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**DESIGN, CONSTRUCTION AND RESULTS OF A LOSS-OF-POOL-WATER
EXPERIMENT WITH A SIMULATED SES-10 REACTOR**

by

JIRI SLABY, Dipl.Ing.

**A thesis submitted to
The Faculty of Graduate Studies and Research
in partial fulfilment of
the requirements for the degree of**

Master of Engineering

**The Ottawa-Carleton Institute for
Mechanical and Aerospace Engineering**

**Department of Mechanical and Aerospace Engineering
Carleton University
Ottawa, Ontario**

May, 1993

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DESIGN, CONSTRUCTION AND RESULTS OF A LOSS-OF-POOL-WATER EXPERIMENT WITH A SIMULATED SES-10 REACTOR

submitted by Jiri Slaby, Dipl.Ing.
in partial fulfilment of the requirements for
the degree of Master of Engineering

Thesis Supervisor

Chair, Department of Mechanical and Aerospace Engineering

Carleton University
May 14, 1993

Abstract

An electrically heated apparatus used to simulate a loss-of-pool-water accident (LOPWA) in a SLOWPOKE Energy System (SES-10) has been designed and built to validate a computer program LOPWAT2 predicting behaviour of the reactor after this kind of accident. Contributions have been made to the development of LOPWAT2 from a previous version called LOPWAT. A version of LOPWAT2, called LOPWATEX, has been developed to represent the experimental conditions of the apparatus. Three stages of experiments were run: steady-state with core module in the open room, water cooled steady state, and transient. Comparison of the experimental results with the LOPWATEX program predictions provides insights into the flow and heat transfer mechanisms, permitted improvements in the program LOPWATEX, and gives confidence to the predictions of the program LOPWAT2 for the actual reactor. Other kinds of tests are planned and possible with only a slight modification of the present apparatus.

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Nomenclature

a	overheat ratio
A	anemometer constant found by calibration
A_c	core flow area, m ²
A_h	total heat transfer area of heaters, m ²
A_L	area of the insulation at average radius, m ²
A_s	cross-sectional area of the sensor, m ²
B	anemometer constant found by calibration
B₅	coefficient in subroutine TEMPGET
C₄	coefficient in subroutine TEMPGET
c_p	air specific heat, J/kg.K
c_p	specific heat of MgO, J/kg.K
d	diameter of hot-wire sensor, m
D_h	hydraulic diameter, m
E	anemometer output voltage, V
F(x)	the function value of the equation of type F(x)=0
F₁	coefficient in subroutine TEMPGET

F_2	coefficient in subroutine TEMPGET
F_3	coefficient in subroutine TEMPGET
F_4	coefficient in subroutine TEMPGET
F_5	coefficient in subroutine TEMPGET
G	air mass velocity, $\text{kg/m}^2.\text{s}$
h	core average heat transfer coefficient, $\text{W/m}^2.\text{s}$
h_{av}	average heat transfer coefficient over the heater sheath increment, $\text{W/m}^2.\text{K}$
h_{im}	manometer fluid height, m
h_s	heat transfer coefficient over the hot-wire sensor, $\text{W/m}^2.\text{K}$
i	variable which counts iteration steps
j	variable which counts heater sheath increments
k_a	air thermal conductivity, W/m.K
k_{ar}	reference air thermal conductivity at T_{ar} , W/m.K
K_c	Fanning friction factor
K_{cc}	correction factor for the calculated pressure losses in LOPWATEX established using m_{HIB} from tests in the open room as described in Section 5.3.1.
K_{ent}	entrance pressure loss
K_{exit}	exit pressure loss
k_r	thermal conductivity of MgO, W/m.K
k_{ins}	thermal conductivity of the insulation, W/m.K
k_s	thermal conductivity of Incoloy sheath, W/m.K
l	length of the hot-wire sensor, m

L	length, m
L_a	active length of heaters, m
L_c	length of the core, m
m	mass flow rate, kg/s
M_t	mass of MgO, kg
m_{HB}	mass flow rate determined from the heat balance on the air in the core, kg/s
m_{HWA}	mass flow rate measured by hot-wire anemometer, kg/s
n	anemometer constant found by calibration
N	number of calibration points
Nu	Nusselt number
p	an integer number which increases by one when sign of $F(x)$ does not change
P	input power to heaters, W
p_a	atmospheric pressure, Pa
P_h	total heat transfer perimeter of heaters, m
P_{st}	heat storage rate in the heaters, W
Q	power transferred to air, W
Q_L	heat loss, W
q_{LR}	heat loss ratio from the core
r	an integer number which increases by one when sign of $F(x)$ changes
R	radius of MgO in the heater, m
R_a	air gas constant, Nm/kg.K
Re	Reynolds number

r_m	radial position in MgO where local temperature equals radial average temperature, m
R_o	outside heater radius, m
R_p	resistance of the hot-wire at the temperature of the ambient fluid, Ω
R_s	resistance of the heated hot-wire sensor at its operating temperature, Ω
R_{sh}	mean radius of heater sheath, m
S	sign value in the iteration method in subroutine TEMPGET
T_a	air temperature, K or $^{\circ}\text{C}$
T_{ar}	reference air temperature, K
T_{av}	average temperature over heater sheath increment, K or $^{\circ}\text{C}$
T_{ax}	air temperature at any axial position, K or $^{\circ}\text{C}$
T_{ca}	average air temperature over the heater increment, K or $^{\circ}\text{C}$
T_c	air temperature at top of riser, K or $^{\circ}\text{C}$
T_r	radial average MgO temperature, K or $^{\circ}\text{C}$
T_{rc}	core-average MgO temperature, K or $^{\circ}\text{C}$
T_{rvo}	core average MgO temperature at beginning of time step, K or $^{\circ}\text{C}$
T_{tvo}	average MgO temperature, K or $^{\circ}\text{C}$
T_{tz}	axial average MgO temperature, K or $^{\circ}\text{C}$
T_d	downcomer air temperature at top of core, K or $^{\circ}\text{C}$
T_b	air temperature at core outlet, K or $^{\circ}\text{C}$
t_i	inside width of the insulation, m
T_k	downcomer air temperature at riser top, K or $^{\circ}\text{C}$
T_m	mean film temperature, K or $^{\circ}\text{C}$

t_o	outside width of the insulation, m
T_r	air temperature at core inlet, K or °C
T_{rw}	temperature of riser wall, K or °C
T_s	hot-wire sensor temperature, K or °C
t_{sh}	heater sheath thickness, m
T_{sh}	heater sheath temperature, K or °C
T_{shz}	heater sheath temperature at any axial position, K or °C
T_w	temperature of tank wall, K or °C
u	air velocity, m/s
u_c	calibrated air velocity, m/s
U_{fo}	overall heat transfer coefficient MgO-coolant, W/m ² .K
U_L	heat transfer coefficient core-insulation, W/m ² .K
u_m	measured air velocity, m/s
x_n	variable in the iteration method in subroutine TEMPGET
x_{n+1}	new value of x_n in the iteration method in subroutine TEMPGET
z	distance from inlet of core, m
Z	$Q/m.c_p$, K or °C

Greek symbols

α	temperature coefficient of resistivity, °C ⁻¹
----------	--

α_{ht}	fraction of heater power transferred to air at a given time
β_i	coefficient in subroutine TEMPGET, ref. [15]
β_o	coefficient in subroutine TEMPGET, ref. [15]
δL	length of heater increment, m
δQ	power to heater increment, W
δt	time step, s
ϵ	standard deviation
μ	air viscosity, kg/m.s
ν_a	kinematic viscosity of air, m ² /s
ν_{ar}	reference kinematic viscosity of air at T_{ar} , m ² /s
ρ	air density, kg/m ³
ρ_L	density of alcohol, kg/m ³
ρ_r	resistivity of the sensor material, $\Omega.m$

Chapter 1

Introduction

1.1 SLOWPOKE Energy System SES-10

The first SLOWPOKE type reactors were built in the 1970's. These reactors generate only 20 kilowatts of heat and are used for research and activation analysis at seven Canadian institutions. Further, one reactor was built at the University of the West Indies in Jamaica. An up-rated version of this reactor was proposed for space heating and low-temperature industrial purposes [1]. The word SLOWPOKE stands for Safe Low Power Critical Experiment.

On July 15, 1987, a 2 Megawatt version of the heating SLOWPOKE, the Slowpoke Demonstration Reactor (SDR), started operation at Whiteshell Nuclear Research Establishment in Manitoba [2]. This reactor was used to test the concept and various components of a heating SLOWPOKE [2,3]. During its operation at 1 Megawatt, the

AECL's understanding of thermal hydraulics and physics was validated and confidence in the concept was provided [4]. The SLOWPOKE Demonstration Reactor was also intended to be used as a demonstration of building heating at Whiteshell Nuclear Research Establishment [5], but it never was used for this purpose [1].

The SLOWPOKE Energy System SES-10 (see Figure 1), designed by AECL Research, is a 10-megawatt heating reactor which is at the forefront of other small heating reactor designs worldwide. The SES-10 was designed primarily for use as a district hot water heating system for building purposes intended to be sited in urban areas and it offers the following advantages [6]:

- low cost energy from a non-polluting fuel
- long-term fuel security for countries currently dependent on imported fossil fuels
- reduced need for rail and road transportation of fossil fuels

The SLOWPOKE concept is a pool type reactor using light water as a coolant as well as a moderator. In the case of SES-10, the dimensions of the pool are approximately 12 m in height and 6 m in diameter. The uranium fuel heats light water contained in the pool. Warm water (approximately 85°C) rises up through a riser to heat exchangers where the heat is removed by the low-pressure, forced-flow secondary loop. A third loop transfers heat to a heat distribution system. The cooled pool water flows to the bottom through the down-comer and re-enters the core (see Figure 1). The flow is driven only

by the buoyant pressure difference between the warm and cold water; thus, no pumps are required in the primary loop.

Two designs of the SES-10 were developed; the design with an open riser duct and a closed riser duct. The closed riser duct design is shown in Figure 1. In this design, the plate type heat exchangers are directly connected to the riser duct. Figure 2 shows the open riser duct design where the heat exchangers are not connected to the riser duct and rising warm water is allowed to mix with the downcomer water. The open riser duct design is now being pursued.

The reactor utilizes low enrichment uranium dioxide fuel which is contained in tubes assembled into the square bundles. One fuel load contains about 1100 kilograms of uranium and serves for about four to six years [7].

The SES-10 has the following safety features [8]:

- Low power level, fuel inventory and core burn-up resulting in extremely low potential hazard.
- Low power density and operating temperature of the fuel and fuel cladding; thus lowering the probability of cladding defect and consequent release of fission products.
- Cooling water in the pool under atmospheric pressure which eliminates pressure driven loss-of-coolant accident.
- Natural circulation of coolant which eliminates the need for coolant pumps and thus their possible failure and consequent flow-loss are excluded.
- Negative void and temperature reactivity coefficients, thus providing inherent

shutdown in the case of power increase after a loss-of secondary cooling.

- Large heat capacity of the pool which provides considerable time for corrective action under shut-down conditions in the event of a loss of coolant in the secondary loop.

- Low reactivity rate additions enabling usage of slow response control devices, compared to power reactor devices; these are two in an SES-10 reactor: absorber rods and plates driven into the core by gravity (without any motors) and slow response liquid absorber with temperature activated valves requiring no external power supply.

- Uninterruptible back-up power supply, which provides power to the most important instrumentation and equipment for up to 12 hours ensuring thus operation of these devices in the case of normal power supply loss.

- Storage of used fuel in the reactor itself until its radioactivity has decayed to a safe level for transportation in a lead flask.

- Water purification system cooler providing a back-up heat sink in the event of loss of the normal cooling systems, i.e. the secondary loop.

- Sealed pool lid preventing a release of radioactive gases, radiolytic hydrogen and water vapour from the pool to the reactor room.

- Air purification circuit capable of removing any fission-product iodine and other gases which could escape to the cover-gas region in the event of fuel cladding defects.

- Continuous monitoring of the reactor room and of the building for fire, radioactivity or intrusion.

The decay heat rejection system has been tested and could be used to prevent the pool water from boiling off and uncovering the reactor core in the case that the heat exchangers would be shut down either for maintenance or because of a loss of power for the secondary loop pumps [9,10].

1.2 Previous Studies of a Loss-of-Pool-Water Accident in SES-10

The loss-of-pool-water accident (LOPWA) is not considered as a basis for licensing purposes due to its very low probability ensured by the following design and operating features [11]:

- Double containment of pool water by a stainless steel tank and a concrete vessel.
- Monitored air gap between the stainless steel tank and the concrete wall which would allow rapid detection of any leakage through the tank.
- Large volume of water in the tank which would take a long time to drain if any leak occurred, thus providing enough time for corrective action.
- Large depth of the tank resulting in the core being below ground water table in almost all cases, so that the core would stay covered even if a leak from the pool occurred and remained undetected indefinitely.

Despite all these safety features, several experimental and analytical studies of such an accident have been completed in recent years. These studies consider only the period after the tank pool is completely emptied of water and the core is cooled only by the air. The transient states during water boiling off are not considered since it is a separate problem requiring more complex analysis.

The experimental study by Schenk [12] documents sheath temperature measurements of electric heaters simulating a SLOWPOKE III bundle. The model did not simulate a tank and a concrete wall. The bundle was constructed of 7 heaters placed

in a pyrex tube representing the core. Some tests were done with a pipe placed on the top of the pyrex tube in order to simulate the riser. Schenk's conclusion was that the sheath temperatures would not exceed 820°C providing the core remained covered for 10 minutes after the reactor shutdown and $1,000^{\circ}\text{C}$ providing the core is uncovered later than 2 minutes after the reactor shutdown.

In analytical study of a loss-of-pool-water accident, Krzaniak [13] developed the computer program LOPWA. This computer program predicts temperatures at critical positions in the SLOWPOKE-10 reactor after a loss-of-pool-water accident for steady state conditions. The computer program was modified for Schenk's experiment and confirmed the measured data reasonably well.

Krzaniak's computer code was improved by Rogers [11,14] to allow for transient heat storage effects in the core and in the concrete walls of the pool and the surrounding ground. This program, LOPWAT, predicts transient behaviour of the core after its uncovering. The results predict that the maximum fuel sheath temperature would reach about 980°C if core uncovering occurred one hour after reactor shutdown. The peak fuel sheath temperatures would be considerably lower for later core uncovering times. Thus, the analysis showed that no significant fuel or fuel sheath damage would be caused under the condition that the core uncovering did not occur earlier than one hour after reactor shutdown.

This thesis describes an experimental apparatus and its components designed to validate the analytical models used in LOPWAT. Concurrent with the experimental program, an improved version of LOPWAT, called LOPWAT2, was developed [15]. Also a modified version of LOPWAT2, called LOPWATEX, was developed for the conditions of the experiment. The experimental apparatus is described in Chapter 2. Chapter 3 presents the instrumentation required to measure needed variables. The developed computer programs and the computer predictions are described in Chapter 4. Chapter 5 presents the experimental results. Discussion of the results is in Chapter 6 and the conclusions are in Chapter 7.

Chapter 2

Experimental Apparatus

2.1 Introduction

The apparatus consists of the following major components: electric heaters, a core box, a riser box, a stand, an inside liner, an outside liner, and a concrete wall. The schematic diagram of the apparatus is shown in Figure 3 and pictures of the apparatus setup and of the core-riser module are shown in Figures 4 and 5 respectively. The apparatus and its components are described in the following sections.

The aim of the design was not to create a scale model of the SES-10 reactor, but to ensure that the important thermohydraulic and heat transfer mechanisms of the reactor are reproduced to the extent possible in the apparatus. The values of the scaling ratios in the model and in the actual SES-10 reactor are shown in Table 1.

Some of the ratios in the model were restricted by practical considerations. The laboratory height influenced the selection of the core and riser heights. Consequently, the electric heater size was affected by the core height and by the availability of heaters on the market. The ratio of surface area of the concrete wall to the surface area of the fuel elements is higher than in the reactor case, since the model core simulates only a bundle in the centre of the SES-10 core.

2.2 Electric Heaters and Their Connection

16 electric round tubular heaters TRI-1644 manufactured by OMEGA Engineering Inc. are used to simulate fuel elements of the core. The heaters are 406.4 mm long with a heated length of 231.8 mm and a diameter of 12.6 mm. The heating coil is insulated by magnesium oxide covered by Incoloy sheath. The heating capacity of heaters is 400 W/heater; however, the maximum requirement of the experiments is approximately 40 W/heater. The maximum allowed voltage for the heaters is 120 V.

The heaters are arranged in a 4x4 array mounted in a core between stainless steel endplates (see Figure 6). The heater terminals are electrically insulated from the core endplates by ceramic washers and bushings. These were cut into needed thickness from ceramic thermocouple protection tubes manufactured by OMEGA Engineering Inc. from Omegatite 300. This material has adequate compressive and tensile strength, and provides

sufficient dielectric strength and volume resistivity even at temperatures as high as 1,500°C; thus, it is safe for a temperature of 800°C in the experiments. The mounting of the heaters allows for an axial clearance of 2.4 mm within the core endplates in order to allow for axial thermal expansion of the heaters.

The heaters are connected as shown in Figure 7, i.e. eight pairs in parallel with each pair in series. Heater hook-up wires 8TQ-1201 (AWG no. 12) and 8TQ-1401 (AWG no. 14) are used. The gauge was selected in consideration of the high test temperatures. The wires are Mica insulated and may be used up to 60 Amps and 1000 V rated at 450°C. The wire leads of the heaters (AWG no. 14) are connected to the terminals screwed on the centering plate (see Section 2.3). Then the main power line (AWG no. 12) is led from the terminals out of the model to the wattmeter and variac (see Section 3.7).

2.3 Core and Riser Boxes, and Stand

The core box consists of a 1/8" (3.2 mm) thick AISI 304 stainless steel square tube. The length of the core is 418 mm and the inside dimensions are 65 mm by 65 mm. The electric heaters are arranged in a 4x4 array in the core and are mounted to the core endplates (see Figure 6). The surface between lower endplate and the core is insulated

against the leakage of hot air from the core into the downcomer by graphite gasket 1/32" (0.8 mm) thick, which may be used in temperatures of up to 500° C. The upper core endplate and core are insulated by three gaskets manufactured from Garlock #900, when the middle gasket is 1/16" (1.6 mm) thick and the others are 1/32" (0.8 mm) thick. The middle gasket has a cut-out and serves as feedthrough for thermocouple wires measuring the heater sheath temperature. The riser box is mounted on the core box and is insulated from the upper endplate by a Garlock #900 gasket 1/16" (1.6 mm) thick. Garlock #900 gaskets may be used at temperatures of up to 400°C.

At the bottom side of the core, the entrance unit is bolted to the core box in order to increase the velocity of the air to a range measurable by the hot-wire anemometer. The entrance unit is 30x30 mm square tube manufactured from AISI 304 stainless steel. The surfaces of the lower endplate and the entrance unit are insulated by two silicone gaskets, 1/4" (6.4 mm) and 1/8" (3.2 mm) thick, which may be used for temperatures of up to 260°C. The power wires (AWG no. 14) connected to the heaters are led out through grooves in these gaskets to the terminals. The fitting for hot-wire probe guide tube is screwed to one side of the entrance unit. The fitting contains a lip seal which can move 2 mm in any radial direction to allow for easier alignment of the leading tube for the hot-wire anemometer guide tube. The attachment of the velocity duct to the core endplate is shown in Figure 8.

The sides of the core box are insulated by Kaowool blanket, which has a density

of 128 kg/m^3 , in order to simulate a fuel element bundle in the center of the core. The Kaowool blanket insulation may be used in temperatures of up to $1,260^\circ\text{C}$. The insulation thickness is 100 mm and its thermal conductivity is 0.0346 W/mK at ambient temperature.

The riser box consists of a $1/8"$ (3.2 mm) thick AISI 304 stainless steel square tube. The length of the riser over the flanges is 411 mm and the inside dimensions are 95 mm by 95 mm. The top of the riser allows for the mounting of an orifice to simulate the pressure drop of the heat exchangers to simulate a closed riser duct design. In the present experiments, only the open riser design was simulated so no orifices were mounted on the top of the riser. Four thermocouple fittings are mounted in the walls for the air temperature measurements (see Figure 3). Three thermocouples are used to measure the heater outlet temperature because of the flow disturbance by the endplate as well as to allow for non-uniform flow distribution in the core. The riser and the core boxes are mounted on a stand. In order to minimize heat loss from the core box, the core endplates are thermally insulated from the riser and from the stand by $1/16"$ (1.6 mm) thick gasket manufactured from Garlock #900 and all screws and nuts joining the core, the riser, and the stand are thermally insulated by Garlock #900 washers.

The stand consists of an AISI 304 stainless steel base plate and four angle legs welded to the base plate (see Figure 8). The base plate is then bolted to a centering plate which fits into the pins on the bottom of the tank, so that the orientation of the

feedthroughs for the hot-wire probe guide tube is fixed against the feedthroughs in the tank and concrete walls.

2.4 Tank

The tank (see Figure 9) is made of a 1/8" (3.2 mm) thick AISI 304 stainless steel cylinder. Its outside diameter is 447 mm and its inside height is 1,219 mm. The bottom of the tank is welded, while the cover is bolted to a flange. Removal of the cover permits insertion or removal of the centering plate with the core, riser, and thermocouples. The tank itself can be inserted in or removed from the concrete cylinder by a chain hoist. A 1/2 ton Mastercraft overhead chain hoist with 3 meter lifting range, CM 633 trolley and an S-beam S 6x12.5 of 4.4 m length were selected and designed for this purpose.

The surfaces of the cover and of the tank flange are insulated by two Garlock #900 gaskets and one Thermofab Millboard style 800 gasket. There are cut-outs in the Thermofab gasket serving as grooves for thermocouple and power supply wires leading out of the tank. These cut-outs are protected from both sides by Garlock #900 gaskets. The bottom side of the tank flange is insulated from the top surface of the concrete wall by another Thermofab Millboard style 800 gasket, thus preventing the air in the air gap between the tank and the inside liner from mixing with the outside air.

The apparatus is designed to allow steady state tests to be performed too. These tests allow reaching the steady state conditions much faster and permit verifying and refining of the computer program by removing the influence of the concrete wall. During steady state tests, the air gap is filled with water in order to remove produced heat. The Thermofab gasket between the tank and the concrete wall is not used during steady state runs so that the user may observe the water level in the gap and ensure that it does not overflow.

Three stainless steel AISI 304 o-ring fittings are welded in the tank wall for the feedthrough tube for the guide tube of the hot-wire probe. Four silicone o-rings #113 are used. This high level of redundancy is chosen in order to ensure that no water (during steady state tests) leaks from the air gap into the tank and thus gets into the contact with live electric power since some o-rings could be damaged during insertion of the leading tube for the guide tube of the hot-wire probe.

With the aid of the alignment tool (508 mm in diameter) which lies on the bottom of the inside liner, the tank is positioned in the center of the inside liner. The tool is made of stainless steel wire of 3/16" (4.8 mm) in diameter with four triangle spacers welded evenly around the perimeter. Spacers force the tank into the desired position as it is inserted into the concrete cylinder. The top of the tank is centralized by bolting it to the concrete wall. This also prevents lifting of the tank by hydrostatic pressure created by water in the air gap during the steady-state tests.

2.5 Inside Liner

The inside liner (see Figure 10) is made of a 1/8" (3.2 mm) thick AISI 304 stainless steel cylinder, which has an outside diameter of 540 mm and a height of 1,226 mm. These dimensions create 43 mm wide air gap between the inside liner and the tank simulating the air gap in the reactor itself.

The inside liner serves two functions. First, it reinforces the concrete wall that must support the large compressive thermal stresses at elevated temperature at the concrete wall internal side. Secondly, it serves as a mold during casting of the concrete. The stainless steel material for the tank and the inside liner was selected for the following reasons: to prevent corrosion which might occur due to elevated temperatures and presence of cooling water in the air gap, and to satisfy the requirement for durability.

2.6 Outside Liner

The outside liner (Figure 11) consists of a 1/4" (6.4 mm) thick ASTM A-36 mild steel cylinder, which has an inside diameter of 940 mm and a height of 1,350 mm. It serves the same purposes as the inside liner, i.e. concrete reinforcement and mold. Four handles are welded to the outside liner allowing for the moving the assembly by a crane. The outside liner is welded to a 1/2" (12.7 mm) thick ASTM A-36 mild steel square plate

1155 mm by 1155 mm which forms the bottom of the assembly. Four steel spacers welded on the plate supported the inside liner during pouring of the concrete in order to create the concrete bottom. The bottom plate is supported by and bolted to seven I-beams W 200x36 in order to distribute the weight of the assembly to a level not exceeding the allowable floor load in the laboratory.

Three tubes with washers are threaded into the fittings in the inside liner and welded to the outside liner. These tubes provide feedthroughs through the concrete wall for the leading tube for the guide tube of the hot-wire probe.

2.7 Concrete Wall and Spacers

The concrete wall is cast between the outside and inside liners. The wall thickness of 200 mm was chosen to ensure that maximum electric heater sheath temperatures approach fuel sheath temperatures predicted for the reactor itself before there is significant heat loss from the outside surface. The concrete bottom thickness is 124 mm. The concrete compressive strength specified by the supplier is 40 MPa with aggregate of maximum size of 10 mm.

The liner spacers (see Figure 12) served two purposes. First, they kept apart the inside and outside liners during the casting of the concrete. Secondly, they positioned the

thermocouples accurately. The thermocouple wires are placed in steel tubes welded to spacers in order to ensure their exact position in the concrete. Spacers and bolts joining them to liners are thermally insulated by teflon plates and bushings in order to minimize heat loss by conduction out of the tank.

Chapter 3

Instrumentation

3.1 Introduction

In order to verify the validity of the predictions of the computer program LOPWAT2 for the loss-of-pool-water accident in the nuclear heating reactor SLOWPOKE, several quantities predicted by the program were measured. The most important variables are temperatures at various locations and the cooling air velocity. The following sections describe the instrumentation and methods used to obtain these measurements and calibrations of the instrumentation.

3.2 Thermocouples

Temperatures are measured at 47 locations as indicated in Figure 3. The data

acquisition system registers six heater sheath temperatures along an inner heater element, one air temperature at the core inlet, three air temperatures at the core outlet (three thermocouples are used because of non-uniform temperature distribution in the flow and because of the flow disturbance by the endplate in Figure 6), one air temperature at riser outlet, three riser wall temperatures (at top, center, and bottom), twelve tank wall temperatures at six axial positions at two circumferential positions 180° apart, five temperatures across the concrete wall at three levels, three temperatures across the center of the concrete bottom, and two thermocouples on the lid and one in the lid insulation. The thermocouple junctions measuring the heater sheath, the riser wall, the tank wall, and the lid temperatures are welded to the surfaces by the capacitance-discharge method.

Type K thermocouple wire, which has a maximum useful temperature range from -200°C to 1250°C, is used for all temperature measurements since it can be used in air over long time periods [16] and it is more resistant to oxidation at elevated temperatures than the other thermocouple wire types [17]. Its limit of error is 2.2°C or 0.75% (whichever is greater) at temperatures above 0°C [18].

Type K thermocouple is formed by chromel (nickel-chromium) and alumel (nickel-aluminum) metals where nickel-chromium is the positive lead and nickel-aluminum is the negative lead. The material used for a thermocouple wire insulation is Kapton in the case of the thermocouple wires (AWG no. 20) poured in the concrete, magnesium oxide insulation sheathed by Inconel 600 in the case of the thermocouple wires (AWG no. 30)

measuring the heater sheath temperature, and fiberglass insulation in the case of the other thermocouple wires (AWG no. 24).

The thermocouples are connected to the data acquisition system board EXP-16, which provides cold-junction compensation as well as a stable amplification of the low thermocouple voltage. The thermocouple output conversion is performed in the data acquisition program SLOWPOKE using a look-up data table provided by the manufacturer of the boards (Omega Engineering Inc.).

A radiation shield (stainless steel tube, O.D. $\frac{1}{4}$ " (6.4 mm), I.D. $\frac{1}{8}$ " (3.2 mm)) is provided for the thermocouples measuring the air temperature, because of the possible measurement error created by the radiation heat exchange between the thermocouple sensor and the surrounding stainless steel riser, the tank walls, and the lid, since the air temperature is in the range of 70 to 390°C while the riser wall, the tank wall, and the lid temperatures are in the range 40-200°C, 10-140°C, and 60-170°C, respectively. With the shield, the radiation error is considerably reduced because the temperature of the shield is closer to the temperature of the air, and the thermocouple "sees" the shield as the walls. The shield is made so that the air may flow freely over the thermocouple [19], so that it does not inhibit convective heat transfer at the thermocouple surface, and so that it extends far enough forward and backward in the flow direction to reduce the solid angle of open radiation to a small value [20]. In order to reduce the error of the measurement further, the outside surface of the shield is polished to keep the emissivity low [21].

3.3 Hot-wire Anemometers

3.3.1 Air Velocity Measurement Techniques

Several methods for the velocity measurement were considered.

Since the expected air velocity is very low, the Pitot tube measurement is not suitable, because the pressure difference would be too small [22].

In the case of the Laser-Doppler Anemometer, the supply of scattering particles creates a problem, because the studied air-flow is in closed space and the experiment lasts several hours. The supply of the particles would have to be continuous through the model bottom with preheated air to the instantaneous temperature of the air presently entering the test section, so that the measurements of the study are not influenced by the temperature of the air coming in with the particles.

Visualization techniques were not convenient for similar reasons to those of Laser-Doppler Anemometry and because of the need for building an observation window in the concrete wall and the riser.

Thus, as the most convenient method of the air velocity measurement, hot-wire anemometry has been selected. The minimum velocity which the constant temperature

hot-wire anemometer can accurately detect occurs at the point when forced convection first dominates over natural convection. For conventional hot-wire anemometers the limit is about 0.15-0.2 m/s [23], which is lower than the minimum velocity expected in the experiment.

3.3.2 Principle and Modes of Operation

The hot-wire anemometer circuitry is based on the Wheatstone bridge with the sensor as one arm of the bridge R_s (see Figure 13). Two fixed resistors, R_1 and R_2 , and one adjustable resistor R_{adj} complete the bridge. The adjustable resistor is set to a value larger than would be required to balance the bridge before the system is placed in operation. When power is applied, the sensor heating current is increased by the feedback amplifier until the bridge becomes balanced. An increase in velocity cools the sensor and unbalances the bridge. The bridge unbalance is sensed by a differential feedback amplifier and the current is added to balance the bridge and to keep the sensor temperature constant. The measured voltage difference across the bridge is proportional to the velocity and is converted into the velocity using a calibration curve for the sensed voltage. Two modes of operation of the hot-wire anemometer are commonly used [23].

The constant temperature mode maintains constant resistance of the hot-wire sensor, and thus, it maintains constant sensor temperature as the velocity changes. As the

velocity of the air flow over the sensor varies, the supplied current is adjusted in order to keep the resistance of the sensor constant, and thus, the voltage also varies. The voltage change as a function of the velocity is recorded during a calibration process at known velocities. The voltage-velocity relationship is non-linear.

The second mode, the constant current mode is, nowadays, used as a resistance thermometer [23], since it has limited frequency response, its sensor performance varies with the flow velocity, and the overheat ratio

$$a = \frac{R_s}{R_p} \quad (1)$$

where R_s is resistance of the heated hot-wire sensor at its operating temperature and R_p is resistance of the hot-wire sensor at the temperature of the ambient air, depends on the flow velocity [24]. The constant current mode keeps current through the sensor constant as the air temperature changes. As the temperature of the air varies, the resistance of the sensor varies with temperature, and therefore the voltage as well. The voltage change as a function of the temperature is determined during a calibration process at known temperatures. The voltage-temperature relationship is linear.

3.3.3 Heat Transfer Laws for Sensors

Heat transfer from a hot-wire sensor is calculated by empirical correlations. Over

the years, several empirical correlations have been developed. The oldest correlation is that by King [23]. In the present work, the most commonly used correlation of Collis and Williams [23] is used:

$$Nu \left(\frac{T_m}{T_a} \right)^{-0.17} = A + B Re^n \quad (2)$$

where T_m is the mean film temperature defined as the arithmetic mean of the sensor temperature T_s and the ambient temperature T_a . A , B , and n are constants found by calibration. The exponent n was reported to be between 0.3 and 0.5 depending on the magnitude of Reynolds number by various researches[22], some researches reported $n=0.7$ [25].

In equation (2), Reynolds number is defined as:

$$Re = \frac{\rho u d}{\mu} \quad (3)$$

and Nusselt number as:

$$Nu = \frac{h_s d}{k_a} \quad (4)$$

All fluid properties are evaluated at the mean film temperature.

3.3.4 Temperature Compensation Method

During the transient experiments, the temperature of the air in the tank slowly

increases. Since the hot-wire is very sensitive not only to velocity changes but also to the temperature changes, temperature compensation must be used. A change in temperature of 1°C may cause an error of measurement of 1.5% [26], because in nonisothermal flows, the effect of temperature variation on a hot-wire anemometer output is comparable to that of velocity variation. Therefore, it is impossible to distinguish the velocity and temperature components of a single hot-wire anemometer output without additional information. These two components can be separated by using two fine wire probes operated in different modes. The expected temperature variation at the core entrance in this experiment is between 20°C and 70°C and the velocity is around 1.1 m/s. Therefore, a temperature compensation method appropriate for the temperature and velocity ranges of the experiment had to be found.

The temperature compensation methods may be divided into two major groups [27]:

1. Changing the sensor temperature to give the same voltage output at the same flow independent of temperature changes. As described by several authors [28,29,30,31], this may be accomplished automatically with various degrees of the circuit complexity. Analog circuits for the two probes can be designed so that simultaneous temperature and corrected velocity signals are made available.

2. Maintaining sensor temperature constant and correcting the resulting data for

measured changes in environment temperature. This method is described by [26,32,33,34,35] and is increasingly used in the recent years due to the availability of the computers. Some of the methods require considerable effort during the calibration process [26,33,34].

From a review of the alternatives, the method of Abdel-Rahman et al.[32] was selected as the most convenient for the present problem.

Abdel-Rahman et al. [32] employ the Collis and Williams equation:

$$Nu \left(\frac{T_m}{T_a} \right)^{-0.17} = A + B Re^n \quad (5)$$

where

$$Re = \frac{\rho u d}{\mu} \quad (6)$$

and

$$Nu = \frac{h_s d}{k_a} \quad (7)$$

Assuming that conduction losses at the end of the sensor are small compared to convection losses, and that radiation losses are negligible, the heat balance on the hot-wire may be written as:

$$\frac{E^2 \rho_r}{R_s A_s} = \pi d h_s (T_s - T_a) \quad (8)$$

Substituting this equation into the equation (7) and using the relationship for the sensor resistance:

$$R_s = \frac{\rho_r l}{A_s} \quad (9)$$

the Nusselt number related to the hot-wire sensor voltage is obtained:

$$Nu = \frac{E^2}{R_s \pi l k_a (T_s - T_a)} \quad (10)$$

The constants A , B , and n are determined by a calibration of the hot-wire for the velocity range. Other researches using the Collis and Williams equation evaluated the air properties at various temperatures, i.e. the air temperature, the hot sensor temperature, the film temperature, or their combinations, depending on the conditions of the air flow. Abdel-Rahman et al. [32] found that the best results at a low range of velocities (0.35-9 m/s) and a large range of temperatures (22-98°C) were obtained by establishing the air properties at the mean film temperature, using equations suggested by Morrison [36]:

$$\frac{k_a}{k_{ar}} = \left(\frac{T_m}{T_{ar}} \right)^{0.86} \quad (11)$$

$$\frac{v_a}{v_{ar}} = \left(\frac{T_m}{T_{ar}} \right)^{1.9} \quad (12)$$

Abdel-Rahman et al. measured air velocities by the hot-wire in the constant

temperature mode, while the temperature of the air was measured by the hot-wire in the constant current mode. The actual temperature compensation was done by computation in the data acquisition software. They reported their temperature compensation procedure to be accurate within $\pm 2\%$ for velocities above 1.5 m/s and to overcompensate by up to 5% for velocities down to 0.8 m/s.

3.3.5 Hot-wire Probe and Anemometers

The two-sensor hot-wire probe Dantec 55P86 shown in Figure 14 was used for the air velocity measurement. The probe was positioned perpendicular to the flow during both the measurement and the calibration. The temperature sensor is 2 mm upstream and 5 mm from the tip of the velocity sensor. This is rather long distance but due to uniform temperature of the air flow at the core entrance it does not cause any serious problems. The probe sensor was protected by a shield as shown in Figure 14 so that it was not damaged during insertion of the probe through the feedthrough at the velocity duct wall or by malfunction of traversing mechanism or data acquisition program. The shield is 3 mm from the tip of and 0.75 mm downstream from the velocity sensor so that it does not disturb the air flow measured by the velocity sensor.

Two constant temperature anemometers manufactured by TSI Incorporated, model series 1050, one with a bridge 1053B and the other with a bridge 1054A, were used for

the measurement. The velocity sensor was connected to a constant temperature bridge, model 1053B, and the temperature sensor was connected to a constant temperature bridge, model 1054A, which has a switch for use in a constant current mode, thus enabling temperature measurement. Both anemometers have two outputs, one on the bridge panel, the other on the power supply panel. The output on the bridge panel was connected to a data acquisition system, while the output on the power supply panel was connected to the oscilloscope, where the stability of the output signal and a level of noise in the output signal was observed. The noise in the signal of the temperature and velocity sensors was less than 0.3% and 0.2% of the signal value, respectively.

3.3.6 Hot-wire Probe Calibration

In the present work, the hot-wire was calibrated for velocity and temperature before and after the experiments as shown in Figures 15, 16, and 17. A significant shift in the velocity calibration of the velocity sensor was observed between the two calibrations. Based on the sensor resistance, its operating voltage at zero air velocity, and the room air temperature, it was concluded that significant random errors were incurred during the calibration before the experiments and that calibration curve was discarded. Since the hot-wire anemometer output voltage was converted to the air velocity in data acquisition system, it was possible to retrieve the voltages and to calculate the velocity using the calibration curve obtained after the experiments. The temperature calibration curves of the velocity and temperature sensors before and after the tests remained

essentially unchanged as it may be seen in Figures 16 and 17 respectively.

The anemometer output voltage recorded as a function of the velocity is shown in Figure 15. Both the velocity and the temperature sensors were calibrated for temperature in an oven, with the temperature measured by a thermocouple placed within 1 mm of the probe. Another shielded thermocouple was positioned within 3 mm to check for the effect of thermal radiation on the results. The velocity sensor resistance and the temperature sensor output voltage were recorded as a function of the changing temperature before and after tests as shown in Figures 16 and 17, respectively. The hot-wire's resistance change with temperature is needed in order to know the temperature of the sensor T_s . Details of the hot-wire temperature compensation method in the software are described in Appendix F.

As will be shown in Chapter 5, the mass flow rates measured by the hot-wire probe m_{HWA} were lower than the mass flow rates obtained from the heat balance on the air m_{HB} . The difference between m_{HWA} and m_{HB} is more significant in tests with the water-cooled tank. These tests were run as first, therefore the calibration of the hot-wire probe and the measurements were far apart in time and the probe could have been contaminated by the possible fumes released from gaskets and epoxy at the high temperatures. Therefore, the hot-wire probe should be calibrated more frequently (at least every day until the user gains experience) than was done in this work. A small contribution to the difference in m_{HWA} and m_{HB} in the tests may be that the velocity calculated during the

calibration from the difference between the static pressure in the calibration unit and the atmospheric pressure is the average velocity while there is developing velocity profile and therefore the velocity in the center region of the nozzle is higher than the average velocity. Thus the voltage of the hot-wire anemometer corresponds in fact to higher velocity than obtained from dynamic pressure calculation. Therefore the velocity in tests is somewhat higher than measured.

The uncertainty analysis of the calibration and of the measurement is described in Appendix E. The uncertainty analysis shows that the inclined manometer used for the hot-wire calibration for the velocity was not convenient for the low pressures of the low velocities. The uncertainty in the calibration of the velocities in the range of those measured in the experiment, i.e. about 1.1 ms/s, is about 8 to 15%. Therefore the calibration unit was able to provide accurate information about the calibrated velocity in very low range of 0.5 to 2 m/s should be obtained or designed. This significant calibration uncertainty was the major contributor to the uncertainty in m_{HWA} which was 10 to 17% as may be seen in Appendix E. Therefore, the mass flow rate was calculated from the heat balance as described in Section 3.4.

3.4 Mass Flow Rate Measurement

Mass flow rate of the air was determined from a heat balance on the air in the

simulated core:

$$m = \frac{Q}{c_p(T_h - T_r)} = \frac{P - Q_L}{c_p(T_h - T_r)} \quad (13)$$

where Q_L represents the heat loss rate from the core. This heat loss rate for the test in the open room was also determined analytically:

$$Q_L = P - Q = U_L A_L \left(\frac{T_h + T_r}{2} - T_r \right) \quad (14)$$

Then the heat loss through the core and insulation as ratio of the power transferred to the air may be obtained using equations (13) and (14):

$$q_{LR} = \frac{Q_L}{P} = \frac{\frac{U_L A_L}{2 c_p}}{1 + \frac{U_L A_L}{2 m c_p}} \quad (15)$$

Correlation for $U_L A_L$ for infinite hollow square pipe was found in reference [43]:

$$U_L A_L = k_{ins} L_c \frac{2\pi}{0.93 \ln \left(\frac{t_o}{t_i} \right) - 0.0502} \quad (16)$$

The correlation is valid under the following condition:

$$\frac{t_o}{t_i} > 1.4 \quad (17)$$

where t_o is outside width and t_i is inside width. In the present case the ratio t_o/t_i equals

to 4.08.

Figure 18 shows the solution over the expected range of air mass flow rates and thermal conductivities of the insulation. Heat loss by conduction to the riser box and stand was minimized by Garlock #900 gaskets. As it may be seen, the assumption of 5% heat loss from the core is, under these conditions, reasonable. The heat loss rate was also confirmed by measurement at 600 and 700 W in the open room when the outside temperature of the insulation was measured. As an approximation for the inside temperature of the insulation, the average air temperature in the core was used. The measurement showed the heat loss of about 4% at both powers. It should also be emphasized that the difference in m , if either 3% or 5% heat loss is assumed, is very small. The specific heat of the air c_p in the equation (13) was calculated from the curve fit at the average air temperature in the core and T_k and T_r were measured. As shown in the error analysis in the Appendix E, the uncertainty of the mass flow rate determined from this measurement is 1.6 to 2% which is substantially more accurate than the mass flow rate measured by the hot-wire anemometry.

3.5 Traversing Mechanism

The hot-wire probe is traversed across the core entrance unit in order to obtain a velocity profile. The traversing mechanism of the probe is shown in Figure 19. Its

movement is controlled by subroutines MOTOR and MOTORB of the data acquisition program SLOWPOKE (see Section 3.6.1). The subroutines send a signal to the motor through a parallel port of the computer to the circuit controller enclosed in the external power supply (power supply from an IBM-PC AT-286 computer). The traversing system transforms the rotational motion of the stepper motor into a linear motion of the probe through a 1/4"-20 threaded shaft. The four-phase stepper motor SLO-SYN, model MO61-DF-301, is used in full-step mode with 24 steps per revolution, i.e. 15° per step, with a non-cumulative accuracy of 5%. This makes for 0.0529 mm/step in linear motion with an accuracy of 0.00265 mm. The overall accuracy of the traversing mechanism is estimated to be ± 0.2 mm in both directions.

3.6 Data Acquisition System

A data acquisition system is used to collect all measured data and to control the traversing mechanism of the hot-wire probe. The system consists of an IBM compatible computer XT-8088, a DAS-8 board and three EXP-16 multiplexer expansion boards.

The DAS-8 board is a plug-in board for IBM-PC computers. It has a 12 bit analog-to-digital (A/D) converter, 8 analog single-ended input channels with a common ground, and four digital outputs. Its analog to digital conversion time is typically 25 μ s and up to 4,000 samples/sec are attainable when operated in BASIC. The calibration

curve of the A/D converter is presented in Figure 20. The analog input channels use a common ground and have a range of ± 5 volts with a resolution of 2.44 mV. The analog input channels are expanded by an expansion multiplexer board EXP-16.

The EXP-16 expansion multiplexer expands one single-ended input channel into 16 differential input channels. It is connected to the DAS-8 board through a standard 37 pin D connector and it is cascable up to 8 boards. Channels are selected by a 4 bit address through digital outputs of the DAS-8 board. The EXP-16 board features a high-grade instrumentation amplifier with a gain switch which configures the entire board for the gain selected. For thermocouple measurements, the EXP-16 board includes cold-junction sensing and compensation that provides a scaling of 24.4 mV/ $^{\circ}$ C enabling compensation in the software. The cold junction compensation has been calibrated against a mercury thermometer.

The sketch of channel connections to the DAS-8 board is provided in Figure 21. Three multiplexer boards with different gain settings are used. The gain of 50 is selected for the multiplexer measuring the heater sheath temperatures, the air temperatures, and the water temperatures. A gain of 1,000 is used for the other multiplexer boards, since all temperatures measured by these boards are relatively low, therefore the voltages are low too, and they may be more amplified. Hot-wire anemometer outputs are connected directly to the DAS-8 board, since their outputs are lower than 5 volts and they cannot be connected to multiplexer boards, because of the gain settings.

3.6.1 Data Acquisition Program SLOWPOKE

A data acquisition program called "SLOWPOKE" has been developed in QuickBASIC language to collect, evaluate, and store measured data for later analysis, and to control the stepper motor for traversing the hot-wire probe. The program is run from the QuickBASIC environment and utilizes a call subroutine "DAS8.BIN" provided by the manufacturer of the data acquisition board DAS-8. The functions of data acquisition board DAS-8 and of expansion multiplexer EXP-16 are controlled by sending various mode values (there are 19 available) to the subroutine DAS8. Modes 0 (initializes the board), 1 (locks the board to a specified channel), 3 (verifies if the board is locked to the right channel), 4 (performs analog to digital conversion), and 14 (locks the board to a specified multiplexer channel) are utilized in this computer code.

The program is based on a main routine and 13 subroutines. The program SLOWPOKE is provided on the enclosed disk as Appendix C. The program acquires 60 values of each variable and averages them. The averaged data is stored, while the instantaneous data is discarded. Data is collected in intervals of 5 minutes.

When the program is started the program loads type "K" thermocouple look-up data and creates output file headlines. Then the execution of the program proceeds to subroutine TEMPGET. This subroutine reads voltages of thermocouples and converts them into degrees of Celsius by call to subroutine TEMPINTRP. Cold junction compensation is obtained by call to subroutine CJCGET.

Subroutine HWAGET reads voltages measured by hot-wire anemometers and converts them into temperature and velocity. These anemometers are connected directly to the DAS-8 board; thus, no selection of multiplexer channels is needed. Voltages are converted into temperature and velocity by using the calibration equations and the temperature compensation method. The program positions the probe at each of five points used for measurement across the velocity duct. The number of positions was limited by a need to wait for 40 seconds after the probe was moved to a new position in order to make sure that the flow is stabilized. The data acquisition and the probe movement took other 45 seconds, thus it was not possible to measure more velocity points within 5 minute interval of temperature measurements. However, it is recommended to take 9 measurements in 20 second intervals in the future.

The hot-wire probe movement is controlled by a stepper motor, which is controlled by subroutines MOTOR and MOTORB. The subroutine MOTOR is used for the positioning of the probe while subroutine MOTORB returns the probe to original position. The stepper motor is controlled by sending byte information to parallel port when bit 1 controls the motor speed, bit 2 controls direction of the motor rotation, and bit 4 controls motor step size. The number of steps to move the probe the desired distance is achieved by flipping bit 1 the correct number of times.

Subroutine SCROUT then displays data on the screen and subroutine DATAOUT stores data in the output files for later analysis. The program sets a time interval of five

minutes between data collections.

3.7 Power Supply and Control

The electric heaters are powered by AC electric current of 120 V voltage from Carleton University's electric power supply. Tests were run over a range of powers from 100 W to 700 W. The power was kept constant during the tests and the higher powers were observed on a 10 A, 150 V wattmeter. For the powers of 100 W and 200 W, a 5 A, 150 V wattmeter was used to improve resolution at low powers. The power was controlled by a 10 A, 120 V Variac. During the tests, power fluctuations of up to $\pm 5\%$ occurred at random because of the other effects of loads on the university power supply.

3.8 Water Flowmeters

The steady state experiments were conducted by extracting the heat generated in the apparatus by water circulated through the air gap between the tank and the inside liner. The water enters the gap through the feedthrough at the bottom of the concrete wall and leaves through the feedthrough at the top of the concrete wall as shown in Figure 3.

Two Dwyer, model RMB-SSV flowmeters with an error of 3% of full scale are provided. Their metering valve is made of stainless steel and pipe connections are 1/4" pipe thread. The flowmeters are connected in parallel; thus, depending on the water flow rate used in the measurement, the appropriate flow meter is selected by the three-way valve, i.e. either 0-12 GPH or 10-100 GPH. The second flow range is used in most experiments. The flowmeters and the three-way valve are built in the chassis aluminum box.

3.9 Calibration of Instrumentation

The following equipment was used during calibration of the boards and of the other instrumentation.

1. A multimeter Fluke, model 8040A. The multimeter was used as a voltmeter during calibration of the data acquisition boards and hot-wire anemometers.
2. The oscilloscope Tektronix, model 2213. The oscilloscope was used to observe the level of noise in the measured signal during the calibration of the hot-wires as well as during the experiments.
3. A Stabil-therm oven manufactured by Blue M Electric Company. The oven

was used for temperature calibration of the hot-wires.

4. Inclined manometer. The inclined manometer was used for the velocity calibration of the hot-wire probe. The velocity sensor was calibrated in a jet flow discharging into the atmosphere. The static pressure in the calibration box was compared to the atmospheric pressure to find the dynamic pressure of the jet flow.

Chapter 4

Computer Programs

4.1 Introduction

Several computer programs have been developed and modified during the course of this work. They are used for computer predictions of the transient air cooling of the actual reactor after a loss-of-pool-water accident and of the air cooling in the experimental apparatus, and for data acquisition. Further, a supportive program for the stepper motor and a program for adjustment of reference settings of the hot-wire anemometer for temperature measurement have been developed.

Only programs predicting the transient behaviour of the reactor and of the apparatus are presented in the following sections. Data acquisition program was presented in Section 3.6.1.

4.2 Computer Program LOPWAT2

Krzaniak [13] developed a computer program, LOPWA, to predict SES core and air temperatures under steady state air-cooling thermo-syphoning conditions.

Krzaniak's computer program was modified by Rogers [11,14] to allow for transient heat storage effects in the core and in the concrete walls of the pool and the surrounding ground and to incorporate other improvements. This program, LOPWAT, predicts transient behaviour of the core after its uncovering.

LOPWAT has now been revised, as LOPWAT2, to improve the modelling of the flow and heat transfer conditions and to reduce computation time. Only a brief outline of the major modifications is given here. The details may be found in [15]. The program LOPWAT2 is provided on the enclosed disk as Appendix A.

The most important change is a use of solutions of the differential equations for heat balances on the air in the riser and downcomer regions to predict air and riser wall temperatures. Also, an additional differential equation was solved to allow for convection heat transfer from the downcomer air into the tank wall below the top of the core.

The second major change in the program includes integration of density over the core, riser, and downcomer lengths instead of using arithmetic average values. The local

densities are calculated using temperatures obtained from the integrated differential equations. This approach represents the buoyant force balance in a more accurate way.

Further changes include the following:

- a new subroutine calculating time averaged heat balance. This subroutine enables an overall heat balance to be made at any time to confirm the internal validity of the analysis.
- a better numerical representation of empirical factors in the mixed-flow heat transfer correlation used in the core region.
- a more accurate transition points between laminar and turbulent flow regimes in selecting heat transfer and friction correlation.
- a more user friendly input and output presentation.

The solutions of the differential equations were prepared for the iteration procedure into the form shown in Table 2. For this purpose, the original set of the equations was written in the matrix form as $AX=B$, where X represents the matrix of the key temperatures, A represents the coefficient matrix and B represents the matrix of the functional values. The equations had to be rearranged so that they satisfy Gauss-Seidel criteria for convergence, i.e. for each row of A the absolute magnitude of the diagonal element should be at least as large as the sum of the absolute magnitudes of the other elements in the row and, in at least one row, larger than the sum [38]. Then the equations had to be rearranged so that the right hand side equaled zero.

The iteration method of Kantaris & Howden [37] was incorporated into the

program to ensure that the basic differential equations are expressed in a well-posed manner. This method is capable of solving linear, polynomial or differential equations, it can handle poorly behaved functions with possible discontinuities and infinities, it does not require differentials to be found, it reaches solution to a predefined accuracy in very few steps, and the initial guesses may be any number. The method uses the following iteration algorithm where x_n represents the riser wall and any of the key air temperatures to be calculated:

$$x_{n+1} = x_n + S + 2 \left(\frac{p}{3} - r - \frac{1}{3} \right) \sinh^{-1}[F(x_n)] \quad (18)$$

where x_{n+1} is new x_n value and x_n is an initial guess or the x_n value from the previous iteration step. r is an integer number which increases by one if $\sinh^{-1} F(x_{n+1})$ is opposite in sign to that of the previous $\sinh^{-1} F(x_n)$ while maintaining p the same as before. p is an integer number which increases by one if $\sinh^{-1} F(x_{n+1})$ has the same sign as $\sinh^{-1} F(x_n)$, while r is kept at its previous value. The hyperbolic sine function serves as an attenuating factor to smooth out the iteration. Since FORTRAN-77 does not provide the $\sinh^{-1}$ function, it has to be expressed as

$$\sinh^{-1}[F(x_n)] = \ln[F(x_n) + \sqrt{(F(x_n))^2 + 1}] \quad (19)$$

$F(x)$ is the function value of any equation in Table 2 with the right hand side of the equation equalled zero, i.e. $F(x)=0$. During the iteration process the value of this

function is calculated and it approaches zero as the solution approaches the root of the equation. For instance, the fourth equation in Table 2 was originally written as:

$$T_h = T_r + Z \quad (20)$$

After rearranging it is:

$$F(x_h) = T_h - T_r - Z = 0 \quad (21)$$

Table 2 also presents the sign value S for the set of equations. Its value is an integer number of +1 or -1 and each equation of the set has its own sign value when the whole set of equations converges. The sign of S had to be found by trial and error method for the equation set before incorporating the method into the LOPWAT2 program (any change in the equation set would require finding new set of signs). In the set of the five equations in Table 2, it created 32 sign combinations. This was solved by the supportive computer program written for this purpose.

After all these modifications were incorporated into the program LOPWAT2, the program predicts the maximum heater sheath temperature and mass flow rate for various core uncovering times as presented in Figures 22 and 23, respectively. It should be noted that these results do not include the heat sink into the tank and concrete walls above the top of the riser. More detailed description of the results may be found in [15].

4.3 Computer Program LOPWATEX

The computer program LOPWAT2 has been adapted to conditions of the experimental model of the SES-10 reactor. The modification is called LOPWATEX and its structure is the same as for the SES-10 conditions. The only physical models that were modified are:

- the model for the reactor core with its chopped-cosine axial power distribution was replaced by a model for the uniformly heated electric heaters. The thermal characteristics of the ceramic-core electric heaters are accounted for in the adapted model.
- the analytical model for thermal response of the ground was omitted. This is irrelevant since the simulated core conditions closely approach steady-state conditions, at all experimental powers, with heater sheath temperatures in the range predicted for the maximum fuel sheath temperatures, before there are significant heat loss rates from the outer surface of the concrete cylinder.

The program LOPWATEX allows calculation of the water-cooled steady-state experiment or the transient experiment. This is user selectable when the program is started. The selection of the water-cooled steady-state experiment keeps the tank wall temperature constant throughout the calculation. Flowcharts of the main calling routine and major subroutines are given in Appendix B. The copy of program LOPWATEX is provided on the enclosed disk as part of Appendix B. The flowcharts are used to describe the program and subroutines in the sequence in which they are called in the main calling routine. The air properties throughout the program are established by calls to subroutines RHOGET, MUGET, KGET, and PRGET.

4.3.1 Input Data

Input data must be defined for the main routine as well as for the subroutine RINTRF, FUELTRANS, FBAL, and CONTRANS. Input values of time step for the main routine in Figure B.1, $DT = 5$ minutes, and for the subroutine FUELTRANS, $DELT = 1$ minute, are used for the calculation of the first 120 minutes of the experiment to eliminate the oscillation of the solution during the initial fast changes of the flow and heat transfer development. Then they are changed to $DT = 10$ minutes and $DELT = 2$ minutes to speed up the calculation.

4.3.2 RINTRF

This subroutine calculates the radiation heat transfer interchange factors from the riser wall to the tank wall and from the tank wall to the inner surface of the concrete. The emissivities of the stainless steel and the concrete in this calculation are 0.95 and 0.90 respectively.

4.3.3 FUELTRANS

This subroutine, shown in Figure B.2, calculates the transient characteristics of the

heaters. It determines the fraction of the input power transferred to the air at any time based on the air mass flow rate of the previous time step, and corresponding core heat transfer coefficient.

The model used to predict sheath temperature is based on a simplified model for a gas-cooled reactor core thermal analysis developed by Rogers in 1959 [14]. Appendix D describes how T_{sh} , α_{hi} , and U_{fo} were derived. The following equation for the heater sheath temperature was obtained:

$$T_{sh} = T_r + \frac{Q}{A_{hi}} \left(\frac{1}{h} + \frac{P_{hi}}{G * A_c * c_p} \frac{L_a}{2} \right) \quad (22)$$

Under transient conditions, the fraction of power transferred into the air is defined as:

$$\alpha_{hi} = \frac{Q}{P} = \frac{1 + \frac{M_f * c_{pf}}{P * \delta t} (T_{fwo} - T_r)}{1 + \frac{c_{pf}}{A_{hi} * \delta t} \left(1 + \frac{P_{hi} * M_f}{G * A_c * c_p} \frac{L_a}{2} \right)} \quad (23)$$

The overall heat transfer coefficient from the heater into the coolant is:

$$U_{fo} = \frac{1}{0.3465 \frac{R_o}{k_f} + \frac{r_{sh}}{k_{sh}} + \frac{1}{h}} \quad (24)$$

The thermal conductivity and specific heat of MgO were found in [42] for magnesite brick. This is reasonable since MgO forms 98.2% of the magnesite brick [43]. A curve fit yielded the following equations valid over the range of 0-1426°C and 0-1300°C, respectively:

$$k_f = 5.66224 - 0.306684 * 10^{-2} * T_{fvo} + 0.9882673 * 10^{-6} * T_{fvo}^2 \quad (25)$$

where T_{fvo} is in °C, and

$$C_{pf} = 763.1038 + 0.4794304 * T_{fvo} - 0.0001242285 * T_{fvo}^2 \quad (26)$$

where T_{fvo} is in Kelvins.

The thermal conductivity of the Incoloy sheath as function of the sheath temperature was found in reference [44]. A curve fit yielded the following equation valid over the range 0-800°C:

$$k_{sh} = 14.06495 + 0.761782 * 10^{-2} * T_{sh} - 0.453849 * 10^{-5} * T_{sh}^2 + 0.1017955 * 10^{-8} * T_{sh}^3 \quad (27)$$

where T_{sh} is in °C.

4.3.4 FBAL

The program establishes the air mass flow rate (m) at any time following the logic of the main calling routine in Figure B.1 using the subroutines FBAL and CHECKROOT. Subroutine FBAL, shown in Figure B.3, is used to calculate residuals of the force-balance equation for high and low guesses of m , then CHECKROOT is used to ensure that the air mass flow rate interval contains a root of the force-balance equation. If not, then the initial guess of m is increased by 1×10^{-5} kg/s and new high and low air flow rates are used. This procedure is repeated until the interval contains the root. Then bisection method on FBAL is used to find m as described in Section 3.6 of reference [14].

When in subroutine FBAL shown in Figure B.3, the riser wall and the air temperatures are established by the call to subroutine TEMPGET. Then after return from TEMPGET, the tank wall temperature is calculated if transient test prediction is required. If the new tank wall temperature is not within 1°C of the previous value, then the calculation is returned to subroutine TEMPGET. If the water-cooled steady-state test is predicted, then the tank wall temperature is kept constant.

The pressure losses in the core and velocity duct were determined by using correlations described below.

The Shah's correlation [39] for the Fanning friction factor in developing laminar

flow was used because the flow through the core and velocity duct is laminar and developing at all powers at any time:

$$K_c = \frac{\frac{K_{\infty}}{4 * x_*} + fRe - 3.44 * x_*^{-0.5}}{3.44 * x_*^{-0.5} + \frac{(1 + C * x_*^{-2})}{Re}} \quad (28)$$

Shah gives values of the coefficients K_{∞} , fRe and C for rectangular, equilateral triangular, and concentric annular geometries. Since the square and equilateral triangular geometries yield about the same results for the core geometry as confirmed by Rogers [45], the coefficients for the square duct are used both for the core and velocity duct, i.e.:

$$K_{\infty} = 1.43$$

$$fRe = 14.227$$

$$C = 0.00029$$

and

$$x_* = \frac{L}{D_h * Re} \quad (29)$$

where: L is the length of the core or duct

D_h is hydraulic diameter of the core or duct

Re is Reynolds number in the core or duct

The entrance and exit pressure losses as functions of the Reynolds number for the core geometry were established by Rogers [45] using information from Idelchik [40]. For this analysis, the end-plates were considered as thick orifice plates, the core entrance was

treated as a sudden contraction region, and the core exit as a sudden expansion region. Since the pressure loss coefficients were function of the Reynolds number, a least square curve fit method yielded the following equations:

$$K_{ent} = 7.289118 - 0.0242156 * Re + 7.51382 * 10^{-5} * Re^2 - 6.392084 * 10^{-8} * Re^3 \quad (30)$$

$$K_{exit} = 5.317635 - 0.0167985 * Re + 5.034408 * 10^{-5} * Re^2 - 4.048577 * 10^{-8} * Re^3 \quad (31)$$

The entrance and exit pressure losses for the velocity duct are found from Shah [41] on the basis of its area ratios for the entrance (velocity duct to the tank area) and exit (velocity duct to the core area):

$$K_{entd} = 0.55$$

$$K_{exid} = 0.7$$

The entrance and exit loss coefficients are however valid for a fully developed flow. Since the flow in the velocity duct and the core is not fully developed and large uncertainties in correlations exist at low Reynolds numbers ($Re = 200$ - 400 in the core and $Re = 1500$ - 3300 in the velocity duct), tests in the open room were used to establish a correction factor for the calculated pressure losses, using the measured air mass flow rates from the heat balance on the air as described in Section 5.3.1.

4.3.5 TEMPGET

This subroutine, shown in Figure B.4, establishes the riser wall and the key air temperatures. The heat transfer coefficients on the inside and outside riser wall and on the tank wall are found by call to subroutine HTCGET. The subroutine HTCGET also calculates the radiation heat transfer coefficient from the riser wall onto the tank wall.

Based on preliminary experimental results (see Section 5.4.2) the analytical model was modified to account for heat loss into the concrete wall above the riser top. This introduces a sixth equation into the set and since the same iteration method as in LOPWAT2 is used, the sign combination when the solution of equations converges has to be found from 64 possible sign combinations. Again, this was solved numerically using the supportive program and Table 3 gives the equation and sign combination which ensures convergence of the solution.

4.3.6 POTEMP

The subroutine POTEMP, as shown in Figure B.5, calculates the maximum heater sheath temperature as well as temperatures along the heater length. The heater is divided to 20 increments and the average temperature and heat transfer coefficient over the length of the heater from the beginning of the active length up to the given increment is

calculated using the equation for heater sheath temperature used in subroutine FUELTRANS. The solution allows for laminar (call to subroutine LAMHT) and turbulent (call to subroutine TURBHT) forced convection, and mixed natural and forced convection by call to subroutine COREHTC. The bisection iteration method [38] as used in LOPWA and LOPWAT [11,13,14] was introduced into this subroutine to ensure convergence. Then the average heat transfer coefficient over each increment is calculated using the equation:

$$h_{av,j} = j * h_{(j+1)} - (j-1) * h_{(j)} \quad (32)$$

where j counts increments of the heater sheath.

The average heater sheath temperature over that increment is:

$$T_{av} = T_{ca} + \frac{\delta Q}{(h_{av,j} * P_{ht} * \delta L)} \quad (33)$$

4.3.7 CONTRANS

Subroutine CONTRANS is shown in Figure B.6. The thermal behaviour of the concrete wall is determined using a transient finite difference model with a radial one-dimensional co-ordinate system. Since the concrete cylinder sits in the room air in the experiment, the outer surface temperature is set constant at room temperature. Over the

duration of a test run, the outer wall of the concrete cylinder remains essentially at room temperature.

4.3.8 LOPWATEX Modification for Tests in Open Room

The computer program LOPWATEX is simplified considerably for the tests in the open room. The subroutines of the program LOPWATEX where the modifications reflecting the conditions of the test in the open room were made are described below:

MAIN ROUTINE - The tank wall temperature is replaced by temperature of the surroundings. This temperature and the core entrance air temperature are kept constant throughout the calculation and the surroundings emissivity is set to 1 to simulate a black body.

TEMPGET - The set of equations is found the same way as described in [15]. The three equations and a simple iteration method may be applied to solve them, i.e. a return command if the accuracy on the temperatures is not satisfied. Therefore no sign value combination had to be found. The equations are presented below:

$$T_h = T_r + Z \quad (34)$$

$$T_s = T_w + \frac{(T_h - T_w)}{e^{(\gamma_s \cdot L_s)}} \quad (35)$$

$$T_w = (1 - \beta_i) T_w + \frac{\beta_i}{2} (T_h + T_c) \quad (36)$$

FBAL - The air density is integrated across the core and riser lengths only, the outside density (the downcomer density in the full model) is kept constant based on the ambient temperature.

Chapter 5

Experimental Results and Discussion

5.1 Introduction

The experiments were run in three stages. The first stage was run under steady state conditions with the core-riser module in the open room, the second stage was run under steady state conditions with the core-riser module in the water cooled tank, and the third stage was run under transient conditions with the core-riser module in the uncooled tank. The power range used in the tests was 100 W to 700 W and the power to the heaters was kept constant during each of the experiments. The power showed fluctuations of $\pm 5\%$ because of university load variations and its effect may be seen in plots for the transient experiments.

5.2 Preliminary Tests

Preliminary tests were run for all three stages to enable refinement of the apparatus for the final measurements. After these tests, more rigid heater connections, thermocouple shields, and the velocity duct for the hot-wire measurement have been incorporated. It was found that in the final tests of Stage 1 and 2 the air temperature T_{k2} was about 20 to 40°C and 10 to 20°C, respectively, higher than during the preliminary tests as shown in Table 4 and 5 respectively. Table 6 shows comparison of air in the final and preliminary tests of Stage 3. Those were essentially the same. The air temperature T_e was about the same in each test, more significant differences, about 10°C, were found at powers of 100, 200, and 400 W of Stage 1. The temperature T_e of final tests was lower than that of the preliminary tests at powers of 500 and 700 W of Stage 1 (about 1 to 2°C) and at power of 400 W of Stage 2 (about 3 to 10°C). The maximum measured heater sheath temperatures in the final tests were about 14°C to 64°C higher than during the preliminary tests as shown in Table 7, 8, and 9 for Stages 1, 2, and 3 respectively.

The higher heater sheath temperatures for the same input power during the final experiments compared to the preliminary tests are attributed mainly to the velocity duct installed below the entrance of the core in the final experiments to accommodate the hot-wire anemometer measurements. The velocity duct increased considerably the pressure drop characteristics of the flow path through the core, decreasing the air flow rate for a

given power input, which decreased the heat transfer coefficients and thus increased the heater sheath temperatures. An additional increase in heater temperatures may have resulted from better connections of electric cables to the heaters, reducing the electrical resistance of the connections, so that more of the total heat dissipation for a given power setting was transferred from the connections to the heaters themselves.

The higher measured air temperatures for the same input power in the final tests, compared to the preliminary tests, can be attributed to the introduction of the velocity duct which decreased the air mass flow rate thus increasing the air temperatures and to the provision of thermocouple shields on the core and riser outlet thermocouples which protected the thermocouple beads against radiation heat loss to the riser wall and heat gain from the core. In the Stage 1, another reason for higher temperatures was about 5°C higher ambient temperature in the final tests and thus core entrance temperature T_c . The good agreement of air temperatures in the preliminary and final transient tests (Stage 3) may be caused by different conditions of the transient tests. The heater sheath temperatures are about 100°C higher than in the other tests and since the thermocouples were not shielded in the preliminary tests, the effect of radiation heat transfer was much more significant than in the other tests.

All measured data for steady-state conditions presented in figures were averaged over 10 measurements (50 minutes) and all standard deviations presented for temperatures are on Celsius scale. In the legends of the presented figures, EXP stands for the

experimental results and PRED stands for the computer predictions.

5.3 Results for the First Stage: Core-Riser Module in Room

Tests with the core-riser module in the open room were run over the range of powers from 100 to 700 W. The purpose of this part of the experiments was to verify and refine the thermohydraulic core of the computer code by isolating the core-riser module from the other components of the apparatus. The heated downcomer air was replaced by the cold room air, which increased the net buoyant force, and the surroundings of the test section were treated as a black body for radiation heat losses from the riser box.

5.3.1 Air Mass Flow Rate

Figure 24A shows a comparison of the predicted mass flow rate with the mass flow rate calculated from the heat balance on the air. The discrepancy in the results is caused by the difference between the actual and theoretical pressure loss coefficients in the core and velocity duct. The theoretical pressure loss coefficients were determined as described in Section 4.3.4. Pressure loss coefficients are difficult to predict accurately, particularly at low Reynolds numbers ($Re = 200-400$ in the core and $1500-3300$ in the

velocity duct) and experimental measurements must be used. At this stage, the correction factor K_{cc} specific only to this work was applied to the total calculated pressure loss in the core and velocity duct. For each guessed K_{cc} factor, the standard deviation of the predicted air mass flow rate from those determined from the heat balance on the air was calculated and the K_{cc} factor with the minimum standard deviation was used. This K_{cc} correction factor was incorporated into the LOPWATEX computer program versions predicting tests of Stage 2 and 3. A correction factor $K_{cc} = 0.517$ with a standard deviation of 2.84% was found and the predicted air mass flow rates using this correction factor are shown in Figure 24B. Figure 24B also shows the air mass flow rate determined from the hot-wire anemometer measurement m_{HWA} and its uncertainty bars. The agreement between m_{HWA} and the mass flow rate determined from the heat balance m_{HB} is better at low powers than at high powers; at power of 100 W the difference is about 17% while at power of 700 W it is 28%. Because of the large calibration uncertainty of the hot-wire probe, as shown in Appendix E, m_{HWA} data is not considered reliable and therefore the results are based on m_{HB} . The uncertainty bars of m_{HB} are not shown because those were within the size of the m_{HB} symbol.

5.3.2 Air Temperatures

As shown in Figure 25A, once the correction factor on the pressure loss in the core and velocity duct is applied, the predicted and measured temperatures at core outlet

T_h show very good agreement with standard deviation of 2.6% as would be expected since the air mass flow rate has been found by a heat balance. The agreement of predicted and experimental T_e is also quite good. The slight overprediction of T_e was thought to be caused by the assumed riser emissivity $\epsilon = 0.79$ in the LOPWATEX program. Therefore the riser emissivity ϵ was set to 0.95 and this change in ϵ decreased T_e by 2 to 4 °C which shows that the results are not very sensitive to change in ϵ . As Figure 25B shows, T_e with the riser emissivity set to 0.95 is overpredicted by 4 to 20°C with a standard deviation of 6.7%. The lower measured T_e than that predicted may result from the mixing of the hot air with the cold room air at their free boundary at the riser top where the thermocouple is located. The measured air temperatures T_h and T_e confirm the predicted significant effect of the heat transfer from the rising air to the riser wall on the riser outlet temperature. The riser wall is cooled mainly by radiation to the surroundings as well as by convection to the air in the open room.

5.3.3 Riser Wall Temperature

Riser wall outer temperatures were measured only in the tests in the open room using data acquisition board terminals needed for tank wall temperature readings in the other experiments. These terminals may be used for riser wall measurements in the future since the uniformity of the flow was confirmed as explained later. The riser wall temperatures were measured at the riser bottom, center, and top, and as Figure 26A

shows, the riser wall temperature decreases with height of the riser as would be expected. The average measured riser wall temperature is about 6 to 54°C higher than the predicted one (when $\epsilon = 0.95$ is used) as the power increases. One reason for the higher measured than predicted riser wall temperatures near the bottom of the riser is the heat flow from the core box to the riser box. The effect of ϵ on the riser wall temperature prediction is shown in Figure 26B where $\epsilon = 0.79$ was used. As it may be seen, the effect of changed ϵ is not very significant and is about the same as in the case of T_e (see Section 5.3.2). By decreasing ϵ from 0.95 to 0.79, the riser wall temperature increases by 2 to 5°C as power increases from 100 to 700 W.

5.3.4 Heater Sheath Temperatures

Figures 27 to 33 show comparisons of predicted and measured temperatures along the heater at input powers of 100 to 700 W respectively. As it may be seen, the predicted and measured values in the centre region of the heater, i.e. from 142 to 258 mm downstream from the core entrance, are in a reasonable agreement. The predicted heater sheath temperature near the upstream end of the heater, i.e. 84 mm downstream from the core entrance, does not follow the linear trend of the other predicted temperatures because of the effect of developing boundary layer which is accounted for by the laminar heat transfer coefficient correlation used. The measured heater sheath temperatures at that location are about 30 to 140°C higher than the predicted ones (as the power increases

from 100 to 700 W) which is caused by the effect of conduction heat transfer from the hotter part of the heater and radiation from the hotter regions of the core, which is not considered in LOPWATEX, as well as by uncertainty in the heat transfer coefficient prediction in the entrance region. The heater sheath temperatures near the downstream end of the heater are overpredicted (for instance, the maximum measured heater sheath temperature, i.e. at 258 mm downstream from the core entrance, is overpredicted by about 20°C to 180°C as the power increases), and the measured temperatures do not follow the predicted linear trend. This is caused mainly by radiation heat transfer to the cooler exit endplate and riser box wall and partially by axial heat conduction into the downstream unheated parts of the heater element. The same effect was observed by Schenk [12]. The radiation losses should not be as severe in the SES-10 core, where the power distribution is of a cosine type. Therefore, the maximum fuel sheath temperature would occur further away from the core exit than in the experimental apparatus where the power distribution is uniform. Thus, the fuel sheath temperatures predicted by the computer program LOPWAT2 should be closer to reality than the predictions of heater sheath temperature by LOPWATEX.

5.4 Results for the Second Stage: Core-Riser Module in the Water Cooled Tank

In this test, the tank wall was cooled by water flowing through the air gap. Again,

the tests were run at powers from 100 to 700 W.

5.4.1 Air Mass Flow Rate

Figure 34 shows good agreement of air mass flow rates predicted by the LOPWATEX with those calculated from heat balance on the air. The standard deviation of the predicted from the measured results is 4.4%. The mass flow rates at low powers of 100 and 200 W are slightly underpredicted while the mass flow rates at the other powers are slightly overpredicted. Figure 34 also shows that the air mass flow rate measured by the hot-wire anemometer m_{HWA} is 30% and 43% lower than the mass flow rate determined from heat balance m_{HB} at power of 100 and 700 W respectively. Again, because of large calibration uncertainty as shown in Appendix E, m_{HWA} data is not considered reliable and therefore the results are based on m_{HB} . The uncertainty bars are shown only for m_{HWA} because the uncertainty bars for m_{HB} were within the size of the m_{HB} symbol.

5.4.2 Air Temperatures

Figure 35A gives a comparison of experimental air temperatures with predicted values using the original version of LOPWATEX. The temperature T_b is overpredicted

by 36 to 112°C and T_c by 20 to 80°C. It was evident from the tank wall temperatures that the tank wall above the top of the riser was acting as an effective heat sink, as will be shown later. That is, it became evident that the hot plume emerging from the riser rises to the top of the tank before reversing to flow down the downcomer. The original version of LOPWATEX, based on LOPWAT2, conservatively assumed that the plume reverses as soon as it leaves the riser. In the light of the experimental results, LOPWATEX was modified to allow for the plume rise to the top of the tank. Predicted air temperatures using the revised analytical model are compared to the measured values in Figure 35B; the excellent agreement between predicted and experimental values is evident. The experimental temperatures T_n are overpredicted at powers of 100 and 200 W by only 4 and 3°C respectively and underpredicted at higher powers (for example by 20°C at power of 700 W). The standard deviation of the predicted data from those measured is 3.9%. T_c is overpredicted by about 3 to 12°C as power increases from 100 to 700 W, corresponding to a standard deviation of 4.5%. As in the case of the test in the open room, the measurements show the significant effect of the heat transfer from the riser air into the riser wall. In this case, the riser wall is cooled by radiation to the tank wall and by convection into the downcomer air.

5.4.3 Heater Sheath Temperatures

The heater sheath temperatures are presented in Figures 36 to 42 for powers of

100 to 700 W respectively. The trends of the predicted and measured heater sheath temperatures are similar to those of the test section in the open room. Again, the predicted and measured values in the centre region of the heater, i.e. from 142 to 258 mm downstream from the core entrance, are in a reasonable agreement. The measured heater sheath temperatures near the upstream end of the heater are 32 to 176°C higher than the predicted ones as the power increases from 100 to 700 W respectively. This is caused by the effect of conduction heat transfer from the hotter part of the heater and radiation from the hotter regions of the core, which is not considered in LOPWATEX, as well as by the uncertainty in the heat transfer coefficient prediction in the entrance region. The heater sheath temperatures near the downstream end of the heater are overpredicted (for instance, the maximum measured heater sheath temperature, i.e. at 258 mm downstream from the core entrance, is overpredicted by about 21°C to 126°C as the power increases), and the measured temperatures do not follow the predicted linear trend. This is caused by radiation heat transfer to the cooler exit endplate and riser box wall, and partially by axial heat conduction into the downstream unheated parts of the heater element. The radiation heat losses should not be as severe in the SES-10 core for the reasons explained in Section 5.3.4.

5.4.4 Tank Wall Temperatures

The tank wall temperatures were measured at six axial positions at each of two

circumferential positions 180° apart. The measured temperatures 180° apart at the same axial position at a given power, presented in Table 10, show very good agreement. Since the convection heat transfer on the tank wall dominates over the radiation heat transfer as showed by the analytical model, it confirms that the air flow distribution was circumferentially uniform. These temperatures also show that the tank wall was very well cooled by circulating water. Nevertheless, the tank wall temperatures increased somewhat from the bottom to the top of the tank, about 2°C at 100 W and about 6.5°C to 7.5°C at 700 W. This temperature increase shows the significance of the heat transfer from the air to the tank wall above the riser top.

5.4.5 Cover and Concrete Bottom Temperatures

The cover and cover insulation temperatures were also measured, as given in Table 11. They show that the tank cover was very well insulated with the estimated heat loss through the cover being, for example, about 1.7% of the input power at 700 W. Due to the excellent tank wall cooling, there is actually a small heat gain through the concrete bottom since the core inlet temperature is reduced below the room temperature in all cases. This is reflected in the temperature gradients shown in Table 12.

5.5 Results for the Third Stage: Long-Term Transient Test

Long-term transient tests were run at powers of 200, 400, and 600 W.

5.5.1 Air Mass Flow Rate

Figure 43 gives air mass flow rates at 400 minutes after the start of the tests. A time of 400 minutes has been chosen for comparison because at that time the air and heater sheath temperatures had already reached quasi-steady state and the air velocity still was in the range for which the hot-wire anemometer was calibrated. The air mass flow rates calculated from the heat balance show good agreement with the predicted values with standard deviation of 3.4%. Figure 43 also shows that the air mass flow rate measured by the hot-wire anemometer m_{HWA} is lower than the mass flow rate measured by the heat balance on the air m_{HB} . Better agreement occurs at power of 600 W where m_{HWA} is about 10% lower than m_{HB} , than at power of 200 W where m_{HWA} is about 17% lower than m_{HB} . The uncertainty bars obtained by the uncertainty analysis in Appendix E are shown only for m_{HWA} because the uncertainty bars for m_{HB} were within the size of the m_{HB} symbol.

5.5.2 Air Temperatures

Figure 44A shows predicted and measured air temperatures at powers of 200, 400, and 600 W at time of 400 minutes after start of the test. The agreement of temperatures at the power of 200 W is very good. T_r , T_b , and T_e are predicted about 19, 11, and 6°C higher, respectively, than the measured ones. The temperatures at powers of 400 and 600 W are more overpredicted; for instance at the power of 600 W, the predicted T_r , T_b , and T_e are 45, 55, and 38°C higher than the measured values. The overprediction of T_r , T_b , and T_e results from the significant cooling of the air caused by the cover and concrete bottom in the actual experiment as shown below. Figure 44B shows the air temperatures at time of 24 hours after start of the test. As it may be seen, the temperatures follow similar trends as those in Figure 44A.

The better prediction of the air mass flow rates (standard deviation of 3.4%) than of the air temperatures (standard deviation of T_b is 13.7% and of T_e is 8.1%) results from a good agreement between measured and predicted values of $\Delta T = T_b - T_r$, while the predicted T_r is higher than the measured T_r . Again, the results show the strong effect of the heat transfer from the riser air to the riser wall on T_e .

The air temperatures as function of time are plotted in Figures 45, 46, and 47 for powers of 200, 400, and 600 W respectively. Figure 45 shows initial faster rises of the predicted air temperatures at a power of 200 W than those for the actual case. This is

caused by neglect of the heat storage in the inactive lengths of the heaters as well as in the core box in the analytical model. This effect should not be so significant in the computer prediction for the actual SES-10 reactor because the ratio of the mass of these parts to the mass of fuel in the reactor is much smaller than in the experimental apparatus. The same trend may be seen in Figures 46 and 47 for powers of 400 and 600 W respectively. The fluctuations in the plots of measured temperatures result from disturbances in the power supply to the heaters because of variations in laboratory loads during the long-term tests. Plots of other parameters as function of time show similar behaviour as will be shown below.

5.5.3 Heater Sheath Temperatures

The time development of the maximum measured heater sheath temperature at position of 258 mm from the upstream end of the heater is shown in Figure 48 for powers of 200, 400, and 600 W. The rise of the predicted sheath temperature is faster than in the actual case because of the neglected heat storage in the parts of the apparatus as described above. The heater sheath temperatures along the heater at powers of 200, 400, and 600 W in Figures 49, 50, and 51 respectively show good agreement of temperature in the center region of the heater, i.e. 142 to 258 mm downstream from the core entrance. The temperature difference of the predicted and measured temperature near the core inlet increases from 56 to 217°C as the power increases from 200 to 600 W. However, the

maximum measured sheath temperature (i.e. 258 mm from the upstream end of the heater) and the predicted one for that location show that the temperature difference is 22, 51, and 48°C at powers of 200, 400, and 600 W respectively. It may be seen from the Figures 49 to 51 that the curve of the measured temperatures shifts up along the predicted temperature curve. The heater sheath temperatures are comparable to the peak temperatures of the actual reactor fuel element sheath¹. The reasons for the differences of the predicted and measured heater sheath temperatures are the same as in the Section 5.3.4.

5.5.4 Tank Wall Temperatures

The tank wall temperatures as functions of time at powers of 200, 400, and 600 W are shown in Figures 52, 53, and 54 respectively. The Figure also shows the average of the two measured temperatures at two circumferential positions 180° apart at each of the six elevations. The program predicts only the average tank wall temperature since variation with height of the tank wall temperature and the concrete temperature is ignored. The predicted average temperatures are underpredicted by about 4, 2, and 9°C at powers

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The predicted maximum heater sheath temperature after 24 hr test in the apparatus is 508°C and 703°C for powers of 400 and 600 W respectively while the predicted maximum fuel sheath temperatures for 10 hr and 20 hr core uncover time are 682°C and 582°C respectively, thus the heater and fuel sheath temperatures are in similar range. Taking into account that the LOPWAT2 code does not include the heat loss into the tank wall above the top of the riser, the peak temperatures of the fuel sheath are overpredicted.

of 200, 400, and 600 W. The predictions would be actually more underpredicted than that when the higher predicted T_r was taken into account, i.e. the predicted temperatures are actually higher than in the test because of the predicted T_r being higher than the experimental T_r . The measured temperatures show that there is a significant temperature gradient from the top to the bottom of the tank wall, about 30, 50, and 70°C after 24 hr of the experiment at powers of 200, 400, and 600 W respectively. The high tank wall temperatures near the top of the tank (134°C at power of 600 W) and corresponding concrete wall temperatures as shown in Section 5.5.5 indicate that the concrete wall above the riser top acts as a significant heat sink.

The measured temperatures show initially slow response to increasing air temperature in the tank, later they increase faster and then again, when the temperatures approach the quasi-steady state, the increase slows down, while the predicted average tank wall temperature increases monotonically. This behaviour results from ignoring the heat capacity of the tank wall in the computer code.

The tank wall temperatures at six axial positions at each of two circumferential positions are shown in Table 13. As in the case of the water-cooled tank, temperatures at each circumferential position at the same elevation are in very good agreement and because the convection heat transfer on the tank wall dominates over the radiation heat transfer, it confirms the uniform distribution of the air flow in the downcomer in this case.

5.5.5 Concrete Wall Temperatures

Figures 55, 56, and 57 show the concrete wall temperatures as function of the time at powers of 200, 400, and 600 W respectively. Again, the measured values show slower response than the predicted temperatures at all powers. They also show that there is a significant temperature gradient from the top to the bottom of the concrete wall of about 11, 18, and 28°C after 24 hr of the experiment at powers of 200, 400, and 600 W respectively. The relatively high temperatures near the top of the concrete wall (52°C at power of 600 W) indicate that the concrete wall above the riser top acts as a significant heat sink. The predicted average concrete wall temperature at the same node where the inner thermocouple was positioned (i.e. 34 mm from the inside liner) is very close to the temperature measured at the top location in the concrete (within 2°C) and about 4 to 14°C higher than the average measured temperature.

The temperature profiles across the concrete wall at powers of 200, 400, and 600 W given in Figures 58, 59, and 60 respectively show that the predicted average concrete wall temperature was close to the measured temperature at the top of the concrete at all nodes across the concrete wall. However, taking into account the higher predicted T_r than measured, the average concrete wall temperature is actually overpredicted. The measured temperatures show logarithmic temperature distribution through the concrete cylinder, but not as significant as predicted and observable only at the top elevation. The break of the measured temperature distribution at the center of the middle elevation is caused by the

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MICROCOPY RESOLUTION TEST CHART
NATIONAL BUREAU OF STANDARDS
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(ANSI and ISO TEST CHART No. 2)

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MICROCOPY RESOLUTION TEST CHART
NATIONAL BUREAU OF STANDARDS
STANDARD REFERENCE MATERIAL 1010a
ANSI and ISO TEST CHART No. 21

inner node thermocouple accuracy and due to the fine temperature scale, it is magnified.

The outside concrete wall temperature was estimated by extending the curves of the measured temperatures up to the concrete outside surface. It was found that the average outside concrete temperature increase above the room temperature was not significant; about 3 to 10°C as the power increased from 200 to 600 W.

5.5.6 Cover and Concrete Bottom Temperatures

From the cover temperatures in Table 14, a heat loss of about 5% of the input power through the cover is predicted. From the concrete bottom temperatures given in Table 15, a 10% heat loss through the concrete bottom is predicted. These losses are considerably higher than in the tests with the water cooled tank and their effect on the behaviour of the system has been shown.

Chapter 6

Conclusions and Recommendations

6.1 Conclusions

An electrically heated apparatus used to simulate the thermal behaviour of the SES-10 reactor following a loss-of-pool-water accident has been designed and built, and experimental results have been obtained. The following conclusions may be drawn from the experience with the apparatus:

- Flexibility of testing at three stages, i.e. core-riser module in the open room, water-cooled steady-state, and transient tests, provided good insights and knowledge of the analytical models.
- The apparatus performed very well in all three stages of the tests.
- Heater sheath temperatures obtained were in the desired range, as designed.
- Average outside liner temperatures in transient tests did not increase much above

the ambient temperature (for example, 10°C at power input of 600 W), as designed.

- Air mass flow rate calculated from the heat balance on the air in the core is obtained with good accuracy.
- Hot-wire anemometer calibration was the only major problem encountered in the research program. A new calibration unit able to provide accurate information about the calibrated velocity at very low range of 0.5 to 2.0 m/s will have to be obtained or designed to improve this part of the apparatus.
- Other tests are possible with only slight modifications of the present apparatus.

The following may be concluded for each test stage and its predictions by LOPWATEX program:

Stage 1 - Core-riser module in the open room:

- Provided confidence in the thermohydraulic part of the computer program.
- Established a correction factor for the pressure loss in the core and velocity duct.
- Showed that the predicted and measured air mass flow rates and temperatures T_i and T_e agree very well.
- Showed that the predicted riser wall temperature is underpredicted by 6 to 54°C as the power input increases from 100 to 700 W.

Stage 2 - Water-cooled steady-state test:

- Verified that the tank and concrete walls above the riser top act as a good heat sink and its simulation was consequently incorporated into the program LOPWATEX.
- Demonstrated that the predicted and measured air mass flow rates and temperatures T_r , T_b and T_e are in a good agreement.
- Proved uniform distribution of the air flow in the tank.
- Showed that the heat loss through the cover was about 1.5% for these conditions.

Stage 3 - Transient test:

- Showed significance of the heat capacity of the non-active parts of the heaters, and of the stainless steel core and riser boxes, and of the tank. Since the predictions does not allow for the heat storage in these parts, the predicted temperatures show faster response than measured ones in the transient changes.
- Verified significance of the heat loss through the cover (about 5%) and concrete bottom (about 10%) under these conditions.
- Showed good agreement of predicted and measured air temperatures with slight overprediction of T_b and T_e . The standard deviations is 13.7 and 8.1% respectively. T_r is overpredicted by 24, 39, and 47°C at 24 hr of test at powers of 200, 400, and 600 W. The overprediction of temperatures is caused by neglect of heat loss through the cover and concrete bottom in the computer program.
- Showed that air mass flow rate is predicted well because predicted and measured

$\Delta T = T_h - T_r$ are in good agreement.

- Showed that the tank wall temperatures are predicted reasonably well.
- Showed that the predicted concrete wall temperatures agree well with the measured concrete temperature at the top location, i.e. the average concrete wall temperature is overpredicted by 4 to 14°C as power increased from 200 to 600 W.
- Showed that quasi-steady state is reached in about 7 hours of the test.

Conclusions on the LOPWAT2 program:

- Its predictions are conservative. This conclusion is supported by the fact that the transient behaviour of the apparatus is predicted conservatively and that the program LOPWAT2 does not include allowance for the significant heat loss into the tank and concrete walls above the riser top.
- These experiments increase confidence that there will be no significant fuel damage in a loss-of-pool-water accident and that there will be no release of fission products into the environment, as concluded in the previous studies [11,14,15].

6.2 Recommendations

The following are recommendations for improvements of the apparatus:

- It is essential for the hot-wire probe calibration to obtain or design a calibration unit able to provide accurate information about calibrated velocity.
- Measurement of more points of the velocity profile across the velocity duct may yield better flow measurement once a valid calibration of the hot-wire anemometer is obtained.
- Better control of power input should be obtained to eliminate power fluctuations caused by university load variations. Further, computer control of power input would enable simulation of the power decay curve as in the actual reactor and power input information could be collected by the data acquisition system.

The following are recommendations for future tests:

- The effect of mixing of the outside air with the inside air may be examined by making a hole in the cover and providing orifices of various sizes. This would provide insight into the cooling effect of outside air which could leak in around cover and feedthroughs of instrumentation for control purposes or when cracks open in the concrete wall or tank above the ground.

- Pressure loss in heat exchangers of the actual reactor may be simulated by mounting an orifice on the riser top.

Recommendations for improvements of the program LOPWATEX:

- The heat storage in the non-active parts of the heaters and in the steel parts of the apparatus should be incorporated into the computer program LOPWATEX. This would provide more confidence in the ability of the program to predict the transient behaviour.
- Heat loss through the cover and concrete bottom could be incorporated in the program as well.

Recommendation for improvements of the program LOPWAT2:

- The heat loss into the tank and concrete walls above the riser top should be incorporated into the program.
- Allowance for heat losses to the top and bottom of the actual reactor could be incorporated into the program.
- Allowance for variation of tank wall and concrete temperatures with height could be incorporated into the program.

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TABLES

Table 1. The values of the ratios in the SES-10 reactor and in the experimental apparatus.

RATIO	SES-10	Model
Pitch of the fuel elements/element diameter	1.49	1.49
Element diameter/element active length	0.0179	0.0471
Height of the riser/height of the core	4.674	0.963
Width of the riser/width of the core	1.47	1.47
Surface area of concrete/surface area of fuel elements	3.33	12.35
Cross-sectional area of riser/ cross-sectional area of downcomer	0.0576	0.0530
Width of the air gap/height of the air gap	0.0659	0.0400

Table 2. The equation and sign combination in subroutine TEMPGET in LOPWAT2.

Equation	Sign value
$T_g - F_2 * T_e - T_w + F_2 * T_w = 0$	-1
$T_g - F_3 * T_r - T_w + F_3 * T_w = 0$	1
$T_h - F_1 * T_e + F_1 * T_w - T_w = 0$	1
$T_h - T_r - Z = 0$	-1
$-\beta_o/2 * T_g - (\beta_i + \beta_o)/2 * T_e - \beta/2 * T_h + T_{rw} - (1 - \beta_i - \beta_o) * T_w = 0$	-1

Table 3. The equation and sign combination in subroutine TEMPGET in LOPWATEX.

Equation	Sign value
$T_g - F_2 * T_k - T_w + F_2 * T_w = 0$	-1
$-T_g + F_3 * T_r + T_w - F_3 * T_w = 0$	-1
$T_k - F_4 * T_e - T_w + F_4 * T_w = 0$	-1
$F_1 * T_e - T_h + T_w - F_1 * T_w = 0$	-1
$-T_r + T_h - Z = 0$	-1
$-\beta_o * T_g - \beta_o * B_5 * T_k - \beta_i * C_4 * T_e - \beta_i * T_h + 2 * T_{rw} - F_5 * T_w = 0$	-1

Table 4. Temperatures of the air in [°C] in the preliminary and final tests in the open room.

Location	T_r	T_{h1}	T_{h2}	T_{h3}	T_e
Power = 100 W					
Prelim.	13.8	68.6	82.9	79.7	55.3
Final	19.0	86.5	102.0	91.1	65.6
Power = 200 W					
Prelim.	10.9	105.6	126.6	122.3	88.0
Prelim.	14.3	126.0	134.2	137.9	98.4
Final	19.2	136.6	157.7	142.1	99.7
Power = 400 W					
Prelim.	15.7	197.3	229.6	221.1	156.0
Prelim.	15.4	212.0	235.8	231.7	144.2
Prelim.	15.4	211.6	225.7	229.6	162.2
Final	20.6	232.0	265.2	240.7	164.3
Final	19.2	232.4	265.2	241.6	167.3
Power = 500 W					
Prelim.	15.0	259.9	275.9	278.3	192.5
Final	20.7	272.2	307.8	280.4	191.1
Power = 600 W					
Prelim.	16.3	300.8	322.0	321.0	216.8
Final	20.8	319.6	360.1	328.7	222.0
Power = 700 W					
Prelim.	17.8	337.8	356.9	360.6	245.7
Final	21.0	354.3	396.7	362.9	243.1

Table 5. Temperatures of the air in [°C] in the preliminary and final water cooled steady state tests.

Location	T_r	T_{h1}	T_{h2}	T_{h3}	T_e
Power = 100 W					
Prelim.	5.7	70.4	79.2	75.8	56.5
Prelim.	5.5	69.8	78.2	75.6	55.3
Final	6.9	70.4	86.9	76.1	56.1
Final	8.1	77.1	91.1	81.0	59.3
Power = 200 W					
Prelim.	5.8	119.0	130.8	126.3	92.3
Prelim.	5.7	119.4	131.1	127.3	94.1
Final	7.6	127.1	149.8	134.0	97.7
Final	8.2	129.9	149.8	135.0	97.8
Power = 300 W					
Prelim.	6.9	170.5	185.4	180.4	132.2
Prelim.	6.7	175.0	190.4	185.1	136.1
Final	7.6	184.1	211.4	191.1	136.0
Power = 400 W					
Prelim.	7.4	229.8	248.3	239.5	171.6
Prelim.	7.1	230.6	249.7	240.7	172.8
Final	8.3	232.7	264.8	240.3	166.7
Final	8.1	233.9	266.6	241.7	168.5
Power = 500 W					
Prelim.	8.2	280.1	300.7	288.2	203.3
Final	8.6	284.6	320.9	291.6	200.0

Table 6. Temperatures of the air in [°C] at 24 hr of the preliminary and final transient tests at input power of 400 W.

Location	T_r	T_{h1}	T_{h2}	T_{h3}	T_e
Prelim.	34.0	332.7	349.1	333.4	243.4
Final	34.6	319.4	349.4	322.2	236.1

Table 7. Temperatures of the heater sheath in [°C] in the preliminary and final tests in the open room.

	Position from the upstream end of the heater [cm]				
	8.4	14.2	20.0	25.8	31.6
Power = 100 W					
Prelim.	68.1	89.4	103.2	115.1	110.7
Final	74.8	98.2	117.2	132.5	129.9
Power = 200 W					
Prelim.	96.8	134.8	159.0	180.0	172.1
Prelim.	98.7	136.1	160.3	181.9	175.7
Final	107.7	149.5	182.1	207.8	203.3
Power = 400 W					
Prelim.	160.2	230.8	270.5	301.9	292.1
Prelim.	163.9	235.3	276.6	309.6	300.8
Prelim.	160.3	230.8	270.8	303.2	293.8
Final	177.5	258.2	311.6	348.9	342.0
Final	176.3	257.2	310.8	347.7	341.1
Power = 500 W					
Prelim.	195.0	281.9	327.7	363.2	353.1
Final	207.6	302.3	362.5	402.2	395.2
Power = 600 W					
Prelim.	227.8	326.8	378.2	415.5	405.2
Final	251.8	365.7	432.4	472.2	464.4
Power = 700 W					
Prelim.	236.3	346.4	399.0	436.7	426.3
Final	284.5	411.4	480.7	500.1	512.0

Table 8. Temperatures of the heater sheath in [°C] in the preliminary and final water cooled steady state tests.

	Position from the upstream end of the heater [cm]				
	8.4	14.2	20.0	25.8	31.6
Power = 100 W					
Prelim.	60.2	81.4	95.4	107.4	102.8
Prelim.	59.5	80.8	94.8	106.9	102.4
Final	65.6	87.8	106.7	122.3	119.1
Final	66.3	88.3	106.8	122.7	120.1
Power = 200 W					
Prelim.	92.7	130.1	155.1	176.2	169.5
Prelim.	93.9	132.4	157.0	178.5	171.0
Final	103.3	145.5	179.9	208.1	202.8
Final	101.3	141.9	174.7	202.2	197.8
Power = 300 W					
Prelim.	125.9	181.5	216.1	244.0	235.8
Prelim.	129.7	187.5	223.0	251.8	243.1
Final	140.1	202.3	249.0	284.8	278.7
Power = 400 W					
Prelim.	166.1	241.3	285.0	317.9	308.8
Prelim.	167.4	243.9	287.6	321.2	311.5
Final	178.0	258.6	315.2	355.3	348.5
Final	180.1	261.9	318.7	359.0	351.7
Power = 500 W					
Prelim.	201.6	291.4	341.0	376.9	367.6
Final	221.8	321.3	385.9	428.4	420.5

Table 9. Temperatures of the heater sheath in [°C] at 24 hr of the preliminary and final transient tests at input power of 400 W.

Test	Position from the upstream end of the heater [cm]				
	8.4	14.2	20.0	25.8	31.6
Prelim.	235.5	323.4	375.5	413.4	407.8
Final	264.6	359.0	418.9	457.3	452.1

Table 10. Temperatures in [°C] along the water-cooled tank wall.

Power	Circu mfer.	Position from the tank bottom [cm]					
		4.00	26.1	48.5	71.7	92.7	114.7
100	0°	4.4	4.5	4.7	5.0	5.3	6.7
	180°	4.7	4.4	4.5	4.7	5.0	6.0
200	0°	5.5	5.8	6.0	6.4	6.7	8.6
	180°	6.1	6.0	5.7	6.1	6.6	8.2
300	0°	7.4	7.7	7.8	8.4	8.6	11.5
	180°	8.0	7.7	7.8	8.3	9.0	11.4
400	0°	8.6	8.9	9.1	9.7	10.2	13.7
	180°	9.1	8.9	8.9	9.6	10.4	13.5
400	0°	7.6	7.9	8.2	9.0	9.3	12.5
	180°	8.0	7.9	7.8	8.7	9.4	12.2
500	0°	8.8	9.3	9.6	10.5	10.9	14.5
	180°	9.3	9.2	9.3	10.2	11.1	14.3
600	0°	10.0	10.5	10.8	11.7	12.2	16.3
	180°	10.3	10.4	10.6	11.5	12.4	15.9
700	0°	10.9	11.6	12.1	13.2	13.7	18.4
	180°	11.4	11.6	11.9	12.9	13.8	17.9

Table 11. Temperatures of the cover and its insulation in [°C] for the water-cooled tank tests.

Power [W]	Bottom surface of the cover	Top surface of the cover	Top of the insulation
100	26.5	26.0	20.2
200	42.5	42.3	22.5
300	51.9	50.2	24.5
400	64.9	63.0	24.8
400	62.2	59.8	25.6
500	75.4	73.3	27.8
600	84.5	82.1	29.9
700	94.8	92.0	32.3

Table 12. Temperatures of the concrete bottom in [°C] for the water-cooled tank tests.

Power	Position from top surface [cm]		
	1.0	6.2	11.4
100	5.4	5.7	9.3
200	6.1	6.5	10.3
300	6.3	6.8	10.4
400	6.0	6.3	9.9
400	5.4	5.7	9.6
500	5.6	5.9	9.5
600	5.5	5.7	9.2
700	5.5	5.6	8.9

Table 13. Temperatures in [°C] along the tank wall for the transient tests at 24 hr after start.

Power	Circumfer.	Position from the tank bottom [cm]					
		4.00	26.1	48.5	71.7	92.7	114.7
200	0°	34.4	39.2	44.6	51.5	58.0	64.2
200	180°	34.3	39.2	44.7	51.8	57.7	64.4
400	0°	48.9	57.5	67.2	78.9	88.7	98.8
400	180°	49.1	57.7	67.3	79.2	88.3	98.7
600	0°	65.1	78.7	93.5	109.1	121.4	134.3
600	180°	65.3	78.7	93.6	109.5	120.9	134.2

Table 14. Temperatures of the cover and its insulation in [°C] for the transient tests at 24 hr after start.

Power [W]	Bottom surface of the cover	Top surface of the cover	Top of the insulation
200	84.6	83.9	25.9
400	130.2	128.8	25.8
600	168.9	167.5	29.2

Table 15. Temperatures of the concrete bottom in [°C] for the transient tests at 24 hr after start.

Power [W]	Position from top surface [cm]		
	1.0	6.2	11.4
200	18.4	16.9	16.0
400	22.8	20.5	18.9
600	28.4	25.0	22.6

FIGURES

FIGURE 1. SCHEMATIC DIAGRAM OF THE SES-10, CLOSED RISER DUCT DESIGN (copied from reference [2]).

FIGURE 2. SCHEMATIC DIAGRAM OF THE SES-10, OPEN RISER DUCT DESIGN.

FIGURE 3. SCHEMATIC DIAGRAM OF APPARATUS.

FIGURE 4. APPARATUS SETUP.

FIGURE 5. CORE-RISER MODULE.

FIGURE 6. CORE ENDPLATE.

FIGURE 7. SCHEMATIC DIAGRAM OF THE HEATER CONNECTIONS.

FIGURE 8. VELOCITY DUCT, CORE, STAND, AND CENTERING PLATE.

FIGURE 9. SKETCH OF THE TANK.

FIGURE 10. INSIDE LINER.

FIGURE 11. SKETCH OF OUTSIDE LINER.

FIGURE 12. SKETCH OF SPACERS.

FIGURE 13. WHEATSTONE BRIDGE.

FIGURE 14. HOT-WIRE PROBE WITH SHIELD.

FIGURE 15. CALIBRATION OF THE VELOCITY SENSOR FOR VELOCITY.

FIGURE 16. CALIBRATION OF THE VELOCITY SENSOR FOR TEMPERATURE.

FIGURE 17. CALIBRATION OF TEMPERATURE SENSOR.

FIGURE 18. HEAT LOSS FROM THE CORE.

FIGURE 19. TRAVERSING MECHANISM.

FIGURE 20. CALIBRATION OF DAS-8 A/D CONVERTER.

FIGURE 21. SCHEMATIC DIAGRAM OF DAS-8 BOARD CONNECTIONS.

FIGURE 22. MAXIMUM FUEL SHEATH TEMPERATURE, SES-10 REACTOR.

FIGURE 23. AIR MASS FLOW RATE, SES-10 REACTOR.

FIGURE 24A. AIR MASS FLOW RATE, CORE-RISER MODULE IN THE OPEN ROOM.

FIGURE 24B. AIR MASS FLOW RATE, CORE-RISER MODULE IN THE OPEN ROOM.

FIGURE 25A. AIR TEMPERATURES, CORE-RISER MODULE IN THE OPEN ROOM, $\epsilon=0.79$.

FIGURE 25B. AIR TEMPERATURES, CORE-RISER MODULE IN THE OPEN ROOM, $\epsilon=0.95$.

FIGURE 26A. RISER WALL TEMPERATURES. CORE-RISER MODULE IN THE OPEN ROOM, $\epsilon = 0.95$.

FIGURE 26B. RISER WALL TEMPERATURES, CORE-RISER MODULE IN THE OPEN ROOM, $\epsilon = 0.79$.

FIGURE 27. HEATER SHEATH TEMPERATURES, CORE-RISER MODULE IN THE OPEN ROOM.
POWER=100 W.

FIGURE 28. HEATER SHEATH TEMPERATURE, CORE-RISER MODULE IN THE OPEN ROOM, POWER = 200 W.

FIGURE 29. HEATER SHEATH TEMPERATURE, CORE-RISER MODULE IN THE OPEN ROOM.
POWER = 300 W.

FIGURE 30. HEATER SHEATH TEMPERATURE, CORE-RISER MODULE IN THE OPEN ROOM.
POWER = 400 W.

FIGURE 31. HEATER SHEATH TEMPERATURE, CORE-RISER MODULE IN THE OPEN ROOM.
POWER = 500 W.

FIGURE 32. HEATER SHEATH TEMPERATURE, CORE-RISER MODULE IN THE OPEN ROOM,
POWER = 600 W.

FIGURE 33. HEATER SHEATH TEMPERATURE, CORE-RISER MODULE IN THE OPEN ROOM, POWER = 700 W.

FIGURE 34. AIR MASS FLOW RATE, WATER-COOLED STEADY-STATE TEST.

FIGURE 35A. AIR TEMPERATURES, WATER-COOLED STEADY-STATE TEST, PREDICTION WITHOUT HEAT LOSS TO THE TANK WALL ABOVE THE RISER TOP.

$\rightarrow$ Th,pred $+$ Te,pred $\blacksquare$ Th,exp $\blacklozenge$ Te,exp

FIGURE 35B. AIR TEMPERATURES, WATER-COOLED STEADY-STATE TEST, PREDICTION ALLOWS FOR HEAT LOSS TO THE TANK WALL ABOVE THE RISER TOP.

FIGURE 36. HEATER SHEATH TEMPERATURE, WATER-COOLED STEADY-STATE TEST.
POWER = 100 W.

FIGURE 37. HEATER SHEATH TEMPERATURE, WATER-COOLED STEADY-STATE TEST, POWER = 200 W.

FIGURE 38. HEATER SHEATH TEMPERATURE, WATER-COOLED STEADY-STATE.
POWER = 300 W.

FIGURE 39. HEATER SHEATH TEMPERATURE, WATER-COOLED STEADY-STATE TEST, POWER = 400 W.

FIGURE 40. HEATER SHEATH TEMPERATURE, WATER-COOLED STEADY-STATE TEST.
POWER = 500 W.

FIGURE 41. HEATER SHEATH TEMPERATURE, WATER-COOLED STEADY-STATE TEST,
POWER = 600 W.

FIGURE 42. HEATER SHEATH TEMPERATURE, WATER-COOLED STEADY-STATE TEST.
POWER = 700 W.

FIGURE 43. AIR MASS FLOW RATE, TRANSIENT TEST AT TIME = 400 min.

FIGURE 44A. AIR TEMPERATURES, TRANSIENT TEST AT TIME = 400 min.

FIGURE 44B. AIR TEMPERATURES, TRANSIENT TEST AT TIME = 24 hr.

Temperature locations

Tr,exp * Th,exp + Te,exp + Tr,pred + Th,pred + Te,pred

FIGURE 45. AIR TEMPERATURES, TRANSIENT TEST, POWER = 200 W.

Temperature locations

$\bullet$ Tr,exp $*$ Th,exp $+$ Tr,pred $\square$ Th,pred $\diamond$ Te,pred

FIGURE 46. AIR TEMPERATURES, TRANSIENT TEST, POWER = 400 W.

Temperature locations

$\circ$ $T_{r,exp}$
 $*$ $T_{h,exp}$
 $+$ $T_{r,pred}$
 $\diamond$ $T_{h,pred}$

FIGURE 47. AIR TEMPERATURES, TRANSIENT TEST, POWER = 600 W.

FIGURE 48. HEATER SHEATH TEMPERATURE, TRANSIENT TEST.

FIGURE 49. HEATER SHEATH TEMPERATURE, TRANSIENT TEST AT TIME = 24 hr, POWER = 200 W.

FIGURE 50. HEATER SHEATH TEMPERATURE, TRANSIENT TEST AT TIME = 24 hr, POWER = 400 W.

FIGURE 51. HEATER SHEATH TEMPERATURE. TRANSIENT TEST AT TIME = 24 hr. POWER = 600 W.

FIGURE 52. TANK WALL TEMPERATURES, TRANSIENT TEST, POWER = 200 W.

FIGURE 53. TANK WALL TEMPERATURES, TRANSIENT TEST, POWER = 400 W.

FIGURE 54. TANK WALL TEMPERATURES, TRANSIENT TEST, POWER = 600 W.

FIGURE 55. CONCRETE WALL TEMPERATURES, INNER NODE, TRANSIENT TEST, POWER = 200 W.

FIGURE 56. CONCRETE WALL TEMPERATURE, INNER NODE, TRANSIENT TEST, POWER = 400 W.

FIGURE 57. CONCRETE WALL TEMPERATURES. INNER NODE. TRANSIENT TEST. POWER = 600 W.

FIGURE 58. CONCRETE WALL TEMPERATURE PROFILE AT TIME = 24 hr, TRANSIENT TEST, POWER = 200 W.

FIGURE 59. CONCRETE WALL TEMPERATURE PROFILE AT TIME = 24 hr. TRANSIENT TEST.
POWER = 400 W.

FIGURE 60. CONCRETE WALL TEMPERATURE PROFILE AT TIME = 24 hr, TRANSIENT TEST, POWER = 600 W.

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Appendices A, B.11, B.12 and C can be viewed on at a diskette available only at
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APPENDIX A

The listing of the LOPWAT2 program may be found in the file LOPWAT2.FOR on the enclosed disk.

APPENDIX B

B.1 Flowcharts of Program LOPWATEX

FIGURE B.1 MAIN CALLING ROUTINE

3 of/de 3

MICROCOPY RESOLUTION TEST CHART
NATIONAL BUREAU OF STANDARDS
STANDARD REFERENCE MATERIAL 1010a
(ANSI and ISO TEST CHART No. 2)

FIGURE B.2 Subroutine FUELTRANS

FIGURE B.3 Subroutine FBAL

FIGURE B.4 Subroutine TEMPGET

FIGURE B.5 Subroutine POTEMP

FIGURE P 6 Subroutine CONTRANS

B.2 Listing of Program LOPWATEX

The listing of the LOPWATEX program may be found in the file LOPWATEX.FOR on the enclosed disk.

APPENDIX C

The listing of the data acquisition program may be found in the file SLOWPOKE.BAS on the enclosed disk.

APPENDIX D

This Appendix describes derivation of T_{sh} , α_{ht} , and U_{ht} . The model used here is based on a simplified model for a gas-cooled reactor developed by Rogers in 1959 as used in [14].

D.1 Heater sheath temperature

The heat balance on the air at any axial position is:

$$\frac{dT_a}{dz} = \frac{QP_{ht}}{GA_c c_p} \quad \text{D.(1)}$$

The equation D.1 was integrated to find the bulk air temperature at any axial position:

$$T_{az} = T_r + \frac{QP_{ht}}{GA_c c_p} z \quad \text{D.(2)}$$

The heater sheath temperature at any axial position can be found by using the core heat transfer coefficient:

$$h(T_{shz} - T_{az}) = \frac{Q}{A_{ht}} \quad \text{D.(3)}$$

By substitution of T_{az} of the equation (D.2) into equation (D.3) and consequent integration, the following equation for the average heater sheath temperature was obtained:

$$T_{sh} = T_f + \frac{Q}{A_{ht}} \left(\frac{1}{h} + \frac{P_{ht}}{G \cdot A_c \cdot c_p} \frac{L_a}{2} \right) \quad \text{D.(4)}$$

D.2 Overall Heat Transfer Coefficient MgO-air

The overall heat transfer coefficient from the heater into the coolant is found from:

$$\frac{1}{U_{fo} R_o} = \frac{\ln \left(\frac{R}{r_m} \right)}{k_f} + \frac{t_{sh}}{k_{sh} R_{sh}} + \frac{1}{h R_o} \quad \text{D.(5)}$$

The first term on the right hand side of this equation represents the thermal resistance of the MgO from the position r_m , where the MgO temperature equals the radial-average MgO temperature, to the MgO outer radius. The second term represents the thermal resistance of the heater sheath and the third term represents the thermal resistance of the convection boundary layer.

Assuming uniform radial heat generation within MgO, r_m is given by:

D.3

$$r_m = \frac{R}{\sqrt{2}} \quad \text{D.(6)}$$

Substituting (D.6) into (D.5), U_{fo} is obtained:

$$U_{fo} = \frac{1}{0.3465 \frac{R_o}{k_f} + \frac{t_{sh}}{k_{sh}} + \frac{1}{h}} \quad \text{D.(7)}$$

D.3 Average MgO temperature

The radial average MgO temperature at any axial position in a heater is calculated from:

$$U_{fo}(T_f - T_{az}) = \frac{Q}{A_{ht}} \quad \text{D.(8)}$$

By substitution of T_{az} of the equation (D.2) into equation (D.8) the following equation for the radial average MgO temperature was obtained:

$$T_{fr} = T_r + \frac{Q}{A_{ht}} \left(\frac{1}{U_{fo}} + \frac{P_{ht}}{GA_c c_p} z \right) \quad \text{D.(9)}$$

The axial average MgO temperature can be found from the following integral:

D.4

$$T_{fz} = \frac{1}{L_a} \int_0^{L_a} T_{fr} dz \quad \text{D.(10)}$$

The integration yields:

$$T_{fz} = T_{fv} = T_r + \frac{Q}{A_{ht}} \left(\frac{1}{U_{fo}} + \frac{P_{ht}}{GA_c c_p} \frac{L_a}{2} \right) \quad \text{D.(11)}$$

D.4 Input Power Fraction Transferred into the Air

The rate of heat storage in the core over a core time step, δt , is given by:

$$P_{st} = M_{fpr} \frac{(T_{fv} - T_{fvo})}{\delta t} \quad \text{D.(12)}$$

The fraction of input power transferred to the cooling air is defined as:

$$\alpha_{ht} = \frac{P - P_{st}}{P} = \frac{Q}{P} \quad \text{D.(13)}$$

By substitution of the equation (D.12) into equation (D.13) we obtain:

$$\alpha_{ht} = 1 - \frac{M_{fpr}(T_{fv} - T_{fvo})}{P \delta t} \quad \text{D.(14)}$$

By substituting equation (D.11) into equation (D.14) and consequent rearrangement, the fraction of power transferred into the air under the transient conditions is obtained:

$$\alpha_{ht} = \frac{1 + \frac{M_f c_{pf}}{P_d \delta t} (T_{fvo} - T_r)}{1 + \frac{c_{pf}}{A_{ht} \delta t} \left(1 + \frac{P_{ht} M_f L_a}{GA_c c_p 2} \right)} \quad \text{D.(15)}$$

APPENDIX E

Uncertainty Analysis

The following analysis is based on [E.1] and [E.2].

1. Mercury thermometer. The accuracy of mercury thermometer ASTM-56C cited by [E.3] is $\pm 0.1^\circ\text{C}$. To be conservative, this accuracy was taken as the uncertainty based on 1σ value since it was unknown if it was based on 3σ or 1σ . Thus, $w_{th} = 0.1^\circ\text{C}$.

2. Thermocouples. Readings of thermocouples (type K) were checked against readings of the above mentioned mercury thermometer. All thermocouples read within $\pm 1.5^\circ\text{C}$ of the thermometer. This difference is considered to be 3σ value. Then, the uncertainty based on 1σ value is $w_{tk} = 0.5^\circ\text{C}$.

3. Temperature. The standard deviation of temperature is then based on mercury thermometer and thermocouple deviations:

$$w_T = \sqrt{w_{th}^2 + w_{tk}^2} \quad \text{E.(1)}$$

and it is found to be $w_T = 0.510^\circ\text{C}$.

4. Power. Observed maximum power fluctuations were $\pm 5\%$ of the power applied. Therefore these fluctuations are used as a measure of accuracy based on 3σ standard deviation. Then, based on 1σ , $w_p = 1.67\%$.

5. Specific heat of air. Specific heat of air was calculated from measured air temperature using the following curve fit:

$$c_p = 975.62 + 50.33 \cdot 10^{-3} \cdot T_a + 126.7 \cdot 10^{-6} \cdot T_a^2 \quad \text{E.(2)}$$

The differentiation with respect to T yields:

$$\frac{dc_p}{dT_a} = 50.33 \cdot 10^{-3} + 2 \cdot 126.7 \cdot 10^{-6} \cdot T_a \quad \text{E.(3)}$$

The uncertainty in c_p is then:

$$w_{c_p} = \sqrt{\left(\frac{dc_p}{dT_a} w_{T_a} \right)^2} \quad \text{E.(4)}$$

The uncertainty evaluated at 100 W: $w_{c_p} = 0.00604\%$

The uncertainty evaluated at 700 W: $w_{c_p} = 0.00638\%$

E.3

This uncertainty may be neglected in the equations below since it is negligible compared to the uncertainties of the other parameters.

6. Mass flow rate calculated from heat balance on the air. The equation is:

$$m = \frac{Q}{c_p(T_h - T_r)} \quad \text{E.(5)}$$

Differentiating the equation with respect to the various parameters with c_p assumed to be constant, yields:

$$\frac{\delta m}{\delta Q} = \frac{1}{c_p(T_h - T_r)} \quad \text{E.(6)}$$

$$\frac{\delta m}{\delta T_h} = -\frac{Q}{c_p(T_h - T_r)^2} \quad \text{E.(7)}$$

$$\frac{\delta m}{\delta T_r} = \frac{Q}{c_p(T_h - T_r)^2} \quad \text{E.(8)}$$

Since these uncertainties are independent of each other, and may be assumed to be reasonably small, the uncertainty in the mass flow rate may be determined using the following equation:

E.4

$$w_{m_{\text{res}}} = \sqrt{\left(\frac{\delta m}{\delta Q} w_Q\right)^2 + \left(\frac{\delta m}{\delta T_h} w_{T_h}\right)^2 + \left(\frac{\delta m}{\delta T_r} w_{T_r}\right)^2} \quad \text{E.(9)}$$

The results are organized in the following table:

Test	Power	Uncertainty [kg/s]	Uncertainty [%]
Open room	100	$2.605 \cdot 10^{-5}$	2.0
	700	$3.274 \cdot 10^{-5}$	1.8
Water cooled	100	$2.575 \cdot 10^{-5}$	2.0
	700	$2.946 \cdot 10^{-5}$	1.8
Transient	200	$1.988 \cdot 10^{-5}$	1.6
	600	$2.413 \cdot 10^{-5}$	1.7

7. Air density. Air density is calculated from the equation of state of an ideal gas:

$$\rho = \frac{P_a}{R_a T_a} \quad \text{E.(10)}$$

based on air temperature measurement. The air pressure is assumed constant at 101.3 kPa in the analysis, but the actual atmospheric pressure fluctuates between 98 and 105 kPa over the year [E.4]. Then the uncertainty based on 1σ is $w_p = 1.17\%$.

The differentiations with respect to terms in the equation are:

E.5

$$\frac{\delta \rho}{\delta p_a} = \frac{1}{R_a T_a} \quad \text{E.(11)}$$

$$\frac{\delta \rho}{\delta T_a} = -\frac{p_a}{R_a T_a^2} \quad \text{E.(12)}$$

Knowing the uncertainty in the temperature measurement and in the air pressure, the uncertainty in the air density may be found:

$$w_\rho = \sqrt{\left(\frac{\delta \rho}{\delta p_a} w_{p_a}\right)^2 + \left(\frac{\delta \rho}{\delta T_a} w_{T_a}\right)^2} \quad \text{E.(13)}$$

The standard deviation of the air density is: $w_\rho = 0.0215 \text{ kg/m}^3$ or 1.7%.

8. Velocity calibration uncertainty. The velocity is related to manometer fluid (alcohol) height by the following equation:

$$u_c = \sqrt{\frac{2h_{im}\rho_L g}{\rho_a}} \quad \text{E.(14)}$$

where ρ_L is the density of alcohol.

The accuracy of the parameters is as follows:

Inclined manometer reading accuracy is estimated to be $\pm 0.3 \text{ mm}$ which is taken as 3σ .

Thus, the uncertainty based on 1σ is $w_{him} = 0.1$ mm.

Alcohol density is assumed to be constant, due to very small temperature changes (within 5°C).

Gravity acceleration is assumed to be constant since the calibration is done at the same height.

Air density accuracy is established in Section 5.

Thus, differentiation of the equation over the non-constant parameters gives:

$$\frac{\delta u_c}{\delta h_{im}} = \frac{1}{2} \left(\frac{2h_{im}\rho_L g}{\rho} \right)^{\frac{1}{2}} \frac{2\rho_L g}{\rho} \quad \text{E.(15)}$$

$$\frac{\delta u_c}{\delta \rho} = \frac{1}{2} \left(\frac{2h_{im}\rho_L g}{\rho} \right)^{\frac{1}{2}} \left(-\frac{2h_{im}\rho_L g}{\rho^2} \right) \quad \text{E.(16)}$$

Then the uncertainty in calibrated velocity is:

$$w_{u_c} = \sqrt{\left(\frac{\delta u_c}{\delta h_{im}} w_{h_{im}} \right)^2 + \left(\frac{\delta u_c}{\delta \rho} w_{\rho} \right)^2} \quad \text{E.(17)}$$

The uncertainty is a function of the velocity as shown in the following table:

Height [mm]	Velocity [m/s]	Uncertainty [m/s]	Uncertainty [%]
1.0	0.74	0.187	25.3
2.0	1.04	0.139	13.3
3.0	1.27	0.121	9.5
4.0	1.47	0.114	7.8
5.0	1.64	0.111	6.8
6.0	1.80	0.111	6.2
7.0	1.95	0.112	5.8
8.0	2.08	0.114	5.5
9.0	2.21	0.117	5.3

9. Velocity duct area. The dimensions were measured with vernier of 0.01 mm resolution which is taken as standard deviation based on 1σ to be conservative. The dimensions of the velocity duct are 0.03 by 0.03 m. The area is then 0.0009 m^2 with an uncertainty of $1.414 \times 10^{-5} \text{ m}^2$.

10. Air temperature measured by hot-wire. The equation for the air temperature is:

$$T_a = 255.3249 - 50.58482 \cdot E \quad \text{E.(18)}$$

When differentiated with respect to E:

$$\frac{\delta T_a}{\delta E} = -50.58482 \quad \text{E.(19)}$$

The maximum fluctuations of the hot-wire anemometer output read by the data acquisition system were $\pm 0.006\text{V}$. These fluctuations are used as a measure of accuracy based on 3σ . The uncertainty based on 1σ is $w_E = 0.002\text{ W}$.

The uncertainty is then found from:

$$w_{T_s} = \sqrt{\left(\frac{\delta T_a}{\delta E} w_E\right)^2} \quad \text{E.(20)}$$

The uncertainty found is $w_{T_s} = 0.101^\circ\text{C}$.

11. Operating temperature of the sensor. The expression to find the operating temperature of the sensor is:

$$T_s = \frac{\frac{R_s}{R_p} - 1}{\alpha} + T_a \quad \text{E.(21)}$$

Differentiation with respect to the various parameters yields:

$$\frac{\delta T_s}{\delta R_s} = \frac{1}{R_p \alpha} \quad \text{E.(22)}$$

E.9

$$\frac{\delta T_s}{\delta T_a} = 1 \quad \text{E.(23)}$$

$$\frac{\delta T_s}{\delta R_p} = -\frac{R_s}{R_p^2 \alpha} \quad \text{E.(24)}$$

$$\frac{\delta T_s}{\delta \alpha} = -\frac{\frac{R_s}{R_p} - 1}{\alpha^2} \quad \text{E.(25)}$$

The uncertainties of the parameters based on 1σ are:

The uncertainty in measurement of the sensor resistance R_s and R_p by the hot-wire anemometer is $w_{R_s} = w_{R_p} = 0.01 \Omega$ based on the resolution of the anemometer as 1σ .

The uncertainty in T_a is established in Section 10.

The uncertainty in α is $w_\alpha = 0.000003419$ or 0.084% .

The uncertainty in the temperature of the sensor is found from:

$$w_{T_s} = \sqrt{\left(\frac{\delta T_s}{\delta R_s} w_{R_s}\right)^2 + \left(\frac{\delta T_s}{\delta R_p} w_{R_p}\right)^2 + \left(\frac{\delta T_s}{\delta T_a} w_{T_a}\right)^2 + \left(\frac{\delta T_s}{\delta \alpha} w_\alpha\right)^2} \quad \text{E.(26)}$$

For the lowest temperature of $T_a = 7.6^\circ\text{C}$, the uncertainty is $w_{T_s} = 1.46^\circ\text{C}$ or 0.55% .

For the highest temperature of $T_a = 46.9^\circ\text{C}$, the uncertainty is $w_{T_s} = 1.13^\circ\text{C}$ or 0.47% .

E.10

Because these are very close to each other, the average is used over the whole temperature range, i.e. $w_{T_s}=1.3^\circ\text{C}$ or 0.51%.

12. Thermal conductivity of the air. The thermal conductivity of the air is found using the equation of Morrison [E.5]:

$$k_a = k_{ar} \left(\frac{T_m}{T_{ar}} \right)^{0.86} = k_{ar} \left(\frac{T_s + T_a}{2T_{ar}} \right)^{0.86} \quad \text{E.(27)}$$

Differentiating over the various parameters yields:

$$\frac{\delta k_a}{\delta T_s} = k_{ar}^{0.86} \left(\frac{T_s + T_a}{2T_{ar}} \right)^{-0.14} \frac{1}{2T_{ar}} \quad \text{E.(28)}$$

$$\frac{\delta k_a}{\delta T_a} = k_{ar}^{0.86} \left(\frac{T_s + T_a}{2T_{ar}} \right)^{-0.14} \frac{1}{2T_{ar}} \quad \text{E.(29)}$$

The uncertainty in the thermal conductivity of the air is:

E.11

$$w_k = \sqrt{\left(\frac{\delta k_a}{\delta T_s} w_{T_s}\right)^2 + \left(\frac{\delta k_a}{\delta T_a} w_{T_a}\right)^2} \quad \text{E.(30)}$$

For the lowest temperature $T_a=7.6^\circ\text{C}$ this yields: $w_k=4.687*10^{-5}$ or 0.184%.

For the highest temperature $T_a=46.9^\circ\text{C}$ this yields: $w_k=4.678*10^{-5}$ or 0.173%.

Since these are close to each other, the average $w_k=4.682*10^{-5}$ or 0.179% is used over the whole temperature range.

13. Viscosity of the air. The viscosity of the air is found from the equation of Morrison [E.5]:

$$v_a = v_{ar} \left(\frac{T_m}{T_{ar}} \right)^{1.9} = v_{ar} \left(\frac{T_s + T_a}{2T_{ar}} \right)^{1.9} \quad \text{E.(31)}$$

Differentiate this with respect to the parameters:

$$\frac{\delta v_a}{\delta T_s} = v_{ar} 1.9 \left(\frac{T_s + T_a}{2T_{ar}} \right)^{0.9} \frac{1}{2T_{ar}} \quad \text{E.(32)}$$

$$\frac{\delta v_a}{\delta T_a} = v_{ar} 1.9 \left(\frac{T_s + T_a}{2T_{ar}} \right)^{0.9} \frac{1}{2T_{ar}} \quad \text{E.(33)}$$

The uncertainty in the viscosity of the air is:

$$w_{v_s} = \sqrt{\left(\frac{\delta v_a}{\delta T_s} w_{T_s}\right)^2 + \left(\frac{\delta v_a}{\delta T_a} w_{T_s}\right)^2} \quad \text{E.(34)}$$

For the lowest temperature $T_s=7.6^\circ\text{C}$ this yields: $w_v=8.661*10^{-8}$ or 0.624%.

For the highest temperature $T_s=46.9^\circ\text{C}$ this yields: $w_v=8.764*10^{-8}$ or 0.490%.

Since these are close to each other, the average $w_v=8.712*10^{-8}$ or 0.557% is used over the whole temperature range.

14. Measured velocity uncertainty. The equation for the measured velocity is:

$$u_m = \sqrt{\frac{\frac{E^2}{k_a(T_s - T_a)} \left(\frac{T_m}{T_a}\right)^{-0.17} - A}{B}} \frac{v_a}{d} \quad \text{E.(35)}$$

Differentiate this with respect to the various parameters to obtain:

$$\frac{\delta u_m}{\delta E} = \frac{v_a}{d} \frac{1}{n} \left(\frac{\frac{E^2}{k_a(T_s - T_a)} \left(\frac{T_m}{T_a}\right)^{-0.17} - A}{B} \right)^{\frac{1}{n}-1} \frac{E}{2} \frac{\left(\frac{T_m}{T_a}\right)^{-0.17}}{k_a(T_s - T_a)} \quad \text{E.(36)}$$

$$\frac{\delta u_m}{\delta k_a} = -\frac{v_a}{d} \frac{1}{n} \left(\frac{\frac{E^2}{k_a(T_s - T_a)} \left(\frac{T_m}{T_a}\right)^{-0.17} - A}{B} \right)^{\frac{1}{n}-1} \frac{\frac{E^2}{k_a^2(T_s - T_a)} \left(\frac{T_m}{T_a}\right)^{-0.17}}{B} \quad \text{E.(37)}$$

E.13

$$\frac{\delta u_m}{\delta v_a} = \frac{1}{d} \sqrt[n]{\frac{\frac{E^2}{k_a(T_s - T_a)} \left(\frac{T_m}{T_a}\right)^{-0.17} - A}{B}} \quad \text{E.(38)}$$

$$\frac{\delta u_m}{\delta d} = -\frac{v_a}{d^2} \sqrt[n]{\frac{\frac{E^2}{k_a(T_s - T_a)} \left(\frac{T_m}{T_a}\right)^{-0.17} - A}{B}} \quad \text{E.(39)}$$

$$\frac{\delta u_m}{\delta T_s} = -\frac{v_a}{d} \frac{1}{n} \left(\frac{\frac{E^2}{k_a(T_s - T_a)} \left(\frac{T_m}{T_a}\right)^{-0.17} - A}{B} \right)^{\frac{1}{n}-1} \left((T_s - T_a) \left(\frac{T_m}{T_a}\right)^{0.17} \right)^{-2} \frac{E^2}{k_a B} D_1 \quad \text{E.(40)}$$

where:

$$D_1 = 0.17 \left(\left(\frac{T_s^{6.88}}{2T_a} + \frac{T_s^{5.88}}{2} \right)^{-0.83} \left(6.88 \frac{T_s^{5.88}}{2T_a} + 5.88 \frac{T_s^{4.88}}{2} \right) \left(\frac{T_a^{4.88} T_s}{2} + \frac{T_a^{5.88}}{2} \right)^{-0.83} \frac{T_a^{4.88}}{2} \right)$$

$$\frac{\delta u_m}{\delta T_a} = -\frac{v_a}{d} \frac{1}{n} \left(\frac{\frac{E^2}{k_a(T_s - T_a)} \left(\frac{T_m}{T_a}\right)^{-0.17} - A}{B} \right)^{\frac{1}{n}-1} \left((T_s - T_a) \left(\frac{T_m}{T_a}\right)^{0.17} \right)^{-2} \frac{E^2}{k_a B} D_2 \quad \text{E.(41)}$$

where

$$D_2 = 0.17 \left(-\left(\frac{T_s^{6.88}}{2T_a} + \frac{T_s^{5.88}}{2} \right)^{-0.83} \frac{T_s^{6.88}}{2T_a^2} - \left(\frac{T_a^{4.88} T_s}{2} + \frac{T_a^{5.88}}{2} \right)^{-0.83} \left(4.88 \frac{T_a^{3.88} T_s}{2} + 5.88 \frac{T_a^{4.88}}{2} \right) \right)$$

The uncertainty in each parameter is:

The uncertainty in voltage is determined in Section 10. $w_E = \pm 0.002$ V.

The uncertainty in w_{k_s} is determined in Section 12.

The uncertainty in w_{v_s} is determined in Section 13.

The tolerance of the wire diameter $d = 5\mu\text{m}$ of the hot-wire is cited to be $\pm 10\%$ based on 3σ value [E.6]. Thus, the uncertainty based on 1σ value is $w_d = 0.1667\mu\text{m}$ or $\pm 3.33\%$.

The uncertainty in w_{T_s} is described in Section 11.

The uncertainty in w_{T_a} is described in Section 10.

The uncertainty in the measured velocity is:

$$w_{u_m} = \sqrt{\left(\frac{\delta u_m}{\delta E} w_E\right)^2 + \left(\frac{\delta u_m}{\delta k_s} w_{k_s}\right)^2 + \left(\frac{\delta u_m}{\delta v_s} w_{v_s}\right)^2 + \left(\frac{\delta u_m}{\delta d} w_d\right)^2 + \left(\frac{\delta u_m}{\delta T_s} w_{T_s}\right)^2 + \left(\frac{\delta u_m}{\delta T_a} w_{T_a}\right)^2} \quad \text{E.(42)}$$

The calculation yields the results in the table on the following page:

Power	Open room test		Water cooled test		Transient test	
	w_{vm} [m/s]	w_{vm} [%]	w_{vm} [m/s]	w_{vm} [%]	w_{vm} [m/s]	w_{vm} [%]
100	0.0433	4.3	0.0254	3.2		
200	0.0434	3.8	0.0247	2.9	0.0455	4.8
300	0.0438	3.7	0.0261	2.6		
400	0.0463	3.5	0.0260	2.8	0.0626	5.6
500	0.0459	3.6	0.0258	2.9		
600	0.0459	3.5	0.0264	2.9	0.0842	6.6
700	0.0458	3.5	0.0270	3.1		

15. Velocity error. Combine the error in the measured velocity with the error of calibration:

$$w_u = \sqrt{(w_{u_i})^2 + (w_{u_c})^2} \quad \text{E.(43)}$$

The uncertainties found are presented in the following table:

Power	Open room test		Water cooled test		Transient test	
	w_u [m/s]	w_u [%]	w_u [m/s]	w_u [%]	w_u [m/s]	w_u [%]
100	0.149	14.9	0.178	22.6		
200	0.137	12.1	0.164	19.1	0.156	16.4
300	0.133	11.1	0.149	15.4		
400	0.127	9.6	0.152	16.2	0.146	13.1
500	0.129	10.1	0.157	17.4		
600	0.128	9.8	0.158	17.5	0.147	11.5
700	0.128	9.9	0.161	18.2		

16. Mass flow rate uncertainty. Mass flow rate is:

$$m = \rho u A \quad \text{E.(44)}$$

Differentiation with respect to variables:

$$\frac{\delta m}{\delta \rho} = u A \quad \text{E.(45)}$$

$$\frac{\delta m}{\delta u} = \rho A \quad \text{E.(46)}$$

$$\frac{\delta m}{\delta A} = \rho u \quad \text{E.(47)}$$

The uncertainty is:

$$w_m = \sqrt{\left(\frac{\delta m}{\delta \rho} w_p\right)^2 + \left(\frac{\delta m}{\delta u} w_u\right)^2 + \left(\frac{\delta m}{\delta A} w_A\right)^2} \quad \text{E.(48)}$$

The uncertainties found are presented in the following table:

Power	Open room test		Water cooled test		Transient test	
	$w_m \cdot 10^4$ [m/s]	w_m [%]	$w_m \cdot 10^4$ [m/s]	w_m [%]	$w_m \cdot 10^4$ [m/s]	w_m [%]
100	1.640	15.5	2.019	23.0		
200	1.514	12.8	1.876	19.7	1.700	16.9
300	1.471	12.1	1.703	16.2		
400	1.414	10.0	1.739	17.1	1.561	13.6
500	1.432	11.2	1.794	18.5		
600	1.419	10.3	1.795	18.4	1.550	12.1
700	1.423	10.7	1.822	19.0		

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APPENDIX F

Hot-wire temperature compensation procedure

F.1. Theory

The temperature compensation method of Abdel-Rahman et al. [F.1], which gives satisfactory results over a low range of velocities (0.35-9 m/s) and a large range of temperatures (22-98°C), is used for the temperature compensation of hot-wire measurements in the experiments. Abdel-Rahman et al. based their method on the Collis and Williams equation for heat transfer from the sensor:

$$Nu \left(\frac{T_w}{T_s} \right)^{-0.17} = A + B Re^a \quad \text{F.(1)}$$

where

$$Re = \frac{ud}{\nu_s} \quad \text{F.(2)}$$

and

$$Nu = \frac{h_s d}{k_s} \quad \text{F.(3)}$$

The temperature dependence of the air properties is expressed by equations of Morrison [F.2] applied at the mean film temperature T_m :

$$\frac{k_a}{k_{ar}} = \left(\frac{T_m}{T_{ar}} \right)^{0.86} \quad \text{F.(4)}$$

$$\frac{\nu_a}{\nu_{ar}} = \left(\frac{T_m}{T_{ar}} \right)^{1.9} \quad \text{F.(5)}$$

Assuming that conduction losses at the end of the sensor are small compared to convection losses, and that radiation losses are negligible, the heat balance on the hot-wire may be written as:

$$\frac{E^2 \rho_r}{R_s A_s} = \pi d h_s (T_s - T_a) \quad \text{F.(6)}$$

Substituting this equation into the equation F.(3) and using the relationship for the sensor resistance:

$$R_s = \frac{\rho_r l}{A_s} \quad \text{F.(7)}$$

the Nusselt number related to the hot-wire sensor voltage is obtained:

$$Nu = \frac{E^2}{R_s \pi l k_a (T_s - T_a)} \quad \text{F.(8)}$$

Collecting the constants π, l and R_s into the coefficients A and B , the Collis and Williams may be rearranged into the form:

$$Nu \left(\frac{T_m}{T_a} \right)^{-0.17} = \frac{E^2}{k_a (T_s - T_a)} \left(\frac{T_m}{T_a} \right)^{-0.17} = A' + B' \left(\frac{\rho u d}{\mu} \right)^n \quad \text{F.(9)}$$

The coefficients A' , B' , and n are determined by calibration of the hot-wire and a consequent least squares fit to calibration data with the exponent n as a variable parameter as described in [F.3]. For each guessed n , the coefficients A' and B' are calculated and then the standard deviation ϵ is found using the equation:

$$\epsilon = \left[\frac{1}{N} \sum_{i=1}^N \left(1 - \frac{u_c}{u_i} \right)^2 \right]^{\frac{1}{2}} \quad \text{F.(10)}$$

where

u_c is the calculated velocity

u is the measured velocity

N is the number of calibration points.

The set of A' , B' , and n with the minimum deviation is used as the calibration function.

F.2. Step-by-step calibration procedure

1. Calibrate the temperature sensor for temperature, i.e. record a voltage change as a function of temperature. Then fit curve to calibration data. the function should be linear.

The calibration equation obtained in this work is:

$$T_a = 255.325 - 50.585 * E \quad \text{F.(11)}$$

2. Calibrate the velocity sensor for temperature, i.e. record a hot-wire resistance change as a function of temperature. Then fit curve to calibration data, the function should be linear.

The calibration equation found in this work is:

$$R_p = 3.776 + 0.0154 * T_a \quad \text{F.(12)}$$

3. Calibrate the velocity sensor for velocity, i.e. record a voltage change with velocity. Then find constants A' , B' , and n , for the minimum standard deviation ϵ using the least squares method.

The following constants were found for the velocity hot-wire sensor in this work:

A'	-0.38829
B'	1.8884
n	0.15

F.3. Step-by-step measurement procedure

The following steps are done by subroutine HWAGET in data acquisition program SLOWPOKE:

1. The temperature sensor voltage is measured.
2. The velocity sensor voltage is measured.
3. The air temperature is found from the temperature sensor voltage using the temperature sensor calibration curve.
4. The velocity sensor resistance R_C at the temperature found in step 3 is found using the velocity sensor calibration function for temperature.
5. The hot-wire sensor temperature T_s is calculated using the equation:

$$T_s = \frac{\frac{R_s}{R_f} - 1}{\alpha} + T_a \quad \text{F.(13)}$$

6. The properties of the air at the film temperature T_m are calculated using the Morrison equations [F.2]:

F.6

$$k_a = k_{ar} \left(\frac{T_m}{T_{ar}} \right)^{0.86} \quad \text{F.(14)}$$

$$v_a = v_{ar} \left(\frac{T_m}{T_{ar}} \right)^{1.9} \quad \text{F.(15)}$$

7. The air velocity is calculated using the rearranged equation F.(9):

$$u = \sqrt{\frac{\frac{E^2}{k_a(T_s - T_e)} \left(\frac{T_m}{T_e} \right)^{-0.17} - A}{B}} \frac{\mu}{\rho d} \quad \text{F.(16)}$$

References

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F.2. Morrison, G.L., *Effects of Fluid Property Variations on the Response of Hot-wire Anemometers*, Journal of Physics E: Scientific Instrumentation, Vol.7, pp.434-6, 1974.

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APPENDIX G

Experimental Procedure

G.1 Fully Transient Test Procedure

1. Adjust "Ref-set" knob on the hot-wire anemometer measuring the temperature to read the correct temperature. To do this, use a mercury thermometer in close vicinity of temperature probe.
2. Align the hot-wire probe shield with the end wall of the velocity duct.
3. Turn on the power supply of the stepper motor.
4. Use the program "MOTOR.BAS" to move the probe to the start position.
5. Place the control of the hot-wire anemometer measuring the velocity into the "Run" position.
6. Load the program "SLOWPOKE.BAS" into the QuickBASIC environment and

execute it.

7. The program asks for permission to overwrite the old output files and then writes "To start, press any key" on the screen.

8. At the same time, set power to heaters to the desired level and hit any key on the computer keyboard to start the measurement.

9. Stop the program any time by using "Control-Break" keys on keyboard.

G.2 Water Cooled Steady State Test Procedure

1. Make sure that the tank is bolted to the concrete wall and that the gasket between the tank flange and the concrete wall is not used.

2. Fill up the air gap with water and watch the water level in the air gap (it takes approximately 10 minutes with fully opened valve). Once the water approaches the top, adjust the desired water flow rate level.

3. Follow steps 1 to 9 of the "Fully transient test procedure" as described in F.1.

G.3 Procedure of the Test with the Test Section in Open Room

1. Follow steps 1 to 9 of the "Fully transient test procedure" as described in F.1.

END

1 8-0 7-9 4

FIN