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MASTER

PRELIMINARY ANALYSIS OF THE IMPACT OF THE
GENERAL ELECTRIC WAVE BEAM GRID SPACER ON
GCFR CORE PERFORMANCE

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STAGGERED WAVE BEAM GRID SPACER ASSEMBLY

CONTENTS

This report consists of the following three separate analyses:

1. Calculation of a Pressure Loss Coefficient for a GCFR Staggered Wave Beam Grid Spacer, - R. Yahalom
2. Effect of Staggered Wave Beam Grid Spacer on Local Clad Temperature Distribution - R. Yahalom, P. Brown
3. Preliminary Assessment of the Effect of Duct Dilation on Using the Staggered Grid Spacer for the GCFR - S. Kaplan

CALCULATION OF A PRESSURE LOSS COEFFICIENT FOR A GCFR STAGGERED WAVE BEAM GRID SPACER

INTRODUCTION

One of the advantages of a grid spaced assembly is the potential reduction in the overall bundle pressure drop. The flow area is less restricted in the staggered wave beam grid than in the full support plate, but four times as many grid layers are required to provide the equivalent support. Still, an overall reduction pressure drop is expected. This memo develops a simple method to calculate the grid frictional pressure drop coefficient.

METHOD

The approach is to represent the flow area in the spacer region by an equivalent duct flow area and calculate the overall pressure loss coefficient by applying expansion and contraction pressure loss formulas. This pressure loss will be added to the bare bundle pressure drop to determine the overall pressure drop of the sub-assembly.

The following schematic diagram (Figure 1) shows a Wave Beam Grid Spacer and the model for an equivalent duct.

Figure 1. Wave Beam Spacer and Equivalent Flow Areas

A_1 is obtained by subtracting from the flow area the beam thickness blocking area. A_2 is derived by assuming an effective beam thickness equal to the line of sight. Detailed formulas are given in Appendix A. The pressure loss for a sudden contraction and expansion is given in Reference 1 pp. 98 and 117. The loss coefficients for stations 1 and 4 are

$$K_1 = 0.5 (1 - A_1/A_0) (A_0/A_1)^2 \quad (1)$$

and

$$K_4 = (1 - A_1/A_0) (A_0/A_1)^2 \quad (2)$$

normalized to bare
bundle gas velocity

for the convergent section we apply the following formula (see p. 96 Ref. 1)

$$K_2 = f(l/De, \delta) (1 - A_2/A_1) (A_0/A_2)^2 \quad (3)$$

$f(l/De, \delta) = 0.24$ is an estimated high value for the present application.

The following formula is used for the divergent part (Ref. 1 p.171)

$$K_3 = 3.2 (\tan \delta)^{1.25} (1 - A_2/A_1)^2 (A_0/A_2)^2 \quad (4)$$

Note that the frictional terms are omitted in (3) and (4). The overall friction loss of one layer of beam spacers is determined as follows:

$$K_f = f L_A \left[(A_o/A_1)^2 / De_1 - 1/De_o \right] \quad (5)$$

where f is a friction factor determined from the Moody diagram and L_A is the axial beam length. De_1 is the hydraulic diameter of the fuel rod bundle with grid spacers, and De_o is the hydraulic diameter of the bare bundle. This expression represents the frictional loss contribution due to one layer of spacers.

A grid spacer consists of two layers of beams, thus the overall grid pressure loss coefficient is,

$$K_G = 2 (K_1 + K_2 + K_3 + K_4 + K_f) \quad (6)$$

Note that all coefficients are normalized to bare bundle flow velocity.

VERIFICATION

General Electric performed water tests and measured pressure drop across a staggered wave beam grid. Figure 2 (from Reference 2) shows experimentally determined grid loss coefficients as a function of Reynolds Number. The foregoing formulas have been applied to the grid spaced driver assembly (GSDA) using the data from Reference 2. See Appendix B for geometrical parameters. The result (denoted by a square) is shown on Figure 2. The calculated coefficient is within the scattered data. It is however, about 15% higher than the average GSDA experimental result. The pressure loss coefficients are listed in Table I.

GCFR

Appendix B includes geometrical parameters for a gas cooled fuel sub-assembly. The calculated grid pressure loss coefficient is 0.321. Table I shows the various pressure drop coefficients. The relative contribution of the friction term to the overall coefficient is less in the GCFR (25%) than in the GSDA (40%) which has a more compact geometry.

Figure 2 Experimentally Determined Grid Loss Coefficient for Staggered Grid

TABLE I. CALCULATED PRESSURE LOSS COEFFICIENTS

	<u>GSDA</u>	<u>GCFR</u>
K_1	0.0266	0.0137
K_2	0.0178	0.0579
K_3	0.0007	0.0218
K_4	0.0532	0.0274
f	0.0235	0.0190
K_f	0.0657	0.0400
K_G	0.328	0.321

OTHER DATA

Figure 3 (From Reference 3) shows spacer pressure drop coefficients obtained experimentally for a three-point contact grid. Considerable reduction in loss coefficients were obtained by streamlining (rounding) the leading edges of the grid.

CONCLUSIONS

A simplified method to calculate grid pressure drop coefficient has been developed and partially verified. Results from water test data indicate that the method may overpredict by as much as fifteen percent. Since the calculation method includes all the important geometrical parameters as well as Reynolds Number, it can be used to compare alternative designs.

REFERENCES

1. Handbook of Hydraulic Resistance - Coefficients of Local Resistance and of Friction, I. E. Idel'chik, AEC - TR-6630.
2. "Experimentally Determined Flow Induced Vibration and Pressure Drop Characteristics of an FTR Staggered Grid Spaced Fuel Assembly," M. L. Canady. GEAP-14080, UC-74B, February 1976.
3. "The Hexagonal Bundle Heat Transfer and Fluid Flow Experiment AGATHE Hex", M. Hudina and G. Markoczy, Nuclear Engineering and Design 40 (1977) 121-131.

Cross section of 37-rod bundle 1 with corner of spacer details.

P/D = 1.3
 Length ≈ 20
 Dimensions in mm

Figure 3

- APPENDIX A
AUXILIARY FORMULAS

The bare bundle flow area is

$$A_0 = 1.5a^2\sqrt{3} - n\pi d^2/4$$

A - 1

Grid flow areas are

$$A_1 = A_0 - L_B t$$

A - 2

$$A_2 = A_0 - L_B L_S$$

A - 3

where the total length of all the beams in one layer is

$$L_B = p(1.5n_1^2 - 0.5)$$

A - 4

$$n_1 = (3 + \sqrt{12n - 3})/6$$

A - 5

and the line of sight is

$$L_S = p\sqrt{3}/2 - d$$

A - 6

The subassembly hydraulic diameter is

$$De_0 = 4A_0/P_0$$

A - 7

where the wetted perimeter is

$$P_0 = 6a + n\pi d$$

A - 8

The hydraulic diameter of a subassembly with a grid spacer is

$$De_1 = 4A_1/P_1 \quad A - 9$$

where

$$P_1 = P_o + 2L_B \quad A - 10$$

From the following sketch a relation can be derived for a small angle δ .

$$\delta = \frac{c - \sqrt{c^2 - 32(L_s - t)R}}{8R} \quad A - 11$$

APPENDIX B

Geometric Parameters for GE - GSDA (water) and GCFR Assemblies

		<u>GSDA</u>	<u>GCFR</u>
A_0 = Bare Bundle Flow Area	in ²	7.26	25.37
A_1 = Grid Flow Area	in ²	6.91	24.71
A_2 = Reduced Grid Flow Area	in ²	6.50	20.72
a = Hexagon Side	in	2.50	4.23
c = Crest of Wave Beam	in	0.375	0.6
d = Fuel Pin Diameter	in	0.23	0.315
De_0 = Bare Bundle Hydraulic Diameter	in	0.168	0.418
De_1 = Grid Hydraulic Diameter	in	0.114	0.263
L_A = Beam Axial Length	in	0.75	1.3
L_B = Total Beam Lengths in One Layer	in	35.2	66.5
L_S = Line of Sight	in	0.0216	0.070
n = Number of Fuel Pins		217	271
n_1 = Number of Pins Along One Side of Hex		9	10
P = Pin Pitch	in	0.2905	0.445
R = Radius of Curvature of Wave Beam	in	0.10	0.15
t = Beam thickness	in	0.010	0.010
δ = Wave Beam Angle	deg	5	15.5

EFFECT OF STAGGERED WAVE BEAM GRID SPACER ON LOCAL CLAD TEMPERATURE DISTRIBUTION

INTRODUCTION

The wave beam spacer has been considered for application in the GCFR fuel sub assembly. A GCFR fuel assembly is less compact than an LMFBR assembly, resulting in a more pronounced wave profile. The wave beam spacer alters the flow in the bundle, thereby changing the heat transfer characteristics of the gas flow. This memo considers the effect of the spacer on the local fuel clad temperature resulting from a change in the bundle flow field.

ANALYSIS

The flow field around the spacers and the fuel pins is very difficult, if not impossible, to predict exactly. Thus we have to assume first a flow pattern around the spacers. We shall assume the flow depicted by the sketch below.

SECTION A-A

This flow pattern is at the plane of spacer/pin contact point only, point A. At a different point (e.g. point B) the spacer does not touch the pin and the flow around the fuel pin is predominantly in the axial direction.

The width of the flow region that is characterized by the above sketch can be estimated by considering stagnation flow around the contact point. The flow velocity close to a stagnation point is $u=(x/R)U$ where x is the distance from the stagnation point, R is the distance at which $u=U$ (e.g. $R \approx$ channel hydraulic radius) and U is the average bare bundle velocity. If we assume that the flow is stagnant when $u/U < 0.01$ then $x \sim 0.002R$. The width of the above flow region can be estimated to be $2x$. The sketch below shows the relation between x and the angle α .

For the thermal analysis that follows we assume conservatively that the width is stagnant and heat is being transferred by gas conduction only. In reality, we expect strong cross flow currents resulting in substantial clad cooling.

Thermal Model

Figure 1 shows a network of nodes modeling two fuel pins, a wave beam spacer and the helium gas flow. There are seven axial positions and each pin has three circumferential nodes. The insert (section A-A) in Figure 1 shows the circumferential position of these nodes. The nodes are inter-connected by heat transfer connectors. This network is set up so that the node

temperatures can be calculated using the General Electric computer code THAP. See Appendix A.

Data

Tables I and II list the data used in the analysis. Part of the data depends on the α which is a measure of the stagnation region width. This region thickness is assumed to be equal to the height of the pin roughness: 0.004". The peak fuel power generation is 13, kW/ft.

Results and Discussion

The clad temperature distribution for the bare bundle case is shown in Figure 2. The effect of the spacer on the clad temperature is shown in Figure 3. $\alpha=1^\circ$ corresponds to a stagnant area three times larger than what has been estimated above. Consequently, the clad temperature is higher than anticipated. The temperature rise is 49°F. The case of $\alpha=5^\circ$ is depicted to show the effect of α on the clad temperature rise.

Figure 4 shows the complete temperature map for the $\alpha=1^\circ$ case. Note that the spacer helps to conduct heat away from the hot spots. Thus, the better the contact between the spacer and the pin the lower the clad temperature. The gap between the spacer and the pin is expected to be smaller than the 0.004 in. assumed here.

Conclusion

The present analysis predicts a local clad temperature rise of about 50°F due to the wave beam spacer. This higher-than-expected temperature increase comes from considering stagnant regions and peak power generation. It is judged that due to the complexity of the physical problem only experimental data will yield more meaningful information than is furnished in this analysis.

FIGURE 2 TEMPERATURE DISTRIBUTION FOR FUEL PIN WITHOUT SPACER GRID

LOCAL EFFECT OF GRID SPACER ON CLAD TEMPERATURE

FIGURE 3

TEMPERATURE DISTRIBUTION FOR FUEL PIN WITH SPACER GRID; $\alpha = 1^\circ$

FIGURE 4

TABLE I - CONNECTORS DATA

Material	Connectors	Conductivity K (Btu/ ft.hr.°F)	Heat Transfer Coefficient h (Btu/ ft ² .hr.°F)	Conductance Length L (ft)	Heat Transfer Area A (ft ²)	THAP INPUT	
						Admittance Y (Btu/hr.°F)	Key
He flow (fluid)	1,2,3,4,5,6					576	
SS316	7,10,13	10		0.0125	$\alpha \times 7.64E-7$	$\alpha \times 6.11E-4$	1
Clad Axial	8,9,11,12	10		0.025	$\alpha \times 7.64E-7$	$\alpha \times 3.06E-4$	1
He: Forced Convection, Clad-He	14,18,19,23 20 24,28,29,33 25,27,30,32 26,31 34,38,39,43 35,37,40,42 36,41		3600 3600 3600 3600 3600 3600 3600		$\alpha \times 5.71E-6$ $\alpha \times 8.56E-6$ $(60-\alpha) \times 1.71E-5$ $(60-\alpha) \times 2.57E-5$ $(60-\alpha) \times 3.42E-5$ $\alpha \times 1.14E-5$ $\alpha \times 1.71E-5$ $\alpha \times 2.28E-5$	$\alpha \times 2.06E-2$ $\alpha \times 3.08E-2$ $(60-\alpha) \times 6.17E-2$ $(60-\alpha) \times 9.24E-2$ $(60-\alpha) \times 0.123$ $\alpha \times 4.12E-2$ $\alpha \times 6.17E-2$ $\alpha \times 8.21E-2$	1 1 2 2 2 1 1 1
Stagnant He	15,17,22 16,21	0.16 0.16		0.00017 0.00017	$\alpha \times 8.56E-6$ $\alpha \times 1.14E-5$	$\alpha \times 8.06E-3$ $\alpha \times 1.07E-2$	1 1
SS316	44,48,49,53	10		$\alpha \times 4.58E-4$	2.08E-5	0.454/ α	3
Clad	54,58,59,63	10		0.0412- $\alpha \times 4.58E-4$	2.08E-5	E(I)=2.08E-4	4
Circum- ferential	45,47,50,52 55,57,60,62 46,51 56,61	10 10 10 10		$\alpha \times 4.58E-4$.0412- $\alpha \times 4.58E-4$ $\alpha \times 4.58E-4$.0412- $\alpha \times 4.58E-4$	3.13E-5 3.13E-5 4.17E-5 4.17E-5	0.683/ α E(I)=3.13E-4 0.910/ α E(I)=4.17E-4	3 4 3 4
He: Forced Convection, Beam-He	64,66		1800		4.63E-4	0.834	
He: Stagnant Bubble-Stream	65 67,68	0.16 0.16		0.003 0.003	9.3E-4 6.9E-4	0.0501 0.0375	

TABLE I - CONNECTORS DATA (continued)

Material	Connectors	Conductivity K (Btu/ ft.hr.°F)	Heat Transfer Coefficient h (Btu/ ft ² .hr.°F)	Conductance Length L (ft)	Heat Transfer Area A (ft ²)	THAP INPUT	
						Admittance Y (Btu/hr.°F)	Key
Contact Beam-Clad	69,70,71	0.16		3.3E-4	$\alpha \times 1.27E-6$	$\alpha \times 6.15E-4$	5
He: Stagnant, Bubble-Clad	72	0.16		0.003	$\alpha \times 1.14E-5$	$\alpha \times 6.16E-4$	1
Bubble-Beam Stagnant He	73	0.16		0.003	1.85E-3	0.100	
	74,75	0.16		0.003	1.39E-3	0.075	
Beam: Axial Cond.	76,79	10		0.0125	3.09E-4	0.247	
	77,78	10		0.0250	3.09E-4	0.124	
He: Forced Conv. Clad- Str.	80,81,82,83		3600		2.06E-3	7.425	

TABLE II - HEAT INPUT DATA

NODES	HEAT GENERATION (Btu/hr)	TEMPERATURE (°F)
8,12,45,49	589-9.82 α	
13,19,38,44	1177	
9,11,46,48	883-14.7 α	
14,18,39,43	6.53 α	
15,17,40,42	9.80 α	
16,41	13.1 α	
10,47	1178-19.6 α	
20,24,33,37	3.28 α	
21,23,34,36	4.92 α	
22,35	6.55 α	
1		800
13,38		959

APPENDIX

THAP - Thermal Hydraulic Analysis Program

INTRODUCTION

THAP (Thermal Hydraulic Analysis Program) is a general-purpose finite-difference heat-transfer computer program written in BASIC, which is intended for use on timeshare computer terminals.

This report describes the program and its use.

CAPABILITIES

The program is intended to perform heat-transfer calculations for problems which have been cast in network form. Problems can be in one, two or three dimensions. It has features built into it that enable it to handle ordinary modes of heat transfer, for either transient or steady-state cases. These modes include the following:

- a) ordinary conduction
- b) surface convection and radiation
- c) moving fluids interacting with conducting surfaces
- d) heat generation

Provision is made for computing parameters that change with temperature or time. In addition, constants may be computed by the program, which in some cases will lessen the amount of input data required.

Actually, the program is highly flexible and can accommodate virtually any thermal-hydraulic problem.

For transient calculations, the program calculates the time-derivatives of slowly-changing nodes less frequently than those of fast-changing nodes. This feature improves the efficiency of the program significantly.

FINITE-DIFFERENCE METHOD

This section contains a short description of the finite-difference method, for the purpose of identifying terms which are used in other parts of this report.

Basically, the finite-difference method is a numerical approach to solving the general heat transfer equation

$$\rho C_p \frac{\partial T}{\partial \theta} = k \left(\frac{\partial^2 T}{\partial x^2} + \frac{\partial^2 T}{\partial y^2} + \frac{\partial^2 T}{\partial z^2} \right) + \dot{q}$$

where ρ = density

C_p = specific heat

T = temperature

x, y, z = spatial coordinates

$\dot{q}$ = volumetric heat generation rate

Using the approximation

$$\frac{\partial^2 T}{\partial x^2} \approx \frac{T_{i-1} + T_{i+1} - 2T_i}{\delta_x^2}$$

where T_{i-1} , T_i , T_{i+1} are adjacent node temperatures

δ_x = separation of nodes

The equation can be re-written

$$\rho C_p \frac{\partial T_i}{\partial \theta} = k \sum_{x,y,z} \frac{(T_{i-1} - T_i) + (T_{i+1} - T_i)}{\delta^2} + \dot{q}$$

suppose δ_z represents the distance from $i-1$ to i and δ_r represents the distance from i to $i+1$. Then,

$$\rho C_p \frac{\partial T_i}{\partial \theta} = k \sum_{x,y,z} \left(\frac{T_{i-1} - T_i}{\delta \delta_z} + \frac{T_{i+1} - T_i}{\delta \delta_r} \right) + \dot{q}$$

This step is not rigorous, because from this point we shall not require that $\delta_z = \delta_r$. However, this change is ordinarily required in order to deal with actual objects. It does introduce an error in the calculations, and the accuracy of the results can depend significantly on how close δ_z is to δ_r .

$$(\delta_x \delta_y \delta_z) \rho C_p \frac{\partial T_i}{\partial \theta} = k \sum_{x,y,z} \left[\frac{\delta_x \delta_y \delta_z}{\delta \delta_z} (T_{i-1} - T_i) + \frac{\delta_x \delta_y \delta_z}{\delta \delta_r} (T_{i+1} - T_i) \right] + (\delta_x \delta_y \delta_z) \dot{q}$$

$$v \rho C_p \frac{\partial T_i}{\partial \theta} = k \sum_{j=1}^6 \frac{A_j}{\delta_j} (T_j - T) + v \dot{q}$$

$v = \delta_x \delta_y \delta_z$ [=Volume of node]
 A_j = heat path area

Capacitance is defined as

$$C = v \rho C_p$$

and form factor is defined as

$$F_j = \frac{A_j}{\delta_j}$$

Furthermore, admittance is defined as

$$Y_j = F_j k$$

Now,

$$C \frac{\partial T_i}{\partial \theta} = \sum_{j=1}^n Y_j (T_j - T_i) + v \dot{q}$$

Convection can be handled by treating it as though the heat transferred were generated at the surface node.

$$v \dot{q}_c \stackrel{\text{eq}}{=} h A_s (T_c - T_i)$$

where h = convection coefficient

A_s = surface area

$$C \frac{\partial T_i}{\partial \theta} = \sum_{j=1}^n Y_j (T_j - T_i) + h A_s (T_c - T_i) + v \dot{q}$$

$$C \frac{\partial T_i}{\partial \theta} = \sum_{j=1}^{n+c} Y_j (T_j - T_i) + v \dot{q} \quad Y_c \equiv h A_s$$

This is the equation solved in the finite-difference method. In order to use it, the customary approach is to divide the object being analyzed into discrete segments. A point location, called a node, is located at the center point of each segment, and connectors are made between the nodes to represent heat flow paths. An admittance can be determined for each connector, and each node has an associated temperature and capacitance. These quantities are the ones used in the finite-difference equation.

STEADY-STATE CALCULATIONS

In steady state calculations, the model is iteratively relaxed so that each node satisfies

$$\sum_{j=1}^n Y_j (T_j - T_i) + \dot{Q}_i = 0$$

where T_i = temperature of node i

Y_j = admittance for connector terminating at node i

T_j = temperature for node at other end of connector i

n = number of connectors terminating at i

$\dot{Q}_i$ = nodal heat generation rate

In the case of temperature-dependent Y , the admittances are iteratively calculated, based on the solution temperatures.

For fluid nodes, the same procedure is followed, except that each node is connected only to wall surface nodes and the node that lies upstream of it. Consequently, the solution satisfies the fluid heat balance.

$$\dot{w} C_p (T_{dn} - T_{up}) = \sum_{i=1}^n h_i A_i (T_i - T_{dn}) + \dot{Q}_{dn}$$

where $\dot{w}$ = mass flow rate

C_p = specific heat

T_{dn} = downstream node temperature

T_{up} = upstream node temperature

h_i = heat transfer coefficient

A_i = surface area

T_i = surface temperature

n = number of surfaces exchanging heat with fluid node

$\dot{Q}_{dn}$ = node heat-generation rate

TRANSIENT CALCULATIONS

In transient calculations, for solid nodes, the program integrates the function

$$\frac{dT_j}{d\theta} = \frac{\sum_{i=1}^n Y_i (T_i - T_j) + \dot{Q}_j}{C_j}$$

where T_j = node temperature

θ = time

n = number of connectors terminating at j

Y_i = admittance of connector i

$\dot{Q}_j$ = node heat generation

C_j = node thermal capacitance

T_i = temperature of node at other end of connector i

In the case of temperature-dependent Y 's and C 's, their values are re-calculated at regular intervals.

Since it is inefficient to calculate time derivatives for every node at each time step, the derivatives are calculated at various frequencies, depending on the time constant for the node concerned. The time constant is defined as $\frac{MC_p}{\sum Y_i}$, where M =node mass, C_p =specific heat, and Y_i =admittances of the connectors which intersect at the node.

The integration scheme used makes use of a half-interval shift so that the average derivative in the integration step is approximated by the derivative calculated 1.5 time steps from the preceding interval startpoint. That is,

$$T_{\theta+\Delta\theta} = T_{\theta} + \Delta\theta \left(\frac{dT}{d\theta} \right)_{\theta}^* + \frac{\Delta\theta}{2}$$

$$\left(\frac{dT}{d\theta}\right)_{\theta}^* + \frac{\Delta\theta}{2} = f\left(T_{\theta} + \frac{\Delta\theta}{2}, \dot{Q}_{\theta} + \frac{\Delta\theta}{2}\right)$$

$$T_{\theta} + \frac{\Delta\theta}{2} = T_{\theta-\Delta\theta} + \frac{3\Delta\theta}{2} \left(\frac{dT}{d\theta}\right)_{\theta}^* - \frac{\Delta\theta}{2}$$

where T is the whole set of node temperatures, $\left(\frac{dT}{d\theta}\right)^*$ is the set of temperature derivatives as calculated, and θ is time.

The time step used in the integration scheme is set at .3 times the minimum nodal time constant. This assures both stability and accuracy.

Fluid nodes are treated as parts of moving slugs. In this scheme, a geographic location for each node is determined that represents the location at the beginning of the integration interval of a particle which passes the node at the end of the integration interval. The temperature at that previous location at the beginning of the interval is found by interpolating from the bracketing nodes. Then, the temperature at the node at the end of the interval is based on that initial temperature, being changed to account for heat transfer to the fluid and heat generation in it.

SPECIAL FEATURES

The program includes an extrapolation routine based on the Aiken-Delta relation

$$T = T_2 + (T_2 - T_1)(T_3 - T_2)/(2T_2 - T_1 - T_3)$$

where T_1, T_2 and T_3 are succeeding recorded values of the temperature at the node being operated upon.

The stability condition that must be met before extrapolation occurs is as follows:

- a) Temperatures are recorded every tenth iteration.
- b) Successive temperatures must change in the same direction and each change must be smaller than the preceding one.

- c) A number of iterations equal to the total number of connectors plus 30 must be executed consecutively without failing criterion (b).

The extrapolation is applied to all nodes in steady-state calculations and to zero-capacitance nodes in transient calculations.

If a number of nodes are connected in series with no lateral connections, the connectors between them can be designated as a string. The program has special procedures for handling strings so that steady-state calculations can be performed very efficiently. To use this feature, the model has to be made so that the connectors in each string are numbered consecutively. The nodes, however, do not have to be numbered consecutively.

PRELIMINARY ASSESSMENT OF THE EFFECT OF DUCT DILATION ON USING THE STAGGERED GRID SPACER FOR THE GCFR

INTRODUCTION

A scoping study was performed to determine the effect of duct dilation of the GCFR duct upon grid spacer tab-to-duct recess engagement. The analysis is tentative in that the NSMH creep correlation is in a state of change. The current irradiation creep correlation for 20% cold-worked type 316 stainless steel, (Rev. 3, 5-5-78) is considered applicable only to N-lot steel. The recommended correlation has been Revision 5 of 6-6-78 which has been distributed with an advanced copy status but was not officially accepted. However, a Revision 6 is being formulated, which may be considerably different from Revision 5 for the low stress, bending behavior associated with duct dilation. The tentative analysis was performed for both revision 3 and revision 5.

Because the creep equation is being revised, it was considered acceptable to use approximate temperatures, fluences, and differential pressures as obtained from the references stated.

The analysis was performed for a downflow core using the data from References 1 and 2. From Reference 3, it was learned that the GCFR reactor designs have recently been changed to an upflow core.

DUCT ENVIRONMENT

Figure 1 shows the axial variations of the differential pressure across the duct wall, the duct temperature, and the duct fluence used for this study.

The pressure distribution is based on data from Reference 2 for a total ΔP across the fuel assembly of 42 psi. The discontinuity in the distribution is due to the change in cladding surface from smooth to roughened (to enhance heat transfer). Based on a breakdown of pressure losses across the assembly, provided in Reference 2, only a small pressure drop advantage may be obtained by using the staggered-grid system. Of the pressure drop across the bundle of 37.1 psi, 4.3 psi is from the grid spacers and 32.8 psi is from the bundle. Thus no preliminary estimate of reduced ΔP of staggered grids was made for this analysis. The pressure drop per inch of length along the roughened cladding surface is approximately 3 times that which occurs along the smooth cladding surface.

The axial temperature variation is based on an inlet temperature of 575° F and outlet temperature of 1030° F, the recommended values for a large GCFR as provided in a presentation of Reference 3. The core top and bottom duct temperatures are approximated considering both the effects of blankets and the typical ΔT between the inner coolant subchannels and the edge coolant subchannels. A linear variation approximation between top and bottom was arbitrary.

DUCT CONDITIONS FOR IRRADIATION ENHANCED CREEP ANALYSIS

Figure 1

The time-continuous analysis was conducted from B.O.L. to a peak axial fast fluence of 1.5×10^{23} . A presentation made by H. J. Snyder of GAC, Reference 3, states that the GCFR development program is based on qualifying the 20% cold-worked type 316 stainless steel assemblies for relatively low burnups, (≈ 50 Mwd/kg). The maximum value of 1.5×10^{23} n/cm² should exceed the value associated with that burnup. At the top and bottom of the core, a value of 40% of peak was selected, based on typical LMFBR flux distributions. A cosinusoidal variation was used.

METHOD OF ANALYSIS

The initial condition for the irradiation creep analysis was the deflected shape at B.O.L. as determined by the deflection equation of a semi-infinite clamped plate with a uniform normal load. The maximum deflection is given by

$$\delta = \frac{pL^4}{384 Et^3 \frac{1}{12(1-\nu^2)}}$$

where

p = pressure at location of interest

L = length of hex flat along hex I.D.,
corner-to-corner ($\frac{6.516''}{\sqrt{3}} = 3.76''$)

t = duct wall thickness (.150")

E = Young's modulus ($\sim 23 \times 10^6$ psi at elevated temperature)

ν = Poisson ratio (~ 0.3)

The time dependent deflection is then calculated by multiplying the elastic deflection by a factor of (1+EK), where K is the time dependent creep function as calculated from the NSMH creep correlation. To facilitate calculations for K, a computer code was written to be implemented via a time-share terminal.

An estimate was also made of the differential swelling between the grid spacer and the duct. The average temperature of the grid spacer is greater than that of the duct because of the coolant subchannel temperature variation. The temperature dependency of swelling is such that the grid spacer will thus swell more than the duct. To calculate the differential growth, a ΔT of 50° F was assumed. The objective of this analysis was to show the sensitivity of tab to recess engagement to swelling and not to rigorously quantify the expected differential growth. This exercise is considered useful in that one of the two creep correlations used (Rev.3) contains the swelling rate as a parameter.

DISCUSSION OF RESULTS

Figure 2 shows the "out-of-flat" duct wall radial deflection due to the irradiation creep enhancement of the pressure induced static deflection. The deflection associated with the Revision 5 creep correlation is large, and use of the current staggered wave-beam grid spacer may be inappropriate if such creep occurs. On the other hand, the deflection associated with the Revision 3 correlation is acceptable.

The current FTR GSDA design has a recess depth of 0.049 inches in a 0.120 inch thick duct wall, and a normal tab engagement of 0.031 inches. The GCFR duct has a 0.150 inch thick wall, which would accommodate a deeper recess, perhaps as much as an 0.079 inch deep recess. The associated increased grid spacer beam length (i.e., tab penetration in the recess) must be compatible with the grid spacer insertion process. The greater beam length (~50% longer for the GCFR grid spacer) and the use of higher beams (say ~1 to 1.25 inches compared to .75 inches for FTR GSDA) will accommodate a greater grid spacer distortion during the insertion process. However it would require a verification test program to determine that sufficient flexibility exists.

DUCT CREEP ENHANCED DILATION
 $(\phi t = 1.5 \times 10^{23}, E > 0.1 \text{ Mev})$

DUCT CREEP ENHANCED DILATION HISTORY
 (WORST AXIAL LOCATION)
 $(\phi t = 1.5 \times 10^{23}, E > 0.1 \text{ Mev})$

Figure 2

Figure 3 shows the sensitivity of the grid spacer-to-duct differential radial growth to the temperature range. It assumes a 50°F ΔT between the grid spacer and the duct. An initial clearance between the grid spacer tabs and duct recess bottoms is provided to accommodate the differential swelling. This is required for the corner regions which are unaffected by creep enhanced dilation. At the mid-lengths of the hex flats, the differential swelling will compensate for the loss of tab-to-recess engagement due to creep.

In the Introduction to this analysis, a brief discussion was presented on the tentative nature of the results in that the irradiation creep correlation for 20% cold-worked type 316 stainless steel is being updated. This is due in part, to account for the General Electric beam bending tests. These tests provide data more appropriate to the duct dilation problem than the pressurized tube tests, both because of the stress distribution and the stress magnitude range. A review of the data indicates that for the stress range of interest used in the GCFR grid spacer study, the creep rates will be considerably closer to that of Revision 3 than Revision 5.

Friction/Wear

The duty cycle of a GCFR assembly is such that relative motion occurs between the roughened fuel pin and the grid spacer. One of the matters of concern is friction between the fuel pin and spacer which might cause buckling of the pin or excessive wear on the pin and spacer surfaces. It is difficult to quantify this problem without some experimental work, but engineering intuition leads to the following judgement:

- a. The radius of curvature on the bend of the Wave Beam is 0.150 inch and the gap between the "ridges" of the roughened pin is 0.033 inch. Therefore, the bend on the Wave Beams would penetrate only 0.001 inch into the 0.004 gap between the ridges.
- b. The ridges of the roughened pin are rounded and the 10-mil thick Wave Beam should have enough flexibility that "binding" should not be a potential problem -- as it could be with a rigid perforated plate.

GRID SPACER-TO-DUCT DIFFERENTIAL RADIAL SWELLING

Figure 3

The preliminary conclusion is that friction and wear should not be a significant problem with the Staggered Wave Beam Grid Spacer.

REFERENCES

1. KWU Fuel Element Drawing Package
2. "Fuel Element Design for the 300 MW(e) Gas-Cooled Fast Breeder Reactor" by A. R. Veca et al, Nuclear Engineering and Design 40 (1977) 81-99, North Holland Publishing Company.
3. GCFR Technical Review Sponsored by DOE and HBA, Rancho Bernardo Inn, May 30-June 1, 1979.

CONTENTS

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This report consists of the following three separate analyses:

1. Calculation of a Pressure Loss Coefficient for a GCFR Staggered Wave Beam Grid Spacer;
~~R. Yahalom~~
2. Effect of Staggered Wave Beam Grid Spacer on Local Clad Temperature Distribution; *and*
~~R. Yahalom, P. Brown~~
3. Preliminary Assessment of the Effect of Duct Dilation on Using the Staggered Grid Spacer for the GCFR. ~~S. Kaplan~~

EDB	☒
ERA	☒
EPA	☐
WDA	☐

INS	☐
RIP	☐
GAP	☐
CRF	☐

TTR ☐
DOE ☒
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