant. At some time, the controller can execute a step-decrease in supplied current to a small but finite constant value. Sampling the sensor voltage drop over time then gives the dynamic response of the heated element resistance, and thus average temperature, which begins an asymptotic approach to the a_0 value. Regression of these data will then allow determination of a_0 , a_1 , and a_2 . If q_{NUC} is known from constant-temperature operation immediately before the step-change, then both UA and T_∞ may be evaluated from a regression, as illustrated in Fig. 5.5. These values may subsequently be applied to compensate the sensor constant-temperature signal for any changes in the environmental loss term.

There are several problems with this seemingly straightforward technique. First, the data must be reduced without detailed knowledge of the sensor conditions. Because the sensor is not truly lumped, but is heterogeneous in composition, the order of the dynamic response is not clear. Linear approximation tells us that a sensor of our design is likely to behave as at least a second-order system, with two discrete exponential decay constants. These decay constants must be estimated to determine the appropriate sampling rate and sample time needed to produce an accurate regression. Also, accuracy of the regression algorithm must not be affected by small changes in sensor properties that may alter the decay constants in operation. In addition, the data to be reduced are likely to be quite noisy, so the regression algorithm must be able to tolerate this.

Second, a controller must be designed to support mode-switching operation. The device must perform the following functions:

- provide feedback control of supplied current to maintain constant resistance to measure nuclear power,
- produce step-changes in the current at prescribed time intervals,
- store the sensor resistance data for some amount of time,
- re-establish feedback control, and then
- perform the regression needed to evaluate the thermal resistance and ambient temperature.

Furthermore, it must be capable of averaging several cycles of mode switching to ensure that the heat loss is not in fact changing before using the calculated thermal information to compensate the measured nuclear power. Clearly, a microprocessor-based digital controller is required to perform these functions.

5.3.2 First-Order Least Squares Regression Algorithm

For the initial sensor model we assume a system of first-order step response to reduce collected sensor data. The sensor temperature response can be described by

$$T(t) = A_0 + A_1 e^{-t/A_2},$$

where A_0 , A_1 and A_2 are constants. Specifically, A_2 is the sensor time constant, A_0 is the temperature at infinity, and the sum of A_0 and A_1 is the initial temperature. To determine the

constants from experimental data, the least square method (LSM) is used. From the definition of LSM, we have the total square error as

$$E = \frac{1}{2} \sum_{i=1}^N (T(t_i) - T_i)^2$$

$$= \frac{1}{2} \sum_{i=1}^N (A_0 + A_1 e^{-t_i/A_2} - T_i)^2$$

or

$$2E = NA_0^2 + \sum_{i=1}^N T_i^2 + A_1^2 \sum_{i=1}^N e^{-2t_i/A_2} - 2A_0 \sum_{i=1}^N T_i$$

$$+ 2A_0 A_1 \sum_{i=1}^N e^{-t_i/A_2} - 2A_1 \sum_{i=1}^N T_i e^{-t_i/A_2}$$

where T_i is the experimental temperature at time t_i , $T(t_i)$ is the temperature obtained at time t_i , and N is the number of experimental data used to evaluate the constants. The next step is to minimize the total square error E . A minimum of the function E is achieved when the following conditions are satisfied:

$$\frac{\partial E}{\partial A_0} = 0, \quad \frac{\partial E}{\partial A_1} = 0, \quad \frac{\partial E}{\partial A_2} = 0$$

The equivalent equations are then

$$NA_0 - \sum_{i=1}^N T_i + A_1 \sum_{i=1}^N e^{-t_i/A_2} = 0$$

$$A_1 \sum_{i=1}^N e^{-2t_i/A_2} + A_0 \sum_{i=1}^N e^{-t_i/A_2} - \sum_{i=1}^N T_i e^{-t_i/A_2} = 0$$

$$A_1 \sum_{i=1}^N t_i e^{-2t_i/A_2} + A_0 \sum_{i=1}^N t_i e^{-t_i/A_2} - \sum_{i=1}^N T_i t_i e^{-t_i/A_2} = 0$$

Clearly, these are non-linear equations. The procedure followed is to iteratively solve the last equation for A_2 , and then directly solve for A_0 and A_1 from the other two equations. A bisection method was chosen to solve for A_2 . Other methods such as Newton's method proved to be less successful.

To test this algorithm, simulated temperature data for a first-order sensor system with a time constant A_2 of 10 seconds, an initial temperature of 200°C, and an ambient temperature A_0 of 40°C were produced. Random noise with amplitude of 100% of the ambient value was superimposed on the root exponential. We then applied the LSM algorithm to regenerate the time constant and asymptote of these data. Figure 5.6 shows the effect of total sample time on the accuracy on the regression of the data given a fixed sampling rate of 100 samples/sec. Even with this noisy signal, a sample equal to the time constant of the decay appears to give adequate accuracy. Sampling rate is varied in Fig. 5.7. Here, the convergence problems characteristic of

least-squares regressions are apparent. The time constant is well predicted by all of the sampling rates evaluated; however, prediction of the asymptote does not converge monotonically on the correct value. Overall, small time steps on the order of 0.001-0.002 s converged acceptably, but time steps of 0.01 s gave better convergence than when either 0.05 or 0.005 s steps were applied. This effect is studied further in Ning (2001).

5.3.3 Regression of Transient Test Data

To evaluate the regression technique with real data, a resistance temperature detector (RTD) was used to simulate a CTPS without nuclear heat. A RTD consists of a thin platinum wire surrounded by a metal/insulator structure that isolates the wire from the ambient conditions and has a heat capacity much greater than the active wire. This gives it dynamics very similar to the planned CTPS device.

A stepped power supply, shown in Fig. 5-8, was used to heat the RTD with a steady current, simulating constant-temperature operation when ambient conditions are held constant. On a trigger signal, the power supply was switched to a much lower voltage, inducing a temperature transient in the RTD. Temperature decay would occur according to the dynamics of the sensor until reaching an asymptote at a temperature somewhat higher than the bulk surrounding fluid. The steady-state temperature was dictated by the energy still being delivered to the sensor, analogous to a constant nuclear heat component in a CTPS. These tests were run with the sensor immersed in a wind tunnel and in still water, demonstrating forced and natural convection conditions, respectively.

The dynamic response of the sensor resistance, and thus temperature, was sampled for 200 seconds at a frequency of 100 Hz. These transient data present a predominantly first-order decay, with a noise level of 5-30% of the signal. The higher noise levels occur at the lower steady-state RTD initial temperatures. These data were reduced using the first-order LSM algorithm. The fit was reasonable when all or half of the data was used, corresponding to about 5 or 2.5 time constants of the system, respectively. When one time constant of data is used, the fit fails to accurately predict the asymptotic approach to an equilibrium temperature. This is poorer performance than that observed in Fig. 5.9 when fitting simulated data, where a good fit was often achieved in less than one time constant. The behavior of the prediction suggests that the measured system is in fact higher-order. A prediction of the system parameters from the first part of the transient captures the higher-order time constants, causing an underprediction of the asymptote. Use of more data reduces the influence of faster-decaying components of the system. A second order algorithm was not applied to reduce these data because the RTD displayed only weak second-order decay.

Seven step-response tests were performed under natural convection conditions in water, while 19 tests were performed in a wind tunnel. The test details are described in Ning (2001). Two air velocities: 3.5 and 5 m/s, and a small range of temperatures from 21 to 27°C were studied. The other tests represent variations in the initial and final temperatures of the sensor when electrical heating is accounted for.

Predicted sensor steady-state temperatures are compared with experimental values in Fig. 5-8. Note that this temperature is not the temperature of the environmental fluid because a low voltage is still imposed on the RTD when the steady state has been achieved. The measured temperatures were obtained from the RTD resistance at steady state, while the predicted values are simply A_0 from the first order regression of the transient data. The comparisons are quite

good, with all but Cases 9 and 11 within the $\pm 1^\circ\text{C}$ accuracy expected from the RTD ambient measurement.

To convert the transient asymptote A_0 from the LSM regression to bulk fluid temperature, and the transient time constants predicted by A_2 to an external convection coefficient h , we must apply the expressions

$$T_\infty = A_0 - \frac{P * A_2}{MC} ,$$

and

$$\frac{1}{hA} = \frac{1}{UA} - R_{INT} = \frac{A_2}{MC} - R_{INT} ,$$

respectively, where M is the mass of the RTD affected by heat transfer, C is the sensor average specific heat, P is the input electrical power, UA is the overall heat transfer coefficient referenced to the outside surface area A , and R_{INT} is the internal sensor thermal resistance.

Generally, for a given sensor, MC and internal thermal resistance are considered known constants because we know the structure and materials of the sensor. However, in this research, we used a commercial temperature sensor whose structure and materials are not specifically known. In addition, the whole length of the RTD does not take part in the measured heat transfer process, and conduction heat transfer in the axial direction of the RTD may be significant. For these reasons, values of MC and R_{INT} were evaluated from the test data. Note that in the case of a production sensor, these would be known parameters stored in the digital controller software, and thus these quantities would not have to be evaluated when performing mode-switching operation.

At steady state, the initial sensor temperature and ambient temperature are related by

$$T_0 = T_\infty + \frac{V}{UA} (q_e''' + q_n''') .$$

Because there is no nuclear power in the RTD, and T_0 is actually A_0 in first order system, the above equation can be rewritten as

$$A_0 = T_\infty + \frac{P}{UA} ,$$

Again, from definition of the time constant we have

$$MC = \gamma * UA = A_2 * UA$$

Combining these last two equations, a formulation for MC is obtained

$$MC = \frac{P * \gamma}{A_0 - T_\infty} = \frac{P * A_2}{A_0 - T_\infty} .$$

To evaluate MC , A_0 and A_2 were predicted using the first-order algorithm while T_∞ was the measured fluid temperature. The 26 transient tests were evaluated for MC based on this model, giving an average MC of $0.5562 \text{ J/}^\circ\text{C}$. This single value is used in the subsequent evaluations of other parameters, although we should note that MC s evaluated in water were slightly larger than those from the wind tunnel, most likely because of the increased importance of axial conduction.

Given measured sensor input power input, the bulk fluid temperature can be calculated using the A_0 regressed from the transient data. Figure 5.10 compares this prediction with the measured bulk fluid temperature from both natural and forced convection cases. With the notable exception of cases 9 and 11, the comparison is quite good.

Also, given measured input power and bulk fluid temperature, the sensor overall thermal resistance UA could be calculated at steady state without introducing any approximations. Well-known correlations for the external convection coefficients of a vertical cylinder in natural and forced convection were then applied to calculate $1/hA$ and subsequently approximate the sensor internal thermal resistance, which should be the same in all cases. The results of this study are shown in Fig. 5.11 for both natural and forced convection cases. Although the calculated external resistance is greatly different in the two cases, the evaluation of the internal resistance agrees quite well. The R_{INT} value for the appropriate sensor temperature was therefore also applied to the final evaluation of h from A_2 .

A direct comparison of the sensor time constant A_2 predicted from the LSM regression of transient data with that found from the MC and steady-state UA values is shown in Fig. 5.12. For the short time-constant condition of natural convection in water, the accuracy is on the order of 10%. The longer time constants from the wind tunnel experiments are also predicted well, with only Case 11 falling out of the 10% range. The variation caused by changes in air velocity is quite clear. For instance, cases 1-4 at 3.5 m/s may be compared to cases 5-8, which were performed at 5 m/s.

Figure 5.13 shows the pertinent comparison of the external convection coefficient predicted from the regression of the transient data and that given by the empirical correlations. Given the need to estimate several parameters and recognizing the fundamental uncertainty in the correlations, these comparisons, although not perfect, appear perfectly reasonable.

Recognizing the inherent convergence issues of the LSM algorithm, and given a degree of care to ensure that chosen data sampling rates do indeed give convergence of the algorithm, this method should be suitable for implementation into a mode-switching controller. Deriving changes in UA and T_∞ from dynamic data can then be applied directly to correct the power output from the constant-temperature power-sensing mode.

Figure 5.5 Representation of the CTPS mode-switching process

Figure 5.6 Simulated noisy sensor data and the result of the LSM regressions as a function of total sample time

Figure 5.7 Simulated noisy sensor data and the result of the LSM regressions as a function of sampling rate

Figure 5.8 Circuit used to perform the transient tests. Channel 0 gives loop current, and Channel 1 gives voltage drop across the RTD.

Figure 5.9 Measured and regressed sensor steady-state temperature

Figure 5.10 Measured and regressed bulk fluid steady-state temperature

Figure 5.11 Steady-state measurements of total thermal resistance, combined with convection resistance from empirical correlations, to give the sensor internal thermal resistance

Figure 5.12 Regressed sensor time constant compared with value derived from *MC* estimation and steady-state *UA* measurements

Figure 5.13 Regressed external convection coefficient, based on measured UA values and evaluation of internal thermal resistance, compared with correlation predictions