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ENGINEERING HOT CHANNEL FACTORS FOR MIXED CARBIDE
AND NITRIDE FUELS IN 5000 MWt LMFBRs

by

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Components Technology Division

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ABSTRACT

This report presents the engineering hot channel factors for the fuel temperature corresponding to the breeding performance and pin diameter optimization studies for mixed carbide and nitride fuels in LMFBRs. A discussion of the methodology employed and the rationale behind the selection of each contributor to the hot channel factors are also included.

1. Motivation and Scope

The object of the present work is to provide an estimate of the engineering hot channel factors for the reference cores considered in the breeding performance and pin diameter optimization studies presented in Reference 1. Both carbide and nitride fuels under nominal operating conditions are considered, with helium or sodium bond. The following parameters are common to all cases:

Power:	5000 MWt
Maximum sodium velocity:	25 ft/sec
Sodium temperature rise across core:	300°F
Sodium outlet temperature	1050°F
Number of pins per assembly:	169
Pellet density:	95% T.D.

All helium-bonded fuels are assumed to have spiral wire spacers, with fin-type spacers for the sodium-bonded pins. Other parameters are listed in Table I and further information provided in Reference 1.

Due to the preliminary nature of this study and since many significant features of the system (such as the inlet plenum configuration, performance characteristics of the control systems, etc.) are not known at this stage, use has been made whenever necessary of the results of past design efforts (References 2, 3 and 4) deviating from them only where the characteristics of our reference cores differed from those of the systems considered in them or where additional information was available. The rationale for adopting or updating each contributor to the hot channel factors, as given in these references is presented below, as well as the analysis supporting the updated values.

Table I. Characteristic Dimensions of Reference Cores

Helium-Bonded Fuels					Sodium-Bonded Pins*				
kW/ft	Pin O.D.	Cladding Thickness (in.)	Gap Width (in.)	Pitch Diameter	kW/ft	Pin O.D.	Gap Width (carbide)	Gap Width (nitride)	Pitch Diameter
14.81	0.3	0.019	0.007	1.238	25	0.3	0.006	0.007	1.370
14.81	0.35	0.022	0.005	1.164	25	0.35	0.005	0.006	1.273
14.81	0.4	0.026	0.005	1.115	25	0.4	0.005	0.005	1.205
14.81	0.45	0.028	0.005	1.081	25	0.45	0.005	0.005	1.157
18.52	0.3	0.019	0.008	1.309	30	0.3	0.012	0.009	1.439
18.52	0.35	0.022	0.007	1.215	30	0.35	0.009	0.007	1.328
18.52	0.4	0.026	0.005	1.155	30	0.4	0.007	0.006	1.250
18.52	0.45	0.028	0.005	1.113	30	0.45	0.006	0.006	1.194
22.22	0.4	0.026	0.006	1.194	35	0.3	0.020	0.013	1.505
22.22	0.45	0.028	0.005	1.144	35	0.35	0.015	0.011	1.380
					35	0.4	0.011	0.009	1.293
					35	0.45	0.008	0.008	1.229

*Cladding thickness for all sodium-bonded cores: 0.015"

2. Methodology

Uncertainties in the temperature distribution in the reactor core arise from uncertainties in the values of various basic parameters in the thermal analysis (such as material properties, expected manufacturing tolerances, etc.) and simplifications in the calculation procedures followed. The correction factors which take into account the differences between the model and the actual physical system will be referred to as subfactors.* Thus, if the deadband built into the control system is such that the reactor power is allowed to exceed the setpoint by 2%, this will result in a 2% increase in the coolant temperature rise, and the subfactor for the correction to the coolant temperature rise due to the power control band will be 1.02. The term hot channel (or hot spot) factor will be reserved for the ratio of the temperature differences within the system expected at the chosen confidence level to their uncorrected values as given by the thermal-hydraulic calculations.

Some of the subfactors exhibit random variations and must be combined statistically. Others are of a deterministic nature and are combined by multiplication. The specification of the variables which enter in the hot channel factor analysis (and, in some cases, their classification as statistical or deterministic) is based upon experience and engineering judgment. The breakdown of subfactors adopted here is that of Westinghouse's FTR analysis first presented in Reference 2 and also followed by Combustion Engineering (References 3 and 4). This breakdown is shown in Table II for the design studies of References 2 through 4, and in Table III for the present study.

The engineering subfactors affected by random variations were combined following the statistical error propagation theory. The basic equations, needed for later derivations, are as follows:

Let y be a function of several variables $x_1, x_2, \dots, x_n$ assumed to be statistically independent:

$$y = f(x_1, x_2, \dots, x_n)$$

Its Taylor series expansion about a nominal value

$$y_{\text{nom}} = f(x_{1 \text{ nom}}, x_{2 \text{ nom}}, \dots, x_{n \text{ nom}})$$

is given by

$$y = y_{\text{nom}} + \left(\frac{\partial f}{\partial x_1}\right)_{x_{1 \text{ nom}}} dx_1 + \left(\frac{\partial f}{\partial x_2}\right)_{x_{2 \text{ nom}}} dx_2 + \dots + \left(\frac{\partial f}{\partial x_n}\right)_{x_{n \text{ nom}}} dx_n \quad (1)$$

For a given system and operating condition, the quantities

$$y_{\text{nom}}, \left(\frac{\partial f}{\partial x_1}\right)_{x_{1 \text{ nom}}}, \left(\frac{\partial f}{\partial x_2}\right)_{x_{2 \text{ nom}}}, \dots, \left(\frac{\partial f}{\partial x_n}\right)_{x_{n \text{ nom}}}$$

*Following the nomenclature of, e.g., Refs. 2 and 5.

Table II. Subfactors and Hot Channel Factors for FFTF at
Rated Operating Conditions (from Ref. 2)

Contributor	Coolant 1	Film 2	Clad 3	Gap 4	Fuel 5
A. Direct					
1. Inlet Flow Maldistribution	1.05	1.012			
2. Intrasubassembly Flow Maldistribution	1.15	1.035			
3. Interchannel Coolant Mixing	0.99*				
4. Power Control Band	1.02	1.02	1.02	1.02	1.02
5. Wire Wrap Peaking**	--	2.00 (1.214)			
Direct Combination**	.219	2.137 (1.297)	1.02	1.02	1.02
B. Statistical					
1. Fissile Fuel Maldistribution	1.035	1.035	1.035	1.035	1.035
2. Power Level Measurement†	1.087	1.087	1.087	1.087	1.087
3. Rod Diameter, Pitch & Bow	1.011				
4. Film Coefficient		1.14			
5. Gap Coefficient††				1.47	
6. Fuel Conductivity					1.10
7. Clad Conductivity & Thickness			1.12		
Statistical Combination of Items 1-7	1.09	1.169	1.152	1.479	1.137
Products of Direct and Statistical*	1.334	2.498 (1.516)	1.175	1.509	1.160

*Worst condition

**Numbers in parenthesis should be used for calculating fuel temperatures

†Value is 1.371 at initial core conditions

††Based on max. gap; decreases for smaller gaps

Table III. Subfactors in the Reference 5000 MWt LMFBRs

Contributor	Coolant 1	Film 2	Cladding 3	Gap 4	Fuel 5
<u>A. Direct</u>					
1. Inlet Flow Maldistribution	1.05	Generated by THESAC			
2. Intra-subassembly Flow Maldistribution	Tables IV and V				
3. Interchannel Coolant Mixing	1.				
4. Power Control Band	1.02	1.02	1.02	1.02	1.02
5. Wire Wrap Peeling		1.214			
<u>B. Statistical</u>					
1. Fissile Fuel Maldistribution	1.035	1.035	1.035	1.035	1.035
2. Power Level Measurement	1.087	1.087	1.087	1.087	1.087
3. Rod Diameter, Pitch & Bow	1.011				
4. Film Coefficient		1.25			
5. Gap Coefficient				Tables IV & V	
6. Fuel Conductivity					1.16
7. Clad Conductivity & Thickness			Tables IV & V		

are known, whereas $dx_1, dx_2, \dots, dx_n$ are independent variables with standard deviations $\sigma_1, \sigma_2, \dots, \sigma_n$, and y is a linear combination of these variables. From statistical theory, the variance of a linear combination of independent variables $a_1u + a_2v + \dots$ is given by $a_1^2\sigma_u^2 + a_2^2\sigma_v^2 + \dots$ where $\sigma_u, \sigma_v, \dots$ are the standard deviations of $u, v, \dots$. Therefore, the variance of y is

$$\sigma_y^2 = \sum_{i=1}^n \left(\frac{\partial f}{\partial x_i} \right)^2 \sigma_{x_i}^2 \quad (2)$$

Obviously the formulas apply to multiples of the standard deviation and were used in this study at the 3 σ confidence level.

The thermal-hydraulic analysis code THESAC (References 6 and 7), which follows this method in its hot spot analysis, was used to combine the engineering subfactors and to generate an overall hot channel factor. The latter was taken as the ratio of the maximum expected to nominal value of the temperature difference between the fuel centerline at the hottest spot and the coolant at the inlet plenum. This factor is the required input to the parametric studies presented in Reference 1. Provision has been made in THESAC to input all the subfactors in Table II, with the exception of the inlet flow maldistribution subfactor for the coolant region (A1,1) and the power control band subfactors (A4,1) through (A4,5). These were taken into account by multiplying the coolant flow and the fissile zone power by $1/(A1,1)$ and $(A4,1)$, respectively.

Since both nitride and carbide fueled cores were to be considered, a modification was made in the existing fuel temperature calculation algorithm in THESAC, so as to allow for the different temperature dependence of the thermal conductivity in either fuel. This modification is described in the appendix.

3. Direct Subfactors

3.1 Inlet Flow Maldistribution

Variations in the inlet velocities of different subassemblies result from mismatch of orificing and from the pressure field in the inlet plenum. Since neither the orificing scheme nor the plenum configuration for the reference cores is known at this point, the FFTF value given in Table II has been adopted. These variations in inlet velocity have an effect on the heat transfer coefficient at the fuel pin surface which is given by subfactor (A1,2). The Nusselt number is computed in THESAC by means of the correlation

$$Nu = 6.66 + 3.126 (P/D) + 1.184 (P/D)^2 + 0.0155 Pe^{0.86} \quad (3)$$

where P/D is the pitch to diameter ratio and Pe the Peclet number. These parameters depend on the subassembly configuration, and the corresponding subfactor was found to vary from 1.011 to 1.017 for the sodium-bonded cores, and from 1.008 to 1.010 for the helium-bonded cores, compared with a value of 1.012 given in Table II for FFTF.

3.2 Intra-subassembly Flow Maldistribution

The combined effect of differences in flow area and hydraulic resistance between the inner and outer channels of a subassembly causes corresponding variations in the flow rate and coolant velocity. The precise way in which this happens is influenced by the system geometry and coolant properties and does not lend itself readily to calculation. It is usually assumed that the standard friction correlations for pipe flow are applicable to the subassembly channels when expressed in terms of an equivalent diameter, and that the same friction factor applies to all subchannels. Under these assumptions, the flow split between subchannels is computed from the bundle average velocity and the subchannel hydraulic diameters by means of equations of the form (Reference 8):

$$V_j = \frac{V_T A_T}{\sum_i N_i A_i \left(\frac{D_i}{D_j}\right)^\alpha} \quad (4)$$

where V_j is the axial velocity in channel j , V_T and A_T are the subassembly average velocity and total flow area, D_i and N_i the equivalent diameter and number of channels of type i (inner, edge or corner channels) and α equal to $(1+m)/(2-m)$ for a friction factor correlation of the form $f = c/Re^m$. Equation (4) predicts coolant velocities which are, in most cases, higher for the outer subchannels. It is only for very high values of the pitch to diameter ratio that the equivalent diameter of the edge channels is smaller than that of the inner channels, in spite of the larger flow area of the former. (Note that the hydraulic diameter is proportional to the ratio of flow area to wetted perimeter, and the latter decreases faster in the inner channels than in the outer ones as the pitch to diameter ratio increases.) This flow split model results in velocity distributions in disagreement with the available experimental data (see, e.g., References 9 and 10), which shows velocity variations smaller than those predicted theoretically. Thus the theoretical predictions for the 217 pin bundle of Reference 10 (0.25" O.D., 1.27 pitch to diameter ratio) give a velocity at the edge channels that is 10% higher than the subassembly velocity, whereas the experimental measurements showed no significant difference between them. In general, the flow split calculated by means of Eq. (4) tends to agree better with the experimental observation as the number of pins per assembly decreases (Ref. 12). The use of the theoretical model is, however, a conservative approach, since it implies an overestimation of the temperature in the hotter inside channels. Subfactors (A2,1) and (A2,2) in Table II were thus computed by means of Equations (4) and (3) for each of the reference cores and are presented in Tables IV and V. Subfactor (A2,1) shows the effect of the flow split on the coolant enthalpy rise and takes into account the effect of both area and velocity differences between subchannels. (A2,2) reflects the decrease in the film coefficient of the inner channels resulting from these velocity differences. In some cases a significant increase in the coolant enthalpy rise is accompanied by a negligible increase (or even a slight decrease) in the film temperature drop. This occurs for those cores with the highest pitch to diameter ratios, where, as indicated above, the outer channels have equivalent diameters which are almost equal to (or even slightly smaller than) those of the inner channels, while still having higher flow areas. To be consistently conservative, subfactor (A2,2) has been made equal to unity when it was found to be smaller than one.

Table IV. Subfactors for Sodium-Bonded Pins

Core kW/ft-Pin O.D.	Subfactors				
	A2,1	A2,2	B7,3	B5,4 (Nitride)	B5,4 (Carbide)
25-.3	1.119	1.001	1.120	1.323	1.366
25-.35	1.127	1.002	1.120	1.366	1.427
25-.4	1.136	1.003	1.120	1.427	1.427
25-.45	1.146	1.004	1.120	1.427	1.427
30-.3	1.115	1.000	1.120	1.268	1.224
30-.35	1.122	1.001	1.120	1.323	1.268
30-.4	1.130	1.002	1.120	1.366	1.323
30-.45	1.138	1.003	1.120	1.366	1.366
35-.3	1.112	1.000	1.120	1.214	1.180
35-.35	1.119	1.000	1.120	1.236	1.201
35-.4	1.125	1.002	1.120	1.268	1.236
35-.45	1.133	1.003	1.120	1.292	1.292
40-.3	1.113	1.000	1.120	1.292	---
40-.4	1.119	1.000	1.120	1.292	---
40-.45	1.126	1.002	1.120	1.292	---
40-.5	1.133	1.003	1.120	1.292	---

Note: These data supplement the data shown in Table III.

Table V. Subfactors for Helium-Bonded Pins

kW/ft-Pin O.D.	Core	Subfactors			
	A2,1	A2,2	B7,3	B5,4	
14.81-0.3	1.170	1.006	1.113	1.52	
14.81-0.35	1.171	1.005	1.110	1.38	
14.81-0.4	1.173	1.006	1.107	1.38	
14.81-0.45	1.191	1.006	1.106	1.38	
18.52-0.3	1.173	1.007	1.113	1.718	
18.52-0.35	1.169	1.006	1.110	1.52	
18.52-0.4	1.172	1.006	1.107	1.38	
18.52-0.45	1.179	1.006	1.106	1.38	
22.22-0.4	1.169	1.006	1.107	1.431	
22.22-0.45	1.173	1.006	1.106	1.38	

Note: These data supplement the data shown in Table III.

3.3 Interchannel Coolant Mixing

The effect of heat and mass exchange between adjacent subchannels is a reduction of temperature gradients throughout the subassembly. The magnitude of the mixing effect is a function of the subassembly configuration and power skew. Thus, the ENERGY code (Reference 11) defines the mixing in terms of a swirl velocity V_s around the periphery of the subassembly, and an effective eddy diffusivity ϵ . These two parameters are found to be functions of the rod pitch to diameter ratio and of the wire pitch to rod diameter ratio. When both V_s and ϵ are made equal to zero, an ideal "isolated channels" situation results. Extensive runs of ENERGY for the various core configurations under study showed only a very slight decrease (well under 1% for all the cases considered) of the enthalpy rise in the central channels when going from "isolated channels" (i.e., $V_s = \epsilon = 0$) to connected channels (i.e., using the values of V_s and ϵ recommended in Ref. 12 for the particular geometry under consideration). Furthermore, there are significant discrepancies in the subassembly temperature profiles predicted by the different thermal-hydraulic codes (Refs. 10 and 12). Hence it was decided to stay on the conservative side and not to take credit for this mixing effect. A value of 1 is entered for subfactor (A3,1) in Table III. (Note that since the effect of interchannel mixing is a reduction of the maximum coolant temperatures, as occurring in the inner channels, the corresponding subfactor would have a value of less than one. 0.99 is given in Table II for FFTF).

3.4 Power Control Band

In order to keep the control systems from nearly continuous operation, a dead band is built into them. As a result the power level will be allowed to exceed periodically the nominal value. A $\pm 2\%$ deadband is designed into the FFTF controls, resulting in a value of 1.02 for subfactors (A4,1) through (A4,5) in Table III. It is assumed that this value is applicable to our reference cores.

3.5 Wire Wrap Peaking

The presence of the wire wrap results in an increase of the clad temperature over its nominal value in the vicinity of the point of contact between cladding and wire. A value of 1.214 was recommended for FFTF when using the film coefficient subfactor in the computation of the fuel centerline temperature, and a value of 1.2 when using it in the computation of cladding temperatures. These values are the result of detailed solutions of the velocity field in the coolant region carried out by D. C. Kolesar and D. L. Koreis at Battelle (Reference 17). Clearly the effect is geometry dependent, but it is expected (Reference 13) that this dependence would not be very significant. This is so, particularly since this subfactor affects only the film temperature drop, which is very small.

4. Statistical Subfactors

4.1 Fuel Maldistribution, Power Measurement, and Rod Pitch and Bowing

The fuel maldistribution subfactors account for variations in fissile content per batch, fissile content per pellet, pellet diameter, and length of fuel per pin, all of which are dependent on fabrication tolerances.

The power level is measured by means of an energy balance on the loop flows and temperatures. Uncertainties on this balance result in uncertainties in the operating power. The values of 1.035 and 1.087 used in the FFTF analysis were preserved in this study.

It is usually assumed that any clearance due to loose spacing of the fuel pins will be distributed evenly within the duct. A rod pitch and bow in subfactor is then included to allow for deviations from this distribution. No assumptions were made in the reference study with regard to the pin clearance, and the FFTF value of 1.011 was used here. Even though this value is meant to be very conservative, it becomes negligible when combined statistically with the other coolant enthalpy rise subfactors.

4.2 Film Coefficient

A value of 1.25 was provided by Combustion Engineering for the Nusselt number correlation included in THESAC.

4.3 Gap Coefficient

The gap conductance variations are a result of changes in gap width and variations of conductance for a given width, both in the case of helium and sodium bonded pins.

The gap width tolerance results from the addition of manufacturing tolerances in the cladding and pellet. For ground pellets, past experience indicates a tolerance of ± 0.001 " for the gap width (Refs. 14 and 15). This tolerance is expected to be higher for commercial manufacturing and, consequently, a range of ± 0.002 " was adopted here for all the reference cores (The fabrication tolerances are independent of cladding and pellet diameters within a wide range of pin diameters.). The effect of a constant 2 mill range in the gap width will, of course, be different for the various gap sizes under consideration.

The conductance of sodium bonded pins is directly proportional to the sodium conductivity k (affected by a 15% uncertainty) and inversely proportional to the gap thickness x , hence $h \sim k/x$. Substituting this expression into Equation 2 results in the error propagation formula:

$$\frac{\sigma_h}{h} = \sqrt{\left(\frac{\sigma_k}{k}\right)^2 + \left(\frac{\sigma_x}{x}\right)^2} \quad (5)$$

In the case of helium bonded pins, the dependence of the gap conductance with cold gap size is presented in Figure 1. Within a neighborhood of a gap width value x , the gap conductance can be expressed as

$$h = h_1 - a(x - x_1) \quad (6)$$

where h_1 is the nominal value of h at $x = x_1$ and a the slope of h at that point. Substituting Equation (6) into (2) gives

Fig. 1 (from Ref. 2). Nominal Value (—) and Error Band (---) of Helium Filled Gaps

$$\frac{\sigma_h}{h_1} = \sqrt{\frac{1}{h_1^2} \sigma_{h_1}^2 + \frac{a^2}{h_1^2} \sigma_x^2}$$

(cf. Equation (5)).

The resulting values for the gap coefficient are presented in Table V. Because the slope of the helium gap conductance increases with gap size, a fixed 0.002" tolerance in the gap width results in a much higher uncertainty at large gap sizes (e.g., 1.718 for the 8 mill gap of the 18.52 kW/ft 0.30" OD core).

4.4 Fuel Conductivity

The values of thermal conductivity for carbide and nitride fuels recommended by Ref. 16 were adopted. Their use required changes to the code THESAC which are described in the appendix. An uncertainty of $\pm 15\%$ is quoted by Reference 16 for high density carbide fuel. According to the same reference, porosity effects decrease the thermal conductivity by a factor of

$$\frac{1 - p}{1 + (1 \pm 0.8)p}$$

For a porosity $p = 0.05$ the uncertainty in this correction factor is $\pm 4\%$, which combined statistically with the $\pm 15\%$ error in the conductivity of dense fuel results in a value of 1.16 for the (B6,5) subfactor of Table III. It is not possible to give an estimate of the uncertainty for the nitride conductivity because of lack of data (Ref. 16). Hence the carbide values were used for nitride fuels also.

4.5 Clad Conductivity and Thickness

The same basic considerations apply here as in the case of sodium bonded gap conductance. A fixed tolerance of ± 0.001 " in the cladding thickness is combined with a $\pm 10\%$ uncertainty in the cladding conductivity to yield the subfactor values of 1.12 for Na bonded pins and 1.06 to 1.113 for the He bonded pins given in Tables IV and V.

5. Hot Channel Factors

The subfactors presented above were input, together with the pertinent core data, to the code THESAC and a maximum expected fuel centerline temperature was obtained. The difference between this temperature and that of the inlet plenum is given in Tables VI through IX, labeled Maximum Temperature Rise. The code was then run using the same core data but with all engineering (i.e., non-nuclear) subfactors equal to one, which yielded a nominal temperature rise. This "nominal" temperature included the nuclear radial peaking factor and was taken at the axial position of maximum fuel centerline temperature. The ratio of the maximum to the nominal temperature rise is the overall hot channel factor presented in the same tables. The nominal temperature rises for each separate region (coolant, film, etc.) are also given, in order to

Table VI. Hot Channel Factors for Sodium-Bonded Carbide Cores

Core		Nominal Temperature Rises *					Max. * Temp. Rise	Hot Channel Factor
kW/ft-Pin O.D.	Coolant	Film	Clad	Gap	Fuel	Total	Total	
25-0.3	252	29	113	17	759	1168	1400	1.198
25-0.35	252	25	96	12	760	1145	1373	1.199
25-0.4	252	21	84	10	762	1128	1354	1.2
25-0.45	252	18	74	9	763	1115	1340	1.202
30-0.3	241	35	130	39	909	1354	1624	1.199
30-0.35	241	31	114	25	912	1322	1587	1.2
30-0.4	241	27	101	17	914	1299	1560	1.201
30-0.45	241	24	89	13	916	1283	1540	1.2
35-0.3	241	38	144	75	1047	1544	1855	1.202
35-0.35	241	35	128	49	1051	1504	1805	1.2
35-0.4	241	32	115	31	1055	1474	1770	1.201
35-0.45	241	29	104	20	1057	1450	1742	1.201
40-0.35	229	74	171	33	1319	1826	2205	1.208
40-0.4	229	62	149	29	1323	1792	2160	1.205
40-0.45	229	52	133	25	1327	1766	2126	1.203
40-0.5	229	45	119	23	1330	1746	2100	1.202

*Taken at the axial position of maximum fuel centerline temperature.

Table VII. Hot Channel Factors for Sodium-Bonded Nitride Cores

Core	Nominal Temperature Rises*						Max.* Temp. Rise	Hot Channel Factor
	kW/ft-Pin O.D.	Coolant	Film	Clad	Gap	Fuel	Total	
25-0.3		252	31	123	22	807	1236	1.198
25-0.35		252	21	101	38	809	1221	1.197
25-0.4		252	17	89	33	811	1203	1.197
25-0.45		252	19	82	10	815	1178	1.199
30-0.3		241	37	133	30	877	1317	1.206
30-0.35		241	32	115	20	881	1288	1.198
30-0.4		241	28	101	15	882	1267	1.198
30-0.45		252	23	88	13	875	1252	1.199
35-0.3		241	43	151	50	1010	1495	1.204
35-0.35		241	38	131	36	1015	1460	1.201
35-0.4		241	33	116	26	1019	1434	1.199
35-0.45		241	29	104	20	1021	1414	1.199
40-0.35		229	74	171	33	1279	1786	1.207
40-0.4		229	62	149	29	1282	1751	1.205
40-0.45		229	52	133	25	1286	1725	1.204
40-0.5		229	45	119	23	1289	1705	1.203

*Taken at the axial position of maximum fuel centerline temperature.

Table VIII. Hot Channel Factors for Helium-Bonded Carbide Cores

Core		Nominal Temperature Rises *					Max. * Temp. Rise	Hot Channel Factor
kW/ft-Pin O.D.	Coolant	Film	Clad	Gap	Fuel	Total	Total	
14.81-0.3	240	16	88	688	433	1465	1991	1.359
14.81-0.35	252	13	87	446	438	1236	1579	1.278
14.81-0.4	252	10	89	391	441	1183	1504	1.272
14.81-0.45	252	9	86	346	444	1136	1441	1.268
18.52-0.3	229	22	111	1134	517	2013	3017	1.498
18.52-0.35	241	18	109	733	534	1635	2208	1.350
18.52-0.4	241	15	113	493	547	1409	1795	1.274
18.52-0.45	241	13	108	437	550	1350	1712	1.268
22.22-0.4	229	20	137	662	640	1694	2198	1.297
22.22-0.45	241	17	130	525	649	1562	1985	1.270

*Taken at the axial position of maximum fuel centerline temperature.

Table IX. Hot Channel Factors for Helium-Bonded Nitride Cores

Core kw/ft-Pin O.D.	Nominal Temperature Rises*						Max.* Temp. Rise	Hot Channel Factor
	Coolant	Film	Clad	Gap	Fuel	Total	To	
14.81-0.3	241	16	88	687	416	1448	1974	1.364
14.81-0.35	252	13	86	446	420	1219	1559	1.279
14.81-0.4	252	10	86	391	423	1165	1484	1.273
14.81-0.45	252	9	86	346	426	1118	1420	1.270
18.52-0.3	229	22	111	1134	511	2006	3006	1.498
18.52-0.35	241	18	109	733	517	1617	2192	1.355
18.52-0.4	241	15	113	493	525	1387	1771	1.277
18.52-0.45	241	13	108	437	528	1328	1688	1.271
22.22-0.4	241	20	137	657	620	1674	2179	1.301
22.22-0.45	241	17	130	525	625	1538	1959	1.273

*Taken at the axial position of maximum fuel centerline temperature.

show the relative importance of each subfactor. Thus, it is seen that the hot channel factors for sodium-bonded pins exhibit very small variations for the different cores in spite of significant differences in the gap conductance subfactor. This is due to the small magnitude of the gap temperature drop with sodium bonds. For example, the sodium bonded carbide core with 25 kW/ft and 0.35 O.D. has a very high gap conductance subfactor (1.427), but since the gap temperature drop is only 12°F, the effect on the overall temperature rise is negligible. The 18.52 kW/ft 0.30" OD helium bonded cores have particularly high hot channel factors, as a result of very high values in both gap conductance subfactor and nominal gap temperature drop. It should be noted, however, that the determination of the gap conductance subfactor for these cores involved extrapolation of the gap conductance well beyond the range of the available data presented in Fig. 1. and therefore the values of these two hot channel factors may have been overestimated. Still, the uncertainty in the fuel temperatures, as reflected in the hot channel factors, is seen to be much higher for the helium bonded pins, mainly as a result of the low value and high uncertainty of the thermal conductance of helium filled gaps, as compared with those of sodium bonded gaps.

6. Effect of Temperature Uncertainties on Expected Fuel Pin Performance

It is seen from the above that the fuel pin temperatures can vary widely from the values predicted by the thermal-hydraulic analysis, due to uncertainties in the latter. This results in uncertainties in the fuel pin performance and lifetime. Although a detailed, time dependent analysis of this effect was felt to be beyond the scope of the study presented here, a cursory appraisal of the range of variability of the fuel and clad swelling rates was made.

The stresses resulting from mechanical interaction between fuel and cladding will be more severe the higher the fuel swelling rate and the lower the clad swelling. The swelling rate of the fuel increases with temperature, whereas that of the cladding decreases with temperature (within the range of cladding temperatures encountered in this study). Hence, the most unfavorable conditions will occur at the highest expected temperatures of both fuel and cladding. Tables X and XI show the ranges of variability of the maximum fuel and cladding temperatures, for both nominal and hot channel conditions. The maximum fuel temperatures were obtained by adding the maximum temperature rises presented in Tables VI through IX to the inlet temperature of 750°F. The corresponding fuel swelling rates (presented in Table X), were computed by means of the following correlations (Ref. 1):

$$\frac{\Delta V}{V}(\%) = B \exp [0.0009 (T_c - 800)]$$

for carbide fuel and for nitride fuel with $T_c > 1300^\circ\text{C}$, and where B is the burnup in atom percent and T_c the initial fuel centerline temperature in °C. And

$$\frac{\Delta V}{V}(\%) = 1.5 B$$

for nitride fuel with $T_c < 1300^\circ\text{C}$.

Table X. Ranges of Maximum Fuel Temperature ($^{\circ}\text{F}$) and Fuel Swelling Rate ($\frac{\Delta V}{V}$ (%) Per Percent Burnup) for Nominal and Expected Conditions

Fuel		Nominal Conditions	Expected Conditions
Na bonded carbide	temp.	1865 to 2576	2090 to 2955
	swelling	1.22 to 1.74	1.36 to 2.1
Na bonded nitride	temp.	1928 to 2536	2163 to 2906
	swelling	1.5 to 1.7	1.5 to 2.05
He bonded carbide	temp.	1886 to 2763	2191 to 3767
	swelling	1.23 to 1.9	1.43 to 3.15
He bonded nitride	temp.	1868 to 2756	2170 to 3756
	swelling	1.5 to 1.9	1.5 to 3.13

Table XI. Ranges of Maximum Clad Temperature ($^{\circ}\text{F}$) and Minimum Clad Swelling ($\Delta\text{V}/\text{V}$ (%)) for Nominal and Expected Conditions
(20% CW 316SS, fast fluence = 3×10^{23} nvt)

Fuel		Nominal Conditions	Expected Conditions
Na bonded carbide	temp.	1188 to 1282	1298 to 1467
	swelling	13.34 to 4.52	3.56 to 0.11
Na bonded nitride	temp.	1193 to 1333	1301 to 1459
	swelling	12.8 to 2.0	3.4 to 0.13
Helium bonded	temp.	1190 to 1221	1310 to 1345
	swelling	13.2 to 9.71	2.96 to 1.6

The maximum cladding temperatures were obtained by separate runs of the code THESAC. In these runs the subfactor for the correction to the film temperature rise due to wire wrap peaking was changed from the 1.214 value shown in Table III to a value of 2, following the recommendations of Ref. 17. The minimum values of clad swelling were then computed from the maximum cladding temperatures using the 20% CW316SS correlation given in Ref. 18. The ranges of maximum temperature and minimum swelling for both nominal and hot channel conditions are presented in Table XI.

As pointed out above the phenomena of fuel-cladding interaction are complex and time dependent. Some qualitative conclusions can be drawn, however, from the data presented in Tables X and XI. It is seen that the consideration of the fuel temperature uncertainties results in increases of the fuel swelling rates. Thus the swelling rate of the helium bonded carbide pins can be as high as 1.9% per percent burnup under nominal conditions, and is expected to increase to 3.15% under hot channel conditions. The minimum values of clad swelling are also affected by the hot channel factors. For example, the minimum values of clad swelling for Na bonded carbide pins range, under nominal conditions, between 13.34 and 4.52%, but can be expected to be as small as 3.56 to 0.11% under hot channel conditions. It would seem, therefore, advisable to use the hot channel pin temperatures when studying the fuel pin performance in order to get conservative estimates of the latter.

Appendix

Modifications to the Computer Code THESAC

In its original form, the thermal analysis code THESAC used in this study computed the fuel centerline temperature under the assumption of a constant thermal conductivity in the fuel. A wide range of variation was found in the fuel temperatures of the different cores under consideration (thus, for helium bonded nitride pins, the fuel temperature is seen from Table IX to range between 1443°F at the fuel surface in the 14.81 kW/ft 0.45" OD core, and 3756°F at the fuel centerline for the 18.52 kW/ft 0.3" OD core). In view of this, it was felt advisable to introduce some modifications to the code that would allow for the temperature dependence of the thermal conductivity. This modification is described below.

1. Fuel Centerline Temperature and Linear Heat Rate

The fuel centerline temperature T_1 is related to the linear heat rate q' by:

$$\frac{q'}{4\pi} = \int_{T_s}^{T_1} k dt \quad (1)$$

where T_s is the fuel surface temperature, given by:

$$T_s = T_2 + \frac{q'}{\pi D_p U_{eff}} \quad (2)$$

T_2 is the local sodium temperature, D_p the fuel pin diameter and U_{eff} an effective thermal conductance (Equation (6) of Ref. 6). Depending on the option chosen, Equations (1) and (2) must be solved for T_1 (if the sub-assembly power is input) or q' (if the maximum allowable temperature is specified). In either case, the resulting solutions take the place of Equations (4), (5) and (6) of Reference 6. Three options are now available when choosing the value of the conductivity k :

1.1 Constant Conductivity

This is the assumption made in the original code and results in the relationship between T_1 and q' given by Equations (4), (5) and (6) of Reference 6.

1.2 Conductivity Varying Linearly with Temperature

The thermal conductivity of carbide fuels can be represented by expressions of the form (Ref. 16):

$$k = a + bT \quad (3)$$

This, when combined with (1) and (2) results in the following equation in the linear heat rate:

$$\frac{bc^2}{2} q'^2 + (bcT_2 + ac + \frac{1}{4\pi}) q' + (aT_2 + \frac{b}{2} T_2^2 - A_1) = 0 \quad (4)$$

with:

$$c = \frac{1}{\pi D_p U_{eff}}$$

$$A_1 = aT_1 + \frac{b}{2} T_1^2$$

If the subassembly power input option in THESAC is chosen, the following equation in T_1 is obtained:

$$\frac{b}{2} T_1^2 + aT_1 - (\frac{q'}{4\pi} + \frac{b}{2} T_s^2 + aT_s) = 0 \quad (5)$$

Equations (4) and (5) have two real roots, but only one is positive.

1.3 Conductivity of Nitride Fuels

The thermal conductivity of nitride fuels increases with temperature in a non-linear manner and can be represented by a second order equation. Available data seems to indicate a leveling off of the conductivity increase at high temperatures; therefore a constant conductivity k_2 has been assumed for temperatures higher than a threshold T_b . Thus,

$$k = a + bT + dT^2 \quad \text{for } T < T_b \quad (6a)$$

$$k = k_2 \quad \text{for } T > T_b \quad (6b)$$

The resulting equation for q' is:

$$\begin{aligned} & \frac{d}{3} c^3 q'^3 + (\frac{b}{2} C^2 + dc^2 T_2) q'^2 + [(dT_2^2 + bT_2 + a)c + \frac{1}{4\pi}] q' + \\ & (\frac{d}{3} T_2^3 + \frac{b}{2} T_2^2 + aT_2) - A_2 = 0 \end{aligned} \quad (7)$$

where:

$$A_2 = \frac{d}{3} T_1^3 + \frac{b}{2} T_1^2 + aT_1 \quad \text{for } T_1 < T_b$$

$$A_2 = k_2(T_1 - T_b) + \frac{d}{3} T_b^3 + \frac{b}{2} T_b^2 + aT_b \quad \text{for } T_1 > T_b$$

and the resulting equation for T_1 is:

$$\frac{d}{3} T_1^3 + \frac{b}{2} T_1 + aT_1 - \left(\frac{d}{3} T_b^3 + \frac{b}{2} T_b^2 + a \right) - \frac{q'}{4\pi} = 0 \quad \text{for } T_1 < T_b \quad (8a)$$

$$T_1 = T_b + \frac{1}{k_2} \left[\frac{q'}{4\pi} - \left(\frac{d}{3} T_b^3 + \frac{b}{2} T_b^2 + a \right) \right] \quad \text{for } T_1 > T_b \quad (8b)$$

Equations (7), (8a) and (8b) are cubic in q' or T_1 and can have, in general, up to three real roots. It can be shown, however, that only the intermediate one has physical meaning, and the code will choose it for the value of heat rate or temperature.

A least square fit of the conductivity values recommended by Ref. 16 yields the following coefficients for Equation (6a)

$$a = 6.07768 \quad b = 4.00976 \times 10^{-3} \quad d = -8.13908 \times 10^{-7}$$

with an error smaller than 2% for temperatures up to 2700°F. The latter value can therefore be used for T_b , with $k_2 = 10.82 \text{ Btu}/(\text{hr-ft-}^\circ\text{F})$. The effect of fuel porosity p is taken into account by multiplying the thermal conductivity by $(1-p)/(1+p)$, again following the recommendations of Ref. 16.

2. Fuel Region Temperature Distribution

The fuel temperature T as a function of distance r from the fuel centerline is given by:

$$\frac{q'}{2\pi R^2} \int_R^r r dr = - \int_{T_s}^T k dT$$

from which:

$$\frac{q'}{4\pi} \left(1 - \frac{r^2}{R^2} \right) = \int_{T_s}^T k dT$$

where R is the pellet radius. A subroutine (TMPRT) has been included to solve this equation for the upper limit of integration T at any specified number of radial nodes.

3. Input Arrangement

The new input variables and their relative addresses are listed in the table below, which supplements the input guide of Ref. 7.

Relative Address	Symbol	Definition
454	AA	Coefficients a, b and d in Equations (3) and (6). Units are Btu/(hr-ft-°F) for the thermal conductivity and degrees F for the temperature. For the case of constant conductivity, omit these three items and input k as indicated in Ref. 7.
455	BE	
456	DD	
457	PP	Fuel porosity
458	XNODS	Number of nodes for fuel region temperature distribution (maximum of 10.).
459	BT	Boundary temperature T_b in Equations (6a) and (6b). (°F)
460	AAK2	Constant thermal conductivity k_2 in Equation (6b). (Btu/hr-ft-°F)

References

1. W. P. Barthold, et al., "Breeding Performance and Pin Diameter Optimization for Mixed Carbide and Nitride Fuels in 5000 MWt LMFBRs", draft.
2. E. H. Novendstern, "Engineering Hot Channel Factors," FRT-372, January 8, 1971.
3. J. A. Vittl, "Preliminary Assessment of Proof Testing LMFBR Carbide Fueled Subassemblies in FTR Positions," COO-2426-5, CERD-370, June 1974.
4. P. R. Betten, "Thermal/Hydraulic Characteristics of Carbide Subassemblies in FTR Driver Positions," FBR-75-55/21280, Combustion Engineering, Inc., April 1975.
5. T. J. Thompson and J. G. Beckerley, "The Technology of Nuclear Reactor Safety," Vol. 2, M.I.T. Press, 1973.
6. R. C. Noyes and R. Pauze, "THESAC--A Thermal and Hydraulic Analysis Code for Sodium Fast Reactor Survey Calculations," LMFBR Technical Note 4, Combustion Engineering, Inc., August 1967, internal memorandum.
7. S. A. Caspersson, "THESAC-IV: Modifications and a Revised Data Input Guide," FBR-73-149/211210, Combustion Engineering, Inc., August 1973, internal memorandum.
8. E. H. Novendstern, "Turbulent Pressure Drop Model for Fuel Rod Assemblies Utilizing a Helical Wire-Wrap Spacer System," Nuclear Engineering and Design 22, p. 19-27 (1972).
9. A. W. Graves and I. Catton, "A Numerical Model of Heat Transfer in a Rod Bundle with Helical Wire Wrap Spacers," Atomics International.
10. J. J. Lorenz, T. Ginsberg and R. A. Morris, "Experimental Mixing Studies and Velocity Measurements with a Simulated 91-Element LMFBR Fuel Assembly," ANL-CT-74-09, Argonne National Laboratory.
11. E. Khan, W. Rohsenow, A. Souin and N. Todreas, "A Simplified Method for Predicting Temperature Distribution in Wire Wrapped Rod Assemblies," Massachusetts Institute of Technology, Cambridge, Massachusetts.
12. E. Khan, N. E. Rodreas, N. M. Rohsenow, and A. A. Souin, "Analysis of Mixing Data Relevant to Wire Wrapped Fuel Assembly Thermal Hydraulic Design," COO-2245-12 Topical Report MITNE-165, Sept. 1974.
13. D. C. Kolesar, BNWL, Personal Communication.
14. J. Barner, LASL, Personal Communication.
15. P. Levine, WARD, Personal Communication.

16. A. B. G. Washington, "Preferred Values for the Thermal Conductivity of Sintered Ceramic Fuel for Fast Reactor Use," TRG-Report-2236, UKAEA, September 1973.
17. P. D. Cohn, et al., "Thermal-Hydraulic and Mechanical Aspects of FTR Fuel Pin Spacer Design," BNWL 750, Battelle Memorial Institute, October 1968.
18. J. J. Laidler, "Alloy Properties Databook," TC-293, Rev. 1, Hanford Engineering Development Laboratory, June 1975.

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