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**SCOPING DESIGN ANALYSES FOR OPTIMIZED SHIPPING CASKS CONTAINING
1-, 2-, 3-, 5-, 7-, OR 10-YEAR-OLD PWR SPENT FUEL**

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J. A. Sucholz

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PREFACE

As noted in the acknowledgments, much of the work reported here was sponsored by the Transportation Technology Center at Sandia National Laboratory. The primary interest of the local sponsor was in the net capacity of the optimized cask designs that finally evolved for 2, 3, 5, 7, and 10-year-old spent fuel. As such, those results were originally included in a companion "Summary Report," ORNL/CSD/TM-150, which also gives the final design specifications for the various casks and a brief 20-page synopsis of the associated analyses. Subsequently, and as part of a separate task, the analytical techniques used in this study were documented more thoroughly, and the mass of useful reference data generated in the course of this study was reviewed and tabulated. These elements were then combined with a more complete discussion of the various design considerations and molded into a more formal set of notes which ultimately formed the basis of this report.

As implied by the title and noted throughout the text, the work described here was intended only as a "scoping" design study for the various types of shipping casks that we sought to optimize. As such, it was our objective to define "the limits of what might be feasible" based on criticality, shielding, and heat transfer considerations. Each of these have been examined and documented in some detail. Eventually, however, questions relating to the overall mechanical design and/or fabrication of the various casks should be examined in more detail. Likewise, the accident/post-accident transient thermal analysis for certain casks should also be reevaluated as one becomes more specific regarding the mechanical design of internal fins or other structural components penetrating through the neutron shield. Unfortunately, such analyses are often highly dependent on the final mechanical design of the various casks and must be examined on a case-by-case basis.

J. A. Bucholz (3-20-82)

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ABSTRACT

This report details many of the interrelated considerations involved in optimizing large Pb, Fe, or U-metal spent fuel shipping casks containing 1, 2, 3, 5, 7, or 10-year-old PWR fuel assemblies. Scoping analyses based on criticality, shielding, and heat transfer considerations indicate that some casks may be able to hold as many as 18-21 ten-year-old PWR fuel assemblies.

In the criticality section, a new type of inherently subcritical fuel assembly separator is described which uses hollow, borated stainless-steel tubes in the wall-forming structure between the assemblies.

In another section, details of many n/ γ shielding optimization studies are presented, including the optimal n/ γ design points and the actual shielding requirements for each type of cask as a function of the age of the spent fuel and the number of assemblies in the cask. Multigroup source terms based on ORIGEN2 calculations at these and other decay times are also included.

Lastly, the numerical methods and experimental correlations used in the steady-state and transient heat transfer analyses are fully documented, as are pertinent aspects of the SCOPE code for Shipping Cask Optimization and Parametric Evaluation. [While only casks for square, intact PWR fuel assemblies were considered in this study, the SCOPE code may also be used to design and analyze casks containing canistered spent fuel or other waste material. An abbreviated input data guide is included as an appendix].

I. INTRODUCTION

With the growing inventory of spent fuel stored at nuclear power plants throughout the country and the anticipated need to transport that fuel to a reprocessing site or other "Away From Reactor" storage facility, it becomes increasingly important to examine the types of spent fuel shipping casks that are currently in use, and those that might be used in the future. The number of casks currently in existence cannot handle the anticipated traffic in the next 10-20 year period. The question, then, is whether to produce additional casks similar to those currently in use, or turn to a new generation of spent fuel shipping casks.

Many of the casks currently in use were designed between the mid-1960's and the early 1970's. At that time spent fuel reprocessing was assumed, and the economics associated with early recovery of the residual plutonium dictated that the fuel be shipped from the reactor site as soon as possible. This typically meant a 3-6 month "cooling off" period. Accordingly, those spent fuel casks had to be designed to handle a fairly large amount of decay heat and very intense neutron/gamma sources. Frequently that meant the use of auxiliary forced cooling systems, and almost always it meant the use of very thick neutron and gamma shields whose size and weight further limited the capacity of those casks to 7 (or 10) PWR fuel assemblies per shipment.^{1,2}

Today, however, much of the spent fuel stored at the various sites is already 5-10 years old. With the continued deferral of reprocessing and the availability of high-density spent fuel storage racks, the inventory of older fuel in this country is expected to grow considerably over the next 10-20 years. Casks for such fuel would, of course, require

much less neutron and gamma shielding. With thinner shields, these casks could be expanded to carry 2-3 times as many assemblies as conventional casks, while still meeting current weight limitations and other regulatory guidelines. The logistics and economics of designing, licensing, building, and utilizing such casks vs. the expanded use of casks like the NLI-10/24¹ and the IF-300² will be examined in a later report. The purpose of this report is to identify realistic designs that might be used for the shipment of 1,2,3,5,7 and 10-year-old PWR fuel assemblies. Subsequent reports will look at optimal designs for BWR fuel, dual purpose casks, canistered fuel, and other advanced concepts.

II. BASIC DESIGN PHILOSOPHY AND OVERVIEW OF ANALYSES

The key physical features of those casks considered in this study are illustrated in Fig. II.1. In each case we have an inner shell, a gamma shield, an outer shell, a neutron shield, and an outside liner. Depending on the amount of decay heat that must be dissipated, the cask(s) may or may not have fins as illustrated in Fig. II.2. (Casks with forced circulation cooling systems were not considered in this study). The inner and outer shells and the outside liner were all assumed to be stainless-steel. The thicknesses of these components varied, depending on the type of gamma shielding being used (cf. Table II.1). For the lead (Pb) cask, these dimensions are comparable to those used in the NLI-10/24 design.¹ For the uranium-metal cask, the dimensions are comparable to (but slightly thicker than) those used in the IF-300 design.² For the iron (Fe) cask, these dimensions are comparable to (but slightly thicker than) those used in an earlier conceptual design of an LMFBR spent fuel shipping cask described in Reference 3. In the latter case, the gamma shield is assumed to be ordinary carbon steel, with a relatively thin inner and outer shell being provided only as a corrosion barrier. In each case, the length of the cavity inside the cask (171.6") was chosen so as to accommodate a 13.8-ft PWR fuel assembly while leaving an additional 6-inch space for an internal axial shock absorber.

Inside the cask(s), the fuel assemblies are separated by means of an aluminum or copper insert, as illustrated in Fig. II.3. Each of the 10 positions shown in Fig. II.3 measures 8.7" x 8.7" and can accommodate most typical PWR fuel assemblies with 0.125" clearance on all four sides. Other "optimal" packing arrangements capable of holding many

Fig. II.1: Overview of a Typical Spent Fuel Shipping Cask

ORNL-DWG 82-12166

- a) The removable aluminum insert containing the spent fuel is more fully illustrated in Fig. II.3 and/or Appendix B.
- b) The thicknesses of the various components in the axial direction were assumed to be the same as in the radial direction.
- c) Actual dimensions of the various components depend on the type of cask, as shown in Table II.1 and/or Appendix F.

Fig. II.2: Overview of a Spent Fuel Shipping Cask with External Cooling Fins

ORNL-DW 82-12161

- a) The removable aluminum insert containing the spent fuel is more fully illustrated in Fig. II.3 and/or Appendix B.
- b) The thicknesses of the various components in the axial direction were assumed to be the same as in the radial direction.
- c) Actual dimensions of the various components depend on the type of cask, as shown in Table II.1 and/or Appendix F.
- d) Sketch not drawn to scale; many casks had 45-50 fins; actual center-to-center spacing was assumed to be 4.0 inches.

Table II.1
Thickness of Various Structural
Components in Each Type of Cask

TYPE OF CASK	THICKNESS IN INCHES		
	INNER SHELL	OUTER SHELL	OUTSIDE LINER
LEAD	1.5	2.0	.75
U-METAL	.75	2.0	.75
IRON	.375	.375	.75

Fig. II.3: Inherently Subcritical, Removable Aluminum Insert
Designed to Hold 10 PWR Fuel Assemblies

ORNL-DWG 82-12177

Optimal packing arrangements
for additional assemblies are
illustrated in Appendix B

Hollow, borated stainless-steel
tubes embedded within the walls
of the insert in order to insure
subcriticality (cf. Sect. III)

more fuel assemblies were also considered (cf. Appendix B). In addition to physically separating the fuel assemblies, the insert must provide a low-resistance path by which the decay heat may be carried away from the innermost assemblies and, secondly, it must provide an inherently passive means of insuring subcriticality under the most reactive conditions conceivable.^{5,6}

To avoid the complexities of a forced circulation cooling system and the special licensing considerations associated with a natural convection water-filled cask, it was assumed that the spent fuel would be shipped dry. This passive mode of heat removal places increased emphasis on thermal radiation and conduction. Use of an aluminum or copper insert, as shown in Fig. II.3, provides a low-resistance thermal conduction path from the interior regions of the cask to the relatively cool inner wall, thus allowing even the innermost assembly to "see" a relatively cool surface (rather than other hot assemblies) to which it can radiate. A modestly detailed 2-D thermal analysis for each of the physical arrangements shown in Appendix B is described in Section V.G.

Most spent fuel shipping casks are, of course, loaded under water. Inadvertent loading of the cask with fresh fuel (or fuel with very little accumulated burnup) is then usually taken as the most reactive condition considered in the licensing application.^{5,6} Because of the larger payload associated with casks designed to carry older spent fuel assemblies, the problem of insuring subcriticality becomes more complicated. This question has been examined in some detail and is discussed in Section III. The approach used here relies on the use and placement of hollow, borated stainless-steel tubes within those portions of the

Al or Cu insert between the assemblies, as shown in Fig. II.4. Given an infinite array of infinitely long PWR fuel assemblies @ 3.4 wt. % U^{235} , we have found this configuration to be safely subcritical with $k_{eff} = 0.916 \pm .003$. Even at 3.9 wt. % U^{235} , this configuration was safely subcritical with $k_{eff} = 0.941 \pm .003$. Use of this inherently subcritical insert design, in all of the casks studied, greatly simplified the task at hand and lent an important degree of credibility to the designs that finally evolved.

Returning to the overall description of the cask(s), we note that lead, iron, and depleted uranium-metal casks were all optimized for the shipment of 1,2,3,5,7 and 10-year-old PWR spent fuel. By volume, the neutron shield in each case consisted of 28.5% water (1.0 g/cc), 66.0% ethylene glycol (1.11 g/cc; $HOCH_2CH_2OH$) and 5.5% potassium tetraborate (1.74 g/cc; $K_2B_4O_7 \cdot 8H_2O$) made with natural boron. By weight, this common mixture of water and antifreeze contains ~1% boron. As described in Section IV.B, considerable effort was spent in determining the relative amounts of neutron and gamma shielding that should be used in each case so as to minimize the overall weight of the loaded cask. Having established the optimal n/ γ split in each case, extensive tables of neutron and gamma shielding requirements were compiled for each type of cask as a function of the age of the fuel and the number of assemblies in the cask (cf. Appendix F).

As shown in Section VII, external fins were required for those casks containing 1 and 2-year-old spent fuel, and for some casks designed for 3-year-old fuel. Both copper and stainless-steel fins were considered, with the copper offering a distinct advantage only for the 1-year-old spent fuel cask(s). In general, fins were not used on a cask if it could

ORNL-DWG 82-12189

Fig. II.4: Details Regarding the Use and Placement of Hollow, Borated Stainless-Steel Tubes Within the Wall-Forming Structure of the Insert Used to Separate Fuel Assemblies and Insure Subcriticality

dissipate the decay heat to the environment (at 130°F) while maintaining an outer skin temperature less than or equal to 250°F. In those cases where fins were necessary, a numerical search was conducted to determine the particular fin dimensions which would satisfy the above criteria while minimizing the overall weight of the loaded cask. In all cases, the circumferential fins were assumed to be spaced on a 4-inch pitch. In the case of the 1-year-old fuel, which imposes a relatively high decay heat load on the cask, the size and mass of the fins may (depending on the type of material) significantly influence the optimal n/γ split used to determine the relative amounts of neutron and gamma shielding. This matter is discussed further in Section IV.B.5.

Using the design philosophy described thus far, the inherently subcritical fuel assembly separator described in Section III, and the neutron and gamma shielding requirements described in Section IV.C and Appendix F, we began to address the basic question: "How many 1,2,3,5,7 or 10-year-old PWR fuel assemblies could be safely shipped in an optimized Pb, Fe, or U-metal rail cask?" To answer that question, we made extensive use of the SCOPE program⁷ which was developed by the Computer Sciences Division at ORNL as a modestly rigorous yet inexpensive code for Shipping Cask Optimization and Parametric Evaluation. Using the optimal packing arrangements shown in Appendix B and the tabulated shielding requirements input by the user, the code will "design" a cask to carry a single fuel assembly and then continue incrementing the number of assemblies until one or more of the design limits can no longer be met. Some typical SCOPE output is presented in Table II.2.⁸

⁸ The SCOPE User's Input Guide, excerpted from ORNL/CSD/TM-148,⁷ is included here as Appendix A. Hopefully, the complete document will be published as funding becomes available.

Table II.2
Typical SCOPE Output for a Number of Cases of Potential Interest

FUEL AGE	MO. ASSY	MATERIALS		THICKNESS IN INCHES		CASE WEIGHT (LBS)	DIMENSIONS IN INCHES				STEADY STATE TEMPERATURES IN DEG. F				MAX FIRE-TEST TEMPERATURES IN DEG. F			
		INSRT	CAN. SHTD	CAN. WETT SHTD	SHLD		ID	OD	LENGTH	FIN HT. THK.	FUEL FIN	INSRT MATL	CAN. SHTD	CASE SURF.	FUEL FIN	INSRT MATL	CAN. SHTD	
10.0	1	AL	FE	FE	FE	171.3	55.8	80.0	195.8	0.0	0.0	351	269	187	180	480	402	391
10.0	16	AL	FE	FE	FE	195.1*	61.9	86.2	195.9	0.0	0.0	357	275	186	179	479	401	388
10.0	18	AL	FE	FE	FE	196.6*	61.9	86.3	195.9	0.0	0.0	369	288	182	184	499	422	401
10.0	19	AL	FE	FE	FE	205.0*	63.8	88.3	196.1	0.0	0.0	371	290	194	186	502	425	404
10.0	21	AL	FE	FE	FE	206.9*	63.8	88.5	196.3	0.0	0.0	381	301	199	190	520	445	415
10.0	22	AL	FE	FE	FE	218.7*	66.6	91.4	196.4	0.0	0.0	385	305	200	191	520	445	416
10.0	13	AL	FE	FE	FE	173.9	52.0	80.7	200.2	0.0	0.0	339	256	179	173	446	367	347
10.0	14	AL	FE	FE	FE	189.1	55.8	84.5	200.3	0.0	0.0	345	258	180	174	446	367	348
10.0	15	AL	FE	FE	FE	189.8	55.8	84.5	200.3	0.0	0.0	347	265	183	177	457	378	355
10.0	16	AL	FE	FE	FE	215.1*	61.9	90.7	200.4	0.0	0.0	353	272	183	176	458	380	353
10.0	18	AL	FE	FE	FE	216.7*	61.9	90.8	200.5	0.0	0.0	365	284	189	181	476	399	366
10.0	18	AL	U	U	U	179.4	61.9	80.4	190.1	0.0	0.0	372	291	196	189	508	431	423
10.0	19	AL	U	U	U	187.3	63.8	82.4	190.2	0.0	0.0	374	293	198	190	511	435	426
10.0	21	AL	U	U	U	189.0	63.8	82.6	190.3	0.0	0.0	385	304	204	195	530	455	438
10.0	22	AL	U	U	U	200.2*	66.6	85.4	190.4	0.0	0.0	388	308	204	196	530	455	439
10.0	23	AL	U	U	U	211.3*	69.2	88.0	190.4	0.0	0.0	387	307	205	196	527	452	440
10.0	24	AL	U	U	U	211.3*	69.2	88.1	190.5	0.0	0.0	392	312	208	199	535	461	445
10.0	25	AL	U	U	U	216.0*	70.1	89.0	190.5	0.0	0.0	405	326	210	200	550	477	449
10.0	26	AL	U	U	U	216.7*	70.1	89.0	190.5	0.0	0.0	410	331	212	203	559	486	454
7.0	14	AL	FE	FE	FE	176.7	55.8	80.4	196.2	0.0	0.0	381	287	193	185	512	422	403
7.0	15	AL	FE	FE	FE	177.4	55.8	80.5	196.3	0.0	0.0	388	294	196	188	524	435	411
7.0	16	AL	FE	FE	FE	201.8*	61.9	86.7	196.4	0.0	0.0	396	302	196	187	524	436	408
7.0	18	AL	FE	FE	FE	203.3*	61.9	86.8	196.5	0.0	0.0	409	316	202	193	545	459	422
7.0	19	AL	FE	FE	FE	211.9*	63.8	88.8	196.6	0.0	0.0	411	319	204	195	549	462	425
7.0	21	AL	FE	FE	FE	213.7*	63.8	88.9	196.7	0.0	0.0	423	332	210	200	569	483	438
7.0	22	AL	FE	FE	FE	225.8*	66.6	91.8	196.8	0.0	0.0	427	336	211	201	570	485	439
7.0	12	AL	FE	FE	FE	168.3	48.9	78.3	200.9	0.0	0.0	366	270	186	178	479	388	361
7.0	13	AL	FE	FE	FE	180.8	52.0	81.4	201.0	0.0	0.0	374	279	188	180	486	396	365
7.0	14	AL	FE	FE	FE	196.4*	55.8	85.2	201.0	0.0	0.0	377	282	189	181	488	398	367
7.0	15	AL	FE	FE	FE	197.2*	55.8	85.3	201.1	0.0	0.0	384	290	192	184	499	410	375
7.0	16	AL	FE	FE	FE	223.1*	61.9	91.4	201.1	0.0	0.0	391	298	192	184	502	414	373
7.0	15	AL	U	U	U	160.3	55.8	74.6	190.4	0.0	0.0	392	298	202	193	534	446	435
7.0	16	AL	U	U	U	183.4	61.9	80.8	190.4	0.0	0.0	399	306	201	192	533	446	431
7.0	18	AL	U	U	U	184.7	61.9	80.8	190.5	0.0	0.0	412	320	07	198	555	469	445
7.0	19	AL	U	U	U	192.8*	63.8	82.8	190.6	0.0	0.0	414	322	09	200	558	473	448
7.0	21	AL	U	U	U	194.5*	63.8	83.0	190.8	0.0	0.0	427	336	16	206	579	495	462
7.0	22	AL	U	U	U	206.0*	66.6	85.8	190.8	0.0	0.0	431	340	17	206	580	495	462
7.0	23	AL	U	U	U	217.3*	69.2	88.5	190.9	0.0	0.0	430	338	218	207	577	493	464
5.0	13	AL	FE	FE	FE	167.8	52.0	77.4	197.0	0.0	0.0	439	325	207	197	583	476	434
5.0	14	AL	FE	FE	FE	182.9	55.8	81.3	197.1	0.0	0.0	443	329	208	198	583	477	436
5.0	15	AL	FE	FE	FE	183.7	55.8	81.3	197.1	0.0	0.0	452	338	213	202	597	491	445
5.0	16	AL	FE	FE	FE	208.7*	61.9	87.5	197.1	0.0	0.0	462	349	212	201	600	496	442
5.0	18	AL	FE	FE	FE	210.1*	61.9	87.5	197.2	0.0	0.0	478	366	220	208	624	521	458
5.0	19	AL	FE	FE	FE	219.0*	63.8	89.6	197.4	0.0	0.0	480	369	223	210	628	525	462
5.0	12	AL	FE	FE	FE	175.1	48.9	79.1	201.8	0.0	0.0	424	308	200	190	546	437	392
5.0	13	AL	FE	FE	FE	187.9	52.0	82.3	201.8	0.0	0.0	434	319	202	192	555	448	397
5.0	14	AL	FE	FE	FE	203.9*	55.8	86.1	201.9	0.0	0.0	438	323	204	194	558	451	399
5.0	15	AL	FE	FE	FE	204.7*	55.8	86.2	201.9	0.0	0.0	447	333	208	197	571	464	408
5.0	16	AL	FE	FE	FE	231.3*	61.9	92.3	207.0	0.0	0.0	457	344	208	197	577	472	406
5.0	15	AL	U	U	U	165.5	55.8	74.9	190.7	0.0	0.0	456	343	220	209	608	503	469
5.0	16	AL	U	U	U	189.2	61.9	81.0	190.7	0.0	0.0	466	354	218	208	611	507	465
5.0	18	AL	U	U	U	190.6*	61.9	81.1	190.8	0.0	0.0	482	371	227	215	635	533	482
5.0	19	AL	U	U	U	198.9*	63.8	83.1	190.9	0.0	0.0	484	374	229	217	639	537	486
5.0	21	AL	U	U	U	200.6*	63.8	83.3	191.1	0.0	0.0	499	390	237	224	662	562	501
5.0	22	AL	U	U	U	212.3*	66.6	86.0	191.1	0.0	0.0	504	395	238	225	663	564	502
5.0	23	AL	U	U	U	233.8*	69.2	88.7	191.1	0.0	0.0	502	393	240	226	660	561	504

a) Pb casks have a 1.5" inner shell, a 2.0" outer shell, and a 0.75" outside liner

b) Fe casks have a 0.375" inner shell, a 0.375" outer shell, and a 0.75" outside liner

c) U-metal casks have a 0.75" inner shell, a 2.0" outer shell, and a 0.75" outside liner

In each case ($N = 1, 2, \dots$), the code will calculate the steady-state temperature distribution throughout the cask and perform a complete 1-D space/time transient thermal analysis following a postulated half-hour fire (cf. Sections V and VI). In each case ($N = 1, 2, \dots$), the code will edit the characteristic dimensions of the cask (including fins, if required), the total weight of the loaded cask, the steady-state temperature at selected points, and the maximum transient temperature in key components.

Details of the accident/post-accident transient thermal analysis performed by SCOPE are described in Section VI. The approach taken solves the actual time-dependent finite-differenced one-dimensional^{*} heat transfer equations for the entire cask. At each time step, the resulting spatial equations are solved using matrix factorization. The steady-state temperature distribution used as the initial condition at the start of the transient assumes that the neutron shield has already been rendered void as a result of the 30-foot drop test. The postulated fire, having a flame temperature of 1475°F, is then assumed to last for 30 minutes. Because the thermal transient peaks in some components long after the external fire has been extinguished, this analysis follows the transient for 12 full hours. Numerical results for this thermal transient analysis package have been benchmarked against HEATING-5⁸ results for similar 1-D models and found to agree quite well (generally within $\pm 1^\circ\text{F}$ at all spatial points during the entire transient). To conservatively estimate the maximum

* To account for the (often large) heat capacity of the insert without adding undue complexity to the problem, the insert has been modeled as a solid annular region just inside the inner shell. The small gap between the inner shell and the insert has been maintained, as has the overall cross-sectional area of the insert.

temperature at points inside the cask cavity (i.e. in the insert and the fuel), the maximum temperature rise in the insert, as given by the simplified 1-D model, is added to the maximum steady-state value given by the detailed 2-D analysis described in Section V.G. Likewise, the maximum fuel pin clad temperature is also recalculated at this elevated temperature.

Using the SCOPE program in conjunction with the shielding data found in Section IV.C and Appendix F, we were able to evaluate a wide range of conceptual designs at minimal cost. (The type of analysis described above would, for example, require ~0.13 seconds of CPU time on an IBM-3033/MVS computer, thus allowing us to generate and analyze hundreds of design variations for about a penny each). SCOPE output for those cases of potential interest is presented in Table VII.3 of Section VII.B. Since many design limits are not absolutely rigid, this rather extensive table shows a number of cases on either side of the nominal design limits. An abbreviated form of this table showing the optimal Pb, Fe, and U-metal cask capacities as a function of the spent fuel's age is given in Table II.3. That table assumes 198,000 lbs (89.8 metric tonnes) as the maximum allowable gross weight. A similar table, Table II.4, assumes 207,000 lbs (93.9 metric tonnes) as the maximum allowable gross weight.* In either case, it appears that optimized designs could be developed which could carry as many as 15-18 5-year-old PWR fuel assemblies or as many as 18-21 10-year-old LWR fuel assemblies.

* For those casks containing 1-year-old spent fuel, the melting of the Pb gamma shield (at 618°F) and the melting of the aluminum insert in the U-metal cask (at 1120°F) were both viewed as real constraints. These constraints were noted when selecting the optimal cases to be presented in Tables II.3 and II.4.

TABLE 11.3
OPTIMAL PB, FE, AND U-METAL CASK DESIGNS vs AGE OF SPENT FUEL (WT < 198,000 lbs)

FUEL AGE	GAMMA SHIELD	NO. OF ASSYS	LOADED WT (1000-LBS)	CASKID (IN)	GSHLD (IN)	NSHLD (IN)	FIN MATL	L-FIN (IN)	T-FIN (IN)
10	PB	18	196.6	61.9	3.90	4.02	--	--	--
7	PB	15	177.4	55.8	4.15	3.95	--	--	--
5	PB	12	183.7	55.8	4.43	4.08	--	--	--
3	PB	15	195.8	55.8	4.99	4.21	--	--	--
2	PB ^h	12	187.3	48.9	5.37	4.25	SS	2.00	0.25
1	PB ^{gh}	6	159.6	39.5	5.60	4.08	CU	2.00	0.25
10	FE	15	189.8	55.8	9.19	3.68	--	--	--
7	FE	15	197.2	55.8	9.65	3.59	--	--	--
5	FE	13	187.9	52.0	10.05	3.57	--	--	--
3	FE	12	187.9	48.9	10.82	3.79	--	--	--
2	FE	10	185.3	46.4	11.33	3.89	--	--	--
1	FE	8	187.8	43.3	12.01	3.91	CU	2.00	0.25
10	U	21	189.0	63.8	2.45	3.42	--	--	--
7	U	21	194.5	63.8	2.61	3.49	--	--	--
5	U	18	190.6	61.9	2.74	3.35	--	--	--
3	U	15	179.7	55.8	3.02	3.57	SS	2.00	0.25
2	U	15	188.8	55.8	3.27	3.67	SS	3.00	0.25
1	U ⁱ	13	197.6	54.8	3.57	3.72	CU	6.00	0.25

TABLE 11.4
OPTIMAL PB, FE, AND U-METAL CASK DESIGNS vs AGE OF SPENT FUEL (WT < 207,000 LBS)

FUEL AGE	GAMMA SHIELD	NO. OF ASSYS	LOADED WT (1000-LBS)	CASKID (IN)	GSHLD (IN)	NSHLD (IN)	FIN MATL	L-FIN (IN)	T-FIN (IN)
10	PB	21	206.9	63.8	3.97	4.13	--	--	--
7	PB	18	203.3	61.9	4.20	4.01	--	--	--
5	PB	15	183.7	55.8	4.43	4.08	--	--	--
3	PB	15	195.8	55.8	4.99	4.21	--	--	--
2	PB ^{gh}	13	201.0	52.0	5.34	4.25	SS	2.00	0.25
1	PB ^{gh}	6	159.6	39.5	5.60	4.08	CU	2.00	0.25
10	FE	15	189.8	55.8	9.19	3.68	--	--	--
7	FE	15	197.2	55.8	9.65	3.59	--	--	--
5	FE	15	204.7	55.8	10.11	3.56	--	--	--
3	FE	13	201.3	52.0	10.85	3.70	--	--	--
2	FE	12	203.5	48.9	11.46	3.92	SS	2.00	0.25
1	FE	10	205.4	46.4	12.17	3.94	CU	3.00	0.25
10	U	22	200.2	66.6	2.46	3.43	--	--	--
7	U	22	206.0	66.6	2.61	3.50	--	--	--
5	U	21	200.6	63.8	2.79	3.45	--	--	--
3	U	18	206.2	61.9	3.06	3.58	SS	2.00	0.25
2	U	15	188.8	55.8	3.27	3.67	SS	3.00	0.25
1	U ^j	15	205.5	55.8	3.60	3.73	CU	8.00	0.25

- a) Pb casks have a 1.5" inner shell, a 2.0" outer shell, and a 0.75" outside liner.
b) Fe casks have a 0.375" inner shell, a 0.375" outer shell, and a 0.75" outside liner.
c) U-metal casks have a 0.75" inner shell, a 2.0" outer shell, and a 0.75" outside liner.
d) In each case, the fuel assemblies are separated by means of a 2.25" thick Al insert.
e) In each case, the accident scenario used in the transient thermal analysis assumes that the region containing the neutron shield had been voided for some time prior to the start of the 30-minute fire and that the cask internals had already reached a new (elevated) state of thermal equilibrium prior to the fire.

- f) For added thermal protection, a 2.5" outer shell was used in lieu of a 2.0" shell.
g) For added thermal protection, a 1.25" outside liner was used in lieu of the 0.75" liner.
h) No. of assys. limited by the onset of melting in the Pb during the thermal transient.
i) For added thermal protection, a 3.0" aluminum insert was used in lieu of a 2.25" insert.
j) In this particular design, the 2.25" thick aluminum insert comes precariously close to the melting point during the thermal transient following the 30-minute fire.

III. DESCRIPTION OF AN INHERENTLY SUBCRITICAL FUEL ASSEMBLY SEPARATOR

III.A GENERAL DESIGN CONSIDERATIONS

In the very early stages of this study, cask designs would be generated and those which appeared acceptable with respect to all other criteria were then checked for subcriticality using the KENO-IV or KENO-IV/CG Monte Carlo codes.^{10,11} Given the setup time required to model each individual cask and the expense associated with such calculations, it was obvious that this was not a very efficient manner in which to proceed. It was then decided to spend some time developing an inherently subcritical fuel assembly separator which would be suitable for use in spent fuel shipping casks. Ideally, such a design could be shown to be safely subcritical ($k_{eff} < 0.95$) regardless of the number of fresh fuel assemblies in the array.

Unlike the open lattice arrangement found in the IF-300, we chose to use a solid insert (or "separator") between the fuel assemblies. This type of construction had been used successfully in the NLI-10/24 and has several advantages: (1) It provides a low resistance path by which the decay heat can be carried (via conduction) to the inner wall of the cask at relatively low temperatures. This allows even the innermost assembly to "see" a relatively cool surface (as opposed to other hot fuel assemblies) to which it can radiate -- thus reducing the maximum fuel pin temperature. (2) If required, forced circulation cooling tubes could be embedded within the insert to further assist in the removal of decay heat. (3) In the accident scenario involving a half-hour fire, the extra heat capacity afforded by the insert itself can absorb a fair

amount of decay heat, thereby reducing the maximum fuel pin temperature during the transient. Aluminum and copper were both considered excellent materials for this application -- aluminum because of its high conductivity and high heat capacity, and because it is relatively lightweight; copper because of its high conductivity and high melting point, and because it has many small absorption resonances in the epithermal energy range. While the copper was found to reduce the reactivity of an otherwise unpoisoned system from 1.40 down to 1.21, it was later found to be worth $\leq 1\%$ in a well-poisoned system. Because of its greater weight and the effect of that weight on the carrying capacity of the cask, it was later discarded in favor of the aluminum.

Unlike the NLI-10/24 which uses poison plates made of Ag/In/Cd, we limited consideration to less exotic materials such as Boral^{*a} and/or borated steel.^{*b} Both are strong thermal absorbers and quite effective in the presence of water. Likewise, both materials have recently found extensive use in high density spent fuel storage racks. Spent fuel storage racks, however, remain under water and are cooled by a certain amount of natural circulation. Spent fuel shipping casks, on the other hand, are typically shipped dry. Under such conditions, the Boral has been known to blister.^{*c} For that reason, borated stainless-steel was selected.

Most high density spent fuel storage racks consist of borated steel (or Boral) canisters separated by several inches of water. This arrange-

*a Metallic mixture of B₄C in aluminum; trademark of Brooks and Perkins, Inc. of Southfield, Michigan.

*b SS-304L with 0-2 wt% Boron, available from Carpenter Steel Corporation of Reading, Pennsylvania.

*c Private Communication, Mr. Neuben Peterson, Office of Nuclear Waste Isolation, Battelle Memorial Institute, Columbus, Ohio (Feb. 1980)

ment creates a very effective neutron flux trap in which the fast neutrons are thermalized, and the thermal neutrons are absorbed by the boron as they attempt to reenter a neighboring fuel assembly. As shown by the 1-D calculations illustrated in Fig. III.1, this type of arrangement is not only very effective, but also very sensitive to the amount of water between the poison plates (or canisters). While the use of the flux trap principle appeared very desirable, the use of individual fuel assembly canisters (or poison plates) has two obvious disadvantages:

(1) Maintaining the (sub)critical spacing between the canisters in a shipping cask subjected to severe impact crash tests may be somewhat tenuous;

(2) The use and placement of a canister and a large water gap between the fuel assembly and the insert, as shown in Fig. III.2, would represent an unnecessary thermal radiation shield when the cask is in transit.

(When in transit, of course, the cask is dry and the decay heat must be transferred from the fuel assembly to the insert by thermal radiation. The presence of this extra barrier would then force the maximum fuel pin temperature to be unnecessarily high.) It was at this point that we decided to move the borated steel and the necessary moderator, and embed both "within" the insert as shown in Figs. III.3 and III.4. This arrangement does not interfere with radiative heat transfer from the assemblies to the insert; it still provides a low resistance path by which the decay heat can then be conducted to the inner wall of the cask; and, most importantly, it makes intentional and repeated use of the flux trap principle throughout the entire array. The basic concept,^{*} illustrated in Fig. III.4, makes use

^{*} The U.S. Department of Energy, Oak Ridge Office of Patent Counsel, has recently filed a patent application on this design concept so as to keep it in the public domain.⁴⁸

Fig. III.1: Reactivity Change Produced by a 1-D Neutron Flux Trap, as a Function of the Amount of Moderator Between the Borated Plates

ORNL-DWG 82-12194

Fig. III.2: Use of Borated Canisters Around Each Assembly to Achieve a Multidimensional Flux Trap

Fig. III.3: Inherently Subcritical, Removable Aluminum Insert
Designed to Hold 10 PWR Fuel Assemblies

ORNL-DWG 82-12178

Optimal packing arrangements
for additional assemblies are
illustrated in Appendix B

Hollow, borated stainless-steel
tubes embedded within the walls
of the insert in order to insure
subcriticality (cf. Fig. III.4)

ORNL-DWG 82-12190

Fig. III.4: Details Regarding the Use and Placement of Hollow, Borated Stainless-Steel Tubes Within the Wall-Forming Structure of the Insert Used to Separate Fuel Assemblies and Insure Subcriticality

of hollow, borated stainless-steel tubes embedded within the aluminum or copper insert as shown. The tubes, of course, would be open at both ends to permit flooding whenever the cask is flooded (i.e. during loading). Alternately, they could be threaded at each end and made part of a forced circulation cooling system. As long as they were full of water whenever the cask is flooded, their optional use in this fashion would in no way compromise their effectiveness as a passive criticality control mechanism.

As shown in Fig. III.4, the design finally selected makes extensive use of rectangular steel tubes. Circular tubes, which would be somewhat easier to manufacture, were considered in many of the more detailed criticality calculations. Unfortunately, the diameters that were required for adequate thermalization (in the tubes between assemblies) did not always permit an integer number of tubes in a given row along the side of an assembly. Using fewer tubes on a wider pitch would increase the neutron streaming between assemblies and raise k_{eff} , while squeezing in one additional tube would reduce the space between the tubes (thereby seriously weakening the insert structure) or increase the pitch between assemblies (thereby increasing the inside and outside diameter of the cask and adding dramatically to the overall weight, thus limiting the design capacity which we hope to optimize). Staggered rows of smaller circular tubes were also considered but later deemed impractical for similar reasons. The use of rectangular tubes, whose x and y dimensions could be varied independently, gave us the freedom to increase the amount of water in the tubes between the assemblies without excessively increasing or decreasing the space between the individual tubes. As illustrated

in Fig. III.5, the use of rectangular vs. circular tubes of comparable diameter does have certain other advantages as well: (1) Because of their shape, the rectangular tubes significantly reduce the solid angle through which uncollided neutrons can stream freely while passing from one assembly to the next; (2) With comparable outside dimensions and wall thicknesses, the rectangular tubes have a larger cross-sectional area which can accommodate ~27% more internal moderator to aid in thermalization, thus making the flux trap more effective; (3) With comparable outside dimensions and wall thicknesses, the rectangular tubes also provide ~27% more borated steel per unit length, thus increasing the absorption probability and making the flux trap still more effective.

Lastly, it was suggested that we consider the use of long, thin rectangular water slots (1.5" x 4" or 1.5" x 8") in the insert between the assemblies, in lieu of the individual tubes shown in Fig. III.4. To increase the mechanical rigidity of the insert and prevent buckling due to differential expansion, the honeycomb structure shown in Fig. III.4 was deemed much more desirable. Presumably, such a structure could be fabricated by first erecting the hollow, borated stainless-steel tubes in the appropriate pattern (cf. Appendix B) and then pouring the cast aluminum alloy into a suitable mold.

ORNL-DWG 82-12197

Fig. III.5: Advantages of Rectangular vs. Circular Tubes in Neutronically Decoupling Adjacent Fuel Assemblies (less free streaming, more borated steel, more moderator inside tubes)

III.B INDIRECTLY RELEVANT CRITICALITY CALCULATIONS

Before arriving at the final insert design shown in Fig. III.4, it was necessary to perform a fair number of scoping calculations -- many of which deserve some brief mention:

(1) For the most part, the early scoping calculations were performed using an infinite array of infinitely long homogenized fuel assemblies, and a highly simplified model of the insert, as shown in Fig. III.6. Interestingly, we found that an explicit pin-by-pin model of a Westinghouse 15 x 15 fuel assembly (with 21 pins removed) would typically yield a reactivity $\sim 1.5\%$ greater than the homogenized assembly as calculated by CSAS2.* Presumably, that difference is due to the additional moderation afforded by explicitly modeling the 21 water holes in the otherwise undermoderated fuel assembly. The cell-averaged cross sections used in the homogenized model did not account for that extra moderation. For scoping calculations, however, the homogenized model was considered adequate.

(2) While most calculations were performed as if the assemblies were infinitely long (i.e. in the most conservative fashion), some were performed assuming an infinite planar array of 12-ft. fuel assemblies with 10" of water or 10" of SS-304 on each end. Those configurations with the water reflectors were found to be $\sim 1.5\%$ less reactive than those with the stainless-steel reflectors, and those with the stainless-steel reflectors were found to be within 0.5% of those where the fuel assemblies were assumed to be infinitely long.

* All criticality calculations were performed using the SCALE System Criticality Safety Analysis Module, CSAS2, in conjunction with the 27-group ENDF-IV cross-section library. See Reference 9.

ORNL-DWG 82-12195

Fig. III.6: Highly Simplified Model of 2-D Flux Trap
Used in Initial Criticality Scoping Calculations

(3) Applying reflective boundary conditions to all four sides of a quarter assembly (as shown in Fig. III.6) affords a convenient way of modeling an infinite array of fuel assemblies, each surrounded by aluminum, borated steel, and water. To establish the relevance of this infinite array to the type of smaller, finite arrays found in spent fuel shipping casks, we considered a series of arrays (1×1 , 2×2 , 3×3 , ...), each reflected by $\sim 20''$ of stainless-steel in all directions. The results are shown in Table III.1. Those results indicate that, in a poisoned system (unlike an unpoisoned system), a 3×3 array may be regarded as "infinite" for all practical purposes. While still conservative, use of an infinite array of infinitely long fuel assemblies as the design basis is not terribly unrealistic.

(4) To determine the approximate amount of borated steel and water that must be used between the fuel assemblies, we considered three different gauges of steel ($y = 0.125''$, $0.25''$, and $0.375''$) and five different amounts of water ($x = 0.25''$, $0.375''$, $0.5''$, $0.75''$, and $1.0''$), as shown in Fig. III.7. The results of these k_{eff} calculations are also shown in Fig. III.7. Note that along the diagonals, where $x + y = C$, the reactivity is roughly constant. Within these limits, this would seem to suggest that, on a volumetric basis, the water is about as effective as the borated steel. Later calculations which explicitly modeled each of the borated stainless-steel tubes also exhibited the same general tendency.

Table III.1
Average k-eff for Several
Finite Arrays of Poisoned
PWR Fuel Assemblies

Size of Array	k-effective
1 x 1	0.797 $\pm$.004
2 x 2	0.851 $\pm$.004
3 x 3	0.874 $\pm$.004
4 x 4	0.880 $\pm$.004
5 x 5	0.884 $\pm$.004
6 x 6	0.886 $\pm$.004
∞ x ∞	0.891 $\pm$.004

Fig. III.7: Sensitivity of K-effective to the Amount of Water and/or Borated Stainless-Steel in the Simplified 2-D Flux Trap Calculations

Note: PWR fuel contained 3.4% U-235, and the steel contained 1.6 wt% natural boron; The calculations were performed using 27 group ENDF-IV data in the KENO-IV Monte Carlo code; Resulting k-effective's accurate to ± 0.004 ; Note that for those cases where $X+Y=C$, the calculated reactivity is roughly constant.

(5) By decreasing the boron content in the stainless-steel tubes from 1.6 wt. % to 1.2 wt. %, the reactivity of a system very similar to that shown in Fig. III.4 increased by only ~1.7% (from $0.905 \pm .005$ to $0.920 \pm .005$). While this 25% reduction in the boron content has only a modest impact on the reactivity, it dramatically increases the ductility of the steel.^{*} Indeed, this added ductility may be necessary if the tubes are to be formed with a rectangular cross-section as shown in Fig. III.4. Unfortunately, further reductions in the boron content would have an increasingly important impact on the reactivity. With 0.01 wt. % boron, for example, this same system yielded a k-effective of $1.10 \pm .004$.

^{*} Personal Communication, Mr. Kermit Goda, Carpenter Steel (Oct., 1980).

III.C DESCRIPTION OF THE FINAL DESIGN AND SUPPORTING CALCULATIONS

Before arriving at the final insert design shown in Fig. III.4, a very similar design using square, borated stainless-steel tubes measuring 1.5" x 1.5" was also given very serious consideration. While the details of the insert and each of the borated stainless-steel tubes were modeled explicitly, the fuel assembly region was homogenized in accordance with standard procedures (i.e. mixture no. 500 was used in the CSAS2 model⁹). The unit-cell dimensions used in this homogenization and in the preparation of the cell-averaged cross-section data are shown in Fig. III.8. Note that the clad is assumed to be zircalloy and that the fuel is assumed to be 3.4 wt. % U^{235} . As in the final analysis, an infinite array of infinitely long fuel assemblies was also assumed. Using KENO-IV¹⁰ as implemented in CSAS2,⁹ this configuration yielded a k-effective of $0.945 \pm .004$.

While it was clear that we could significantly reduce k-effective by increasing the size of the borated steel tubes (thereby thermalizing and absorbing more neutrons), we first tried to add trace amounts of other materials with large resonances in the epithermal energy range. Silver, tantalum, tungsten, indium, and gadolinium were tried to no avail. A 0.5 wt. % addition of any of these elements in the borated stainless-steel typically yielded a $\Delta k \lesssim 1.0\%$.

In an unpoisoned system, copper is also known to be a modestly good absorber because of its many small resonances in the epithermal range. In a heavily poisoned system such as this, however, its introduction had very little impact. In this particular case, for example, use of a copper rather than an aluminum insert would decrease k_{eff} by less than 0.5%.

Fig. III.8: Unit Cell Dimensions for a Typical PWR Fuel Assembly

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← Typical fuel pin dimensions for the 15x15 assembly shown below:

$$P = 0.563 \text{ inch} \quad t_c = 0.0243 \text{ inch}$$

$$D_r = 0.422 \text{ inch} \quad D_f = 0.3659 \text{ inch}$$

FUEL: UO_2 10.41 g/cc, 3.4 wt% U^{235}

GAP: VOID

CLAD: ZIRCALLOY

MOD: WATER; 1.0 g/cc

← Typical 15x15 PWR fuel assembly containing 204 fuel pins and 21 water holes (where the control spider and instrumentation thimble used to be); Active length of fuel is 144 inches.

*Several criticality calculations were performed with each of the 204 fuel pins modeled explicitly.

*The simplified CSAS2 calculations used homogenized cell-averaged data for the fuel assembly based on the unit cell shown above.

In view of our inability to absorb an adequate number of epithermal neutrons, it was decided to increase the size of the borated stainless-steel tubes, as shown in Fig. III.4. Note that the orientation of these 1.5" x 1.75" tubes is also important. Oriented as shown, this configuration yields a k-effective of $0.916 \pm .003$. Turning them in the opposite direction would not only weaken the structural integrity of the insert but, interestingly enough, yield a k-effective which is $\sim 1.6\%$ higher.

The final insert design shown in Fig. III.4 is believed to be "inherently subcritical" on the grounds that, when flooded, an infinite array of fresh fuel assemblies (3.4 wt. % U^{235}) will yield a calculated k-effective of $0.916 \pm .003$. As a final check, another detailed calculation was performed assuming an artificially high enrichment of 3.9 wt. % U^{235} . This "worst case" calculation yielded a k-effective of $0.941 \pm .003$. While both calculations used the homogenized model of the fuel assembly and might, therefore, underestimate k-effective by $\sim 1.5\%$, both are believed to be safely subcritical.

IV. DETERMINATION OF SHIELDING REQUIREMENTS

IV.A GENERATION AND SUBSEQUENT REDUCTION OF ORIGIN SOURCE TERMS

Before determining the actual shielding requirements as a function of the fuel's age, the various source terms had to be generated using the ORIGIN-2 code.¹⁹ Subsequent to that, much of the data had to be reduced to a form which was suitable for multigroup shielding analysis -- in particular, neutron spectra had to be generated for the spent fuel at various decay times. This section describes the burnup-dependent cross-section data that went into the ORIGIN calculation, the output from the ORIGIN calculation, and the subsequent data reduction that was necessary prior to the multigroup shielding analysis.

IV.A.1 CROSS-SECTION DATA USED BY ORIGIN

The ORIGIN calculations required to generate the necessary source terms may be performed in two stages: (1) The first stage follows the fuel while it is in the reactor. It is during this time that the various radioactive fission products and activation products are generated.

The higher-order actinides which dominate the neutron source and decay heat terms are also generated during this stage. To the extent that good burnup-dependent cross-section data was used in the ORIGIN calculation, these terms will be more (or less) accurate. (2) The second stage follows the fuel after it has been removed from the reactor. The accuracy of this calculation (determined primarily by the various decay constants) then has a profound influence on the neutron, photon, and decay heat sources at the various decay times of interest in this study.

The burnup-dependent one-group ORIGIN cross-section libraries for the "Standard PWR" and the "Standard BWR" were generated as noted in Ref. 13. In both cases, the starting point was an 84-group AMPX master library, complete with resonance parameters.¹⁴ For the standard 3-cycle PWR, three separate 84-group working libraries were prepared -- one for fuel having been in the reactor 0-12 months, one for fuel having been in the reactor 12-24 months, and one for fuel having been in the reactor 24-36 months. Typical mid-cycle number densities (N_1 , N_2 , N_3) corresponding to each composition were input to the NITAWL code¹⁵ which performed the necessary resonance self-shielding calculations for all the actinides using the Nordheim Integral Treatment.¹⁶ Each of the resulting 84-group libraries were then collapsed down

Fig. IV.1: Procedure Used for Processing the 84-Group AMPX Master Cross Section Library into Burnup-Dependent 1-Group ORIGIN Cross Sections

to five groups using a one-dimensional S_8P_3 XSDRNPM lattice-cell calculation.¹⁷ Since XSDRNPM first performs a detailed calculation for the fine group flux at each point in the fuel and moderator, and then defines the 5-group cell-averaged constants so as to preserve the various reaction rates, the new data inherently accounts for the local space/energy self-shielding effects within the individual fuel rods.

Having established the 5-group cell-weighted libraries described above, the CITATION code¹⁸ was then used to generate the 1-group burnup-dependent cross-section library to be used by ORIGEN.¹⁹ In reality, each of the three cell-weighted libraries generated by XSDRNPM correspond to different batches of fuel assemblies in a typical PWR (0-12 months, 12-24 months, 24-36 months). Because of the checkerboard loading pattern used in such reactors, the same "reactor-averaged" spectrum should be used to collapse these 5-group libraries down to 1-group. To determine the burnup-dependent 5-group reactor-averaged spectrum, a series of 3-region (neutronically homogeneous) infinite-reactor depletion calculations were performed as shown in Fig. IV.2. For the typical PWR in equilibrium, fresh fuel at 3.2 wt. % U^{235} is introduced in zone 1 at the beginning of each cycle, while spent fuel having acquired 33,000 MWD/MT burnup is discharged from zone 3 at the end of each cycle. Between cycles, the CITATION code automatically shuffles the remaining fuel from zone to zone and reassigns the 5-group cross-section data as necessary. At five different times during a typical cycle, CITATION would save the number densities and multigroup reaction rates in each zone, and subsequently use that data to define an appropriate set of 1-group cross-section data for each zone. Given the

PWR Fuel Management Calculation				
Zone	Fuel burnup at which 5-group cross sections were determined (GWD/MTHM)	Status of fuel at beginning of cycle	Status of fuel at end of cycle	Disposition of fuel at end of cycle
1	5	Fresh	Irradiated one cycle	Moved to Zone 2
2	16	Irradiated one cycle	Irradiated two cycles	Move to Zone 3
3	27	Irradiated two cycles	Irradiated three cycles	Discharge

- a) Cycle length was defined as 293.3 full-power days (@ 37.5 MW/MTHM)
 b) While one-dimensional slab geometry was used, the infinitesimal thickness of each zone (0.001 cm) effectively makes this a zero-dimensional calculation

Fig. IV.2: Three-Region, Neutronically Homogeneous, Infinite Reactor Model
 Used in the CITATION Fuel Management and Depletion Calculation(s)

5 time steps and the 3 zones (corresponding to first, second, and third-cycle fuel), this procedure yielded a unique, spectrum-weighted 1-group microscopic cross-section library for PWR fuel at each of 15 different burnup intervals. The burnup-dependent cross-section data for the 53 nuclides shown in Table IV.1 was then used to supplement the basic ORIGEN-2 library^{19,20} containing data for ~800 other nuclides.

The burnup-dependent one-group ORIGEN cross-section libraries for the "Standard BWR" were generated in a similar fashion. Because BWR fuel typically remains in core for four years (as opposed to three years in a PWR), four 84-group "working" libraries were prepared by NITAWL and four 5-group "weighted" libraries were subsequently prepared by XSDRNPM and used in CITATION. In equilibrium, fresh fuel at 2.75 wt. % U^{235} was introduced in zone 1 at the beginning of each cycle, while spent fuel having acquired 27,500 MWD/MT was discharged from zone 4 at the end of each cycle. One-group cross-section data for each zone was calculated and saved at five different times during a typical cycle, thus yielding a unique, spectrum-weighted 1-group microscopic cross-section library for BWR fuel at each of 20 different burnup intervals.

In addition to differences in initial enrichment, exposure time, and total burnup, the models used for the PWR and BWR also differed in several other respects: (1) The PWR lattice-cell calculations assumed ~550 ppm of soluble boron in the moderator. (In reality, this would vary from ~1100 ppm to ~0 ppm during each cycle); (2) The average void fraction used in the BWR lattice-cell calculations was assumed to be ~0.392 at 1050 psia. (Accurate depletion studies require that we weight those number densities in the high-power region of the reactor more heavily

Table IV.1
Nuclides Having Burnup-Dependent Cross-Section
Data in the ORIGEN2 Fuel-Depletion Calculation

H-1	B-10	B-11	C-12	N-14
O-16	Na-23	Cr-52	Mn-55	Fe-56
Ni-58	Zr-92	Nb-93	Mo-98	Sn-119
Th-232	Pa-233	U-233	U-234	U-235
U-236	U-237	U-238	U-239	U-240
Np-237	Np-238	Pu-236	Pu-238	Pu-239
Pu-240	Pu-241	Pu-242	Pu-243	Am-241
Am-242M	Am-242	Am-243	Cm-242	Cm-243
Cm-244	Cm-245	Cm-246	Cm-247	Cm-248
Bk-249	Cf-249	Cf-250	Cf-251	Cf-252
Cf-253	Cf-254	Es-253		

than those in the low-power region.¹³ Thus, while the volume-averaged void fraction in a typical BWR is appreciably higher (~ 0.5), the axial power-averaged void fraction used here is deemed more appropriate for this type of calculation). In a PWR where the moderator is $\sim 590^\circ\text{F}$ at 2250 psia, the average density was taken to be ~ 0.713 g/cc; (3) The specific power prescribed for the PWR and the BWR 5-group depletion calculations differed considerably. For the PWR we used 37.5 MW/MTHM, while for the BWR we used only 25.9 MW/MTHM. Further details may be found in Ref. 13.

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IV.A.2 RELEVANT DETAILS OF THE ACTUAL ORIGEN CALCULATION

The ORIGEN-2 code¹⁹ was used to generate the neutron, photon, and decay heat source terms found in Tables IV.2 and IV.3. As in the previous CITATION calculations, the PWR fuel was irradiated at a specific power of 37.5 MW/MTHM for 880 full-power days during a 3-year period, resulting in a net burnup of 33,000 MWD/MTHM. Likewise, the BWR fuel was irradiated at a specific power of 25.9 MW/MTHM for 1062 full-power days during a 4-year period, resulting in a net burnup of 27,500 MWD/MTHM. ORIGEN-2 was used to calculate the buildup of the various fission products, activation products, and higher-order actinides during this time. Subsequent ORIGEN-2 calculations were then used to determine the resulting neutron, photon, and decay heat source terms at various decay times, as shown in Tables IV.2 and IV.3.

The ORIGEN-2 code¹⁹ (as opposed to the original ORIGEN code²⁰) was used for a variety of reasons: (1) The cross sections of the principal fissile and fertile actinides could vary with burnup; (2) The recoverable energy per fission had been made nuclide-dependent; (3) The (α, n) yields from $\text{Cm}^{242}\text{O}_2$, $\text{Cm}^{244}\text{O}_2$, $\text{Pu}^{238}\text{O}_2$ and other oxides had been explicitly modeled to reflect recent experimental data;²⁶ and (4) The decay and photon libraries for ~450 isotopes had been recently updated to include the best available data currently in the Evaluated Nuclear Structure Data File²² at ORNL. For completeness, the significance of each of these will be touched on briefly:

Experience with more sophisticated reactor physics codes shows that the cross-section data for the principal actinide nuclides do not vary linearly with burnup and that the use of a constant, average cross-

Table IV.2
Neutron, Photon, and Decay Heat Source Terms for
Spent PWR Fuel Assemblies at Various Decay Times

FUEL AGE	DECAY HEAT WATTS/ASSY	NEUTRN SRC (N/S)/ASSY	GAMMA SRC (P/S)/ASSY
DISH	1.017E+06	3.654E+08	5.318E+18
120D	1.109E+04	2.614E+08	9.099E+16
150D	9.514E+03	2.423E+08	7.824E+16
1Y	4.697E+03	1.571E+08	3.990E+16
2Y	2.462E+03	1.098E+08	2.147E+16
3Y	1.533E+03	9.708E+07	1.366E+16
4Y	1.080E+03	9.179E+07	9.817E+15
5Y	8.452E+02	8.816E+07	7.804E+15
7Y	6.369E+02	8.200E+07	5.967E+15
8Y	5.862E+02	7.914E+07	5.500E+15
10Y	5.240E+02	7.374E+07	4.900E+15
13Y	4.708E+02	6.635E+07	4.349E+15
15Y	4.461E+02	6.188E+07	4.079E+15
18Y	4.163E+02	5.577E+07	3.743E+15
20Y	3.993E+02	5.207E+07	3.548E+15
23Y	3.766E+02	4.701E+07	3.287E+15
25Y	3.628E+02	4.395E+07	3.126E+15

*Assumes 0.4614 metric tonnes of heavy metal per
PWR fuel assembly and a burnup of 33,000 MWD/MT

Table IV.3
Neutron, Photon, and Decay Heat Source Terms for
Spent BWR Fuel Assemblies at Various Decay Times

FUEL AGE	DECAY HEAT WATTS/ASSY	NEUTRN SRC (N/S)/ASSY	GAMMA SRC (P/S)/ASSY
DISH	2.776E+05	1.216E+08	1.485E+18
120D	3.215E+03	8.446E+07	2.650E+16
150D	2.766E+03	7.764E+07	2.287E+16
1Y	1.383E+03	4.737E+07	1.184E+16
2Y	7.331E+02	3.077E+07	6.464E+15
3Y	4.620E+02	2.653E+07	4.175E+15
4Y	3.296E+02	2.494E+07	3.047E+15
5Y	2.610E+02	2.394E+07	2.453E+15
7Y	2.003E+02	2.229E+07	1.907E+15
8Y	1.855E+02	2.152E+07	1.767E+15
10Y	1.675E+02	2.008E+07	1.585E+15
13Y	1.519E+02	1.811E+07	1.415E+15
15Y	1.446E+02	1.692E+07	1.330E+15
18Y	1.356E+02	1.528E+07	1.224E+15
20Y	1.304E+02	1.430E+07	1.162E+15
23Y	1.234E+02	1.295E+07	1.077E+15
25Y	1.191E+02	1.212E+07	1.025E+15

*Assumes 0.1833 metric tonnes of heavy metal per
BWR fuel assembly and a burnup of 27,500 MWD/MT

section set (as in the original ORIGEN code) would not give satisfactory results for many nuclides of interest. In particular, it was found that the cross sections used near the end of an irradiation cycle had a much greater impact on the discharge fuel composition than those cross sections used near the beginning of the cycle.¹³ Use of the burnup-dependent cross sections described in the previous section greatly alleviated this difficulty.

In the past, the recoverable energy per fission ($REPF$) was assumed to be 200 Mev for all nuclides. Unfortunately, this is not a real good approximation for many nuclides, and the resulting calculated flux required to sustain a given power level near end-of-life was not terribly accurate. As a result, the discharge concentrations of those isotopes produced by neutron capture were known to contain some error. Since that time, the recoverable energy per fission has been measured for many nuclides and incorporated directly into the ORIGEN-2 data base.¹⁹ A semi-empirical correlation of the form $REPF = C_1 + C_2 Z^2 \sqrt{A}$ has also been adopted for those nuclides lacking experimental data.

Neutron sources in a spent fuel assembly can arise from spontaneous fission of the actinides or from (α, n) reactions wherein an alpha particle given off by Cm^{242} , Cm^{244} , Pu^{238} (or some other α -emitter) collides with the oxygen bound in the fuel matrix and releases a certain number of neutrons with a characteristic spectrum depending on the energy of the α particle. For fuel which is more than two or three years old, the spontaneous fission source is clearly predominate ($\geq 10:1$). For fuel which is ≤ 1 year old, the (α, n) source can represent between one-third and one-half of the total neutron source. Since the (α, n) neutron source spectra are considerably harder than the spontaneous fission

neutron source spectra, these sources must be properly accounted for in subsequent shielding calculations. In the original ORIGEN code, the correlation for the neutron yield per α -disintegration as a function of α -energy was based on experimental measurements involving a thin BeO_2 target.²⁰ Since that time, many measurements involving heavy-metal oxide matrices have been performed.^{25,26} ORIGEN-2 now contains measured (α, n) neutron yield data for seven of the principal contributors.* Semi-empirical correlations for lesser contributors (based on measurements in CmO_2 and PuO_2) have also been adopted.

Lastly, it should be noted that the decay and photon libraries used in the ORIGEN-2 code have been upgraded significantly. Whereas the original libraries contained data for ~850 isotopes, as taken from the "Table of Isotopes" published in 1968,²⁴ the new libraries contain updated data for ~450 of the principal radioactive nuclides.²¹ This new data was taken primarily from the Evaluated Nuclear Structure Data File as published in the Nuclear Data Sheets.^{22,23} The updated libraries contain new and revised data on isotope half-lives, decay energies, branching ratios, photon sources, and beta particles. The β -particle data has since been used to calculate the amount of bremsstrahlung radiation resulting from the deceleration of beta particles in the heavy-metal fuel matrix.

For engineering purposes, bremsstrahlung radiation can be defined as a continuous photon spectrum resulting from the deceleration of beta particles or positrons as they pass through the coulombic fields of surrounding nuclei. It differs from other photon sources in that its

* See Table 4.3 on page 41 of Ref. 19.

energy spectrum is always continuous. While bremsstrahlung radiation is negligible in many cases because of the general low intensity and energy of the radiation, it can be important in cases where nuclides with high-energy beta particles are present, or in cases where other high-energy, high-intensity gamma emitters are absent. In such cases, bremsstrahlung can contribute significantly to the penetrating radiation dose.

To amplify this point just a bit, one would do well to note that much (and in some cases, virtually all) of the higher-energy photon flux emanating from an older spent fuel assembly ($\geq$ 1-year-old) is due to the bremsstrahlung radiation resulting from Rh^{106} , Pr^{144} , and Y^{90} . These nuclides are produced from relatively long-lived precursor nuclides, as shown in Table IV.4. Note that these nuclides come into being long after many of the high-intensity gamma emitters have died out (1-2 years). Also note that they each decay by giving off a fairly high-energy (~ 3 Mev) beta particle. While the total bremsstrahlung-induced photon yield resulting from these nuclides is relatively low compared to the total photon source, the fact that they dominate the spectrum at higher energies makes them quite important in subsequent shielding calculations -- especially for older fuel.

Further information regarding the ORIGEN-2 code and the various decay and photon libraries may be found in References 19 and 21.

Table IV.4
 Several Important Mother-Daughter Chains
 Yielding Long-Term High-Energy β Particles

IV.A.3 REDUCTION OF THE ORIGIN SOURCE TERMS TO A USEFUL MULTIGROUP FORMAT

Using the ORIGEN-2 code,¹⁹ the neutron, photon, and decay heat source terms were generated for the standard PWR and BWR fuel assemblies at each of 17 decay times (discharge, 120d, 150d, 1y, 2y, ..., 25y). Tables IV.5 and IV.6 show a concise summary of this data on a "per assembly" basis. Note that each PWR fuel assembly was initially assumed to contain 0.4614 metric tonnes of heavy metal, while each BWR fuel assembly was initially assumed to contain 0.1833 metric tonnes of heavy metal. The decay heat source terms are useful just as they are. For the neutron and gamma source terms to be useful in a multigroup shielding calculation, however, requires some additional knowledge of the respective spectra.

For the actual multigroup shielding calculations described in Sections IV.B and IV.C, we used the DLC-23/CASK library, available from the Radiation Shielding Information Center at ORNL.²⁷ This coupled, 40-group (22n-18γ), P_3 cross-section library has undergone several revisions over the years, but still enjoys wide popularity throughout the industry.^{1,30} The 22-group neutron data used in this library was originally collapsed from a 104-group data set using the fine-group flux calculated by ANISN for a typical PWR spent fuel shipping cask.²⁷ Subsequently, the secondary gamma-production terms were calculated by the POPOP4 code, and the 18-group gamma cross sections were calculated using the MUG code.²⁷ More recently, the ANSI-standard flux-to-dose conversion factors²⁸ were calculated at ORNL using the DOSE code²⁹ (now available through RSIC as part of AMPX-II¹²). The 22-18 group structure and the corresponding

Table IV.5
Neutron, Photon, and Decay Heat Source Terms for
Spent PWR Fuel Assemblies at Various Decay Times

FUEL AGE	DECAY HEAT WATTS/ASSY	NEUTRN SRC (N/S)/ASSY	GAMMA SRC (P/S)/ASSY
DISH	1.017E+06	3.654E+08	5.318E+18
120D	1.109E+04	2.614E+08	9.099E+16
150D	9.514E+03	2.423E+08	7.824E+16
1Y	4.697E+03	1.571E+08	3.990E+16
2Y	2.462E+03	1.098E+08	2.147E+16
3Y	1.533E+03	9.708E+07	1.366E+16
4Y	1.080E+03	9.179E+07	9.817E+15
5Y	8.452E+02	8.816E+07	7.804E+15
7Y	6.369E+02	8.200E+07	5.967E+15
8Y	5.862E+02	7.914E+07	5.500E+15
10Y	5.240E+02	7.374E+07	4.900E+15
13Y	4.708E+02	6.635E+07	4.349E+15
15Y	4.461E+02	6.188E+07	4.079E+15
18Y	4.163E+02	5.577E+07	3.743E+15
20Y	3.993E+02	5.207E+07	3.548E+15
23Y	3.766E+02	4.701E+07	3.287E+15
25Y	3.628E+02	4.395E+07	3.126E+15

*Assumes 0.4614 metric tonnes of heavy metal per
PWR fuel assembly and a burnup of 33,000 MWD/MT

Table IV.6
Neutron, Photon, and Decay Heat Source Terms for
Spent BWR Fuel Assemblies at Various Decay Times

FUEL AGE	DECAY HEAT WATTS/ASSY	NEUTRN SRC (N/S)/ASSY	GAMMA SRC (P/S)/ASSY
DISH	2.776E+05	1.216E+08	1.485E+18
120D	3.215E+03	8.446E+07	2.650E+16
150D	2.766E+03	7.764E+07	2.287E+16
1Y	1.383E+03	4.737E+07	1.184E+16
2Y	7.331E+02	3.077E+07	6.464E+15
3Y	4.620E+02	2.653E+07	4.175E+15
4Y	3.296E+02	2.494E+07	3.047E+15
5Y	2.610E+02	2.394E+07	2.453E+15
7Y	2.003E+02	2.229E+07	1.907E+15
8Y	1.855E+02	2.152E+07	1.767E+15
10Y	1.675E+02	2.008E+07	1.585E+15
13Y	1.519E+02	1.811E+07	1.415E+15
15Y	1.446E+02	1.692E+07	1.330E+15
18Y	1.356E+02	1.528E+07	1.224E+15
20Y	1.304E+02	1.430E+07	1.162E+15
23Y	1.234E+02	1.295E+07	1.077E+15
25Y	1.191E+02	1.212E+07	1.025E+15

*Assumes 0.1833 metric tonnes of heavy metal per
BWR fuel assembly and a burnup of 27,500 MWD/MT

flux-to-dose conversion factors are shown in Table IV.7. Because of its wide popularity, and the fact that it was originally designed for the purpose of performing shielding calculations in spent fuel shipping casks, this library was selected for use in the present study.

Fortunately, ORIGEN-2 does generate an 18-group gamma source spectrum for the actinides, activation products, and fission products at each decay time. Each of these were, in fact, weighted by the corresponding yield to obtain the net spectrum at each decay time. Unfortunately, the ORIGEN-2 group structure (shown in Table IV.8) does not correspond exactly to the 18-group gamma structure used in the DLC-23 library. To affect the necessary transformation, the gamma sources were considered to be constant (i.e. independent of energy) within each ORIGEN energy group.^{*a,b} The resulting 18-group gamma spectra to be used in the multigroup shielding calculations are shown in Tables C.4 and C.5 of Appendix C. Note that a separate gamma spectrum is given at each of the 17 decay times (discharge, 120d, 150d, 1y, 2y, ..., 25y) for the spent PWR fuel and the spent BWR fuel.

Determining the corresponding neutron spectrum is not as straightforward, since ORIGEN-2 simply edits the total number of neutrons (by nuclide) resulting from spontaneous fission and/or (α ,n) reactions. Fortunately, the (α ,n) neutron source is due almost entirely to the

^{*a} For decay times in excess of one year, the fission products account for ~98% of the total photon source. Except for ORIGEN groups 9 and 10 where the direct gammas from Cs^{137} clearly dominate, much of the remaining photon source is due to the bremsstrahlung effect which does, in fact, yield a slowly varying continuous source spectra.

^{*b} While the highest energy group in the DLC-23 library did not completely overlap the highest energy group in the ORIGEN library, all the photons in that ORIGEN group were placed in group 1. As may be seen from Tables C.4 and C.5 of Appendix C, the resulting error is completely negligible.

Table IV.7
Energy Group Structure and ANSI Standard Flux-to-Dose Conversion Factors
Corresponding to the Coupled (22n-18v) DLC-23/CASK Cross-Section Library

NEUT GRP.	ENERGY RANGE		ANSI STANDARD DOSE FACTOR ^a	GAMMA GRP.	ENERGY RANGE		ANSI STANDARD DOSE FACTOR ^b
1	14.92 Mev	-- 12.20 Mev	194.49	1	10.00 Mev	-- 8.00 Mev	8.7716
2	12.20 Mev	-- 10.00 Mev	159.71	2	8.00 Mev	-- 6.50 Mev	7.4785
3	10.00 Mev	-- 8.18 Mev	147.06	3	6.50 Mev	-- 5.00 Mev	6.3748
4	8.18 Mev	-- 6.36 Mev	147.73	4	5.00 Mev	-- 4.00 Mev	5.4136
5	6.36 Mev	-- 4.96 Mev	153.39	5	4.00 Mev	-- 3.00 Mev	4.6221
6	4.96 Mev	-- 4.06 Mev	150.62	6	3.00 Mev	-- 2.50 Mev	3.9596
7	4.06 Mev	-- 3.01 Mev	138.92	7	2.50 Mev	-- 2.00 Mev	3.4686
8	3.01 Mev	-- 2.46 Mev	128.43	8	2.00 Mev	-- 1.66 Mev	3.0192
9	2.46 Mev	-- 2.35 Mev	125.27	9	1.66 Mev	-- 1.33 Mev	2.6276
10	2.35 Mev	-- 1.83 Mev	126.32	10	1.33 Mev	-- 1.00 Mev	2.2051
11	1.83 Mev	-- 1.11 Mev	128.94	11	1.00 Mev	-- .80 Mev	1.8326
12	1.11 Mev	-- 0.55 Mev	116.85	12	.80 Mev	-- .60 Mev	1.5228
13	550 Kev	-- 111 Kev	65.2090	13	.60 Mev	-- .40 Mev	1.1725
14	111 Kev	-- 3.35 Kev	9.1878	14	.40 Mev	-- .30 Mev	.87594
15	3350 ev	-- 583 ev	3.7134	15	.30 Mev	-- .20 Mev	.63061
16	583 ev	-- 101 ev	4.0086	16	.20 Mev	-- .10 Mev	.38338
17	101 ev	-- 29 ev	4.2946	17	.10 Mev	-- .05 Mev	.26693
18	29 ev	-- 10.100 ev	4.4761	18	.05 Mev	-- .01 Mev	.93477
19	10.100 ev	-- 3.060 ev	4.5673				
20	3.060 ev	-- 1.120 ev	4.5355				
21	1.120 ev	-- .414 ev	4.3701				
22	.414 ev	-- .010 ev	3.7142				

a) Flux-to-dose conversion factor given in $(\mu\text{Rem/hr})/(\text{neut/sec}\cdot\text{cm}^2)$

b) Flux-to-dose conversion factor given in $(\mu\text{Rem/hr})/(\text{proton/sec}\cdot\text{cm}^2)$

Table IV.8
Photon Energy Groups Used in ORIGEN2

PHOTON GROUP	ENERGY RANGE	

18	14.000 Mev	8.000 Mev
17	8.000 Mev	6.000 Mev
16	6.000 Mev	4.000 Mev
15	4.000 Mev	3.000 Mev
14	3.000 Mev	2.500 Mev
13	2.500 Mev	2.000 Mev
12	2.000 Mev	1.500 Mev
11	1.500 Mev	1.000 Mev
10	1.000 Mev	0.700 Mev
9	0.700 Mev	0.450 Mev
8	0.450 Mev	0.300 Mev
7	0.300 Mev	0.150 Mev
6	0.150 Mev	0.100 Mev
5	0.100 Mev	0.070 Mev
4	0.070 Mev	0.045 Mev
3	0.045 Mev	0.030 Mev
2	0.030 Mev	0.020 Mev
1	0.020 Mev	0.010 Mev

8 individual α -emitters shown in Table IV.9, of which Pu^{238} , Cm^{242} , and Cm^{244} are the most predominate. Each of these emit alphas of somewhat different energy, and each yields a characteristically different neutron spectrum in the heavy-metal oxide,^{31,32} as shown in Table IV.10. Converting each of these to the 22-group structure shown in Table IV.7, and weighting each according to their respective (α, n) neutron yields as given by ORIGEN-2, we can then reconstruct the "net" (α, n) neutron spectrum at each decay time. Likewise, the spontaneous fission neutron source is due almost entirely to the 6 nuclides shown in Table IV.11, of which Cm^{242} , Cm^{244} , and Cm^{246} are the most predominate. Each of these have a characteristically different spontaneous fission neutron spectrum,³¹ as shown in Table IV.12. Converting each of these to the 22-group structure shown in Table IV.7, and weighting each according to their respective (spontaneous fission) neutron yields as given by ORIGEN-2, we can then reconstruct the "net" spontaneous fission neutron spectrum at each decay time. Using the "net" spontaneous fission neutron spectrum and the "net" (α, n) neutron spectrum, and weighting them with the total spontaneous fission neutron source and the total (α, n) neutron source, we can then reconstruct a composite neutron spectrum characteristic of the spent fuel at each decay time. The resulting 22-group neutron spectra, to be used in the multigroup shielding calculations, are shown in Tables C.4 and C.5 of Appendix C. Note that a separate neutron spectrum is given at each of the 17 decay times (discharge, 120d, 150d, 1y, 2y, ..., 25y) for the spent PWR fuel and the spent BWR fuel.

Table IV.9
Principal Nuclides Responsible for the (α ,n) Neutron Source

% of (α ,n) neutron source due to alphas from	1-YR-OLD PWR FUEL	10-YR-OLD PWR FUEL
(Pu ²³⁸ , Cm ²⁴² , Cm ²⁴⁴)	98.50%	73.70%
(Pu ²³⁹ , Pu ²⁴⁰ , Am ²⁴¹ , Am ²⁴³ , Cm ²⁴³)	1.49%	26.29%
	-----	-----
	99.99%	99.99%

Table IV.10
Alpha-N Neutron Spectra Resulting from Cm-242,
Cm-244, and Pu-238 in an Oxide Matrix

Energy, Mev	Cm ²⁴²	Cm ²⁴⁴	Pu ²³⁸
0.1 --- 0.3	0.00025	0.00112	0.00126
0.3 --- 0.5	0.00050	0.00474	0.00500
0.5 --- 0.7	0.00100	0.00573	0.00789
0.7 --- 0.9	0.00125	0.01171	0.01368
0.9 --- 1.1	0.00381	0.01869	0.02209
1.1 --- 1.3	0.00752	0.02991	0.03577
1.3 --- 1.5	0.01404	0.04237	0.04998
1.5 --- 1.7	0.02257	0.05732	0.06313
1.7 --- 1.9	0.03812	0.06978	0.07891
1.9 --- 2.1	0.05016	0.08224	0.09469
2.1 --- 2.3	0.06521	0.09221	0.09995
2.3 --- 2.5	0.08527	0.09720	0.09995
2.5 --- 2.7	0.10032	0.09720	0.09995
2.7 --- 2.9	0.10534	0.09470	0.09469
2.9 --- 3.1	0.10534	0.08474	0.07891
3.1 --- 3.3	0.10534	0.06978	0.05787
3.3 --- 3.5	0.09029	0.05483	0.03998
3.5 --- 3.7	0.07524	0.03489	0.02367
3.7 --- 3.9	0.05016	0.02093	0.01578
3.9 --- 4.1	0.03260	0.01396	0.01052
4.1 --- 4.3	0.02006	0.00947	0.00631
4.3 --- 4.5	0.01404	0.00598	0.00000
4.5 --- 4.7	0.00903	0.00050	0.00000
4.7 --- 4.9	0.00251	0.00000	0.00000
	1.00000	1.00000	1.00000

*Normalized spectra generated from the energy-dependent yield data given in Refs. 31 and 32.

Table IV.11
Principal Nuclides Responsible for the
Spontaneous Fission Neutron Source

% of spontaneous fission neutron source due to spontaneous fission of	1-YR-OLD PWR FUEL	10-YR-OLD PWR FUEL
(Cm ²⁴² , Cm ²⁴⁴ , Cm ²⁴⁶)	98.51%	97.18%
(Pu ²³⁸ , Pu ²⁴⁰ , Pu ²⁴²)	1.46%	2.81%
	-----	-----
	99.99%	99.99%

Table IV.12
Spontaneous Fission Neutron Spectra Resulting
from Cm-242, Cm-244, and Cm-246

Energy, Mev	Cm ²⁴²	Cm ²⁴⁴	Cm ²⁴⁶
0.3 --- 0.4	0.03903	0.03849	0.03869
0.4 --- 0.6	0.08111	0.07973	0.08185
0.6 --- 0.8	0.08111	0.08156	0.08185
0.8 --- 1.0	0.07097	0.07056	0.07069
1.0 --- 1.2	0.07097	0.07239	0.07217
1.2 --- 1.4	0.07097	0.07148	0.07143
1.4 --- 1.6	0.06083	0.06231	0.06176
1.6 --- 1.8	0.05576	0.05590	0.05580
1.8 --- 2.0	0.05069	0.05040	0.04985
2.0 --- 2.2	0.04613	0.04582	0.04613
2.2 --- 2.4	0.04258	0.04215	0.04241
2.4 --- 2.6	0.04056	0.04032	0.04092
2.6 --- 2.8	0.03295	0.03299	0.03274
2.8 --- 3.0	0.02788	0.02749	0.02753
3.0 --- 3.2	0.02535	0.02566	0.02530
3.2 --- 3.4	0.02535	0.02474	0.02530
3.4 --- 3.6	0.02383	0.02383	0.02381
3.6 --- 3.8	0.01876	0.01924	0.01860
3.8 --- 4.0	0.02078	0.02016	0.02083
4.0 --- 4.4	0.02636	0.02658	0.02604
4.4 --- 4.8	0.02231	0.02199	0.02158
4.8 --- 5.2	0.01622	0.01650	0.01637
5.2 --- 5.6	0.01267	0.01283	0.01265
5.6 --- 6.0	0.00963	0.01008	0.00893
6.0 --- 6.4	0.00760	0.00751	0.00744
6.4 --- 6.8	0.00558	0.00541	0.00536
6.8 --- 7.2	0.00380	0.00376	0.00379
7.2 --- 7.6	0.00284	0.00284	0.00283
7.6 --- 8.0	0.00238	0.00229	0.00238
8.0 --- 8.8	0.00253	0.00257	0.00253
8.8 --- 9.6	0.00076	0.00075	0.00074
9.6 --- 10.4	0.00081	0.00079	0.00082
10.4 --- 11.2	0.00051	0.00052	0.00052
11.2 --- 12.8	0.00036	0.00036	0.00036
	1.00000	1.00000	1.00000

*Normalized spectra generated from the energy-dependent yield data given in Reference 31.

IV.B OPTIMAL n/ γ SPLIT FOR SELECTING RELATIVE AMOUNTS OF NEUTRON AND GAMMA SHIELDING

The purpose of this conceptual design study was to develop a new generation of lead, iron, and uranium-metal spent fuel casks which have been optimized for the shipment of 1,2,3,5,7 and 10-year-old PWR spent fuel assemblies. Because the neutron and gamma source terms have decayed significantly (relative to 120-day-old fuel), less neutron and gamma shielding is required. While observing previous restrictions on the overall weight, these thinner shields can be moved outward, thus expanding the inner cavity and the net carrying capacity of the cask. A necessary and very important part of this study, therefore, was the accurate determination of the neutron and gamma shielding requirements for each type of cask as a function of the age of the fuel and the number of assemblies in the cask (cf. Section IV.C). Prior to that, however, it was necessary to establish some criteria for determining the relative amounts of neutron and gamma shielding that should be used in each case. Obviously, one could use such a thick gamma shield that no neutron shield would be required, or such a thick neutron shield that no gamma shield would be required. Because the borated water is not a particularly good gamma shield and because the heavy-metal is not a particularly good neutron shield, there is an "optional" n/ γ split which would minimize the weight of the cask in each case. Because of the different shielding materials being considered (i.e., Pb/water, Fe/water, and U-metal/water), a separate determination had to be made for each type of cask. Likewise, because the neutron and gamma source terms decay at different rates and the average energy of each spectra shifts

as a function of decay time, there was no "a priori" reason to assume that the optimal n/λ split(s) would remain constant. Indeed, in the case of the 1-year-old spent fuel shipping cask, the presence of long, thick stainless-steel fins required on the exterior of the cask for effective heat transfer dictated the choice of a somewhat different n/λ split than might be used if the cask were bare or had copper fins. The purpose of this section is to describe how the optimal n/λ split was established in each case. Application of this information and discussion of the actual shielding requirements as a function of the age of the fuel and the number of assemblies in the cask is deferred to Section IV.C.

IV.B.1 CALCULATIONAL MODEL AND PROCEDURE USED IN ESTABLISHING THE SHIELDING REQUIREMENTS

As described in Section IV.B.2, a one-dimensional multigroup S_2P_3 transport calculation was typically used to determine the flux distribution throughout the cask and the resulting dose rate 10 feet from the centerline. Typically, the dry spent fuel and the aluminum insert were modeled as a large homogenized region on the interior of the cask. The diameter of that region would, of course, depend on the thickness of the insert and the number of fuel assemblies inside the cask (cf. Appendix E). As noted in Section II, each cask was assumed to consist of an inner shell, a gamma shield, an outer shell, a neutron shield, and an outside liner. The thickness of the inner shell, outer shell, and outside liner were fixed as shown in Table II.1, depending on the type of gamma shield being considered. Additional details regarding the model and the actual calculations are given in Section IV.B.2. Because the total dose rate 10 feet from the centerline was fixed at 10 mrem/hr, we really performed a set of "zone width" search calculations to determine the shielding requirements in each case. In each case, the calculations proceeded in the following fashion:

- (1) The neutron source terms in the homogenized fuel region were set to zero, leaving only the gamma source terms. Likewise, the thickness of the neutron shield and the outside liner were both set to zero.
- (2) Given this configuration, the thickness of the gamma shield was varied until some prescribed dose rate ($\pm 1\%$) was achieved at a point 10 feet from the centerline.

This prescribed dose rate (D'_γ) represents the first of two design points. It is a free parameter which we varied in our search for the optimal n/γ split. In a typical zone width search, the prescribed dose rate ($\pm 1\%$) could usually be obtained after 4 or 5 iterations. Each iteration, of course, involved a full-blown multigroup 1-D transport calculation using the 18-group gamma source.

- (3) Having determined and "fixed" the thickness of the gamma shield, the second phase of the calculation could begin. In this phase, both the neutron and the gamma source terms were assumed to be present in the homogenized fuel region. The neutron shield and outside liner were also restored, with the outside liner having a fixed thickness as noted in Table II.1.
- (4) Given this restored configuration, the thickness of the neutron shield was varied until the total dose rate 10 feet from the centerline was reduced to 10 mrem/hr ($\pm 1\%$). This second "design point" was considered fixed in all of our calculations. Note that it includes the dose due to escaping neutrons and secondary gammas as well as the dose due to primary gammas. As in the previous case, the prescribed dose rate ($\pm 1\%$) could usually be obtained after 4 or 5 iterations. Here, however, each iteration involved a full-blown 40-group 1-D transport calculation using the 22-group neutron source and the 18-group gamma source.

IV.B.2 COMPUTER CODES USED IN ESTABLISHING THE SHIELDING REQUIREMENTS

The XSDRNPM code³⁵ was used to perform the one-dimensional multi-group n/γ transport calculations for the flux distribution throughout the cask. Neutron multiplication and self-absorption within the homogenized fuel region were both properly accounted for. Likewise, spatial self-shielding of the gammas by the fuel matrix was properly accounted for.* Use of the P_3 cross-section data found in the DLC-23 library²⁷ allowed us to account for the forward-directed anisotropic scattering terms which are characteristic of high-energy neutrons and gamma-rays, and which are quite important in deep-penetration shielding problems. Use of an S_8 angular quadrature (having 24 discrete angular directions) allowed us to account for the increasingly anisotropic nature of the angular flux as one moves further and further out into the shield. Earlier scoping calculations using S_2 , S_4 , S_6 , S_8 , S_{12} , and S_{16} quadrature sets showed the S_8 quadrature to yield quite acceptable results at reasonable cost.

Given the angular flux on the surface of the cask, the XSDOSE code³⁴ was used to calculate the resulting dose rate at the midplane, 10 feet from the centerline. This code, recently developed for the SCALE system at ORNL,³³ directly integrates the angular flux as seen from a point some distance removed from a finite surface having a known flux distribution. Because of the direct numerical integration, this method circumvents the difficulty that discrete-ordinate codes have in representing the angular

* Given that $\lesssim 15\%$ of all the photons emitted from a fuel assembly ever get out of the fuel assembly, this self-shielding effect is quite important. It also explains why the neutron and gamma shielding requirements are relatively insensitive to the total number of fuel assemblies in a cask.

streaming of neutrons in large voids exterior to a cylindrical surface. More importantly, the integration is carried out only over a finite surface, thus yielding a somewhat more realistic result than what might be obtained from an infinite 1-D calculation. (Because the angular flux distribution from the infinite 1-D XSDRNPM calculation is assumed to be spatially uniform over the finite surface, the XSDOSE results will still be somewhat conservative). Having calculated the multigroup scalar flux 10 feet from the centerline, XSDOSE then applies the ANSI-Standard flux-to-dose conversion factors shown in Table IV.7 to obtain the resulting neutron and gamma dose rates at the point of interest.

The XSDRNPM and XSDOSE codes are both available from the Radiation Shielding Information Center at ORNL. Both are presently included in the AMPX-II system, and in the more recent SCALE system for Standardized Computational Analyses and Licensing Evaluation.^{12,33}

Within the framework of the SCALE system, a special control module (known as CASKSIZE)^{*} was written to perform the type of analyses described in Section IV.B.1. With only a minimal amount of engineering input, it would set up and perform a series of XSDRNPM and XSDOSE calculations to determine the amount of gamma shielding required and the amount of neutron shielding required. At the end of each XSDRNPM/XSDOSE calculation, the CASKSIZE control module would read the results which had been saved in a special on-line file, extrapolate or interpolate the particular zone width as required, and then set up and perform the next series of calculations. This automated procedure proved invaluable in performing the large number of calculations required for this study.

* The CASKSIZE control module described above was developed as a special-purpose in-house analysis tool for this particular study. While it does operate within the environment provided by the SCALE system, there are no plans to distribute it as part of the official NRC-sponsored package.³³

IV.B.3 OTHER BACKGROUND INFORMATION RELEVANT TO THE SHIELDING CALCULATIONS

To determine the optimal n/λ split which would minimize the total weight for each type of cask, a number of shielding calculations were performed using the procedure described in Section IV.B.1. For a given type of cask, one could perform a series of such calculations -- each time changing the initial design point (D'), determining the corresponding shielding requirements, and noting the effect of those shield thicknesses on the overall weight of the loaded cask. Indeed, the studies to obtain the optimal n/λ design point were performed in exactly that fashion.

In addition to determining the shielding requirements as described in Section IV.B.1, the CASKSIZE program also calculates the total weight of the loaded cask using the rather simplified model shown in Fig. IV.3. Although not shown, it also accounts for the weight of the spent fuel and the aluminum or copper insert on the interior of the cask. For the purpose of this n/λ optimization study, the CASKSIZE program assumed an internal cavity length of 144 inches. Allowing for a 13.8-ft. fuel assembly and a 6-inch axial shock absorber, as was done in the SCOPE calculations described in Section VII, would, of course, yield a somewhat higher and more realistic estimate of the overall weight. Either way, the parameter to be optimized (weight per unit length) would be essentially the same in each case, and the sought-after minima in the W vs. D' curve would occur at the same point.

At the time that these shielding calculations were being performed, the insert design described in Section III had not yet fully evolved. While aluminum had been decided on as the best insert material for casks containing 3, 5, 7 and 10-year-old fuel, there was some thought given to

Fig. IV.3: Simplified Model Used by the CASKSIZE Shielding Code When Estimating the Overall Weight of the Loaded Cask

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- a) The aluminum insert and spent fuel assemblies were treated as a single homogenized zone in the 1-D shielding calculation.
- b) The diameter of this zone did, of course, depend on the number of assemblies and the thickness of the insert between assemblies.
- c) The thicknesses of the other components in the axial direction were then assumed to be the same as in the radial direction.
- d) The actual thickness of the inner shell, outer shell, and outside liner varied with the type of cask, as shown in Table II.1.

the use of copper for casks containing 1 and 2-year-old fuel. It was thought, for example, that the higher conductivity and the higher melting point of a copper insert might offer a significant advantage in those cases where the insert had to dissipate larger amounts of decay heat. To verify that calculations based on an aluminum insert would yield conservative results, a short series of preliminary calculations were performed for Pb/water, Fe/water, and U-metal/water casks, each containing 8 (and later 15) 1-year-old spent fuel assemblies. In each case the calculations were performed twice -- first using a 1-inch aluminum separator between the fuel assemblies and then again using a 1-inch copper separator. The results are shown in Table IV.13. Note that in each case, the casks with the copper inserts required slightly less neutron shielding and slightly less gamma shielding. Despite the slightly reduced shielding requirements, those casks with the copper inserts consistently weighed more than those casks with the aluminum inserts (owing to the greater mass of the insert itself). Because the use of an aluminum insert typically reduces the overall weight of the loaded cask and consistently led to a conservative estimate of both the neutron and gamma shielding requirements, a 1-inch thick solid aluminum insert was assumed in all of the subsequent studies in this section and in Section IV.C.

Table IV.13
Effect of a 1" Copper vs a 1" Aluminum Insert on the Shielding Requirements
and Overall Weight of Various Casks Containing 1-Year-Old PWR Spent Fuel

TYPE OF G-SHLD	NO. OF ASSYS	TYPE OF INSERT	INITIAL DESIGN POINT (MR/HR)	RESULTING GAMMA SHLD THICKNESS (INCHES)	FINAL DESIGN POINT (MR/HR)	RESULTING NEUT. SHLD THICKNESS (INCHES)	TOTAL MASS OF LOADED CASK IN MET. TONNES	NOMINAL GAMMA DOSE 10-FT FROM CENTR-LINE (MR/HR)	NOMINAL NEUT. DOSE 10-FT FROM CENTR-LINE (MR/HR)
PB	15	AL	30	5.86	10	5.10	79.26	9.45	0.562
PB	15	CU	30	5.34	10	4.84	90.34	9.57	0.432
PB	8	AL	30	5.58	10	5.01	58.06	9.58	0.428
PB	8	CU	30	5.06	10	4.82	63.89	9.70	0.317
FE	15	AL	30	12.37	10	3.92	90.11	9.82	0.216
FE	15	CU	30	11.57	10	3.78	100.18	9.81	0.198
FE	8	AL	30	11.95	10	3.89	67.06	9.80	0.166
FE	8	CU	30	11.16	10	3.75	71.95	9.86	0.155
U	15	AL	30	3.47	10	5.23	70.04	9.58	0.438
U	15	CU	30	3.18	10	4.98	82.08	9.75	0.327
U	8	AL	30	3.31	10	5.17	50.50	9.73	0.328
U	8	CU	30	3.02	10	5.02	57.20	9.81	0.234

IV.B.4 DETERMINATION OF THE OPTIMAL n/ γ SPLIT FOR 10-YEAR-OLD FUEL

To determine the optimal n/ γ split which would minimize the total weight for each type of cask, a number of shielding calculations were performed using the procedure described in the previous three sections. Based on some preliminary calculations involving 10-year-old fuel, it was assumed that each cask contained 21 PWR spent fuel assemblies arranged as shown in Appendix B and separated by a 1-inch thick solid aluminum insert. For each type of cask [Pb/water, Fe/water, and U-metal/water], we performed a series of such calculations -- each time changing the initial design point (D_Y^1), determining the corresponding shielding requirements, and noting the effect of those shield thicknesses on the overall weight of the loaded cask.

Results for the Pb/water cask are shown in Table IV.14, a typical line of which may be interpreted in the following manner: "Assuming no neutron source, no neutron shield, and no outside liner, the dose rate ten feet from the centerline could be reduced to 20 mr/hr by using 4.24 inches of gamma shielding between the inner and outer shell. Given that amount of gamma shielding, and now accounting for the neutron shield and outside liner as well as the presence of both the neutron and gamma sources in the fuel, the dose rate ten feet from the centerline could be reduced to 10 mr/hr by using 2.80 inches of neutron shielding between the outer shell and the outside liner. The total mass of the loaded cask (assuming the assemblies to be 12.0 feet long) would then be 70.89 metric tonnes. Ten feet from the centerline, the total gamma dose rate (including primary and secondary gammas) would be 7.36 mr/hr, while the

Table IV.14
 Alternate Sets of Shielding Thicknesses and Subsequent n/γ Dose Rates for a Pb
 Cask Containing 21 10-Year-Old PWR Fuel Assemblies ($R_1 = 71.8$ cm; $L_1 = 12$ ft)

INITIAL DESIGN POINT (MR/HR)	RESULTING GAMMA SHLD THICKNESS (INCHES)	FINAL DESIGN POINT (MR/HR)	RESULTING NEUT. SHLD THICKNESS (INCHES)	TOTAL MASS OF LOADED CASK IN MET. TONNES	NOMINAL GAMMA DOSE 10-FT FROM CENTR-LINE (MR/HR)	NOMINAL NEUT. DOSE 10-FT FROM CENTR-LINE (MR/HR)	ACCIDENTAL GAMMA DOSE 10-FT FROM CENTR-LINE (MR/HR)	ACCIDENTAL NEUT. DOSE 10-FT FROM CENTR-LINE (MR/HR)
5	5.15	10	1.48	77.21	2.43	7.66	2.71	36.4
8	4.84	10	1.70	74.78	3.59	6.40	4.26	37.1
13	4.52	10	2.10	72.50	5.38	4.62	6.88	37.8
20	4.24	10	2.80	70.89	7.36	2.62	10.5	38.5
25	4.09	10	3.40	70.34	8.41	1.63	13.1	38.9
30	3.97	10	4.13	70.18	9.07	0.964	15.7	39.2
37	3.83	10	5.25	70.34	9.55	0.460	19.3	39.5
45	3.71	10	6.43	70.73	9.78	0.229	23.4	39.8
70	3.42	10	9.38	72.13	9.99	0.0537	36.3	40.6
150	2.92	10	14.62	75.64	9.98	0.0069	77.7	41.9

total neutron dose rate would be 2.62 mr/hr. Should the cask ever be damaged such that the neutron shield is rendered void, the total gamma dose rate ten feet from the centerline would rise to 10.5 mr/hr while the total neutron dose rate at that point would rise to 38.5 mr/hr.⁸ Examining these results, one can see that the overall weight of the loaded cask (W) passes through a minima when the initial design point (D'_1) is 30 mr/hr. These results, along with those for the Fe/water cask and the U-metal/water cask, are plotted in Fig. IV.4.⁸ Note that the Fe/water cask passes through the minima when an initial design point of 35 mr/hr is used, while the U-metal/water cask passes through the minima when an initial design point of 25 mr/hr is used. That their respective minimas are so close together is most interesting, especially when one considers the significant differences in their densities and stopping powers. In passing, one might also note that a U-metal cask will typically weigh considerably less than a Pb cask of equal capacity, and that the Pb cask will typically weigh considerably less than an Fe cask of equal capacity.

⁸ Numerical results like those shown in Table IV.14 are given in Appendix D for all three types of casks.

Fig. IV.4: Weight of Pb, Fe, and U-metal Casks for 10-Year-Old PWR Spent Fuel, as a Function of the Initial Design Point Used to Determine the Relative Amount of Neutron and Gamma Shielding

IV.B.5 DETERMINATION OF THE OPTIMAL n/γ SPLIT FOR 1-YEAR-OLD FUEL

To determine the optimal n/γ split for casks containing 1-year-old fuel, we proceeded exactly as in the previous section. Based on some preliminary calculations involving 1-year-old fuel, it was assumed that each cask contained 13 PWR spent fuel assemblies arranged as shown in Appendix B and separated by a 1-inch thick solid aluminum insert. For each type of cask, we performed a whole series of calculations as noted previously. Numerical results, like those shown in Table IV.14, are given in Appendix D for all three types of 1-year-old spent fuel casks. The corresponding plots of the total loaded weight as a function of the initial design point (D_i) are shown in Fig. IV.5. While the optimal initial design points (23, 26, and 30 mr/hr) did shift slightly to the left of the corresponding points for the 10-year-old fuel (25, 30, and 35 mr/hr), the shift was remarkably little -- especially when one notes that the ratio of the photon to neutron source density is four times greater than it was in the older fuel!

Unfortunately, the final determination of the optimal n/γ split (or the optimal initial design point) is not as simple for the 1-year-old spent fuel cask as it was for the 10-year-old spent fuel cask. As noted in Section VII, those casks designed to carry older spent fuel did not require the use of fins to dissipate the decay heat to the environment. On the other hand, casks containing 1 and 2-year-old spent fuel (and some casks containing 3-year-old spent fuel) did require fins in order to dissipate the decay heat and keep the outer skin temperature under 250°F. In the case of the 1-year-old spent fuel casks, for example, the fins often added as much as 10-15,000 lbs. to the weight of the cask -- markedly changing the weight per unit length. Designs which emphasized

Fig. IV.5: Weight of Pb, Fe, and U-metal Casks for 1-Year-Old PWR Spent Fuel, as a Function of the Initial Design Point Used to Determine the Relative Amount of Neutron and Gamma Shielding

the gamma shielding and had lower initial design points ($D' \lesssim 30$) tended to have longer, thicker fins than those (larger diameter) casks which emphasized neutron shielding. As a consequence, the whole W vs D' curve would become sharper and shift somewhat to the right if one were to include the weight of the fins resulting from the steady-state heat transfer design analysis.

To assess the impact of the necessary fins on the optimal n/v split (ie, on the optimal initial design point, D'_v), the shielding thicknesses shown in Tables D.1-D.3 of Appendix D were used as input to the SCOPE code⁷ which then generated a series of somewhat more realistic designs^{#a} corresponding to the description given in Section II. The results for the Pb/water casks are shown in Table IV.15; results for the Fe/water casks and the U-metal/water casks are included in Appendix E. Shown in Table IV.15 is the overall weight of various Pb/water casks having stainless-steel fins and designed to carry 10, 11, or 12 spent fuel assemblies; also shown is the overall weight of various Pb/water casks having copper fins and designed to carry 11, 12, or 13 spent fuel assemblies.^{#b,c} Note that each

^{#a} These more realistic designs have an internal cavity length of 171.6 inches so as to accommodate a 13.8-ft PWR fuel assembly while leaving an additional 6-inch space for an internal axial shock absorber.

^{#b} As noted above, the shielding thicknesses used in each case were those previously calculated assuming 13 assemblies per cask. In fact, this is a pretty good approximation since the shielding requirements are rather insensitive to the actual number of assemblies in the cask (cf. Appendix F).

^{#c} The length and thickness of the fins vary from case-to-case. In each case, a careful numerical search was conducted to determine the particular fin dimensions which would dissipate the decay heat under the prescribed constraints while minimizing the overall weight of the loaded cask. (See Section V.B for additional details regarding this search). Because of their lower thermal conductivity, the stainless-steel fins tended to be longer, thicker, and heavier than the copper fins.

Table IV.15
Actual Weight of Several 1-Year-Old Stainless-Steel Finned and
Copper-Finned Pb Casks as a Function of the Initial Design Pt.

INITIAL DESIGN POINT (MR/HR)	RESULTING GAMMA SHLD THICKNESS (INCHES)	RESULTING NEUT. SHLD THICKNESS (INCHES)	TOTAL WEIGHT OF FINNED CASKS AS FUNCTION OF INITIAL DESIGN POINT					
			STAINLESS-STEEL-FINNED CASKS			COPPER-FINNED CASKS		
			10-ASSYS	11-ASSYS	12-ASSYS	11-ASSYS	12-ASSYS	13-ASSYS
5	7.21	1.80	196.9	215.0	231.2	193.4	194.7	207.2
7	6.95	1.96	192.6	211.8	226.9	188.8	190.1	202.4
12	6.54	2.39	186.7	205.8	221.1	182.2	183.5	195.5
20	6.14	3.34	181.6	200.8	216.3	177.8*	179.1*	190.8*
25	5.97	4.11	179.8	196.6	211.5	177.3*	177.6*	190.3*
30	5.83	5.15	179.5	196.3	208.8	177.1*	178.5*	190.1*
37	5.67	6.52	177.1*	194.6	207.4	179.1	180.5	192.1
45	5.52	7.98	177.0*	193.0*	205.7	181.6	181.9	194.7
75	5.13	11.98	178.9	193.3*	200.9*	189.2	189.5	202.6
110	4.84	15.02	185.1	194.7	202.1*	195.7	197.4	209.4
150	4.61	17.45	190.8	201.7	203.4	203.2	205.0	217.1

- a) Weight of loaded cask given in 1000-lb units (194.5 = 194,500 lbs).
- b) While the shielding requirements shown here are based on a cask containing 13 1-year-old FWR fuel assemblies (cf. Table D.1), the shielding requirements for casks containing 10 or 11 assemblies will be essentially the same (cf. Table IV.18).
- c) The asterisks in each column denote those designs whose overall weight is very close to the minima for the cask in question.

line of Table IV.15 corresponds to a different initial design point (D_Y'), as designated in the left-most column. From Table IV.15, we see that casks with stainless-steel fins have an optimal initial design point near 45 mr/hr while those casks with copper fins have an optimal initial design point near 25 mr/hr. While both satisfy the 10 mr/hr total dose requirement ten feet from the centerline, use of the larger initial design point requires about twice as much neutron shielding as does the other (i.e., the "make-up" of the shield would be quite different). Interestingly enough, we note that the optimal initial design point for the cask with copper fins is very close to that obtained for the bare cask (see Fig. IV.5), while that obtained for the cask with stainless-steel fins is significantly different. The explanation, of course, lies in the thermal conductivity of the two materials [~ 11.0 (Btu/hr)/ft $^{\circ}$ F for the stainless-steel, and ~ 210.0 (Btu/hr)/ft $^{\circ}$ F for the copper]. Because of their lower thermal conductivity, the stainless-steel fins tend to be longer, thicker, and heavier than the copper fins. Because they are significantly heavier, their impact on the W vs. D_Y' curve is more pronounced. On the other hand, if the thermal conductivity were nearly infinite, one could have very long infinitely thin fins dissipate an arbitrarily large amount of decay heat while adding nothing to the weight of the cask. In that limit, the optimal design point would be no different than that for the bare cask.

The same general tendencies noted in Table IV.15 also appear to be true for the Fe/water cask and the U-metal/water cask (cf. Appendix E). Recommended values of the initial design point (D_Y') for various types of casks containing 1-year-old spent fuel are given in Table IV.16. These values were used in subsequent shielding calculations, as noted in Section IV.C.

Table IV.16
 Recommended Values of the Initial Design
 Point for Various Casks Designed to Carry
 1-Year-Old Spent Fuel (D_Y' given in mr/hr)

TYPE OF CASK	SS-304 FINS	COPPER FINS	BARE CASK
U-METAL	45*	23	23
LEAD	45	26	26
IRON	37	30	30

*Optimal initial design point in this case was actually 70-150 mr/hr. In that range, however, the neutron shield was unrealistically thick (ie, 12-18 inches). Because of this, 45 mr/hr was selected as the best realistic value.

IV.B.6 DETERMINATION OF THE OPTIMAL n/ γ SPLIT FOR 2, 3, 5 AND 7-YEAR-OLD FUEL

While it was anticipated that the optimal initial design points (D_Y^1) for 3-year-old spent fuel casks would be bracketed by the earlier results, a final set of calculations were performed to verify that assumption. Based on some preliminary scoping calculations involving 3-year-old fuel, these detailed calculations were performed assuming that each cask contained 15 PWR spent fuel assemblies arranged as shown in Appendix B. Numerical results for these calculations are shown in Appendix D. The corresponding plots of the loaded cask weight as a function of the initial design point (D_Y^1) are shown in Figure IV.6. Note that the optimal design points are, in fact, bracketed by the results for the 1-year-old and 10-year-old spent fuel casks (cf. Fig. IV.6 and/or Tables D.4-D.6). It should also be noted that subsequent design calculations using the SCOPE code have since confirmed the assumption that fins were either not necessary (in the case of the Pb/water cask and the Fe/water cask), or so small ($\leq 2''$ high $\times$ 0.25'' thick) that they had no impact on the choice of the optimal n/ γ split (cf. Table VII.3 of Section VII.B).

Armed with the above information, one can confidently interpolate the optimal initial design points (D_Y^1) for the 5-year-old and 7-year-old spent fuel casks, as shown in Table IV.17.

Likewise, one can also interpolate the optimal initial design points (D_Y^1) for the 2-year-old spent fuel casks, as shown in Table IV.17. In this case, however, as in the case of the 1-year-old spent fuel casks, it was necessary to assess what impact the fins might have on the optimal

Fig. IV.6: Weight of Pb, Fe, and U-metal Casks for 3-Year-Old PWR Spent Fuel, as a Function of the Initial Design Point Used to Determine the Relative Amount of Neutron and Gamma Shielding

Table IV.17
 Recommended Values of the Initial Design
 Point for Casks Designed to Carry 1,2,3,
 5,7 or 10-Yr-Old Spent Fuel (D_Y^i in mr/hr)

AGE	U	PB	FE
1y	23	26	30
2y	24	27	31
3y	24	28	32
5y	24	29	33
7y	25	29	34
10y	25	30	35

*Values for the 1, 3 and 10-year-old spent fuel were independently determined. All of the others were simply interpolated.

initial design point. Using the values shown in Table IV.17, we generated the corresponding shielding requirements for all three types of casks and subsequently performed a more detailed series of design calculations using the SCOPE code. In each case, the necessary fins were found to be so small ($\leq 3''$ high x $0.25''$ thick) that they should have no impact on the choice of the optimal n/ γ split (cf. Table VII.3 of Section VII.B). A more rigorous investigation similar to that performed for the 1-year-old spent fuel casks confirmed that finding.

IV.C SHIELDING REQUIREMENTS AS A FUNCTION OF DECAY TIME

The purpose of this conceptual design study was to develop a new generation of lead, iron, and uranium-metal spent fuel casks which have been optimized for the shipment of 1,2,3,5,7 and 10-year-old PWR spent fuel assemblies. A necessary and very important part of this study was the accurate determination of the neutron and gamma shielding requirements for each type of cask as a function of the spent fuel's decay time and the number of assemblies in the cask. A detailed compilation of those shielding requirements has been included as Appendix F. For quick reference, a more abbreviated listing of that data is also given in Tables IV.18 and IV.19. Note that these results were obtained following the procedure outlined in Section IV.B.1, using the optimal design points (D_Y^*) given in Tables IV.16 and IV.17. In each case, the total dose rate 10 feet from the centerline was calculated to be 10 mrem/hr. It should also be noted that each cask was assumed to consist of an inner shell, a gamma shield, an outer shell, a neutron shield, and an outside liner as shown in Fig. II.1. The thickness of the inner shell, outer shell, and outside liner were assumed to depend on the type of cask, as noted in Table II.1. In each case, the dry spent fuel and the (1" thick) aluminum insert were modeled as a large homogenized region on the interior of the cask. The diameter of that region varied with the number of fuel assemblies as shown in Appendix B.

Table IV.18
Optimized Shielding Requirements for Various Types of
1-Year-Old Spent Fuel Shipping Casks, as a Function of
the Number of Assemblies in the Cask

1-YEAR-OL PWR SPENT FUEL SHIPPING CASKS					
TYPE FIN	TYPE CASK	NO. OF ASSYS	INITIAL DESIGN POINT	THICKNESS (IN INCHES)	
				G-SHIELD	N-SHIELD
SS	PB	1	45	4.58	8.21
SS	PB	8	45	5.31	8.04
SS	PB	13	45	5.52	7.98
SS	FE	1	37	10.67	5.45
SS	FE	8	37	11.76	5.36
SS	FE	13	37	12.08	5.41
SS	U	1	45 ^d	2.74	8.54
SS	U	8	45 ^d	3.16	8.41
SS	U	13	45 ^d	3.28	8.31
CU	PB	1	26	5.00	3.77
CU	PB	8	26	5.73	4.14
CU	PB	15	26	5.99	4.41
CU	FE	1	30	10.91	3.83
CU	FE	8	30	12.00	3.91
CU	FE	15	30	12.40	3.96
CU	U	1	23	3.03	2.98
CU	U	8	23	3.44	3.52
CU	U	15	23	3.60	3.73
CU	U	1	37 ^e	2.82	6.89
CU	U	10	37 ^e	3.30	6.86
CU	U	15	37 ^e	3.39	6.83

- a) As noted in text, the type of fin used on the cask influenced the choice of the initial design point in the shielding calculations; the fins themselves, however, were not modeled in the subsequent shielding calculations.
- b) The initial design point (D_0) is given here in mrem/hr; it was used to establish the relative amount of neutron and gamma shielding as described in Section IV.B.1.
- c) When used in conjunction with the various structural components shown in Table II.1, the neutron and gamma shielding requirements shown here are sufficient to reduce the total dose down to 10 mrem/hr at a point 10 feet from the centerline.
- d) Optimal initial design point in this case was actually 70-150 mrem/hr. In that range, however, the neutron shield was unrealistically thick (ie, 12-18 inches). Because of this, 45 mrem/hr was selected as the best realistic value.
- e) Additional shielding calculations using this non-optimal design point were performed so that we could later determine if these alternate shield dimensions offered any additional protection to the cask during the thermal transient.

Table IV.19
Optimized Shielding Requirements for Various Types of Casks, as a Function
of the Age of the Spent Fuel and the Number of Assemblies in the Cask

1-YEAR-OLD PWR FUEL ASSEMBLIES				5-YEAR-OLD PWR FUEL ASSEMBLIES			
TYPE CASK	NO.OF ASSYS	THICKNESS (IN INCHES)		TYPE CASK	NO.OF ASSYS	THICKNESS (IN INCHES)	
		G-SHIELD	N-SHIELD			G-SHIELD	N-SHIELD
PB	1	5.00	3.77	PB	1	3.55	3.60
PB	8	5.73	4.14	PB	8	4.19	3.95
PB	15	5.99	4.41	PB	18	4.48	4.07
FE	1	10.91	3.83	FE	1	8.79	3.44
FE	8	12.00	3.91	FE	8	9.76	3.51
FE	15	12.40	3.96	FE	18	10.19	3.57
U	1	3.03	2.98	U	1	2.21	2.54
U	8	3.44	3.52	U	8	2.58	3.09
U	15	3.60	3.73	U	18	2.74	3.35

2-YEAR-OLD PWR FUEL ASSEMBLIES				7-YEAR-OLD PWR FUEL ASSEMBLIES			
TYPE CASK	NO.OF ASSYS	THICKNESS (IN INCHES)		TYPE CASK	NO.OF ASSYS	THICKNESS (IN INCHES)	
		G-SHIELD	N-SHIELD			G-SHIELD	N-SHIELD
PB	1	4.47	3.71	PB	1	3.27	3.54
PB	8	5.18	4.06	PB	8	3.91	3.82
PB	15	5.44	4.24	PB	21	4.27	4.03
FE	1	10.11	3.81	FE	1	8.35	3.49
FE	8	11.18	3.86	FE	8	9.31	3.55
FE	15	11.56	3.90	FE	21	9.84	3.62
U	1	2.72	3.10	U	1	2.05	2.67
U	8	3.13	3.49	U	8	2.40	3.14
U	15	3.27	3.67	U	21	2.61	3.49

3-YEAR-OLD PWR FUEL ASSEMBLIES				10-YEAR-OLD PWR FUEL ASSEMBLIES			
TYPE CASK	NO.OF ASSYS	THICKNESS (IN INCHES)		TYPE CASK	NO.OF ASSYS	THICKNESS (IN INCHES)	
		G-SHIELD	N-SHIELD			G-SHIELD	N-SHIELD
PB	1	4.06	3.73	PB	1	2.98	3.68
PB	8	4.74	4.05	PB	8	3.61	3.93
PB	18	5.05	4.24	PB	21	3.97	4.13
FE	1	9.52	3.68	FE	1	7.90	3.61
FE	8	10.54	3.74	FE	8	8.35	3.66
FE	18	10.99	3.82	FE	21	9.39	3.71
U	1	2.49	2.86	U	1	1.89	2.61
U	8	2.88	3.30	U	8	2.25	3.06
U	18	3.05	3.58	U	21	2.45	3.42

*When used in conjunction with the various structural components shown in Table II.1, the neutron and gamma shielding requirements shown here are sufficient to reduce the total dose down to 10 mrem/hr at a point 10 feet from the centerline of the cask.

V. STEADY-STATE THERMAL ANALYSIS

The key physical features of those casks considered in this study are illustrated in Fig. V.1. In each case, we have an aluminum or copper insert (i.e. a "basket") containing a number of spent fuel assemblies as shown in Appendix B, a stainless-steel inner shell, a gamma shield, an outer shell, an hydrogenous neutron shield, and an outside liner. Depending on the amount of decay heat that must be dissipated, the cask(s) may or may not have fins as illustrated in Fig. V.2. In general, fins were not used on a cask if it could dissipate the decay heat to the environment (at 130°F) while maintaining an outer skin temperature less than or equal to 250°F. In those cases where fins were necessary, a numerical search was conducted to determine the particular fin dimensions which would satisfy the above criteria while minimizing the overall weight of the loaded cask. In each case, the SCOPE code for Shipping Cask Optimization and Parametric Evaluation⁷ was used to calculate the steady-state temperature distribution throughout the cask and to determine the maximum temperature rise in each component during or after a postulated 30-minute fire. Pending future funding, complete documentation of the SCOPE code will be provided as ORNL/CSD/TM-148. The purpose of this section is to review those aspects of the thermal analysis which had direct bearing on the present study.

Fig. V.1: Overview of a Typical Spent Fuel Shipping Cask

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- a) The removable aluminum insert containing the spent fuel is more fully illustrated in Fig. II.3 and/or Appendix B.
 b) The thicknesses of the various components in the axial direction were assumed to be the same as in the radial direction.
 c) Actual dimensions of the various components depend on the type of cask, as shown in Table II.1 and/or Appendix F.

Fig. V.2: Overview of a Spent Fuel Shipping Cask with External Cooling Fins

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- The removable aluminum insert containing the spent fuel is more fully illustrated in Fig. II.3 and/or Appendix B.
- The thicknesses of the various components in the axial direction were assumed to be the same as in the radial direction.
- Actual dimensions of the various components depend on the type of cask, as shown in Table II.2 and/or Appendix F.
- Sketch not drawn to scale; many casks had 45-50 fins; actual center-to-center spacing was assumed to be 4.0 inches.
- When calculating the net weight, the fins were assumed to extend outward from the outer shell as shown in Fig. II.2.

V.A DETERMINATION OF THE OUTSIDE SURFACE TEMPERATURE FOR A FINLESS CASK

To calculate the steady-state temperature distribution throughout the cask, one starts by calculating the temperature at the surface and then works his way inward, one component at a time. Assuming the cask has no fins, the temperature at the surface can be calculated in an iterative fashion using the following expression:

$$Q_{\text{leak}} = A_{\text{cask}} \left\{ \sigma \epsilon_c (T_{\text{surf}}^4 - T_{\text{amb}}^4) + C (T_{\text{surf}} - T_{\text{amb}})^{4/3} \right\} \quad (\text{V.1})$$

where T_{surf} and T_{amb} are the surface and ambient temperatures in degrees Rankine ($^{\circ}\text{R}$)

σ is the Stefan-Boltzmann constant
 $[0.1714 \times 10^{-8} (3\text{tu/hr})/(\text{ft}^2)(^{\circ}\text{R})^4]$

ϵ_c is the surface emissivity of the cask; typically ~ 0.587
 for stainless-steel (dimensionless)

C is the constant (0.18) used in the McAdams correlation³⁶
 for natural convection heat transfer when one has a
 horizontal cylinder in air

(Note: for large rail and truck casks at these elevated
 temperatures [$T_{\text{amb}} \sim 130^{\circ}\text{F}$], the Grashof-Prandtl number
 product shows the boundary layer to be in the turbulent
 regime, thus dictating the present choice for C).

A_{cask} is the surface area of the outside liner, not including
 the two ends (ft^2)

Q is the total decay heat load imposed by the spent fuel (Btu/hr)
[based on ORIGEN results and the number of fuel assemblies]*

and Q_{leak} is the total amount of heat dissipated by the cask per unit
time, as given by Eq. V.1 (Btu/hr)

To solve Eq. V.1 for the cask surface temperature, the SCOPE code uses a "binary-split" search procedure wherein it first assumes a very high value for $T_{\text{surf}}^{(0)}$ ($\sim 3460^\circ\text{R}$), calculates a value for $Q_{\text{leak}}^{(1)}$, picks $T_{\text{surf}}^{(1)}$ to be midway between T_{amb} and $T_{\text{surf}}^{(0)}$, and calculates another value for $Q_{\text{leak}}^{(1)}$; depending on whether $Q_{\text{leak}}^{(1)}$ is higher or lower than the known decay heat load (Q), the code will then pick $T_{\text{surf}}^{(2)}$ so as to be midway between T_{amb} and $T_{\text{surf}}^{(1)}$, or midway between $T_{\text{surf}}^{(1)}$ and $T_{\text{surf}}^{(0)}$. In only a very few number of iterations, T_{surf} can be calculated to any desired degree of accuracy (typically $\pm 0.05^\circ\text{R}$). Indeed, this type of "binary-split" search procedure is used for many of the steady-state heat transfer calculations described in this section.

If, given an ambient temperature of 130°F , the surface temperature is found to be less than 250°F , external cooling fins are not required. Knowing the outside surface temperature, one can then go on and calculate the temperature distribution throughout the rest of the cask, as noted below.

* In this scoping design study, it was assumed that the cask would normally be sheltered from the direct rays of the sun by means of an opaque covering over a large, light-weight frame structure surrounding the entire cask. If unprotected, the Q term described above would have to include both the total decay heat load imposed by the spent fuel and the total solar heat load absorbed by that part of the cask facing the sun (cf. Sect. 5.2.2 of Ref. 4, and 10CFR71, Appendix A, paragraph 1). For larger casks carrying older spent fuel, the solar heat load may be significant relative to the decay heat load. With an ambient temperature of 130°F , this added heat load may raise the surface temperature of such casks by $20\text{--}40^\circ\text{F}$.

V.B DETERMINATION OF OPTIMAL FIN DIMENSIONS (IF REQUIRED) AND THE TEMPERATURE ON THE OUTER SURFACE OF THE OUTSIDE LINER

In those cases where fins are necessary, the SCOPE code will perform a numerical search to determine the particular fin dimensions (ℓ_f and t_f) which will satisfy the above criteria while minimizing the total weight of the loaded cask. The procedure used in this numerical determination is illustrated in Fig. V.3a, and the corresponding equations are summarized in Table V.1. Note that the overall profile of such a cask is as shown in Fig. V.2.* Given that the fins are spaced on a 4-inch pitch, the number of fins (N_f) will be fixed by the overall length of the cask.

The purpose of the numerical search, of course, is to find the optimal thickness (t_f) and the optimal length ($\ell_f = r_1 - r_0$) which will minimize the weight of the loaded cask while keeping the temperature on the outside surface of the outside liner below 250°F. As shown in Fig. V.3a, the fin thickness is varied from 0.25" to 2.0" in increments of 1/16 inch while the fin length is varied from 2" to 12" in increments of 1 inch. Given an arbitrary set of dimensions (ℓ_f^* and t_f^*), one must first calculate the various geometric view factors (fin $\rightarrow$ environment, fin $\rightarrow$ fin, fin $\rightarrow$ cask, cask $\rightarrow$ environment, etc.) as shown in Appendix G, and then use those view factors to calculate the effective emissivity of the fins and the cask (ϵ_f' and ϵ_c'), again as noted in Appendix G. This calculation is quite

* When calculating the total weight of the loaded cask, the fins are assumed to extend outward from the outer shell, through the hydrogenous neutron shield, through the outside liner, and out into the surrounding air. In the heat transfer calculation, however, the fins are assumed to be mounted directly on the outside liner at $r = r_0$ such that the temperature at the base of the fins (T_{base}) is the same as the temperature on the outside surface of the outside liner (T_{surf}).

**FIGURE V.3A
DETERMINE OPTIMAL FIN DIMENSIONS**

**FIGURE V.3B
CALCULATE TEMPERATURE AT BASE OF FINS**

**Fig. V.3: Flow Chart Illustrating
the Procedure Used to Determine
the Optimal Fin Dimensions and
Subsequently Calculate the Tem-
perature at the Base of the Fins**

Table V.1

EQUATIONS USED IN DETERMINING THE OPTIMAL FIN DIMENSIONS AND/OR
IN CALCULATING THE TEMPERATURE AT THE BASE OF THE FINS

$$A_{fin} = 2\pi r_1 t_f + 2\pi(r_1^2 - r_o^2)$$

$$A_{fins} = N_f A_{fin}$$

$$A_{base} = 2\pi r_o t_f$$

$$A_{cask\ body} = A_{bar\ cask} - N_f A_{base}$$

Given the (assumed) dimensions, calculate the corresponding view factors & ϵ'_f and ϵ'_c as shown in Appendix G.

Assume some value for T_{base} and calculate the following parameters:

$$h_c = \left\{ \epsilon'_c (T_{cask}^4 - T_{amb}^4) + C_1 (T_{cask} - T_{amb})^{4/3} \right\} / (T_{cask} - T_{amb})$$

where $T_{cask} = T_{base}$ and $C_1 = 0.18$

$$h_f = \left\{ \epsilon'_f (T_{fin}^4 - T_{amb}^4) + C_2 (T_{fin} - T_{amb})^{4/3} \right\} / (T_{fin} - T_{amb})$$

where $T_{fin} = T_{base}$ and $C_2 = 0.19$

$$\eta = \frac{1}{1 + \frac{2}{3} \left(\frac{h_f l_f^2}{k_f t_f} \right) \sqrt{\frac{r_1}{r_o}}}, \quad \text{where } l_f = r_1 - r_o$$

$$Q_{leak} = A_{cask\ body} h_c (T_{cask} - T_{amb}) + \eta A_{fins} h_f (T_{fin} - T_{amb})$$

important since the effective emissivity of the two surfaces in the given configuration may be quite different from the tabulated material properties. Indeed, if the fins are fairly long and thick (i.e. close together), the effective emissivities will both be less than the smallest tabulated value, and both will be so strongly dominated by the geometric view factors that they are only weakly dependent upon the actual material properties. Assuming the temperature at the base of the fins to be 250°F (710°R), noting that $T_{\text{cask}} = T_{\text{base}}$, and assuming $T_{\text{fin}} = T_{\text{base}}$, we can calculate the heat transfer coefficients for the cask body and the fins as shown in Table V.1. The constants C_1 and C_2 are the McAdam's coefficients for natural convection adjacent to a horizontal cylinder and a vertical plane in air when the boundary layer is in the turbulent regime ($\text{GrPr} > 10^9$).³⁶ That we assume the average temperature of the fin to be the same as the temperature at the base is indeed an approximation. Since the entire surface of the fin is dissipating heat to the environment, the tip of the fin may be considerably cooler than the base. To account for this in the expression for Q_{leak} , we multiply $A_{\text{fins}} h_f (T_{\text{fin}} - T_{\text{amb}})$ by the fin effectiveness (η) which is defined as the ratio of the heat actually transferred to the environment by the fin, to that which would be transferred to the environment if the entire fin were at the same temperature as the base. Table V.1 shows the Dusenberre approximation⁴¹ for the effectiveness of an arbitrary circumferential fin of constant thickness (i.e. rectangular profile). It obviously depends on the thickness of the fin (t_f), the thermal conductivity of the fin (k_f), the length of the fin ($\ell_f = r_1 - r_0$),

and the heat transfer coefficient (h_f). In addition to being fairly accurate (cf. Appendix H), this simple approximation allows one to physically see how η varies with each of the principle parameters. In the actual calculation, however, the SCOPE code normally determines the fin effectiveness (η) using a much more rigorous analytical expression, as noted in Appendix H. The final expression for Q_{leak} then accounts for both radiative and convective heat transfer from the cask body and the fins. If, with $T_{\text{base}} = 250^\circ\text{F}$, this calculated value of Q_{leak} is greater than the known decay heat load (Q), this particular set of fin dimensions (ℓ_f^* and t_f^*) will be saved for future consideration (cf. Fig. V.3a). After such calculations have been performed for the whole range^{*a} of fins ($2'' \leq \ell_f^* \leq 12''$, $0.25'' \leq t_f^* \leq 2''$), and those capable of dissipating the known decay heat load (Q) have been identified, the code will then select that design (ℓ_f , t_f) which minimizes the total weight of the loaded cask.^{*b}

Given the length and thickness of the optimized fins, the code will then calculate the actual temperature at the base of the fins (i.e. on the outside surface of the outside liner). With $T_{\text{base}} = 250^\circ\text{F}$, the optimized fins will (by design) yield $Q_{\text{leak}} \geq Q$; thus, with $Q_{\text{leak}} = Q$, one will naturally obtain $T_{\text{base}} \leq 250^\circ\text{F}$. Given the actual fin dimensions, the

^{*a} To be more precise, calculations for ϵ_f' , ϵ_c' , h_c , h_f , η , and Q_{leak} are not performed for the "whole range" of fins, but only for those which stand a chance of reducing the total weight of the loaded cask relative to the previous minimum. The actual logistics used are shown in Fig. V.3a.

^{*b} In the event that none of the designs considered can dissipate the known decay heat load while maintaining $T_{\text{base}} \leq 250^\circ\text{F}$, the code will set $\ell_f = 12''$ and $t_f = 2''$. It will then calculate the actual temperature at the base of t_f fins and proceed with the rest of the analysis.

first step is to calculate the various geometric view factors as shown in Appendix G, and then to use those view factors to calculate the effective emissivity of the fins and the cask (ϵ'_f and ϵ'_c), again as noted in Appendix G. As shown in Fig. V.3b, these calculations need to be performed only once. To determine the actual temperature at the base of the fins (T_{base}), we then use the same "binary-split" search procedure previously described for the finless cask — except in this case, we calculate the heat transfer coefficient for the cask body (h_c), the heat transfer coefficient for the fins (h_f), the fin effectiveness (η), and the total heat leak rate (Q_{leak}) using the equations shown in Table V.1.* In only a very few number of iterations, T_{base} can be converged to any desired degree of accuracy (typically $\pm 0.05^\circ R$).

* The fin effectiveness is, of course, still calculated as shown in Appendix H.

V.C CALCULATION OF ΔT ACROSS THE OUTSIDE LINER

Knowing the temperature on the outside surface of the outside liner (T_{surf} or T_{base}), one can determine the temperature distribution throughout the cask by calculating the ΔT across each of the respective components, one at a time. By simple conduction, the ΔT across the outside liner is given by

$$\Delta T \Big|_{\text{OL}} = \left(\frac{Q}{L_{\text{iOL}}} \right) \left(\frac{1}{2\pi k_{\text{OL}}} \right) \ln \left(\frac{r_{\text{oOL}}}{r_{\text{iOL}}} \right) \quad (\text{V.2})$$

where Q is the total decay heat load imposed by the spent fuel, r_{oOL} is the outside radius of the outside liner, r_{iOL} is the inside radius of the outside liner, L_{iOL} is the "inside" length of the outside liner ($L_{\text{iOL}} = L_{\text{oOL}} - 2t_{\text{OL}}$), and k_{OL} is the thermal conductivity. For the purpose of this study, the stainless-steel outside liner was assumed to have a constant thermal conductivity of 11.0 (Btu/hr)/ft $^{\circ}$ F. Knowing the temperature on the outside surface of the outside liner (T_{surf} or T_{base}) and the ΔT across the component ($\Delta T \Big|_{\text{OL}}$), one can calculate the temperature on the inside surface of the outside liner (T_{iOL}).

V.D CALCULATION OF ΔT ACROSS THE NEUTRON SHIELD

Calculating the ΔT across the neutron shield is not quite as simple. The difficulty there stems from the fact that the neutron shield is a mixture of borated water and ethylene glycol (cf. Section II). Because it is a fluid trapped within a long horizontal cylindrical annulus (cf. Fig. V.1), large kidney-shaped convective loops are set up which, in fact, transport much of the decay heat from the outside of the outer shell to the inside of the outside liner, as shown in Fig. V.4a and V.4b. To calculate the ΔT across this region, one typically^{2,42} assumes the fluid to have an effective thermal conductivity (k_e) which depends upon the inside and outside radius of the region, the average temperature of the fluid, the ΔT across the region, and the flow regime as characterized by the Grashof and Prandtl numbers. Having conducted an extensive series of natural convection heat transfer experiments for various fluids (including water) trapped between two long horizontal cylinders, Liu et al.⁴² recommend the following correlation* for the effective thermal conductivity of the fluid:

$$k_e = (0.135) k_f \left(\frac{Pr^2 Gr}{1.36 + Pr} \right)^{0.278} \quad \text{for } 10^3 < \left(\frac{Pr^2 Gr}{1.36 + Pr} \right) < 10^8 \quad (V.3a)$$

and

$$k_e = k_f \quad \text{for } \left(\frac{Pr^2 Gr}{1.36 + Pr} \right) < 10^3. \quad (V.3b)$$

* While Eq. V.3a was originally developed for fairly wide gaps⁴² ($1.5 \leq r_2/r_1 \leq 7.5$), it has since been used by General Electric⁷ to calculate the ΔT across the neutron shield in the IF-300 rail cask where $r_2/r_1 \approx 1.18$. In that cask, the outside radius of the outer shell (r_1) is 24.75" and the inside radius of the outside liner (r_2) is 29.25", with a 4.5" neutron shield in between. Most of the casks considered in this study typically have $r_2 \approx 40"$, $\Delta r \approx 4"$, and $r_2/r_1 \approx 1.10$.

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Fig. V.4: Naturally Occurring Convective Loops in the Annular Region Between Concentric Cylinders at Different Temperatures

Note that the actual flow pattern depends on the ΔT across the region, the size of the region, and the density and viscosity of the fluid in the region.

Here, the Grashof number (Gr) is defined as

$$Gr = g \beta \left(\frac{r_o}{r_i} \right)^3 (r_o - r_i)^3 \Delta T \quad (V.4)$$

where g is the gravitational constant (4.17×10^3 ft/hr²)

β is the volumetric expansion coefficient ($^{\circ}\text{F}^{-1}$)

ρ is the density (lbm/ft³)

μ is the dynamic viscosity (lbm/hr)/ft

and ΔT is the temperature difference across the region ($^{\circ}\text{F}$).

Likewise, the Prandtl number (Pr) is defined as

$$Pr = \frac{c_p \mu}{k_f} \quad (V.5)$$

where c_p is the specific heat of the fluid (Btu/lbm)/ $^{\circ}\text{F}$

μ is the dynamic viscosity (lbm/hr)/ft

and k_f is the tabulated value of the fluid's thermal conductivity
at T_{avg} (Btu/hr)/ft $^{\circ}\text{F}$.

Given the effective thermal conductivity of the fluid, one could then
calculate the heat transfer rate as

$$Q_{heat} = k_e \left[\frac{2\pi L_{oos}}{\ln\left(\frac{r_o}{r_i}\right)} \right] \Delta T \quad (V.6)$$

where L_{oos} is the "outside" length of the outer shell. The catch,
of course, is that all of the material properties used in Eqs. V.4 and
V.5 should be evaluated at some "mean" temperature

$$T_{avg} = (T_i + T_o)/2 = T_{iOL} + (0.5)\Delta T \quad (V.7)$$

which is not known in advance. Because the ΔT across the neutron shield is typically pretty small ($<25^\circ\text{F}$ for casks containing 1-year-old fuel), the solution algorithm used in the SCOPE code proceeds in the following fashion:

- (1) Assume $\Delta T = 1^\circ\text{F}$; calculate T_{avg} as per Eq. V.7; and evaluate the material properties at that temperature by interpolating the readily available tabulated data.
- (2) Knowing the Grashof and Prandtl numbers, use Eqs. V.3a and V.3b to evaluate the effective thermal conductivity of the fluid.
- (3) Knowing the effective thermal conductivity (k_e), calculate the heat transfer rate (Q_{leak}) using Eq. V.6.
- (4) If the calculated heat transfer rate (Q_{leak}) is greater than the total decay heat rate (Q), stop. If Q_{leak} is less than Q , increment ΔT by 1°F and return to step (1).
- (5) Once the ΔT ($\pm 1^\circ\text{F}$) has been found, back up one degree and repeat the above process in increments of 0.05°F .

As shown in the IF-300 Consolidated Safety Analysis Report², the effective thermal conductivity of the fluid as calculated by Eq. V.3a may be one to two orders of magnitude greater than the tabulated fluid property, depending upon the decay heat load and the various other parameters. Knowing the temperature on the inside surface of the outside liner (T_{iOL}) and the ΔT across the neutron shield, one can then calculate the temperature on the outside surface of the outer shell (T_{oOS}).

V.E CALCULATION OF ΔT ACROSS THE OUTER SHELL, THE GAMMA SHIELD, AND THE INNER SHELL

Knowing the temperature on the outside surface of the outer shell (T_{oOS}), one can calculate the temperature distribution back through the outer shell, gamma shield, and inner shell. By simple conduction, the ΔT across each component is given by

$$\Delta T \Big|_{OS} = \left(\frac{Q}{L_{iOS}} \right) \left(\frac{1}{2\pi k_{OS}} \right) \ln \left(\frac{r_{oOS}}{r_{iOS}} \right) \quad (V.8)$$

$$\Delta T \Big|_{GS} = \left(\frac{Q}{L_{iGS}} \right) \left(\frac{1}{2\pi k_{GS}} \right) \ln \left(\frac{r_{oGS}}{r_{iGS}} \right) \quad (V.9)$$

$$\Delta T \Big|_{IS} = \left(\frac{Q}{L_{iIS}} \right) \left(\frac{1}{2\pi k_{IS}} \right) \ln \left(\frac{r_{oIS}}{r_{iIS}} \right) \quad (V.10)$$

where Q is the total decay heat load imposed by the spent fuel, r_{oX} is the outside radius of the given component, r_{iX} is the inside radius, L_{iX} is the "inside" length ($L_{iX} = L_{oX} - 2t_X$), and k_X is the thermal conductivity. For the purpose of this study, the stainless-steel shells were assumed to have a constant thermal conductivity of 11.0 (Btu/hr)/ft°F. Likewise, the Pb, Fe, and U-metal gamma shields were assumed to have constant thermal conductivities of 18.0, 26.0, and 15.0 (Btu/hr)/ft°F, respectively. Typically, these ΔT 's are < 25°F for casks carrying 1-year-old spent fuel and < 5°F for casks carrying 10-year-old spent fuel. Knowing the temperature on the outside surface of the outer shell (T_{oOS}) and the ΔT across each of these components, one can calculate the temperature on the inside surface of the inner shell (T_{iIS}).

V.F DETERMINATION OF THE TEMPERATURE ON THE OUTSIDE SURFACE OF THE INSERT

Inside the cask, the fuel assemblies are separated by means of an aluminum or copper insert as illustrated in Fig. II.3 (cf. Section II, Section III, and Appendix B). So as to provide enough clearance for easy removal, we have assumed a 0.125" gap between the outside surface of the insert (r_I) and the inside surface of the inner shell (r_{iIS}). Before we can calculate the temperature distribution inside this region, we must first calculate the ΔT across the gap and the temperature (T_{OI}) on the outside of the insert. Because this gap is so thin, convective forces may be ignored and the heat transfer rate (Q_{leak}) can be represented as:

$$Q_{leak} = (2\pi r_I L_I) \left\{ \left(\frac{1}{\frac{1}{\epsilon_I} + \frac{1}{\epsilon_{iIS}} - 1} \right) (T_{OI}^4 - T_{iIS}^4) + \left(\frac{1}{r_{iIS} - r_I} \right) \int_{T_{iIS}}^{T_{OI}} k_{air}(T) dT \right\} \quad (II.17)$$

where T_{OI} and T_{iIS} are the surface temperatures in degrees Rankine,

ϵ_I and ϵ_{iIS} are the actual emissivities of the two surfaces (dimensionless),

σ is the Stefan-Boltzmann constant [0.1714×10^{-8} (Btu/hr)/(ft²)(°R)⁴],

and L_I is the length of the insert (13.8 feet in this study).

Note that this expression accounts for both radiation and conduction.

Because the ΔT across the gap may be quite substantial ($\sim 250^\circ\text{F}$ for casks carrying 1-year-old spent fuel, and $\sim 100^\circ\text{F}$ for casks carrying 10-year-old spent fuel), the thermal conductivity of the air is integrated from T_{iIS} to T_{OI} . Because $k_{air}(T)$ varies almost linearly with T , the SCOPE code numerically integrates the tabulated data using the trapezoidal

formula. To determine the actual temperature on the outside surface of the insert (T_{OI}), we again use the same "binary-split" search procedure previously used to determine the temperature on the surface of the cask — ie: we first assume some very high value for $T_{OI}^{(0)}$ ($\sim 3460^\circ R$), use Eq. V.11 to calculate a value for $Q_{leak}^{(0)}$, pick $T_{OI}^{(1)}$ to be midway between T_{iIS} and $T_{OI}^{(0)}$, and calculate another value for $Q_{leak}^{(1)}$; depending on whether $Q_{leak}^{(1)}$ is higher or lower than the known decay heat load (Q), we then pick $T_{OI}^{(2)}$ to be midway between T_{iIS} and $T_{OI}^{(1)}$, or midway between $T_{OI}^{(1)}$ and $T_{OI}^{(0)}$; etc., until T_{OI} converges to any desired degree of accuracy (typically $\pm 0.05^\circ R$).

V.G CALCULATION OF THE MAXIMUM ΔT ACROSS THE INSERT

In addition to physically separating the spent fuel assemblies, the insert itself also provides a low resistance path by which decay heat can be conducted outward, away from the innermost fuel assemblies. Before one can calculate the maximum fuel pin temperature, he must, of course, determine the temperature of (that part of) the insert surrounding the innermost fuel assembly. Likewise, the designer will want to check the temperature of the insert at that point in order to verify that it is in no danger of melting. Given the temperature on the outside of the insert (T_{OI}), the task then is to calculate the maximum ΔT across the insert. Given a highly conducting material such as aluminum or copper, virtually all of this temperature difference will be due to the heat flow through (i.e. along) the relatively thin web separating the spent fuel assemblies (cf. Fig. V.5).^{*} To accurately determine the maximum ΔT across the insert, of course, requires a complete solution of the 2-D heat conduction problem -- i.e. one must (simultaneously) solve for the actual heat flow through each segment of the webbing and for the temperature at each node. As shown in Appendix I, the simple 10-assembly configuration illustrated in Fig. V.5 would require that one solve a system of 17 nodal equations and 26 branch equations. Insert configurations designed to carry more fuel assemblies (cf. Appendix B) would give rise to a much larger system of simultaneous equations. Fortunately, this 2-D heat conduction problem needs to be solved only once for each configuration. Thereafter, the ΔT across the insert (for a given configuration) may then be scaled up or down in proportion to

^{*} Typically, this webbing may be anywhere from 0.25" to 2.0" thick.

**Fig. V.5: Simplified Diagram of a Removable Aluminum Insert
Designed to Hold 10 PWR Fuel Assemblies**

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Optimal packing arrangements
for additional assemblies are
illustrated in Appendix B

Solution of corresponding
2-D heat conduction problem
is described in Appendix I

the decay heat load and the thermal resistance offered by each segment of webbing. A sketchy but rigorous mathematical proof of this is offered in Appendix I. In that same appendix, we also show how each of the configurations shown in Appendix B were analyzed using an electrical analog of the 2-D heat conduction problem. Using those results, the maximum T across the insert may be calculated conveniently and accurately as

$$T_{\text{insert}} = Q_A \left(\frac{P/t}{Lk} \right) \left[\frac{V_{\text{max}} - 10}{160} \right] \quad (\text{V.12})$$

where, for each configuration, the parameter V_{max} is given in Table V.2;

where Q_A = total decay heat per assembly (Btu/hr)

L = length of the insert (feet)

k = thermal conductivity of the insert (Btu/hr)/ft 2 F

t = effective thickness of the webbing (inches); See Fig. V.6

and P = pitch between assemblies (inches)

As noted in Section III, it is often necessary for the designer to provide for some type of neutron poison in the insert between the fuel assemblies. Fig. V.6 illustrates how hollow, borated stainless-steel tubes might be embedded within the insert so as to ensure subcriticality. To the extent that these reduce the cross-sectional area of the webbing through which the decay heat may flow, the thermal resistance of the webbing will be somewhat higher. To account for this in Eq. V.12, the effective thickness (t) should be calculated as $t = t_1 - t_2$ where t_1 is the actual thickness of the insert (between fuel assemblies) and t_2 is the thickness of the embedded poison.

Table V.2
Maximum Nodal Voltage in the Electrical Analog
of the 2-D Heat Conduction Problem for the Var-
ious Insert Configurations Shown in Appendix B

NO.OF ASSYS	V _{max}	NO.OF ASSYS	V _{max}
1	10.0000	24	232.8571
2	30.0000	26	310.4216
3	38.0000	31	365.8975
4	50.0000	32	370.0000
5	30.0000	34	412.6838
7	76.6667	37	395.7141
8 ^a	101.3043	39	485.0764
8 ^b	83.3333	42	516.7627
10	130.0000	44	506.4119
12	116.6667	46	556.5295
13	153.0601	48	595.8389
15	165.0000	52	590.3225
18	237.7922	56	729.4521
21	230.0000	58	734.3955
22	250.0000	61	733.9207

a) Configuration such that Dia/Pitch= $\sqrt{15.68}$

b) Configuration such that Dia/Pitch= $\sqrt{17.00}$

Fig. V.6: Inherently Subcritical, Removable Aluminum Insert Designed to Hold 10 PWR Fuel Assemblies

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EFFECTIVE THICKNESS IS
GIVEN BY: $t = t_1 - t_2$

Note the use of hollow, borated stainless-steel tubes within the walls of the insert in order to insure subcriticality. Their presence effectively reduces the thickness of the webbing through which the decay heat must flow.

Knowing the temperature on the outside surface of the insert (T_{OI}) and the maximum ΔT across the insert, one can then calculate the maximum temperature on the innermost surface of the insert (T_{iI}) as

$$T_{iI} = T_{OI} + \Delta T_{\text{insert}} \quad \cdot \quad (V.13)$$

V.H DETERMINATION OF THE MAXIMUM FUEL PIN CLAD TEMPERATURE

Knowing the maximum temperature on the innermost cell of the insert (T_{iI}), the maximum fuel pin clad temperature (T_c) can then be calculated in an iterative fashion using the Wooton-Epstein Equation:^{*}

$$Q_{\text{leak}} = 4w L_A \left\{ \sigma \left(\frac{C_1}{\frac{1}{\epsilon_c} + \frac{1}{\epsilon_I} - 1} \right) (T_c^4 - T_{iI}^4) + C_2 (T_c - T_{iI})^{4/3} \right\} \quad (V.14)$$

where T_c and T_{iI} are the surface temperatures in degrees Rankine ($^{\circ}R$)

ϵ_c and ϵ_I are the actual emissivities of the two surfaces (dimensionless)

σ is the Stefan-Boltzmann constant [0.1714×10^{-6} (Btu/hr)/(ft²)($^{\circ}R$)⁴]

w is the width of the fuel assembly (typically 0.7 feet for PWR fuel)

L_A is the active length of the fuel (typically 12 feet for PWR fuel)

C_1 is a geometric constant which depends on the number of fuel pins as described below

and C_2 is an experimentally determined constant (0.118); see Appendix J.

Note that this correlation accounts for natural air convection and radiation cooling of a spent fuel assembly in a horizontal cask. It was originally developed by Wooton and Epstein after analyzing a series of experiments in which an electrically heated parallel-rod assembly in a horizontal

* This semi-theoretical, semi-empirical correlation was originally developed by R. O. Wooton and H. M. Epstein as part of an unpublished paper which they first prepared in August, 1963. Since that time, it has been used by General Electric in licensing the IF-300 spent fuel shipping cask. Because of the widespread use of the Wooton-Epstein Equation in more recent years and the lack of an easily obtainable reference, pertinent portions of their original paper have been included here as Appendix J.

enclosure was used to simulate isolated spent fuel assemblies from the Connecticut Yankee reactor (cf. Appendix J). The geometric constant (C_1) depends upon the number of fuel pins (N) in a single row of a square fuel assembly:

$$C_1 = \frac{4N}{(N+1)^2} \text{ for odd } N; \quad C_1 = \frac{4}{N+2} \text{ for even } N \quad (V.15a,b)$$

where $N=17$ for a 17×17 assembly, and $N=8$ for an 8×8 assembly, etc.

A theoretical derivation of these formulae is provided in Appendix J. As also noted in Appendix J, the convective constant (C_2) was experimentally determined so as to conservatively overpredict the maximum fuel pin temperature when given the heat generation rate for a particular assembly.

To actually determine the maximum fuel pin clad temperature (T_c), the SCOPE code again uses the same "binary-split" search procedure previously used to determine the temperature on the surface of the cask -- ie: we first assume some very high value for $T_c^{(0)}$ ($\sim 3460^\circ R$), use Eq. V.14 to calculate a value for $Q_{leak}^{(0)}$, pick $T_c^{(1)}$ to be midway between T_{1I} and $T_c^{(0)}$, and calculate another value for $Q_{leak}^{(1)}$; depending on whether $Q_{leak}^{(1)}$ is higher or lower than the known decay heat generation rate in the given assembly (Q_A), we then pick $T_c^{(2)}$ to be midway between T_{1I} and $T_c^{(1)}$, or midway between $T_c^{(1)}$ and $T_c^{(0)}$; etc., until T_c converges to any desired degree of accuracy (typically $\pm 0.05^\circ R$).

VI. ACCIDENT/POST-ACCIDENT TRANSIENT THERMAL ANALYSIS

Each cask design evaluated by the SCOPE code⁷ is subjected to a steady-state thermal analysis as described in Section V, and to an accident/post-accident transient thermal analysis as described below. The accident scenario is assumed to proceed as follows:*

- (1) As a result of the 30-foot drop test (or an actual accident), the outside liner is assumed to be ruptured, thus permitting the (borated-water) neutron shield to leak out.
- (2) With the neutron shield voided as shown in Fig. VI.1, the cask is allowed to reach a new state of thermal equilibrium. The internal temperature distribution will, of course, be considerably elevated relative to the nominal case.
- (3) The cask is then assumed to be totally engulfed by fire having a flame temperature of 1475°F. This is assumed to last for 30 minutes before being extinguished. During this time, the cask is being heated externally by the fire, and internally by the decay heat which is being generated continuously by the spent fuel.
- (4) In the post-accident situation, the excess heat imparted to the cask by the fire will have to be dissipated to the environment, along with the (stored) decay heat that was not able to escape during the fire.

* This scenario differs from that used in the analysis of the NLI-10/24 and the IF-300 insofar as they do not allow the cask to attain a new state of thermal equilibrium (as indicated in Step 2) prior to the start of the half-hour fire.^{1,2} For the purpose of this study, however, the present version of the accident scenario was adopted as the worst possible limiting case. With only a few minor changes, the SCOPE code could easily be modified to analyze either scenario.

Because of the high heat capacity of many of the components and the high thermal resistance offered by the now voided neutron shield, it may take several hours before the innermost components return to thermal equilibrium. Indeed, the temperature of the insert may continue to rise for two or three hours after the fire has been extinguished. For that reason, the SCOPE code follows the thermal transient for 12 full hours.

[While conservative in most respects, the version of the SCOPE code used in this study did not account for direct solar heating of the cask before or after the 30-minute fire. With only a few minor changes, the code could easily be modified to incorporate one or both of these effects. Indeed, if a constant solar heat flux equal to the daily maximum of ~ 900 watts/m² is applied to the projected area of the cask only after the fire, then it can be shown that the maximum transient temperatures for the fuel, insert, and gamma shield would only be 2-5°F greater than those cited in this report. If one were to ignore diurnal variations and assume this same solar heat load both before and after the 30-minute fire, then the maximum transient temperatures for these components would be 5-15°F greater than the values cited here for finned casks carrying 1 or 2-year-old spent fuel, and 15-30°F greater than the values cited here for the large finless casks carrying 3, 5, 7 or 10-year-old spent fuel. Properly accounting for diurnal variations prior to the fire would reduce these projected increases by a factor of two, even if nocturnal cooling was not permitted].

VI.A PRELIMINARY REMARKS REGARDING THE 1-D MODEL ACTUALLY USED AND A 2-D ELECTRICAL ANALOG MODEL ONCE CONSIDERED

The simplified 1-D model used in the space/time transient analysis is shown in Fig. VI.1. To account for the (often large) heat capacity of the insert without adding undue complexity to the problem, the insert has been modeled as a solid annular region just inside the inner shell. The small gap (0.125") between the inner shell and the insert has been maintained, as has the overall cross-sectional area of the insert. As shown, the region normally occupied by the neutron shield is assumed to be void throughout this analysis. Lastly, the presence of fins on the surface of the cask may be accounted for as illustrated in Appendix K.

Before settling on the simplified 1-D model shown in Fig. VI.1, a more complex 2-D model was considered which would preserve much of the detail within the cask cavity. A two-dimensional electrical analog for a 10-assembly cask having no neutron shield and no outside liner is shown in Fig. VI.2. The interested reader will note that this model is very similar to the 2-D steady-state network model described in Appendix I -- except that here a capacitor (representing the thermal heat capacity of each segment of webbing) is shunted across the independent current sources (representing the decay heat from each assembly). Likewise, the thermal resistance and heat capacity of each of the major components is represented as a series of RC circuits back-to-back. While the electrical analog shown in Fig. VI.2 does not account for radiation and convection to the environment, it can model a temporary step change in the surface temperature by switching from V_1 to V_2 and back again. This 2-D electrical analog and a similar one used

Fig. VI.1: Simplified 1-D Model Used in the Space/Time Transient Thermal Analysis

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Fig. VI.2: Two-Dimensional Electrical Analog Corresponding to a 10-Assembly Cask Having No Neutron Shield and No Outside Liner

to model a U-metal cask carrying 15 1-year-old spent fuel assemblies were both analyzed using the Princeton Circuit Analysis Program (PCAP).⁴³ In the solution of the transient problem, the code must solve a rather large set of coupled linear equations at each time step. (Indeed, one has a separate branch equation for the voltage across each resistor and each capacitor, and a current balance equation for each node). Subsequent to these analyses, it became obvious that the ΔT across the insert during the transient was relatively small compared to the ΔT across the other major components during the transient. This suggested that the complicated 2-D network shown in Fig. VI.2 could perhaps be approximated by a single resistor, capacitor, and current source.^{*a} Such a simplification would, of course, greatly reduce the amount of computational work required at each time step. The simplified 1-D circuit models were then analyzed under the same transient conditions. As expected, the transient behavior throughout the remainder of the cask was essentially unchanged. Based on this experience, we chose to model the insert as a single annular region just inside the inner shell, as shown in Fig. VI.1.^{*b} Determination of the maximum fuel pin temperature during the transient is made using a separate procedure as noted below (cf. Section VI.G).

^{*a} The thermal resistance in this simplified model was, of course, adjusted so as to produce the same equilibrium ΔT across the insert as the rigorous 2-D steady-state solution.

^{*b} As shown in Fig. VI-5 of the Consolidated Safety Analysis Report for the IF-300 Spent Fuel Shipping Cask², the THTD model used by General Electric makes this same type of approximation. While that cask does not have a solid aluminum or copper insert, the heat capacity of the fuel is represented as a single annular region just inside the inner shell.

VI.B IMPORTANCE OF THE VARIOUS RADIATION AND CONVECTION TERMS

While the electrical analog model shown in Fig. VI.2 was useful in establishing the validity and the usefulness of the 1-D model shown in Fig. VI.1, it cannot (unfortunately) account for some of the more important physical phenomena:*

- (1) It cannot, for example, account for radiation and convection to or from the environment during and after the 30-minute fire;
- (2) It cannot account for radiation and convection across the voided region where the neutron shield used to be; and
- (3) It cannot account for radiation and conduction across the small gap between the insert and the inner shell.

All these phenomena are important and must be properly accounted for. In steady-state, for example, the ΔT across the thin air gap and the voided neutron shield may both be $\sim 200^\circ\text{F}$, while the temperature drop across each of the various structural components may only be $\sim 20^\circ\text{F}$. During the 30-minute fire, the large (nonlinear) resistance afforded by the voided neutron shield is very important in protecting the cask, whereas after the fire it dramatically slows the return to equilibrium. In both cases, it is important and must be properly accounted for. Likewise, the large steady-state ΔT between the insert and the inner shell

* The difficulty, of course, lies in the fact that the heat flow, which is analogous to the electrical current, depends upon the actual temperature difference ($T_2 - T_1$) across the solid structural components, and upon ($T_2^* - T_1^*$) across the voids and gaps. While either process can be modeled separately, they cannot both be modeled in a single circuit without the use of special nonlinear resistive components.

is also quite important and must be properly accounted for. Because of this large ΔT , and the fact that the temperature on the inside surface of the inner shell rises relatively little during the fire, the net heat flow during the transient is still outward, although at a somewhat slower rate. If there were no gap, or if the steady-state ΔT were not so large, there could at times be an inward flow of heat across this interface. Lastly, it is very important to properly account for radiation and convection to and from the environment during and after the 30-minute fire. If, for example, one had no gap and no void, the temperature distribution throughout the cask would still be very sensitive to the manner in which the heat transfer to and from the outer surface is modeled — ie: the temperature on the surface does not "instantaneously" climb to 1475°F as the electrical analog would imply; indeed, for this particular configuration (no gap, no void, no outside liner), the temperature on the outside surface of the cask would only climb to ~970°F near the end of the 30-minute fire.* In other cases where one does have an outside liner and a voided neutron shield, the outside surface temperature would, of course, rise much faster. The point to be made here is that the increase is not instantaneous, and that the rate of increase must be properly calculated.

* With pure conduction at every interface and with the large heat capacities of the various components, the fire simply can't "pump the heat in fast enough" to raise the surface temperature much higher than this during the half-hour it is allowed to burn.

VI.C DERIVATION OF THE TIME-DEPENDENT FINITE-DIFFERENCED ONE-DIMENSIONAL HEAT TRANSFER EQUATIONS, AND SOME REMARKS REGARDING NUMERICAL STABILITY

The time-dependent finite-differenced one-dimensional heat transfer equations actually solved by the SCOPE program are derived in Appendix K. For illustrative purposes, let us consider a 1-D model wherein we would like to calculate the temperature at each of the 13 different nodes shown in Fig. VI.1. (By changing a single DATA statement in the actual code, one can specify any arbitrary number of nodes in any given zone). For the region surrounding each node, one can write a macroscopic heat balance equation of the form:

$$\left[\begin{array}{l} \text{Heat flowing into} \\ \text{the region (through} \\ \text{the left-hand} \\ \text{boundary) per unit} \\ \text{time} \end{array} \right] = \left[\begin{array}{l} \text{Heat flowing out of} \\ \text{the region (through} \\ \text{the right-hand} \\ \text{boundary) per unit} \\ \text{time} \end{array} \right] + \left[\begin{array}{l} \text{Heat being} \\ \text{absorbed by} \\ \text{the region} \\ \text{per unit} \\ \text{time} \end{array} \right] \quad (\text{VI.1})$$

Looking at the expanded diagram shown in Fig. VI.3, we may (for example) write the following heat balance equation for node 10:

$$-k A_{9A} \left. \frac{\partial T}{\partial r} \right|_{9A} = h_{10} A_{10} (T_{10} - T_{11}) + \rho c_p V_{9A \rightarrow 10} \frac{\partial T_{10}}{\partial t} \quad (\text{VI.2})$$

where k = thermal conductivity of the material (Btu/hr)/ft²°F
 h_{10} = heat transfer coefficient for the particular surface
 (Btu/hr)/ft²°F
 A_{10} = surface area perpendicular to the heat flow (ft²)
 $V_{9A \rightarrow 10}$ = volume of the region between the two surfaces (ft³)
 ρ = density of the material in this region (lbm/ft³)
 and c_p = specific heat capacity of the material (Btu/lbm)/°F .

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Fig. VI.3: Macroscopic Heat-Balance Diagram for the Region Just to the Left of the Voided Neutron Shield

Assuming the spatial mesh intervals are reasonably close together, a linear approximation may be made for the spatial derivative, yielding

$$-k (2-r_{9A}L) \left(\frac{T_{10} - T_9}{r_{10} - r_9} \right) = h_{10} (2-r_{10}L) (T_{10} - T_{11}) + \rho c_p [(r_{10}^2 - r_{9A}^2)L] \frac{\partial T_{10}}{\partial t} \quad (\text{VI.3})$$

Applying the "θ-differencing" scheme described in Appendix K to the time derivative, this becomes

$$-a_{10} \left[(\theta T_{10}^{n+1} + (1-\theta) T_{10}^n) - (\theta T_9^{n+1} + (1-\theta) T_9^n) \right] = b_{10} \left[(\theta T_{10}^{n+1} + (1-\theta) T_{10}^n) - (\theta T_{11}^{n+1} + (1-\theta) T_{11}^n) \right] + (T_{10}^{n+1} - T_{10}^n) \quad (\text{VI.4})$$

$$\text{where } a_{10} = \left(\frac{2r_{9A}k}{r_{10} - r_9} \right) \left(\frac{\Delta t}{\rho c_p (r_{10}^2 - r_{9A}^2)} \right) \quad (\text{VI.5a})$$

$$b_{10} = (2r_{10} h_{10}) \left(\frac{\Delta t}{\rho c_p (r_{10}^2 - r_{9A}^2)} \right) \quad (\text{VI.5b})$$

T_i^n represents the temperature at node "i" during the n^{th} time interval

and T_i^{n+1} represents the temperature at node "i" during the next time interval (n+1).

Note that with $\theta = 0$, Eq. VI.4 corresponds to the forward-differenced approximation to Eq. VI.3; with $\theta = 1$, it corresponds to the backward-differenced approximation; and with $\theta = 0.5$, it corresponds to the central-differenced approximation.^{8,44} Rearranging terms, Eq. VI.4 can be written more conveniently as:

$$\begin{aligned}
& (-\theta a_{10}) T_9^{n+1} + (1 + \theta a_{10} + \theta b_{10}) T_{10}^{n+1} + (-\theta b_{10}) T_{11}^{n+1} \\
& = T_{10}^n + (1-\theta) \left[a_{10} (T_9^n - T_{10}^n) - b_{10} (T_{10}^n - T_{11}^n) \right] \quad (VI.6)
\end{aligned}$$

Proceeding in this same fashion, similar equations may be derived for each of the nodes shown in Fig. VI.1. The equation for node 1 will, of course, have a fixed source term on the right-hand side of the equation to describe the decay heat being generated by the spent fuel. Likewise, the equation for node 13 will have a variable source term on the right-hand side of the equation which describes the ambient temperature as a function of time ($T_{amb} = 130^\circ\text{F}$ for $t < 0$; $T_{amb} = 1475^\circ\text{F}$ for $0 < t < 30$ minutes; and $T_{amb} = 130^\circ\text{F}$ for $t > 30$ minutes). The constants used in the equation for node 13 will, of course, be calculated differently, depending on whether or not the cask has fins. A detailed derivation of the finite-differenced heat transfer equation for each node is provided in Appendix K. Collecting those results, we see that we end up with a coupled set of simultaneous equations which must be solved at each time step (cf. Table VI.1). With $\theta = 0$, these (forward-differenced) equations degenerate to a decoupled set of algebraic formulae for T_i^{n+1} which may be evaluated explicitly at each time step. Unfortunately, these forward-differenced equations ($\theta=0$) may become unstable and yield grossly incorrect results after only a few time steps unless one uses an exceedingly small Δt (cf. Appendix K). With $\theta=1$, these (backward-differenced) equations do remain stable regardless of the time step. In this particular case, one will, of course, have to solve the whole system of spatially coupled

Table VI.1

FINAL FORM OF THE TIME-DEPENDENT FINITE-DIFFERENCED ONE-DIMENSIONAL HEAT TRANSFER EQUATIONS

$$(1 + \theta b_1) T_1^{n+1} + (-\theta b_1) T_2^{n+1} = T_1^n + (1 - \theta) [b_1(T_2^n - T_1^n)] + s$$

$$(-\theta a_2) T_1^{n+1} + (1 + \theta a_2 + \theta b_2) T_2^{n+1} + (-\theta b_2) T_3^{n+1} = T_2^n + (1 - \theta) [a_2(T_1^n - T_2^n) + b_2(T_3^n - T_2^n)]$$

$$(-\theta a_3) T_2^{n+1} + (1 + \theta a_3 + \theta b_3) T_3^{n+1} + (-\theta b_3) T_4^{n+1} = T_3^n + (1 - \theta) [a_3(T_2^n - T_3^n) + b_3(T_4^n - T_3^n)]$$

•

•

•

$$(-\theta a_{12}) T_{11}^{n+1} + (1 + \theta a_{12} + \theta b_{12}) T_{12}^{n+1} + (-\theta b_{12}) T_{13}^{n+1} = T_{12}^n + (1 - \theta) [a_{12}(T_{11}^n - T_{12}^n) + b_{12}(T_{13}^n - T_{12}^n)]$$

$$(-\theta a_{13}) T_{12}^{n+1} + (1 + \theta a_{13} + \theta b_{13}) T_{13}^{n+1} = T_{13}^n + (1 - \theta) [a_{13}(T_{12}^n - T_{13}^n) + b_{13}(T_{amb}^n - T_{13}^n)]$$

*Note: T_i^n denotes the temperature at node i at the end of the n^{th} iteration.

equations at each time step. Because they can be cast in the form of a tridiagonal matrix, the solution can be obtained quickly and easily using matrix factorization.⁴⁵ Interestingly, the accumulated error in the solution at any point in time is roughly proportional to (Δt) . With $\theta=0.5$, one obtains what in heat transfer circles is known as the Crank-Nicolson model.^{8,46} Given a set of constant coefficients, it is stable regardless of the size of the time step, and generally more accurate than either of the other two schemes. [Here the error, ϵ , is proportional to $(\Delta t)^2$]. Because of its greater accuracy, one can use somewhat larger time steps to obtain a given degree of refinement.* In the SCOPE code, the value of θ is typically initialized to 0.5 in order to obtain the Crank-Nicolson solution. As with $\theta = 1$, matrix factorization⁴⁵ is used to obtain the complete spatial solution at each time step.

As noted earlier, the actual equations coded into the SCOPE program have been generalized to allow for any number of nodes in any given zone. (In practice, we have found the 13 equations illustrated here to be quite adequate in most cases). For casks not having a solid aluminum or copper insert, the corresponding nodal equations are deleted from the matrix and the solution proceeds as before. For casks not having a neutron shield

* Unfortunately, the heat transfer coefficients (h) appearing in the equations for nodes 3,4,10,11 and 13 do depend on temperature, and hence on time (t). Accurate approximation of these will limit the maximum Δt that can be used.^{8,46} Nevertheless, since these are often very slowly varying parameters, the Δt used can (at times) be quite large. In the SCOPE code, we use a Δt as small as 9 seconds during the rapidly varying portion of the transient, and as large as 10 minutes during the more slowly varying portion of the transient ($t >$ several hours). The use of a large, variable time step greatly reduces the overall calculational time. The whole question of accuracy is addressed in Section VI.E.

and outside liner, the corresponding nodal equations may again be deleted, with the solution again proceeding as before. Likewise, analyses for casks having fins would be carried out using the equation(s) shown in Table K.8, whereas those not having fins would be analyzed using the equation(s) shown in Table K.7. In any event, the resulting tridiagonal system of equations can be solved quickly and easily at each time step using matrix factorization (i.e., a "direct" solution technique as opposed to an iterative technique). This, combined with the use of a large, variable time step permitted by the Crank-Nicolson differencing scheme, provides for a very fast and efficient means of solving the time-dependent finite-differenced one-dimensional heat transfer equations under the accident and post-accident conditions. More details of the actual solution are discussed in Section VI.D.

VI.D PROCEDURE FOR SOLVING THE TIME-DEPENDENT FINITE-DIFFERENCE ONE-DIMENSIONAL HEAT TRANSFER EQUATIONS THROUGHOUT THE TRANSIENT

VI.D.1 DESCRIPTION OF THE STEP-BY-STEP PROCEDURE ACTUALLY USED

Numerical solution of the 1-D thermal transient problem proceeds in the following fashion:

- (1) With the neutron shield voided as shown in Fig. VI.1, the steady-state temperature distribution must be determined at each node. This is done in much the same fashion as described in Section V, except that here: (a) one uses a constant linear heat rate ($q' = Q/L_A$, where L_A is the active length of the fuel); and (b) the ΔT across the voided neutron shield is now calculated using the equations shown in Section VI.D.3. This new steady-state temperature distribution is based on an ambient temperature of 130°F.
- (2) For the next half-hour, the cask is assumed to be totally engulfed by fire having a flame temperature of 1475°F.
- (3) Knowing the initial temperature distribution at $t = 0$, calculate the heat transfer coefficient at the gap between the insert and the inner shell ($h_3^{(0)}$), across the void between the outer shell and the outside liner ($h_{10}^{(0)}$), and at the surface of the cask ($h_{13}^{(0)}$, or $h_{cask}^{(0)}$ and $h_{fin}^{(0)}$, as the case may be). The heat transfer coefficient(s) at the surface of the cask should be calculated assuming a flame temperature of 1475°F and a flame emissivity of 0.9.
- (4) Using the notation set forth in Fig. VI.1 and Table VI.1, the matrix coefficients $b_3^{(0)}$, $a_4^{(0)}$, $b_{10}^{(0)}$, $a_{11}^{(0)}$, and $b_{13}^{(0)}$ should all be evaluated using the heat transfer coefficients just calculated. (See Appendix K for additional details).

- (5) Knowing the initial temperature distribution and all of the necessary heat transfer coefficients, solve the time-dependent finite-differenced one-dimensional heat transfer equations shown in Table VI.1 for the spatial temperature distribution at time t_1 .
 - (6) Knowing the spatial temperature distribution at time t_1 , recalculate each of the heat transfer coefficients ($h_3^{(1)}$, $h_{10}^{(1)}$, $h_{13}^{(1)}$, etc.)
 - (7) Using the notation set forth in Fig. VI.1 and Table VI.1, the matrix coefficients $b_3^{(n)}$, $a_4^{(n)}$, $b_{10}^{(n)}$, $a_{11}^{(n)}$, and $b_{13}^{(n)}$ should all be recalculated using the most recent values for the various heat transfer coefficients. (See Appendix K for additional details).
 - (8) Knowing the spatial temperature distribution and all of the necessary heat transfer coefficients at time t_1 , solve the time-dependent finite-differenced one-dimensional heat transfer equations for the spatial temperature distribution at time t_2 .
 - (9) Repeat (6), (7), and (8) for each time interval (t_n) until the end of the 30-minute fire (i.e. until $t_n = 0.5$ hrs.)
 - (10) Reset the ambient temperature to 130°F.
 - (11) Repeat (6), (7), and (8) for each time interval (t_n) until the cask has cooled off and is close to equilibrium (i.e. until $t_n \geq 12$ hours).
- . The actual time steps used in this calculation and the accuracy of the solution are discussed in Section VI.E. The purpose of this section is to document how the various heat transfer coefficients ($h_3^{(n)}$, $h_{10}^{(n)}$, $h_{13}^{(n)}$, etc.) are calculated during and after the 30-minute fire.

VI.D.2 DETERMINATION OF THE HEAT TRANSFER COEFFICIENT BETWEEN THE INSERT AND THE INNER SHELL

Because the gap between the insert and the inner shell is so thin, convective forces may be ignored and the heat transfer rate (Q_{leak}) can be written as

$$Q_{\text{leak}} = A_I \left\{ \left(\frac{\sigma}{\frac{1}{\epsilon_I} + \frac{1}{\epsilon_{IS}} - 1} \right) (T_3^4 - T_4^4) + \left(\frac{1}{r_4 - r_3} \right) \int_{T_4}^{T_3} k_{\text{air}}(T) dT \right\} \quad (\text{VI.7})$$

where T_3 and T_4 are the surface temperatures in degrees Rankine ($^{\circ}\text{R}$)

ϵ_I and ϵ_{IS} are the actual emissivities of the two surfaces
(dimensionless)

σ is the Stefan-Boltzmann constant [0.1714×10^{-8} (Btu/hr)/(ft²)($^{\circ}\text{R}$)⁴]

and A_I is the area of the insert perpendicular to the flow of heat (ft²)

The overall heat transfer coefficient at any given time is therefore defined as

$$h_3 = \left(\frac{1}{T_3 - T_4} \right) \left\{ \left(\frac{\sigma}{\frac{1}{\epsilon_I} + \frac{1}{\epsilon_{IS}} - 1} \right) (T_3^4 - T_4^4) + \left(\frac{1}{r_4 - r_3} \right) \int_{T_4}^{T_3} k_{\text{air}}(T) dT \right\} \quad (\text{VI.8})$$

where T_3 and T_4 are known from the spatial solution at the previous time step. Because the ΔT across the gap may be quite substantial ($\sim 250^{\circ}\text{F}$ for casks carrying 1-year-old spent fuel, and $\sim 100^{\circ}\text{F}$ for casks carrying 10-year-old spent fuel), the thermal conductivity of the air is integrated from $T_4^{(n)}$ to $T_3^{(n)}$. Because $k_{\text{air}}(T)$ varies almost linearly with T , the SCOPE code numerically integrates the tabulated data using the trapezoidal formula.

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VI.D.3 DETERMINATION OF THE HEAT TRANSFER COEFFICIENT BETWEEN THE OUTER SHELL AND THE OUTSIDE LINER

In the void which now exists between the outer shell and the outside liner, one must account for both radiation and convection. While the region is essentially void, it still contains air which is a fluid medium capable of supporting and/or developing large kidney-shaped convective loops within the region, as shown in Fig. VI.4a and VI.4b. Combining both terms, the overall heat transfer rate (Q_{leak}) across the region may be

written as

$$Q_{\text{leak}} = A_{\text{OS}} \left\{ \left[\frac{\sigma}{\frac{1}{\epsilon_{\text{OS}}} + \left(\frac{r_{10}}{r_{11}}\right) \left(\frac{1}{\epsilon_{\text{OL}}} - 1\right)} \right] (T_{10}^4 - T_{11}^4) + k_e \left(\frac{T_{10} - T_{11}}{r_{11} - r_{10}} \right) \right\} \quad (\text{VI.9})$$

where T_{10} and T_{11} are the surface temperatures in degrees Rankine ($^{\circ}\text{R}$)

ϵ_{OS} and ϵ_{OL} are the actual emissivities of the two surfaces
(dimensionless)

σ is the Stefan-Boltzmann constant [$0.1714 \times 10^{-8} (\text{Btu/hr})/(\text{ft}^2)(^{\circ}\text{R})^4$]

k_e is the effective thermal conductivity of the fluid (air) in the
region

and A_{OS} is the outside surface area of the outer shell ($2\pi r_{10}L$).

The overall heat transfer coefficient at any time is therefore defined as

$$h_{10} = \left[\frac{\sigma}{\frac{1}{\epsilon_{\text{OS}}} + \left(\frac{r_{10}}{r_{11}}\right) \left(\frac{1}{\epsilon_{\text{OL}}} - 1\right)} \right] \left(\frac{T_{10}^4 - T_{11}^4}{T_{10} - T_{11}} \right) + \left(\frac{k_e}{r_{11} - r_{10}} \right) \quad (\text{VI.10})$$

where T_{10} and T_{11} are known from the spatial solution at the previous time step. Before h_{10} can be evaluated, however, the effective thermal conductivity of the region (k_e) must be determined. Having conducted an extensive series of natural convection heat transfer experiments for

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Fig. VI.4: Naturally Occurring Convective Loops in the Annular Region Between Concentric Cylinders at Different Temperatures

various fluids (including air) trapped between two long horizontal cylinders, Liu et al.⁴² recommend the following correlation for the effective thermal conductivity of the air in the region:

$$k_e = (0.135)k_{\text{air}} \left(\frac{\text{Pr}^2 \text{Gr}}{1.36 + \text{Pr}} \right)^{0.278} \quad \text{for } 10^3 < \left(\frac{\text{Pr}^2 \text{Gr}}{1.36 + \text{Pr}} \right) < 10^8 \quad (\text{VI.11a})$$

and

$$k_e = k_{\text{air}} \quad \text{for} \quad \left(\frac{\text{Pr}^2 \text{Gr}}{1.36 + \text{Pr}} \right) < 10^3 \quad . \quad (\text{VI.11b})$$

Here, the Grashof number (Gr) is defined as

$$\text{Gr} = g\beta \left(\frac{\rho}{\mu} \right)^2 (r_{11} - r_{10})^3 \left| T_{11}^{(n)} - T_{10}^{(n)} \right| \quad (\text{VI.12})$$

where g is the gravitational constant (4.17×10^8 ft/hr²)

β is the volumetric expansion coefficient of the air ($^{\circ}\text{F}^{-1}$)

ρ is the density of the air (lbm/ft³)

and μ is the dynamic viscosity of the air (lbm/hr)/ft .

Likewise, the Prandtl number (Pr) is defined as

$$\text{Pr} = \frac{c_p \mu}{k_{\text{air}}} \quad (\text{VI.13})$$

where c_p is the specific heat of the air (Btu/lbm)/ $^{\circ}\text{F}$

μ is the dynamic viscosity of the air (lbm/hr)/ft

and k_{air} is the tabulated value of the air's thermal conductivity at

$$T_{\text{avg}}^{(n)} \quad (\text{Btu/hr})/\text{ft}^{\circ}\text{F} \quad .$$

Note, of course, that all of the material properties used in Eqs. VI.12

and VI.13 should be (and indeed are) calculated at the "mean" temperature corresponding to the previous time step:

$$T_{\text{avg}}^{(n)} = (T_{10}^{(n)} + T_{11}^{(n)})/2 \quad . \quad (\text{VI.14})$$

For convenience, some of the parameters such as the density (ρ) and the volumetric expansion coefficient (β) are calculated using the perfect gas law. The others, of course, are evaluated at the appropriate temperature by linearly interpolating the tabulated data.

VI.D.4 DETERMINATION OF THE HEAT TRANSFER COEFFICIENT(S) AT THE SURFACE, BEFORE AND AFTER THE FIRE

Before and after the 30-minute fire, the heat transfer rate at the surface of a bare (finless) cask may be calculated as

$$Q_{\text{leak}} = A_{\text{cask}} \left\{ \sigma \epsilon_c (T_{13}^4 - T_{\text{amb}}^4) + C (T_{13} - T_{\text{amb}})^{4/3} \right\} \quad (\text{VI.15})$$

where T_{13} and T_{amb} are the surface and ambient temperatures in degrees Rankine ($^{\circ}\text{R}$)

σ is the Stefan-Boltzmann constant [$0.1714 \times 10^{-8} (\text{Btu/hr})/(\text{ft}^2)(^{\circ}\text{R})^4$]

ϵ_c is the surface emissivity of the cask (dimensionless)

C is the constant (0.18) used in the McAdams correlation³⁶ for natural convection heat transfer when one has a horizontal cylinder in air

and A_{cask} is the surface area of the outside liner, not including the ends ($2\pi r_{13} L$).

The overall heat transfer coefficient at any given time is therefore defined as

$$h_{13} = \sigma \epsilon_c \left(\frac{T_{13}^4 - T_{\text{amb}}^4}{T_{13} - T_{\text{amb}}} \right) + C (T_{13} - T_{\text{amb}})^{1/3} \quad (\text{VI.16})$$

where T_{13} is known from the spatial solution at the previous time step [$T_{13}^{(n)}$], and $T_{\text{amb}} = 130^{\circ}\text{F} = 590^{\circ}\text{R}$. For a cask with fins, the time-dependent finite-differenced 1-D heat transfer equation for node 13 is more complex (cf. Appendix K) and involves the heat transfer coefficient for the cask body (h_c), the heat transfer coefficient for the fins (h_f), and the fin effectiveness (η) -- each of which must be calculated at the

beginning of each time interval. As in Section V.B, the heat transfer coefficients for the cask and fins are given by

$$h_c = \sigma \epsilon'_c \left(\frac{T_{13}^4 - T_{amb}^4}{T_{13} - T_{amb}} \right) + C_1 (T_{13} - T_{amb})^{1/3}, \quad C_1 = 0.18 \quad (VI.17a)$$

and

$$h_f = \sigma \epsilon'_f \left(\frac{T_{13}^4 - T_{amb}^4}{T_{13} - T_{amb}} \right) + C_2 (T_{13} - T_{amb})^{1/3}, \quad C_2 = 0.19 \quad (VI.17b)$$

where the effective emissivities (ϵ'_c and ϵ'_f) are calculated as in Appendix G. Since the effective emissivities (ϵ'_c and ϵ'_f) depend only on the physical properties of the given materials and the geometric view factors associated with the given design, they maintain the same numerical values they had during the steady-state calculation, and need not be updated during the transient analysis. The heat transfer coefficients (h_c and h_f) must, however, be updated at the beginning of each time step using the surface temperature from the previous time step [$T_{13}^{(n)}$]. Because the fin effectiveness (η) depends on the overall heat transfer coefficient for the fin (h_f), it must also be re-evaluated at the beginning of each time step. As a good first-order approximation, the fin effectiveness may be calculated as

$$\eta = \frac{1}{1 + \frac{2}{3} \left(\frac{h_f r_f^2}{k_f t_f} \right) \sqrt{\frac{r_f}{r_{13}}}} \quad (VI.18)$$

where h_f is the overall heat transfer coefficient for the fin at the

beginning of the current time step (Btu/hr)/ft²°F

k_f is the thermal conductivity of the fin (Btu/hr)/ft°F

l_f is the length of the fin in feet ($l_f = r_f - r_{13}$)
and t_f is the thickness of the fin (ft) .

To be more precise, the SCOPE code actually reevaluates the fin effectiveness at the beginning of each time step using the more rigorous procedure described in Appendix H.

VI.D.5 DETERMINATION OF THE HEAT TRANSFER COEFFICIENT(S) AT THE SURFACE, DURING THE 30-MINUTE FIRE

During the 30-minute fire, the cask is assumed to absorb heat by radiation only. With a flame temperature of 1475°F (1935°R), a flame emissivity of 0.9, and a cask absorption coefficient of 0.8, as required by regulations, the actual heat absorption rate for a bare (finless) cask is given by⁴:

$$Q = A_{\text{cask}} \sigma \alpha_{\text{cask}} [\epsilon_{\text{flame}} T_{\text{flame}}^4 - T_{13}^4] \quad (\text{VI.19a})$$

$$= A_{\text{cask}} \sigma (0.8) [(0.9)(1935)^4 - T_{13}^4] = A_{\text{cask}} \sigma (0.8) [(1885)^4 - T_{13}^4] \quad (\text{VI.19b})$$

The effective ambient temperature during the fire is therefore 1425°F (1885°R). Accordingly, the overall heat transfer coefficient at any given time during the fire is calculated as

$$h_{13} = \sigma \alpha_c \left(\frac{T_{\text{amb}}^4 - T_{13}^4}{T_{\text{amb}} - T_{13}} \right), \quad T_{\text{amb}} = 1885^\circ\text{R}, \quad \alpha_c = 0.8 \quad (\text{VI.20})$$

where T_{13} is known from the spatial solution at the previous time step $[T_{13}^{(n)}]$. Likewise, in the case of a cask having fins, the corresponding heat transfer coefficients (h_c and h_f) are calculated as

$$h_c = \sigma \alpha_c \left(\frac{T_{\text{amb}}^4 - T_{13}^4}{T_{\text{amb}} - T_{13}} \right), \quad T_{\text{amb}} = 1885^\circ\text{R}, \quad \alpha_c = 0.8 \quad (\text{VI.21a})$$

$$h_f = \sigma \alpha_f \left(\frac{T_{\text{amb}}^4 - T_{13}^4}{T_{\text{amb}} - T_{13}} \right), \quad T_{\text{amb}} = 1885^\circ\text{R}, \quad \alpha_f = 0.8 \quad (\text{VI.21b})$$

where again, T_{13} is known from the spatial solution at the previous time step $[T_{13}^{(n)}]$, and σ is the well-known Stefan-Boltzmann constant $[0.1714 \times 10^{-8} (\text{Btu/hr})/(\text{ft}^2)(^\circ\text{R})^4]$. During the fire, the temperature along the surface of the fin will be essentially constant and, for that reason, a fin effectiveness (η) of 1.0 is assumed.

VI.E SELECTION OF SUITABLE TIME STEPS FOR SOLVING THE TRANSIENT PROBLEM, AND SUBSEQUENT VALIDATION OF THE SCOPE CODE

With the Crank-Nicolson ($\theta = 0.5$) differencing scheme used in the SCOPE code, we were free to use a fairly large, variable time step in the transient calculation without undue concern for numerical stability (cf. Section VI.C or Appendix K). As noted in Section VI.B, however, the values ascribed to the various heat transfer coefficients can often dominate the spatial behavior of the transient temperature distribution throughout the cask. These, unfortunately, do depend on the temperatures of the various surfaces and must be updated at the end of each time step as noted in Section VI.D. Updating these heat transfer coefficients on a frequent basis will obviously provide a more accurate solution. Even in a cask without gaps or voids, the accuracy of the finite-differenced solution would be somewhat dependent on the size of the time steps used. After experimenting with several of the "typical" cask designs encountered in this study and determining the minimum time step required for an accurate solution during each phase of the transient, we finally decided to use ~80 time steps during the 30-minute fire and ~226 time steps in the 11.5 hour period following the fire. The size of the individual time steps vary from 9 seconds to 10 minutes, as shown in Table VI.2.^{*}

Before attempting to validate the SCOPE code against an accepted standard, it is important to point out that the thermal response of different cask designs may be significantly different. Fig. VI.5, for example, shows the calculated thermal response of a uranium-metal cask

^{*} While this selection of time-dependent time intervals is now fixed in the SCOPE code, it could be changed by means of a single DATA statement in the transient analysis routine.

Table VI.2
Time Steps Used by the SCOPE Code for the Transient
Thermal Analysis During and After the 30-Minute Fire

TIME	t	N
0 min -- 3 min	9 sec	20
3 min -- 9 min	15 sec	24
9 min -- 18 min	27 sec	20
18 min -- 30 min	45 sec	16
30 min -- 33 min	9 sec	20
33 min -- 39 min	15 sec	24
39 min -- 48 min	27 sec	20
48 min -- 66 min	45 sec	24
66 min -- 96 min	75 sec	24
96 min -- 3 hrs	2 min	42
3 hrs -- 6 hrs	5 min	36
6 hrs -- 12 hrs	10 min	36

Fig. VI.5: Calculated Space/Time Thermal Response of a Large Uranium-Metal Cask During and After the Postulated 30-Minute Fire (Model Assumes Voided Neut Shield and Thin Gap by Insert)

designed to carry 15 1-year-old FWR spent fuel assemblies. The cask has a 0.125" gap between the insert and the inner shell, and a 3.75" void where the neutron shield used to be. To dissipate the steady-state decay heat load, 8" copper fins were attached circumferentially to the outside surface of the outside liner. Profile "A" shows the steady-state temperature distribution with the neutron shield in place, while profile "0" shows the steady-state temperature distribution after the neutron shield has been lost.^{*} Profiles 1 through 8 show the temperature distribution at different times during the transient. Because of the insulating effect afforded by the absence of the neutron shield, the temperature of the outside liner may vary from 250°F, up to 1400°F, and back down to 250°F during the transient, while temperatures throughout the rest of the cask vary much less (i.e. from ~700°F, up to 900°F, and back down to ~700°F) and exhibit only a very slight spatial gradient. Fig. VI.6, on the other hand, shows the calculated thermal response of the same cask if the neutron shield, outside liner, and circumferential fins

* As noted previously, the thermal conductivity of the aluminum insert is artificially adjusted downward in this simplified 1-D model so as to yield the same steady-state ΔT across the insert that one would obtain with the 2-D heat conduction model described in Section V.G. While that adjustment changes the slope of the steady-state temperature profile inside the insert, it has little or no effect on the transient response throughout the rest of the cask. Indeed, we have even found this adjustment to be relatively unimportant in calculating the actual temperature rise in the insert as a result of the transient -- i.e. with $k = 3.8$ (Btu/hr)/ft°F, the maximum difference between profile 7 and profile A in Fig. VI.5 was 298.03°F; whereas with $k = 140.0$ (Btu/hr)/ft°F, the maximum difference between profile 7 and profile A was 299.89°F; the two profiles were, of course, considerably steeper in the one case and considerably flatter in the other.

Fig. VI.6: Calculated Space/Time Thermal Response of a Similar Uranium-Metal Cask During and After the Postulated 30-Minute Fire (Model Assumes No Neut Shield, Outside Liner, or Ext Fins; It Also Assumes Perfect Contact Between Insert and Inner Shell)

were to be removed.^{*} (In addition, this model also assumes perfect contact between the insert and the inner shell). Given this configuration, the thermal response of the system is altogether different. Indeed, two things become immediately obvious: (1) Because the heat from the fire is conducted inward much more readily and absorbed by the "whole" cask, the surface temperature does not increase nearly as much or as rapidly as in the previous case -- ie: the fire just can't "pump the heat in fast enough" to raise the surface temperature much above 970°F during the half-hour it is allowed to burn; (2) Because of the enhanced inward flow of heat during the fire and the enhanced outward flow of heat after the fire, we now see some fairly steep, rapidly changing spatial gradients across the gamma shield and the other structural components.

The two cases represented by Fig. VI.5 and Fig. VI.6 are significantly different, and each represents a severe test of the computational algorithm and the Δt 's used in the solution. The first requires that we accurately calculate and frequently update the heat transfer coefficients on the external surface and across the two internal voids. The second requires that the Δt 's be small enough that we can accurately calculate the severe gradients across the various components as a function of time.

To validate the 1-D transient thermal analysis package used in the SCOPE code and the adequacy of the time intervals (Δt 's) shown in Table VI.2, two configurations similar to those shown in Fig. VI.5 and Fig. VI.6 were analyzed using the HEATING-5 code⁸. To insure the accuracy of the

^{*} With no fins, of course, the steady-state temperature on the surface of the cask would be considerably hotter (~480°F under nominal conditions).

HEATING-5 results, the forward-differenced explicit solution technique was used with an exceedingly small time increment. Whereas the stability criteria (edited by the HEATING-5 code) required that one use a uniform Δt less than or equal to 9 seconds, we intentionally used a Δt of 0.9 seconds throughout the entire 12-hour transient (~ 48000 time steps). The SCOPE calculations, on the other hand, used 306 time steps as shown in Table VI.2. The first benchmark configuration did, in fact, correspond to that shown in Fig. VI.6 insofar as it had no gap, no neutron shield, no outside liner, and no fins. The SCOPE results and the HEATING-5 results at various points throughout the cask are shown in Table VI.3 at nine different times. While there are a couple of instances where the results differ by 2, 3, or 4°F, they generally agree to within $\pm 1^\circ\text{F}$ at all spatial points during the entire transient. The second benchmark configuration differs from that shown in Fig. VI.5 only to the extent that the fins were ignored in both sets of calculations.^{*a,b} Because of that, the outside liner did not absorb the heat from the fire quite as rapidly as the finned cask shown in Fig. VI.5. Nevertheless, the thin gap on the left and the large void on the right still provide an important degree of thermal insulation, and the transient behavior is very similar to that shown in Fig. VI.5. The SCOPE results and the HEATING-5 results for this configuration are shown in Table VI.4. Again we note that the two sets of results generally agree to within $\pm 1^\circ\text{F}$ at all spatial points during the entire transient.

^{*a} The version of HEATING-5 that was available at the time these calculations were performed could not account for external fins.

^{*b} In these particular calculations, the thermal conductivity of the insert was not readjusted so as to reproduce the 2-D steady-state ΔT across the insert. The profile in the insert is therefore much flatter than that shown in Fig. VI.5. That adjustment, however, has essentially no impact on the transient temperature distribution anywhere else in the cask.

Table VI.3

Comparison of SCOPE and HEATING-5 Results for the Transient Temperature Distribution (°F) Inside a Large Uranium-Metal Cask During and After the Postulated 30-Minute Fire (the model used here is essentially identical to that shown in Fig. VI.6)

		← INSERT →		← INNER SHELL →		← GAMMA SHIELD →		← OUTER SHELL (1") →		← OUTSIDE LINER (1") →			
t=0 sec	SCOPE	709.12	610.01 ^b	533.17	529.26	525.45	512.08	500.21	495.70	491.33	486.95	482.71	
	HEATINGS	709.17	615.60 ^c	533.29	529.40	525.56	512.57	500.32	495.84	491.43	487.09	482.82	
t=3 min	SCOPE	709.12	610.01 ^b	533.36	529.74	526.43	517.32	522.91	536.57	560.20	596.89	647.09	
	HEATINGS	709.17	615.60 ^c	533.36	529.67	526.20	516.46	520.39	533.70	557.39	593.76	644.34	
t=18 min	SCOPE	710.87	619.60 ^b	597.02	602.09	609.25	650.36	712.27	744.05	780.53	822.77	869.34	
	HEATINGS	710.85	623.96 ^c	594.64	599.86	607.23	647.19	710.83	742.49	779.38	821.38	868.37	
t=30 min	SCOPE	721.08	647.89 ^b	681.12	689.25	699.40	750.55	817.65	849.50	884.61	923.90	966.02	
	HEATINGS	721.14	650.17 ^c	678.50	686.74	697.11	746.92	816.16	847.94	883.48	922.59	965.11	
t=37 min	SCOPE	732.54	670.60 ^b	723.25	729.68	737.03	768.60	792.71	796.88	796.74	791.75	781.81	
	HEATINGS	732.92	671.88 ^c	721.88	728.40	735.97	767.18	792.85	797.10	797.03	792.13	782.16	
t=54 min	SCOPE	771.23	717.64 ^b	736.74	737.05	737.07	734.79	727.46	722.64	715.52	708.88	699.95	
	HEATINGS	772.34	718.29 ^c	737.19	737.41	737.36	735.02	727.52	722.73	716.56	708.98	700.01	
t=2.5 hr	SCOPE	856.33	756.59 ^b	673.92	669.35	664.77	647.82	631.56	625.02	618.44	611.62	604.77	
	HEATINGS	856.99	762.75 ^c	674.67	670.12	665.50	649.03	632.25	625.75	619.11	612.34	605.43	
t=5.0 hr	SCOPE	823.07	719.17 ^b	631.42	626.75	622.14	605.49	590.11	584.10	578.17	572.12	566.15	
	HEATINGS	823.38	725.70 ^c	632.03	627.39	622.74	606.56	590.70	584.73	578.74	572.74	566.72	
t=12 hrs	SCOPE	755.03	654.04 ^b	574.96	568.75	564.63	550.00	536.77	531.69	526.71	521.69	516.78	
	HEATINGS	755.25	660.07 ^c	573.33	569.15	565.00	550.78	537.14	532.08	527.06	522.08	517.14	

a) Note that this cask has no neutron shield, no outside liner, and no external cooling fins. As a matter of calculational convenience, the 2" outer shell shown in Fig VI.6 was represented by a 1" outer shell in perfect contact with a 1" outside liner.

b) Temperature calculated at R=2.0688 ft

c) Temperature calculated at R=2.0520 ft

Table VI.4

Comparison of SCOPE and HEATING-5 Results for the Transient Temperature Distribution ($^{\circ}\text{F}$) Inside a Large Uranium-Metal Cask During and After the Postulated 30-Minute Fire (the model used here is similar to that shown in Fig VI.5 but lacks ext fins)

		INSERT			INNER SHELL			GAMMA SHIELD			OUTER SHELL			OUTSIDE LINER		
t=0 sec	SCOPE	1065.48	1062.27	1059.05	818.70	814.48	810.31	796.14	782.85	773.24	763.93	476.16	473.14	470.14		
	HEATING5	1065.49	1062.28	1059.05	818.70	814.48	810.32	796.14	782.85	773.24	763.93	476.16	473.14	470.14		
t=3 min	SCOPE	1065.66	1062.00	1059.20	819.06	814.73	810.53	795.93	783.26	774.33	768.36	678.30	688.03	718.98		
	HEATING5	1065.49	1062.28	1059.05	818.70	814.48	810.32	796.18	783.11	774.31	767.88	679.90	689.45	720.31		
t=9 min	SCOPE	1065.57	1061.90	1059.12	820.02	815.75	811.74	798.72	790.10	788.19	794.27	1006.82	1017.49	1040.65		
	HEATING5	1065.49	1062.28	1059.06	819.51	815.36	811.39	798.84	789.93	788.13	794.45	1010.93	1021.47	1044.47		
t=30 min	SCOPE	1066.86	1063.33	1060.85	864.14	861.02	859.07	859.82	871.47	889.85	916.88	1278.61	1289.67	1301.09		
	HEATING5	1067.10	1063.98	1061.09	864.04	860.99	859.07	859.67	871.58	889.71	917.23	1278.93	1289.67	1301.25		
t=36 min	SCOPE	1068.35	1064.88	1062.55	884.92	882.08	880.36	881.41	890.43	901.06	910.85	1018.56	1016.01	1003.67		
	HEATING5	1068.65	1065.58	1062.85	884.90	882.13	880.43	881.30	890.56	901.02	911.04	1018.56	1016.09	1003.89		
t=54 min	SCOPE	1075.45	1072.07	1069.93	916.33	913.42	910.77	902.50	895.77	890.16	883.60	726.59	722.72	716.34		
	HEATING5	1075.93	1072.92	1070.39	916.27	913.43	910.80	902.79	895.88	890.37	883.72	726.24	722.45	716.12		
t=2.5 hr	SCOPE	1102.25	1098.67	1096.08	895.09	891.11	887.16	872.74	859.24	848.31	837.42	538.33	534.50	530.64		
	HEATING5	1103.45	1100.30	1097.27	894.72	890.82	886.90	872.98	859.07	848.33	837.26	538.19	534.42	530.57		
t=4.5 hr	SCOPE	1105.61	1101.93	1099.13	872.99	868.60	864.29	848.90	835.00	824.15	813.69	514.73	511.15	507.61		
	HEATING5	1107.40	1104.18	1100.91	872.53	868.21	863.92	849.04	834.69	824.01	813.38	514.47	510.95	507.43		
t=12 hrs	SCOPE	1084.46	1080.78	1077.96	841.93	837.52	833.23	818.10	804.68	794.41	784.71	491.65	488.37	485.16		
	HEATING5	1086.96	1083.73	1080.45	841.75	837.41	833.13	818.46	804.59	794.47	784.59	491.57	488.34	485.13		

*Note that this model accounts for the small (0.125") gap between the insert and the inner shell. It also accounts for the large void left by the absence of the borated-water neutron shield between the outer shell and the outside liner.

*in these calculations, the thermal conductivity of the insert has not been adjusted so as to reproduce the 2-D steady-state ΔT across the insert. That adjustment, however, has essentially no impact on the transient temperature distribution anywhere else in the cask.

VI.F ESTIMATION OF THE MAXIMUM INSERT AND GAMMA SHIELD TEMPERATURES DURING THE TRANSIENT

Just prior to performing the 1-1 thermal transient calculation, we must solve for the steady-state temperature distribution at each node after the neutron shield has been voided. This is done in much the same fashion as described in Section V, except that here: (a) one uses a constant linear heat rate ($q' = Q/L_A$, where L_A is the active length of the fuel); and (b) the ΔT across the voided neutron shield is now calculated using the equations shown in Section VI.D.3. The resulting temperature distribution (cf. profile "0" in Fig. VI.5, VI.6, or VI.7) then serves as the initial condition for the transient problem. In parallel with this 1-1 steady-state calculation, we also perform a similar 1-D steady-state calculation for the temperature distribution at each node with the neutron shield in place (cf. profile "A" in Fig. VI.5 or VI.7). This calculation differs from that described in Section V only insofar as we now use a constant linear heat rate ($q' = Q/L_A$) which is more consistent with the 1-D transient calculation. The resulting temperature distribution at each node, $T_A(r_i)$, then serves as a convenient "reference" distribution from which we can calculate the maximum temperature rise in each component at each instant during the transient.* Assuming the same 13 node model illustrated in Fig. VI.1, the maximum temperature rise in the gamma shield would be calculated as

* Depending on the structure of the cask, the maximum temperature rise in any given component may occur on the inner or outer surface of the component, or at any point in between (cf. Fig. VI.5 and Fig. VI.6). To determine the maximum temperature rise in each component, therefore, requires that we continually check the temperature rise at each node $[T(r_i, t_n) - T_A(r_i)]$.

Fig. VI.7: Definition of the Local Temperature Rise at Each Instant (As Subsequently Used to Determine the Maximum Temperature Rise in Each Component During the Entire Transient)

$$\Delta T_Y^{\max} = \max_n \left\{ \Delta T_6^{(n)}, \Delta T_7^{(n)}, \Delta T_8^{(n)} \right\}, \quad n = 0, 1, 2, \dots, 306 \quad (\text{VI.22})$$

where

$$\Delta T_i^{(n)} = T(r_i, t_n) - T_A(r_i), \quad n = 0, 1, 2, \dots, 306 \quad (\text{VI.23})$$

Likewise, the maximum temperature rise in the insert would be calculated as

$$\Delta T_I^{\max} = \max_n \left\{ \Delta T_1^{(n)}, \Delta T_2^{(n)}, \Delta T_3^{(n)} \right\}, \quad n = 0, 1, 2, \dots, 306 \quad (\text{VI.24})$$

Throughout the transient, the SCOPE code internally keeps track of the maximum temperature rise in each component and the time at which it occurred. At the end of the calculation, it prints an estimate of the maximum fuel pin clad temperature during the transient and the maximum insert and gamma shield temperatures during the transient. The method used to estimate the maximum fuel pin clad temperature during the transient is described in Section VI.G. The maximum insert and gamma shield temperatures during the transient are calculated as

$$T_Y^{\max} = T_{IGS} + \Delta T_Y^{\max} \quad (\text{VI.25})$$

and

$$T_I^{\max} = T_{II} + \Delta T_I^{\max} \quad (\text{VI.26})$$

where $\Delta T_Y^{\max}$ and $\Delta T_I^{\max}$ are given by Eqs. VI.22 and VI.24, and T_{IGS} and T_{II} are the nominal steady-state temperatures on the inside surface of the gamma shield and the innermost surface of the insert, as

calculated in Sections V.E and V.G respectively. Because Eqs. VI.25 and VI.26 take the maximum temperature rise in each component and add this to the maximum steady-state temperature in the component, the estimates of the maximum fire-test temperatures given by the SCOPE code should (by intent) be slightly conservative.

VI.G ESTIMATION OF THE MAXIMUM FUEL PIN CLAD TEMPERATURE DURING THE TRANSIENT

Estimating the maximum fuel pin clad temperature during the transient is not quite as easy as estimating the maximum temperature in the insert or the gamma shield. At the outset, two things should be noted:

- (1) Because we do not account for the heat capacity of the fuel or the explicit spatial arrangement of the components inside the cask cavity, we can only make an estimate of the maximum fuel pin temperature based on a steady-state calculation (although a good argument for doing just that is provided below);
- (2) Given that we must make a conservative, but reasonable, estimate based on some steady-state calculation, we must decide what insert temperature to assume in such a calculation (since, because of the radiation term in the Wootton-Epstein equation, the calculated ΔT will be small at elevated temperatures and large at lower temperatures).

In most casks, a small air gap ($\sim 0.125"$) is provided for clearance between the insert and the inner shell. In such cases, the temperature of the insert changes relatively little over a long period of time $[T(r_i, t_n) - T_0(r_i)]$ increasing, at most, $40\text{--}50^\circ\text{F}$ over a 4-5 hour period].^{*} Because the temperature of the insert changes relatively little over a long period of time, the heat flow from the fuel to the insert is relatively unaffected. Obviously the biggest ΔT between the

^{*} The temperature "increase" referred to here is the difference between the maximum temperature during the transient and the steady-state value that one would have after the neutron shield has been voided $[T(r_i, t_n) - T_0(r_i)]$. The $40\text{--}50^\circ\text{F}$ temperature rise cited here would, for example,ⁿ be characteristic of a large uranium-metal cask carrying 15 1-year-old PWR fuel assemblies. As such, it represents the worst case considered in this study. For casks containing 10-year-old spent fuel, the maximum temperature rise in the insert $[T(r_i, t_n) - T_0(r_i)]$ is typically much less ($< 8^\circ\text{F}$ over a 4-5 hour period).

fuel and the insert will occur when none of the decay heat is being stored in the fuel and it's all being radiated to the insert.^{*} To conservatively estimate the maximum temperature of the fuel during the transient, we therefore take this steady-state ΔT and add it to the maximum temperature of the insert during the transient ($T_I^{\max}$), as given by Eq. VI.26.

At this point it should be noted (as in Section V.H) that the steady-state ΔT between the hottest fuel pin and the surrounding insert depends not only on the decay heat generation rate within the assembly, but also on the actual temperature of the cell wall surrounding the fuel assembly (i.e. the insert temperature). Because of the radiation term in the Wootton-Epstein equation (as well as the nonlinear convective term), this calculated ΔT will be small if one assumes a very high insert temperature, and large if one assumes a low insert temperature. Use of the nominal insert temperature with the neutron shield in place [T_{iI} or $T_A(r_i)$] would, therefore, yield an unrealistically large ΔT , while use of the maximum insert temperature ($T_I^{\max}$) would underestimate the ΔT and lead to a possibly nonconservative result. For a realistic, yet slightly conservative (i.e. high) estimate of this ΔT , one should use the lowest temperature on the innermost surface of the insert during or after the fire.

* Strictly speaking, this statement is not absolutely correct since eventually (i.e. later in the transient) the fuel must radiate not only the decay heat that is being generated continuously, but also, some of the heat that was stored there earlier. This, however, is a slow process taking 12-24 hours in many cases. In the practical sense then, the above statement is essentially correct and the resulting logic is fairly sound.

Under the present scenario, the lowest value is obviously the equilibrium value after the neutron shield has been lost. It is this value, given by

$$T_{iI}^{(0)} = T_{iI} + [T_0(r_3) - T_A(r_3)] \doteq T_0(r_1) \quad , \quad (\text{VI.27})$$

which is then used in the Wootton-Epstein equation to calculate the maximum steady-state temperature difference (ΔT_{ss}) between the hottest fuel pin and the surrounding insert. Details of that calculation are described in Section V.H. Finally, the maximum fuel pin clad temperature is then estimated as

$$T_{\text{fuel}}^{\text{max}} \doteq \Delta T_{ss} + T_I^{\text{max}} \quad (\text{VI.28})$$

where T_I^{max} is given by Eq. VI.26.

VII. CONCEPTUAL DESIGNS FOR OPTIMIZED SPENT FUEL SHIPPING CASKS CONTAINING 1,2,3,5,7 AND 10-YEAR-OLD PWR FUEL ASSEMBLIES

The purpose of this conceptual design study was to develop a new generation of lead, iron, and uranium-metal spent fuel casks which have been optimized for the shipment of 1,2,3,5,7 and 10-year-old PWR spent fuel assemblies. Using the overall design philosophy described in Section II, the inherently subcritical fuel assembly separator described in Section III, and the optimized neutron and gamma shielding requirements described in Section IV.C and Appendix F, we began to address the basic question: "How many 1,2,3,5,7 or 10-year-old PWR fuel assemblies could actually be shipped in a Pb, Fe, or U-metal cask?" To answer that question, we made extensive use of the SCOPE program which was developed by the Computer Sciences Division at ORNL as a modestly rigorous yet inexpensive code for Shipping Cask Optimization and Parametric Evaluation.⁷ Using the optimal packing arrangements shown in Appendix B and the tabulated shielding requirements input by the user (cf. Appendix F), the code will "design" a cask to carry a single fuel assembly and then continue incrementing the number of assemblies until one or more of the design limits can no longer be met. In each case ($N = 1, 2, \dots$), the code will calculate the steady-state temperature distribution throughout the cask and perform a complete 1-D space/time transient thermal analysis following a postulated half-hour fire as described in Sections V and VI. In each case ($N = 1, 2, \dots$), the code will edit the characteristic dimensions of the cask (including fins, if required), the total weight of the loaded cask, the steady-state temperature at selected points, and the maximum transient temperature in key components.

VII.A OTHER ENGINEERING CONSIDERATIONS AFFECTING THE OPTIMIZATION OF CERTAIN CASKS

As noted in Section IV.B, a fair number of preliminary scoping studies had to be performed prior to determining the optimal shielding requirements. In the case of 1, 3, and 10-year-old fuel, for example, we had to determine the approximate number of assemblies that could be shipped in each type of cask so that we could then make a realistic determination of the optimal n/γ split to be used in determining the actual shielding requirements. In the case of 1 and 2-year-old fuel, we had to determine what impact the heavier stainless-steel fins would have on the choice of the optimal n/γ split. Indeed, in the case of the 1-year-old fuel, we found that the optimal n/γ split was significantly affected by the weight of the fins (cf. Table IV.15 and the accompanying discussion in Section IV.B.5). Subsequently, one set of optimal shielding requirements was generated for 1-year-old spent fuel casks having stainless-steel fins and another set of optimal shielding requirements was generated for casks having copper fins (cf. Appendix F). Because of its higher thermal conductivity and higher melting point, copper was also considered as an alternate material for the insert in those (1-year-old) spent fuel casks having a higher decay heat load.

Using the appropriate set of shielding requirements in each case, we thought it wise near the end of this study to reevaluate each of the above designs as a prelude to our final analyses. The results of these scoping calculations are shown in Table VII.1. Rather than being limited by the temperature of the insert (as initially feared), we see that the Fe and U-metal casks are, in fact, weight limited. Typically, those casks having

Table VII.1
Results of Initial Scoping Calculations Showing the
Carrying Capacity (N_{assys}) and Limiting Constraints
for Various Types of 1-Year-Old Spent Fuel Casks

SS-FINS							
AL-INSERT				CU-INSERT			
THICKNESS OF INSERT	Pb	Fe	U	THICKNESS OF INSERT	Pb	Fe	U
2.25"	4 ^b	9/10 ^a	11 ^a	2.25"	4 ^b	6 ^a	9 ^a
2.75"	4 ^b	8 ^a	11 ^a	2.75"	4 ^b	6 ^a	8 ^a
3.25"	4 ^b	8 ^a	11 ^a	3.25"	4 ^b	5 ^a	8 ^a

CU-FINS							
AL-INSERT				CU-INSERT			
THICKNESS OF INSERT	Pb	Fe	U	THICKNESS OF INSERT	Pb	Fe	U
2.25"	4 ^b	10 ^a	15 ^{a,e}	2.25"	4 ^b	6 ^a	10 ^a
2.75"	4 ^b	8 ^a	13 ^a	2.75"	4 ^b	6 ^a	10 ^a
3.25"	5 ^d	8 ^a	13 ^a	3.25"	5 ^c	5 ^a	8 ^a

- a) Weight limited (ie: design shown is already between 195-207,000 lbs)
- b) Pb gamma shield begins to melt (@ 618°F) during transient if $N > 4$
- c) Pb gamma shield begins to melt (@ 618°F) during transient if $N > 5$; in this particular case where cask has a Cu insert, $W = 159,800$ lbs
- d) Pb gamma shield begins to melt (@ 618°F) during transient if $N > 5$; in this particular case, $W = 134,700$ lbs. Note that increasing the size (and heat capacity) of the insert did afford the gamma shield some additional protection during the transient. Increasing the size (and heat capacity) of the outer shell and outside liner would permit as many as 6 assemblies, while still protecting the gamma shield.
- e) Insert temperature during the transient can get as high as 1125°F, which is very close to the melting point of many aluminum alloys.

an aluminum insert were able to accommodate 2-4 more PWR fuel assemblies than their counterparts with the copper inserts. [In each case, the difference is due solely to the larger mass of the copper (vs. aluminum) insert]. For that reason, casks with copper inserts were not considered further in the final set of analyses. From Table VII.1 it is also clear that the use of copper fins can offer some advantage -- especially in the case of the U-metal cask, where the design with copper fins can carry 2-4 more assemblies than the one with stainless-steel fins. Because of the general interest in these materials, both were considered further in the final set of analyses (cf. Table VII.3 in Section VII.B). Lastly, we note from Table VII.1 that the capacity of the Pb cask is generally limited by the onset of melting during the postulated 30-minute fire. Depending upon the amount of melting, this may or may not be a real design constraint. Insofar as the present version of the SCOPE code cannot account for the change of phase or calculate the amount of melting that might occur, this has been assumed to be a limiting constraint in Table VII.1. In Table VII.3 of Section VII.B, however, we do include the overall weight and characteristic dimensions of Pb casks designed to carry as many as fourteen 1-year-old PWR fuel assemblies. A more detailed thermal analysis of these casks should be performed at some later time.

Noting that the copper-finned Pb cask with an aluminum insert was significantly below the weight limit for large rail casks (~200,000 lbs), a few design modifications were made in an attempt to increase the carrying capacity while still protecting the gamma shield from melting. As shown in Table VII.1, increasing the thickness of the insert (between fuel assemblies) from 2.75" to 3.25" did increase the heat capacity of the insert to the

point where the cask could accommodate five 1-year-old assemblies. Further increasing the thickness of the insert proved fruitless. From a spatial edit of the temperature distribution as a function of time (during and after the 30-minute fire), it became obvious that the Pb gamma shield would first start melting along its outside surface due to the higher temperatures there. We decided, therefore, to increase the size (and net heat capacity) of the outside liner and the outer shell in order to absorb more of the external heat load in these components and reduce the temperature on the outer surface of the Pb gamma shield. By using a 1.25" thick outside liner (instead of a 0.75" outside liner) and a 2.5" outer shell (instead of a 2.0" outer shell) we found that we could accommodate up to six 1-year-old PWR assemblies in a cask whose aluminum insert was only 2.25" thick.* In this case, increasing the thickness of the insert had little measurable effect (except, of course, to increase the weight). In Table VII.3 of Section VII.B we show the thermal characteristics of this modified design, and the overall weight and characteristic dimensions of similar Pb casks designed to carry as many as thirteen 1-year-old PWR fuel assemblies.

The copper-finned U-metal cask having a 2.25" thick aluminum insert is another interesting case deserving further comment (cf. Table VII.1). It is interesting, of course, because it can carry as many as fifteen 1-year-old spent fuel assemblies. Weighing ~205,500 lbs, however, it is

* Increasing the size of the outside liner and the outer shell still further would have helped even more. In such cases, however, one could have "made do" with less Pb gamma shielding, and the whole cask would have started to resemble an Fe/water cask more and more. Since our intent was to optimize the basic Pb/water cask, we elected not to increase the thickness of these components any further.

very close to (or slightly over) the upper design limit for a large rail cask. During the thermal transient following a 30-minute fire, the insert temperature can also rise as high as $\sim 1125^{\circ}\text{F}$ which, while still safe, is precariously close to the melting point of many aluminum alloys.^{*a} Even if we could increase the thickness of the outside liner or outer shell (which we cannot do here because of the weight limitation), it would be of little avail since the temperature rise in the insert is caused primarily by the self-absorption of the decay heat which cannot escape to the environment during the fire or for a long time thereafter. Should the above design not prove acceptable, the only viable option would then be to increase the thickness of the insert which, because of the weight limitation, would force us to reduce the number of assemblies in the cask. Use of a 3" aluminum insert would reduce the maximum temperature in the insert during the transient to 968°F for a 197,600 lb. cask containing thirteen 1-year-old PWR fuel assemblies. The overall weight, characteristic dimensions, and temperature distribution for this and similar casks are shown in Table VII.3 of Section VII.B.

As noted in Section IV.B, the relative amount of neutron and gamma shielding required to reduce the total dose down to a specified level is quite sensitive to the n/γ split [or "initial design point", D'_γ].^{*b} In

^{*a} Al-214 (ASTM/G4A) containing 0.1% C, 3.5-4.5% Mg, < 0.4% Fe, < 0.3% Si, < 0.3% Mn, < 0.2% Ti, and < 0.1% Zn, for example, melts between 1076°F and 1184°F depending upon the amount of magnesium in the alloy.⁴⁷

^{*b} For an explanation of the "initial design point" and how it was used to determine the actual shielding requirements, see Section IV.B.1.

the copper-finned U-metal cask described above, the shielding thicknesses ($t_y \approx 3.57''$, $t_n \approx 3.72''$) were based on an initial design point of 23 mr/hr. Based on some very preliminary calculations involving a copper-finned U-metal cask having a 1" aluminum insert, we had earlier indicated that an initial design point of 37 mr/hr might yield a slightly lighter cask (cf. Table IV.18). Accordingly, an alternate table of shielding data was prepared based on that design point (cf. Appendix F). As part of the reevaluation effort prior to the final analyses, we took those alternate shield dimensions for the copper-finned U-metal cask and performed still another series of SCOPE calculations using the 2.25" aluminum insert and the 3.0" aluminum insert. Except for the composition of the shield, the results were not much different from those described above. As before, the 2.25" insert yielded a 15-assembly design that was very close to the weight limit and in which the aluminum insert was fairly close to the melting point. With the 3.0" insert, the maximum insert temperature during the transient was reduced to 967°F for a 201,300 lb. cask containing thirteen 1-year-old PWR fuel assemblies. In this particular case, the U-metal gamma shield would have been 3.36" thick while the neutron shield would have been 6.84" thick. Insofar as this alternate set of shielding data offered no particular advantage when used in conjunction with the 2.25" or 3.0" aluminum insert, it was not used at all in the final set of analyses described in Section VII.B.

Prior to the final analyses, we also conducted a somewhat more limited reevaluation of our earlier cask designs for 2-year-old spent fuel. Based on the experience with the 1-year-old fuel and the substantially lower decay heat load generated by the 2-year-old fuel (cf. Table IV.2), there was no

reason to even consider the use of a copper insert. Using the overall design philosophy described in Section II, the inherently subcritical fuel assembly separator described in Section III, and the optimized neutron and gamma shielding requirements described in Section IV.C and Appendix F, we then performed a series of scoping calculations for Pb, Fe, and U-metal casks carrying 8-18 2-year-old spent fuel assemblies arranged as shown in Appendix B. In each case of practical interest ($W < 210,000$ lbs), the (stainless-steel) fins were found to be so small that they had no impact on the choice of the optimal n/γ split. Since they were typically $\leq 3"$ long $\times$ $0.25"$ thick, no significant weight reduction could be realized by switching to copper. Because of that, 2-year-old spent fuel casks with copper fins were not considered in the final set of analyses.

One particular case of interest in these scoping calculations involved a Pb cask carrying thirteen 2-year-old FWR fuel assemblies. When loaded, this particular cask weighed $\sim 193,300$ lbs. Unfortunately, the transient thermal analysis indicated that there would be some melting of the Pb gamma shield during the 30-minute fire. [To avoid that problem, the "next best" case would have been a 178,700 lb. cask carrying eleven assemblies]. From a spatial edit of the temperature distribution as a function of time (during and after the 30-minute fire), it became obvious that the Pb gamma shield would first start melting along its outside surface due to the higher temperatures there. We decided, therefore, to increase the size (and net heat capacity) of the outer shell in order to absorb more of the external heat load (in the outer shell) and reduce the temperature on the outside surface of the Pb gamma shield. By using a 2.5" outer shell (instead of a 2.0" outer shell), we found that the maximum transient temperature of the

Pb gamma shield for the 13-assembly cask could be reduced below the melting point ($T_{\gamma}^{\max} = 616^{\circ}\text{F}$). With this modification, the 13-assembly cask now weighed ~201,000 lbs. A similar 12-assembly cask would weigh ~187,300 lbs. and have a maximum transient gamma shield temperature of ~609°F. In Table VII.3 of Section VII.B, we show the overall weight, characteristic dimensions, and temperature distributions for 2-year-old Pb casks with and without this design modification.

Shipping cask designs for 3, 5, 7 and 10-year-old spent fuel were also developed using the SCOPE code and the optimized neutron and gamma shielding requirements described in Section IV.C and Appendix F. All of these designs were found to be weight-limited, and none required any special modification. It was further noted that, with the exception of the U-metal casks for 3-year-old spent fuel, none of the casks studied required external cooling fins. The carrying capacity, overall weight, characteristic dimensions, and temperature distribution for each of these casks are shown in Table VII.3 of Section VII.B.

VII.B FINAL RESULTS AND CONCLUSIONS

The purpose of this conceptual design study was to develop a new generation of lead, iron, and uranium-metal spent fuel casks which have been optimized for the shipment of 1,2,3,5,7 and 10-year-old PWR spent fuel assemblies. The key physical features of those casks considered in this study are illustrated in Fig. VII.1. In each case we have an inner shell, a gamma shield, an outer shell, a neutron shield, and an outside liner. Depending on the amount of decay heat that must be dissipated, the cask(s) may or may not have fins as illustrated in Fig. VII.2.^{*a} The inner and outer shells and the outside liner were all assumed to be stainless-steel. The thicknesses of these components varied as noted in Table VII.2, depending on the type of gamma shielding used.^{*b} The neutron shield in each case consisted of 28.5% water, 66.0% ethylene glycol, and 5.5% potassium tetraborate. By weight, this common mixture of water and antifreeze contains ~1% (natural) boron. In each case, the length of the cavity inside the cask (171.6") was chosen so as to accommodate a 13.8 ft. PWR fuel assembly while leaving an additional 6-inch space for an internal axial shock absorber. Inside the cask, the fuel assemblies are assumed to be separated by means of a 2.25" thick aluminum insert, as illustrated in Figs. VII.3 and VII.4. Each of the 10 positions shown in Fig. VII.3 measures 8.7" x 8.7"

^{*a} In general, fins were not used on a cask if it could dissipate the decay heat to the environment (at 130°F) while maintaining an outside surface temperature $\leq 250^\circ\text{F}$. When required, stainless-steel fins were used (except as noted in Table VII.3).

^{*b} In each case, the amount of neutron and gamma shielding used in conjunction with these components was chosen so as to reduce the total dose rate down to 10 mrem/hr at a point 10 feet from the centerline. See Sect. IV.C for additional details.

Fig. VII.1: Overview of a Typical Spent Fuel Shipping Cask

ORNL-DWG 82-12169

- a) The removable aluminum insert containing the spent fuel is more fully illustrated in Fig. VII.3 and/or Appendix B.
- b) The thicknesses of the various components in the axial direction were assumed to be the same as in the radial direction.
- c) Actual dimensions of the various components depend on the type of cask, as shown in Table VII.2 and/or Appendix F.

Fig. VII.2: Overview of a Spent Fuel Shipping Cask with External Cooling Fins

ORNL-DWG 82-12163

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- a) The removable aluminum insert containing the spent fuel is more fully illustrated in Fig. VII.3 and/or Appendix B.
- b) The thicknesses of the various components in the axial direction were assumed to be the same as in the radial direction.
- c) Actual dimensions of the various components depend on the type of cask, as shown in Table VII.2 and/or Appendix F.
- d) Sketch not drawn to scale; many casks had 45-50 fins; actual center-to-center spacing was assumed to be 4.0 inches.

Table VII.2
Thickness of Various Structural
Components in Each Type of Cask

TYPE OF CASK	THICKNESS IN INCHES		
	INNER SHELL	OUTER SHELL	OUTSIDE LINER
LEAD	1.5	2.0	.75
U-METAL	.75	2.0	.75
IRON	.375	.375	.75

**Fig. VII.3: Inherently Subcritical, Removable Aluminum Insert
Designed to Hold 10 PWR Fuel Assemblies**

ORNL-DWG 82-12179

Optimal packing arrangements
for additional assemblies are
illustrated in Appendix B

Hollow, borated stainless-steel
tubes embedded within the walls
of the insert in order to insure
subcriticality (cf. Fig. VII.4)

ORNL-DWG 82-12191

Fig. VII.4: Details Regarding the Use and Placement of Hollow, Borated Stainless-Steel Tubes Within the Wall-Forming Structure of the Insert Used to Separate Fuel Assemblies and Insure Subcriticality

and can accommodate most typical PWR fuel assemblies with 0.125" clearance on all four sides. As noted in Section III, hollow borated stainless-steel tubes were embedded within the walls of the insert so as to insure subcriticality, even in the most reactive configuration. Other "optimal" packing arrangements capable of holding many more fuel assemblies were also considered (cf. Appendix B).

Using the SCOPE program in conjunction with the optimized shielding data described in Section IV.C and Appendix F, we were able to evaluate a wide range of conceptual cask designs at minimal cost. SCOPE output for those cases of potential interest is presented in Table VII.3. Each line of output gives the age of the spent fuel (in years); the number of assemblies in the cask; the materials used for the insert, gamma shield, and neutron shield; the thickness of the gamma shield and the neutron shield; the overall weight of the loaded cask; and some characteristic dimensions [including the inside diameter of the cask, the outside diameter of the cask (including fins, if required), the overall exterior length of the cask, and the length and thickness of the fins (if required)]. It also gives the steady-state temperature on the outside surface of the cask, on the innermost surface of the gamma shield, at the hottest point inside the insert, and on the surface of the hottest fuel pin. Lastly, it estimates the maximum transient temperature in the gamma shield, in the insert, and on the surface of the hottest fuel pin during or after the postulated 30-minute fire.

Since many design limits such as the overall cask weight (and some of the thermal constraints) are not absolutely rigid, this rather extensive table shows a number of cases on either side of the nominal design limits.

Table VII.3
SCOPE Results for a Number of Cases of Potential Interest

FUEL AGE	NO. ASSY	MATERIALS			THICKNESS IN INCHES		CASE WEIGHT (KLBS)	DIMENSIONS IN INCHES					STEADY STATE TEMPERATURES IN DEG. F				MAX FIRE-TEST TEMPERATURES IN DEG. F		
		INSRT	CAN. SHLD	WRT SHLD	CAN. SHLD	WRT SHLD		ID	OD	LENGTH	FIN WT.	FIN THK.	FUEL PIN	INSRT MATL	CANNA SHLD	CASE SURF.	FUEL PIN	INSRT MATL	CANNA SHLD
10.0	15	AL	PB	H2O	3.84	4.03	171.3	55.8	80.0	195.8	0.3	0.0	351	249	187	180	480	402	391
10.0	16	AL	PB	H2O	3.86	4.03	195.1*	61.9	86.2	195.9	0.0	0.0	357	275	186	179	479	401	388
10.0	18	AL	PB	H2O	3.90	4.02	196.6*	61.9	86.3	195.9	0.0	0.0	369	288	192	184	459	422	401
10.0	19	AL	PB	H2O	3.92	4.06	205.0*	63.8	88.3	196.1	0.0	0.0	371	290	194	186	502	425	404
10.0	21	AL	PB	H2O	3.97	4.13	206.9*	63.8	88.5	196.3	0.0	0.0	381	301	199	190	520	445	415
10.0	22	AL	PB	H2O	3.98	4.15	218.7*	66.6	91.4	196.4	0.0	0.0	385	305	200	191	520	445	416
10.0	13	AL	FE	H2O	9.13	3.68	173.9	52.0	80.7	200.2	0.0	0.0	339	256	179	173	446	367	347
10.0	14	AL	FE	H2O	9.16	3.68	189.1	55.8	84.5	200.3	0.0	0.0	341	258	180	174	446	367	348
10.0	15	AL	FE	H2O	9.19	3.68	189.8	55.8	84.5	200.3	0.0	0.0	347	265	183	177	457	378	355
10.0	16	AL	FE	H2O	9.22	3.68	215.1*	61.9	90.7	200.4	0.0	0.0	353	277	183	176	458	380	353
10.0	18	AL	FE	H2O	9.27	3.68	216.7*	61.9	90.8	200.5	0.0	0.0	365	284	189	181	476	399	366
10.0	18	AL	U	H2O	2.41	3.33	179.4	61.9	80.4	190.1	0.0	0.0	372	291	196	189	508	431	423
10.0	19	AL	U	H2O	2.42	3.36	187.3	63.8	82.4	190.2	0.0	0.0	374	293	198	190	511	435	426
10.0	21	AL	U	H2O	2.45	3.42	189.0	63.8	82.6	190.3	0.0	0.0	385	304	204	195	530	455	438
10.0	22	AL	U	H2O	2.46	3.43	200.2*	66.6	85.4	190.4	0.0	0.0	388	308	204	196	530	455	439
10.0	23	AL	U	H2O	2.47	3.44	211.3*	69.2	88.0	190.4	0.0	0.0	387	307	205	196	527	452	440
10.0	24	AL	U	H2O	2.48	3.45	212.0*	69.2	88.1	190.5	0.0	0.0	392	312	208	199	535	461	445
10.0	25	AL	U	H2O	2.49	3.46	213.7*	70.1	89.0	190.5	0.0	0.0	405	326	210	200	550	477	449
10.0	26	AL	U	H2O	2.49	3.47	215.7*	70.1	89.0	190.5	0.0	0.0	410	331	212	203	559	486	454
7.0	14	AL	PB	H2O	4.12	3.94	176.7	55.8	80.4	196.2	0.0	0.0	381	287	193	185	512	422	403
7.0	15	AL	PB	H2O	4.15	3.95	177.4	55.8	80.5	196.3	0.0	0.0	388	294	196	188	524	435	411
7.0	16	AL	PB	H2O	4.16	3.97	201.8*	61.9	86.7	196.4	0.0	0.0	396	302	196	187	524	436	418
7.0	18	AL	PB	H2O	4.20	4.01	203.3*	61.9	86.8	196.5	0.0	0.0	409	316	202	193	545	459	422
7.0	19	AL	PB	H2O	4.22	4.01	211.9*	63.8	88.8	196.6	0.0	0.0	411	319	204	195	549	462	425
7.0	21	AL	PB	H2O	4.27	4.03	213.7*	63.8	88.9	196.7	0.0	0.0	423	332	210	200	569	483	438
7.0	22	AL	PB	H2O	4.29	4.06	225.8*	66.6	91.8	196.8	0.0	0.0	427	336	211	201	570	485	439
7.0	12	AL	FE	H2O	9.56	3.59	168.3	48.9	78.3	200.9	0.0	0.0	366	270	186	178	479	388	361
7.0	13	AL	FE	H2O	9.59	3.59	180.8	52.0	81.4	201.0	0.0	0.0	374	279	188	180	486	396	365
7.0	14	AL	FE	H2O	9.62	3.59	196.4*	55.8	85.2	201.0	0.0	0.0	377	282	189	181	488	398	367
7.0	15	AL	FE	H2O	9.65	3.59	197.2*	55.8	85.3	201.1	0.0	0.0	384	290	192	184	499	410	375
7.0	16	AL	FE	H2O	9.68	3.59	223.1*	61.9	91.4	201.1	0.0	0.0	391	298	192	184	502	414	373
7.0	15	AL	U	H2O	2.53	3.37	160.3	55.8	74.6	190.4	0.0	0.0	392	298	202	193	534	446	435
7.0	16	AL	U	H2O	2.54	3.38	183.4	61.9	80.8	190.4	0.0	0.0	399	306	205	192	533	446	431
7.0	18	AL	U	H2O	2.56	3.40	184.7	61.9	80.8	190.5	0.0	0.0	412	320	207	198	555	469	445
7.0	19	AL	U	H2O	2.58	3.43	192.8*	63.8	82.8	190.6	0.0	0.0	414	322	209	200	558	473	448
7.0	21	AL	U	H2O	2.61	3.49	194.5*	63.8	83.0	190.8	0.0	0.0	427	336	216	206	579	495	462
7.0	22	AL	U	H2O	2.61	3.50	206.0*	66.6	85.8	190.8	0.0	0.0	431	340	217	206	580	495	462
7.0	23	AL	U	H2O	2.62	3.51	217.3*	69.2	88.5	190.9	0.0	0.0	430	338	218	207	577	493	464
5.0	13	AL	PB	H2O	4.39	4.07	167.8	52.0	77.4	197.0	0.0	0.0	435	325	207	197	583	476	434
5.0	14	AL	PB	H2O	4.41	4.07	182.9	55.8	81.3	197.1	0.0	0.0	443	329	209	198	583	477	436
5.0	15	AL	PB	H2O	4.43	4.08	183.7	55.8	81.3	197.1	0.0	0.0	452	338	213	202	597	491	445
5.0	16	AL	PB	H2O	4.45	4.07	208.7*	61.9	87.5	197.1	0.0	0.0	462	349	212	201	600	496	442
5.0	18	AL	PB	H2O	4.49	4.07	210.1*	61.9	87.5	197.2	0.0	0.0	478	366	220	208	624	521	458
5.0	19	AL	PB	H2O	4.51	4.12	219.0*	63.8	89.6	197.4	0.0	0.0	480	367	223	210	628	525	462
5.0	12	AL	FE	H2O	10.02	3.57	175.1	48.9	79.1	201.9	0.0	0.0	424	303	200	190	546	437	392
5.0	13	AL	FE	H2O	10.05	3.57	187.9	52.0	82.3	201.8	0.0	0.0	434	311	202	192	555	448	397
5.0	14	AL	FE	H2O	10.08	3.56	203.7*	55.8	86.1	201.9	0.0	0.0	438	323	204	194	558	451	399
5.0	15	AL	FE	H2O	10.11	3.56	204.7*	55.8	86.2	201.9	0.0	0.0	447	333	208	197	571	464	408
5.0	16	AL	FE	H2O	10.14	3.56	231.3*	61.9	92.3	202.0	0.0	0.0	457	344	208	197	577	472	406
5.0	15	AL	U	H2O	2.71	3.33	165.5	55.8	74.9	190.7	0.0	0.0	456	343	220	209	608	503	469
5.0	16	AL	U	H2O	2.72	3.33	189.2	61.9	81.0	190.7	0.0	0.0	466	354	218	208	611	507	465
5.0	18	AL	U	H2O	2.74	3.35	190.6*	61.9	81.1	190.8	0.0	0.0	482	371	227	215	635	533	482
5.0	19	AL	U	H2O	2.76	3.38	198.9*	63.8	83.1	190.9	0.0	0.0	484	374	229	217	639	537	484
5.0	21	AL	U	H2O	2.79	3.45	200.6*	63.8	83.3	191.1	0.0	0.0	499	390	237	224	662	562	501
5.0	22	AL	U	H2O	2.79	3.44	212.3*	66.6	86.0	191.1	0.0	0.0	504	395	238	225	663	567	502
5.0	23	AL	U	H2O	2.80	3.44	223.8*	69.2	88.7	191.1	0.0	0.0	502	393	240	226	660	571	504

- a) Pb casks have a 1.5" inner shell, a 2.0" outer shell, and a 0.75" outside liner
b) Fe casks have a 0.375" inner shell, a 0.375" outer shell, and a 0.75" outside liner
c) U-metal casks have a 0.75" inner shell, a 2.0" outer shell, and a 0.75" outside liner

Table VII.3 (cont)
SCOPE Results for a Number of Cases of Potential Interest

FUEL AGE	NO. ASSY	MATERIALS			THICKNESS IN INCHES		CASK WEIGHT (KILBS)	DIMENSIONS IN INCHES					STEADY STATE TEMPERATURES IN DEG. F				MAX F. RE-TEST TEMPERATURES IN DEG. F		
		INSRT	CAN. SHLD	HEUT SHLD	CAN. SHLD	HEUT SHLD		ID	OD	LENGTH	FIN HT.	FIN THK.	FUEL PIN	INSRT MATH	CANNA SHLD	CASK SCRF.	FUEL PIN	INSRT MATH	CANNA SHLD
3.0	12	AL	PH	H2O	4.92	4.19	166.5	48.9	75.7	198.3	0.0	0.0	597	428	249	231	761	607	517
3.0	13	AL	PH	H2O	4.94	4.20	179.2	52.0	78.8	198.4	0.0	0.0	614	447	252	234	775	624	522
3.0	14	AL	PH	H2O	4.97	4.20	195.0*	55.8	82.6	198.4	0.0	0.0	619	454	255	237	778	628	525
3.0	15	AL	PH	LO	4.99	4.21	195.8*	55.8	82.7	198.5	0.0	0.0	632	468	262	242	795	647	538
3.0	16	AL	PH	H2O	5.01	4.22	222.1*	61.9	88.9	198.6	0.0	0.0	651	489	261	242	808	663	534
3.0	10	AL	FE	H2O	10.68	3.77	175.1	46.4	78.3	203.5	0.0	0.0	571	400	228	214	704	545	453
3.0	11	AL	FE	H2O	10.75	3.78	184.5	48.9	81.0	203.7	0.0	0.0	575	403	233	218	710	552	462
3.0	12	AL	FE	H2O	10.82	3.79	181.9	48.9	81.2	203.8	0.0	0.0	589	420	241	224	728	573	477
3.0	13	AL	FE	H2O	10.85	3.79	201.3*	52.0	84.3	203.9	0.0	0.0	606	438	244	227	745	592	483
3.0	14	AL	FE	H2O	10.88	3.79	218.2*	55.8	88.1	203.9	0.0	0.0	612	445	247	229	750	598	486
3.0	14	AL	U	H2O	3.01	3.54	174.9	55.8	75.9	191.7	0.0	0.0	625	460	264	247	791	643	551
3.0	15	AL	U	H2O	3.02	3.57	179.7	55.8	80.0	191.8	2.0	0.25	614	448	236	219	808	660	586
3.0	16	AL	U	H2O	3.03	3.58	204.8*	61.9	86.1	191.8	2.0	0.25	632	469	236	218	818	673	581
3.0	18	AL	U	H2O	3.06	3.58	206.2*	61.9	86.2	191.9	2.0	0.25	654	493	247	226	848	706	602
3.0	19	AL	U	H2O	3.07	3.59	215.3*	63.8	87.2	191.9	2.0	0.25	656	495	250	229	852	711	607
3.0	21	AL	U	H2O	3.11	3.61	217.0*	63.8	88.3	192.0	2.0	0.25	675	517	259	236	879	741	625
2.0	9	AL	PH	H2O	5.23	4.10	161.7	46.4	73.6	198.7	0.0	0.0	737	510	271	249	898	693	557
2.0	10	AL	PH	H2O	5.28	4.13	167.5	46.4	77.7	193.9	2.0	0.25	736	508	247	223	925	722	598
2.0	11	AL	PH	H2O	5.32	4.19	178.7	48.9	80.5	199.1	2.0	0.25	739	511	253	227	950	728	608
2.0	12	AL	PH	H2O	5.37	4.25	180.0	48.9	80.7	199.3	2.0	0.25	757	532	261	234	953	756	626*
2.0	13	AL	PH	H2O	5.39	4.25	193.3*	52.0	83.8	199.4	2.0	0.25	780	560	265	237	974	782	632*
2.0	14	AL	PH	H2O	5.41	4.24	210.0*	55.8	87.6	199.4	2.0	0.25	788	570	268	240	979	788	635*
2.0	15	AL	PH	H2O	5.44	4.24	210.7*	55.8	87.7	199.4	2.0	0.25	803	588	276	245	999	812	651*
2.0	16	AL	PH	H2O	5.46	4.24	238.3*	61.9	93.8	195.5	2.0	0.25	833	624	275	245	1023	841	646*
2.0	8	AL	FE	H2O	11.18	3.86	169.9	43.3	76.4	204.7	0.0	0.0	704	470	253	234	840	626	502
2.0	9	AL	FE	H2O	11.26	3.87	183.8	46.4	79.7	204.9	0.0	0.0	728	499	260	240	867	658	514
2.0	10	AL	FE	H2O	11.33	3.89	185.3	46.4	79.9	205.0	0.0	0.0	749	523	271	248	892	688	534
2.0	11	AL	FE	H2O	11.40	3.91	202.1*	48.7	86.6	205.2	2.0	0.25	731	502	244	219	893	688	559
2.0	12	AL	FE	H2O	11.46	3.92	207.5*	48.9	86.7	205.4	2.0	0.25	748	523	252	225	915	714	576
2.0	13	AL	FE	H2O	11.50	3.92	217.6*	52.0	89.9	205.4	2.0	0.25	772	550	256	229	939	743	583
2.0	13	AL	U	H2O	3.25	3.66	171.5	52.0	76.4	192.4	2.0	0.25	784	565	275	247	988	798	659
2.0	14	AL	U	H2O	3.26	3.67	187.2	55.8	80.7	192.5	2.0	0.25	792	574	277	250	992	803	662
2.0	15	AL	U	H2O	3.27	3.67	188.8	55.8	82.7	192.5	3.0	0.25	802	587	277	248	1014	828	685
2.0	16	AL	U	H2O	3.29	3.67	214.9*	61.9	88.8	192.5	3.0	0.25	831	622	276	247	1036	856	679
2.0	17	AL	U	H2O	3.30	3.7	216.6*	61.9	90.9	192.5	4.0	0.25	842	635	279	248	1056	879	700
2.0	18	AL	U	H2O	3.31	3.67	220.2*	61.9	92.9	192.6	5.0	0.31	852	646	280	248	1076	901	718
2.0	9	AL	PH	H2O	5.23	4.10	168.5	46.4	74.6	199.7	0.0	0.0	737	509	270	246	895	689	545
2.0	10	AL	PH	H2O	5.28	4.13	174.4	46.4	78.7	199.9	2.0	0.25	736	508	247	222	922	718	582
2.0	11	AL	PH	H2O	5.32	4.19	186.0	48.9	81.5	200.1	2.0	0.25	738	511	252	225	926	723	582
2.0	12	AL	PH	H2O	5.37	4.25	187.3	48.9	81.7	200.3	2.0	0.25	756	532	261	232	943	751	609
2.0	13	AL	PH	H2O	5.39	4.25	201.0*	52.0	84.8	200.4	2.0	0.25	780	560	265	235	971	778	616
2.0	14	AL	PH	H2O	5.41	4.24	218.1*	55.8	88.6	200.4	2.0	0.25	788	570	268	238	976	785	619*
2.0	15	AL	PH	H2O	5.44	4.24	218.9*	55.8	88.7	200.4	2.0	0.25	803	588	276	243	995	808	634*

- a) "b" casks have a 1.5" inner shell, a 2.0" outer shell, and a 0.75" outside liner
b) Fe casks have a 0.375" inner shell, a 0.375" outer shell, and a 0.75" outside liner
c) U-metal casks have a 0.75" inner shell, a 2.0" outer shell, and a 0.75" outside liner
d) Fins, when required, were assumed to be stainless-steel unless noted otherwise
e) For added thermal protection, a 2.5" outer shell was used in lieu of the 2.0" shell

Table VII.3 (cont)
SCOPE Results for a Number of Cases of Potential Interest

FUEL AGE	NO. ASST	MATERIALS			THICKNESS IN INCHES		CASK WEIGHT (KLBS)	DIMENSIONS IN INCHES					STEADY STATE TEMPERATURES IN DEG. F				MAX FIRE-TEST TEMPERATURES IN DEG. F		
		INSRT	CAN.	NEUT	CAN.	NEUT		ID	OD	LENGTH	FIN WT.	FIN THK.	FUEL PIN	INSRT MTRL	CANNA SHLD	CASK SURF.	FUEL PIN	INSRT MTRL	CANNA SHLD
1.0	3	AL	PB	H2O	4.79	8.16	99.0	28.2	62.6	206.0	0.0	0.0	834	456	242	223	995	648	541
1.0	4	AL	PB	H2O	4.89	8.14	109.4	31.0	65.5	206.2	0.0	0.0	882	519	266	242	1050	720	581
1.0	5	AL	PB	H2O	5.00	8.11	130.0	34.6	73.3	206.3	2.0	0.25	872	505	249	221	1063	735	635*
1.0	6	AL	PB	H2O	5.10	8.09	149.5	37.5	78.3	206.5	2.0	0.25	926	535	260	230	1114	804	651*
1.0	7	AL	PB	H2O	5.20	8.06	151.4	39.5	78.5	206.6	2.0	0.25	958	619	277	242	1155	857	684*
1.0	8	AL	PB	H2O	5.31	8.04	168.4	43.3	82.5	206.8	2.0	0.25	991	662	287	249	1185	899	697*
1.0	9	AL	PB	H2O	5.36	8.03	183.1	46.4	87.7	206.9	3.0	0.25	1025	706	288	248	1226	951	720*
1.0	10	AL	PB	H2O	5.41	8.01	189.2	46.4	91.8	207.0	5.0	0.31	1046	733	293	248	1263	997	757*
1.0	11	AL	PB	H2O	5.46	8.04	205.6*	48.9	96.4	207.1	6.0	0.38	1044	731	296	250	1268	1003	774*
1.0	12	AL	PB	H2O	5.50	8.06	217.7*	48.9	100.6	207.2	8.0	0.50	1062	755	300	250	1305	1047	805*
1.0	7	AL	FE	H2O	11.61	5.38	170.1	39.5	80.4	208.6	2.0	0.25	951	612	273	239	1115	813	628
1.0	8	AL	FE	H2O	11.76	5.36	182.5	43.3	84.6	208.9	2.0	0.25	986	654	282	246	1148	857	643
1.0	9	AL	FE	H2O	11.84	5.40	204.5*	46.4	89.9	209.1	3.0	0.25	1019	698	281	245	1188	909	664
1.0	10	AL	FE	H2O	11.92	5.43	207.2*	46.4	92.1	209.3	4.0	0.25	1042	729	293	250	1220	949	693
1.0	11	AL	FE	H2O	11.99	5.42	224.8*	48.9	98.8	209.4	6.0	0.31	1039	725	295	249	1225	955	711
1.0	9	AL	U	H2O	3.19	8.39	155.1	46.4	86.6	201.8	5.0	0.25	1023	704	289	251*	1246	973	766
1.0	10	AL	U	H2O	3.22	8.38	175.6	46.4	88.5	201.8	6.0	0.44	1043	729	291	249	1261	1017	795
1.0	11	AL	U	H2O	3.24	8.41	192.0*	48.9	93.3	201.9	7.0	0.50	1041	727	295	250	1296	1023	813
1.0	12	AL	U	H2O	3.27	8.44	208.8*	48.9	97.4	202.0	9.0	0.69	1058	750	298	249	1327	1070*	849
1.0	13	AL	U	H2O	3.28	8.31	229.8*	52.0	102.2	201.8	10.0	0.75	1095	798	300	250	1368	1121*	864
1.0	3	AL	PB	H2O	5.32	3.94	97.6	28.2	55.2	198.6	0.0	0.0	842	466	255	235	996	650	539
1.0	4	AL	PB	H2O	5.48	4.02	113.1	31.0	62.5	199.1	2.0	0.25	868	500	239	213	1050	718	599
1.0	5	AL	PB	H2O	5.54	4.05	127.0	34.6	66.3	199.3	2.0	0.25	874	508	253	225	1058	730	627*
1.0	6	AL	PB	H2O	5.60	4.08	146.0	39.5	71.3	199.5	2.0	0.25	928	578	264	233	1110	801	644*
1.0	7	AL	PB	H2O	5.67	4.11	147.4	39.5	71.5	199.7	2.0	0.25	960	621	281	245	1151	854	679*
1.0	8	AL	PB	H2O	5.73	4.14	164.7	43.3	77.6	199.8	3.0	0.25	986	655	276	238	1181	894	697*
1.0	9	AL	PB	H2O	5.78	4.21	178.8	46.4	80.9	200.1	3.0	0.25	1022	703	283	243	1219	945	711*
1.0	10	AL	PB	H2O	5.83	4.29	181.4	46.4	83.2	200.3	4.0	0.25	1042	729	285	240	1250	984	740*
1.0	11	AL	PB	H2O	5.88	4.34	193.2*	48.9	85.9	200.5	4.0	0.25	1042	729	292	245	1251	986	754*
1.0	12	AL	PB	H2O	5.97	4.38	195.7*	48.9	88.1	200.7	5.0	0.25	1060	752	295	243	1279	1021	779*
1.0	13	AL	PB	H2O	5.94	4.40	209.8*	52.0	91.2	200.8	5.0	0.25	1097	802	300	247	1315	1069*	787*
1.0	14	AL	PB	H2O	5.97	4.41	227.4*	55.8	95.1	200.8	5.0	0.25	1110	818	304	250	1326	1084*	792*
1.0	7	AL	FE	H2O	11.92	3.90	170.2	39.5	78.1	206.2	2.0	0.25	951	609	269	235	1111	808	624
1.0	8	AL	FE	H2O	12.01	3.91	187.8	43.3	82.2	206.4	2.0	0.25	984	652	278	241	1145	853	640
1.0	9	AL	FE	H2O	12.09	3.93	202.6*	46.4	85.5	206.6	2.0	0.25	1021	701	287	248	1184	906	656
1.0	10	AL	FE	H2O	12.17	3.94	205.4*	46.4	87.7	206.8	3.0	0.25	1039	725	286	243	1212	941	684
1.0	11	AL	FE	H2O	12.24	3.96	218.0*	48.9	90.3	207.0	3.0	0.25	1039	725	294	248	1214	944	698
1.0	11	AL	U	H2O	3.53	3.67	170.5	48.9	80.4	193.0	5.0	0.25	1040	726	293	248	1264	1001	783
1.0	12	AL	U	H2O	3.56	3.72	172.7	48.9	82.5	193.1	6.0	0.25	1058	749	296	246	1292	1036	808
1.0	13	AL	U	H2O	3.57	3.72	185.9	52.0	85.6	193.2	6.0	0.25	1095	799	301	250	1328	1084*	815
1.0	14	AL	U	H2O	3.58	3.73	203.6*	55.8	91.4	193.7	7.0	0.25	1104	811	296	245	1337	1096*	821
1.0	15	AL	U	H2O	3.60	3.73	205.5*	55.8	95.5	193.3	8.0	0.25	1120	831	299	244	1360	1125*	841
1.0	16	AL	U	H2O	3.61	3.74	232.0*	61.9	97.6	193.3	7.0	0.25	1177	906	305	251*	1412	1193*	832
1.0	4	AL	PB	H2O	5.48	4.02	124.6	31.0	64.5	201.1	2.0	0.25	867	499	238	210	1046	713	569
1.0	5	AL	PB	H2O	5.54	4.05	139.4	34.6	68.3	201.3	2.0	0.25	874	507	252	221	1054	725	597
1.0	6	AL	PB	H2O	5.60	4.08	159.6	39.5	73.3	201.5	2.0	0.25	927	577	263	229	1107	797	615
1.0	7	AL	PB	H2O	5.67	4.11	161.1	39.5	73.5	201.7	2.0	0.25	960	621	280	241	1147	849	649*
1.0	8	AL	PB	H2O	5.73	4.14	176.4	43.3	77.6	201.8	2.0	0.25	992	663	290	248	1179	893	664*
1.0	9	AL	PB	H2O	5.78	4.21	194.2*	46.4	82.9	202.1	3.0	0.25	1023	704	284	240	1217	942	683*
1.0	10	AL	PB	H2O	5.83	4.29	195.8*	46.4	83.2	202.3	3.0	0.25	1048	737	298	249	1248	982	709*
1.0	11	AL	PB	H2O	5.88	4.34	209.3*	48.9	87.9	202.5	4.0	0.25	1043	730	295	243	1249	984	727*
1.0	12	AL	PB	H2O	5.92	4.38	211.9*	48.9	90.1	202.7	5.0	0.25	1061	753	297	241	1277	1019	752*
1.0	13	AL	PB	H2O	5.94	4.40	226.8*	52.0	93.2	202.8	5.0	0.25	1098	803	303	245	1314	1067*	761*
1.0	10	AL	U	H2O	3.50	3.63	168.1	48.9	78.1	192.9	4.0	0.25	974	639	291	250	1187	902	757
1.0	11	AL	U	H2O	3.53	3.67	181.0	51.6	83.0	193.0	5.0	0.25	975	641	288	245	1194	911	772
1.0	12	AL	U	H2O	3.56	3.72	183.2	51.6	85.1	193.1	6.0	0.25	993	664	291	243	1220	944	796
1.0	13	AL	U	H2O	3.57	3.72	197.6*	54.8	88.4	193.2	6.0	0.25	1012	689	296	247	1237	968	803
1.0	14	AL	U	H2O	3.58	3.73	215.7*	58.9	92.5	193.2	6.0	0.25	1018	697	299	249	1240	973	806
1.0	15	AL	U	H2O	3.60	3.73	217.7*	58.9	94.5	193.3	7.0	0.25	1032	716	301	248	1262	1000	826

- a) Pb casks have a 1.5" inner shell, a 2.0" outer shell, and a 0.75" outside liner
b) Fe casks have a 0.375" inner shell, a 0.375" outer shell, and a 0.75" outside liner
c) U-metal casks have a 0.75" inner shell, a 2.0" outer shell, and a 0.75" outside liner
d) Pins, when required, were assumed to be stainless-steel unless noted otherwise
e) To reduce the weight of the cask, copper fins were used in lieu of stainless-steel fins
f) For added thermal protection, a 1.25" outside liner was used in lieu of the 0.75" liner
g) For added thermal protection, a 2.5" outer shell was used in lieu of the 2.0" shell
h) For added thermal protection, a 3.0" thick insert was used in lieu of the 2.25" insert

In particular, we note that the inside diameter of a cask (and hence its overall weight) is a nonuniform function of the number of assemblies in the cask (cf. Appendix B). Hence, while there may be very little difference in the overall weight of casks designed to carry 16, 17, or 18 assemblies, a cask designed to carry 14 or 15 assemblies would weigh considerably less, while one designed to carry 19, 20, or 21 assemblies would weigh somewhat more. Because of the "magic numbers" associated with the various packing arrangements, the rigid enforcement of one weight limit as opposed to another may have very little impact on the carrying capacity of some casks while at the same time having a severe impact on the carrying capacity of others. With the aid of Table VII.3, each cask can be evaluated on a case-by-case basis.

In addition to the designs fitting the generic description given above, we had previously identified several variations which we felt might be of potential interest (cf. Section VII.A). In the case of the 2-year-old Pb cask, for example, increasing the thickness of the outer shell to 2.5" afforded the gamma shield some additional thermal protection during the 30-minute fire. In the case of the Pb, Fe, and U-metal casks designed to carry 1-year-old spent fuel, we considered one series of designs having stainless-steel fins and an alternate series of designs having copper fins. Both series are presented in Table VII.3. Because a slightly different n/γ split was used in determining the shielding requirements in the two cases, those with copper fins generally had considerably less neutron shielding and slightly more gamma shielding (cf. Section IV.B.5 and/or Table IV.18). Of the two types of casks, the copper-finned casks were generally somewhat lighter and offered some advantage. Indeed, the copper-finned

U-metal cask for 1-year-old fuel can hold 2-4 more assemblies than the original cask with stainless-steel fins. Unfortunately, due to the large internal decay heat load, the 2.25" aluminum insert comes precariously close to melting during the thermal transient following the postulated 30-minute fire.^{*} To rectify that difficulty, we also considered an alternate series of designs for a copper-finned U-metal cask having a 3.0" thick aluminum insert. Lastly, we note that the capacity of the 1-year-old Pb cask is generally limited by the onset of melting near the outer surface of the gamma shield during the 30-minute fire. In the case of the copper-finned Pb cask for 1-year-old fuel, we also considered an alternate series of designs in which the 0.75" outside liner was increased to 1.25" and the 2.0" outer shell was increased to 2.5". While this did offer some additional thermal protection as noted in Section VII.A, the capacity of the 1-year-old Pb cask is still limited by the onset of melting during the postulated 30-minute fire. Depending on the amount of melting, this may or may not be a real design constraint. Insofar as the present version of the SCOFE code cannot account for the change of phase or calculate the amount of melting that might occur, those cases where the transient temperature is in excess of 618°F should be viewed with caution. For the sake of completeness, Table VII.3 does show the overall weight and characteristic dimensions of Pb casks designed to carry as many as fourteen 1-year-old PWR fuel assemblies. A more detailed thermal analysis of these casks should be performed at some later time.

^{*} Insert temperatures in excess of 1120°F should be viewed with caution. See Section VII.A for additional details regarding this design and the proposed alternate.

Since many design limits such as the overall cask weight (and some of the thermal constraints) are not absolutely rigid, Table VII.3 shows a number of cases on either side of the nominal design limits. Given the nonuniform manner in which the inside cask diameter (and the overall weight) must increase as a function of the number of assemblies in the cask, this rather extensive table is most useful. Ultimately, however, one must eventually answer the basic question: "How many 1,2,3,5,7 or 10-year-old PWR fuel assemblies could actually be shipped in a Pb, Fe, or U-metal cask?" Using the information generated thus far, and assuming 198,000 lbs (89.9 metric tonnes) as the maximum allowable weight for the loaded cask, Table VII.4 shows the estimated capacity for each type of cask as a function of the spent fuel's decay time. Assuming that a slightly heavier cask could be tolerated, the estimated capacity for 12 of these 18 designs could be adjusted upward -- some by as many as three assemblies. Table VII.5, for example, shows the estimated capacity for each type of cask as a function of the cooling time assuming a slightly higher weight limit of 207,000 lbs (93.9 metric tonnes).^{*} In either case, it appears that optimized designs could be developed which could carry as many as 15-18 5-year-old PWR fuel assemblies or as many as 18-21 10-year-old PWR fuel assemblies.

* For those casks containing 1-year-old spent fuel, the melting of the Pb gamma shield (at 618°F) and the melting of the aluminum insert in the U-metal cask (at 1120°F) were both viewed as real constraints. These constraints were noted when selecting the optimal cases to be presented in Tables VII.4 and VII.5.

TABLE VII.4
OPTIMAL PB, FE, AND U-METAL CASK DESIGNS vs AGE OF SPENT FUEL (WT < 198,000 lbs)

FUEL AGE	GAMMA SHIELD	NO. OF ASSYS	LOADED WT (1000-LBS)	CASK ID (IN)	CSHLD (IN)	NSHLD (IN)	FIN MATL	L-FIN (IN)	T-FIN (IN)
10	PB	18	196.6	61.9	3.90	4.02	--	--	--
7	PB	15	177.4	55.8	4.15	3.95	--	--	--
5	PB	15	183.7	55.8	4.43	4.08	--	--	--
3	PB	15	195.8	55.8	4.99	4.21	--	--	--
2	PB ^{fh}	12	187.3	48.9	5.37	4.25	SS	2.00	0.25
1	PB ^{fh}	6	159.6	39.5	5.60	4.08	CU	2.00	0.25
10	FE	15	189.8	55.8	9.19	3.74	--	--	--
7	FE	15	197.2	55.8	9.65	3.59	--	--	--
5	FE	15	187.9	52.0	10.05	3.57	--	--	--
3	FE	12	187.9	48.9	10.82	3.79	--	--	--
2	FE	10	185.3	46.4	11.33	3.89	--	--	--
1	FE	8	137.8	43.3	12.01	3.91	CU	2.00	0.25
10	U	21	189.0	63.8	2.45	3.42	--	--	--
7	U	21	194.5	63.8	2.61	3.49	--	--	--
5	U	18	190.6	61.9	2.74	3.35	--	--	--
3	U	15	179.7	55.8	3.02	3.57	SS	2.00	0.25
2	U	15	188.8	55.8	3.27	3.67	SS	3.00	0.25
1	U ¹	13	197.6	54.8	3.57	3.72	CU	6.00	0.25

- a) Pb casks have a 1.5" inner shell, a 2.0" outer shell, and a 0.75" outside liner.
b) Fe casks have a 0.375" inner shell, a 0.375" outer shell, and a 0.75" outside liner.
c) U-metal casks have a 0.75" inner shell, a 2.0" outer shell, and a 0.75" outside liner.
d) In each case, the fuel assemblies are separated by means of a 2.25" thick Al insert.
e) In each case, the accident scenario used in the transient thermal analysis assumes that the region containing the neutron shield had been voided for some time prior to the start of the 30-minute fire and that the cask internals had already reached a new (elevated) state of thermal equilibrium prior to the fire.

TABLE VII.5
OPTIMAL PB, FE, AND U-METAL CASK DESIGNS vs AGE OF SPENT FUEL (WT < 207,000 LBS)

FUEL AGE	GAMMA SHIELD	NO. OF ASSYS	LOADED WT (1000-LBS)	CASK ID (IN)	CSHLD (IN)	NSHLD (IN)	FIN MATL	L-FIN (IN)	T-FIN (IN)
10	PB	21	206.9	63.8	3.97	4.13	--	--	--
7	PB	18	203.3	61.9	4.20	4.01	--	--	--
5	PB	15	183.7	55.8	4.43	4.08	--	--	--
3	PB	15	195.8	55.8	4.99	4.21	--	--	--
2	PB ^{fh}	13	201.0	52.0	5.39	4.25	SS	2.00	0.25
1	PB ^{fh}	6	159.6	39.5	5.60	4.08	CU	2.00	0.25
10	FE	15	189.8	55.8	9.19	3.68	--	--	--
7	FE	15	197.2	55.8	9.65	3.59	--	--	--
5	FE	15	204.7	55.8	10.11	3.56	--	--	--
3	FE	13	201.3	52.0	10.85	3.79	--	--	--
2	FE	12	203.5	48.9	11.46	3.92	SS	2.00	0.25
1	FE	10	205.4	46.4	12.17	3.94	CU	3.00	0.25
10	U	22	200.2	66.6	2.46	3.43	--	--	--
7	U	22	206.0	66.6	2.61	3.50	--	--	--
5	U	21	200.6	63.8	2.79	3.45	--	--	--
3	U	18	206.2	61.9	3.06	3.58	SS	2.00	0.25
2	U	15	188.8	55.8	3.27	3.67	SS	3.00	0.25
1	U ^j	15	205.5	55.8	3.60	3.73	CU	8.00	0.25

- f) For added thermal protection, a 2.5" outer shell was used in lieu of a 2.0" shell.
g) For added thermal protection, a 1.25" outside liner was used in lieu of the 0.75" liner.
h) No. of assys. limited by the onset of melting in the Pb during the thermal transient.
i) For added thermal protection, a 3.0" aluminum insert was used in lieu of a 2.25" insert.
j) In this particular design, the 2.25" thick aluminum insert comes precariously close to the melting point during the thermal transient following the 30-minute fire.

APPENDICES

APPENDIX A

ABBREVIATED INPUT DATA GUIDE FOR THE SHIPPING CASK
OPTIMIZATION AND PARAMETRIC EVALUATION CODE (SCOPE)

The SCOPE code⁷ was developed by the Computer Sciences Division at ORNL as a modestly rigorous yet inexpensive code for Shipping Cask Optimization and Parametric Evaluation. Given the neutron and gamma shielding requirements (as input), the code may be used as a conceptual design tool for the evaluation of various casks designed to carry square fuel assemblies, circular canisters of nuclear waste material, or circular canisters containing "intact" spent fuel assemblies. The code may be used to evaluate a specific design, or to search for the maximum number of fuel assemblies (or canisters) that might be shipped in a given type of cask. Fig. A.1 shows an overview of the basic cask configuration assumed by the SCOPE code. While the fuel assemblies or canisters may be suspended inside the cask cavity with a minimal amount of support ($MINSRT = 14$), one generally has a solid aluminum or copper insert (i.e. "basket") inside the cask to hold the circular canisters or square fuel assemblies in place. Fig. A.2 shows an aluminum insert designed to carry ten PWR fuel assemblies. Figs. A.3 and A.4 show some of the other optimal packing arrangements for square fuel assemblies and circular canisters. Geometric constants for still other "optimal" packing arrangements have been incorporated within the code, which can now handle up to 61 square fuel assemblies or any number of circular canisters. In the "search" mode ($NELEM = 0$), the code will use these built-in packing arrangements and the tabulated shielding requirements input by the user to "design" a cask carrying one fuel assembly (or canister); it will then continue to increment the number of assemblies (or canisters) until one or more of the design limits can no longer be met. In each case

Fig. A.1: Major Structural Components of a Shipping Cask as Modeled in the SCOPE Code

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- The interior region is 6 inches longer than a fuel assembly or canister so as to accommodate an internal axial shock absorber.
- The thicknesses of the various components in the axial direction are each assumed to be the same as in the radial direction.
- If the steady-state thermal design analysis shows that external cooling fins are not required, t_f and l_f are both set to zero.
- If this design analysis shows that fins are required, the code will calculate and edit the optimal fin dimensions (t_f and l_f).
- Estimates of the overall weight include the spent fuel, the aluminum or copper insert, and the external cooling fins (if reqd).

Fig. A.2: Simplified Diagram of a Removable Aluminum Insert Designed to Hold 10 PWR Fuel Assemblies

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Other optimal
arrangements are
shown in Fig. A.3

For an alternate,
more sophisticated
model, see Fig. A.5

N=10 Dia = $(\sqrt{18})$ PITCH

N=15 Dia = $(\sqrt{26})$ PITCH

N=12 Dia = $(\sqrt{20})$ PITCH

N=18 Dia = $(\sqrt{32})$ PITCH

N=13 Dia = $(\sqrt{22.6})$ PITCH

N=21 Dia = $(\sqrt{34})$ PITCH

Fig. A.3: Optimal Packing Arrangements for Casks Containing 10-21 Square Fuel Assemblies

Fig. A.4: Optimal Packing Arrangements for Casks Containing 5-12 Circular Canisters

($N = 1, 2, 3, \dots$), the code will calculate the steady-state temperature distribution throughout the cask and perform a complete 1-D space/time transient thermal analysis following a postulated half-hour fire. In each case, the code will edit the characteristic dimensions of the cask (including fins, if required), the total weight of the loaded cask, the steady-state temperature distribution at selected points, and the maximum transient temperature in key components. The purpose of this appendix is to give a brief description of the input required by the SCOPE code.

Except for the title card (which is read using a 20A4 format), all input is read using the free-form input processor described below. Each case consists of a title card, a parameter card,^{*} shielding data, and a termination card. If desired, the user may enter several sets of input data back-to-back, as long as each set starts with a new title card.

The free-form input processor implemented herein allows alphanumeric data, floating-point data, and integer data to be entered in an unformatted manner. All 80 columns of any card may be used and data, with certain exceptions noted below, can start or end in any column.

Any alphanumeric data entry must be followed by two or more blanks. Any numeric data entry must be followed by one or more blanks. Integers may be used for floating values if the particular item has no decimal value (i.e., 10 will be interpreted as 10.0). Likewise, 1+4, 1.0+4, 1.0E+4 or 10000.0 will all be interpreted as 1.0×10^4 . Note that embedded blanks

^{*} Because of the free-form input processor, the input data for the Parameter Card may be continued on as many punched cards as necessary. Likewise, the shielding data may also extend over as many punched cards as necessary.

are not allowed within the representation for a given number. The only exception to this is that a single blank may precede an unsigned exponent in a floating-point number. Thus, for example, 1.0E 04 would be correctly interpreted as 1.0×10^4 .

This free-form processor has provisions for multiple entries of the same data value. This is done by entering the number of repeats, followed by either R, *, or \$, followed by the data value to be repeated. For example, 5R2 or 5*2 enters five successive 2's in the input data. There should be no blanks between the number of repeats and the repeat flag (R, *, or \$), but each multiple entry must be separated from the rest of the data by one or more blanks. Multiple zeros may be specified as nZ where n is the number of zeros to be entered. There should not be any blanks between the n and the Z, but the nZ must be separated from the rest of the data by one or more blanks.

***** INPUT DATA *****

***TITLE CARD - All 80 columns may be used for a descriptive title

***PARAMETER CARD:

1. MELEM - Integer ID number denoting the type of waste material;
 enter 21 for an intact PWR fuel assembly ($\rho=220.9$ lb/ft³)
 enter 22 for an intact BWR fuel assembly ($\rho=199.3$ lb/ft³)

User may also select any of the following alternate forms:

MELEM	TYPE OF MATERIAL	DENSITY (lb/ft ³)	CONDUCTIVITY (Btu/hr)/ft ² °F
19	high level waste	212.0	0.70
23	high level waste	113.0	0.25
10	concentrated high level waste	707.6	18.00
11	waste solidified in aluminum silicate	170.0	80.00
16	radioactive hulls	81.2	0.30
18	radioactive hulls	284.0	1.20
20	radioactive hulls	203.0	0.60
17	radioactive shot	370.0	0.35

2. BURNUP - average burnup of the fuel (MWD/MT); enter 0 if not known;
 this parameter is no longer used, but is still edited for
 bookkeeping purposes
3. TIME - Cooling time (in years); ie, age of fuel since discharge;
 this parameter is no longer used, but is still edited for
 bookkeeping purposes
4. WHEAT - Decay heat (watts/ft³) given off by the waste material;
 enter 0 if MELEM denotes a PWR or BWR fuel assembly
5. DHEAT - Decay heat (watts/assembly) given off by each assembly;
 enter 0 if MELEM does not denote a PWR or BWR fuel assembly

6. SRCN - Neutron source [(neut/sec)/assembly]; this parameter is no longer used, but is still edited for bookkeeping purposes
7. SRGG - Gamma source [(photons/sec)/assembly]; this parameter is no longer used, but is still edited for bookkeeping purposes
8. ITYPE - Flag denoting the type of payload in the cask; for circular canisters containing square assemblies enter 1; otherwise: enter 1 for circular canisters containing waste; enter 2 for square assemblies (without can)
9. NPINS - Number of fuel pins per assembly; applies only if MELEM denotes a PWR or BWR fuel assembly; enter 0 otherwise
10. MCAN - Integer ID number denoting type of material used for canister; enter 0 if no canister is used; if ITYPE=2, always set MCAN=0; If ITYPE=1, user may select from any of the following materials: 4-copper; 5-aluminum; or 6-stainless steel (usually 0 or 6)
11. ODCAN - Outside diameter of canister in inches (if MCAN>0); Width of spent fuel assembly in inches (if MCAN=0)
12. TKCAN - Wall thickness of canister (in inches); enter 0 if MCAN=0
13. HTCAN - Length of the canisters in feet (if MCAN>0); Length of fuel assembly in feet (if MCAN=0)
14. HTVOID - Portion of canister not occupied by waste material (in feet); applies only if MCAN>0; if MCAN=0, enter 0
15. HTFUEL - Portion of fuel assembly containing UO_2 (ie, active length of the fuel in feet); applies only if MELEM denotes a PWR or BWR fuel assembly; enter 0 otherwise
16. MINSRT - Integer ID number denoting type of material used for the insert (the insert being a grid-like device used to separate the canisters or fuel assemblies inside the cask); possible choices: 4-copper; 5-aluminum; 6-stainless steel; 14-air (usually 5)
17. EMINST - Emissivity of the insert material (dimensionless); recommended values: 0.22 for aluminum; 0.76 for copper; and 0.58 for stainless-steel; enter 0 if MINSRT=14
18. TKINST - Thickness of the insert between fuel assemblies (inches); includes TPOISN; for square assemblies, see Fig A.5; If ITYPE=1, TKINST is the minimum thickness of the insert between any two circular canisters
19. TPOISN - Thickness of the neutron poison embedded in the insert between the fuel assemblies (inches); see Fig A.5; enter 0 if ITYPE=1

**Fig. A.5: Inherently Subcritical, Removable Aluminum Insert
Designed to Hold 10 PWR Fuel Assemblies**

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Note the use of hollow, borated stainless-steel tubes within the walls of the insert in order to insure subcriticality. Their presence effectively reduces the thickness of the webbing through which the decay heat must flow.

If not used, set TPOISN=0.0

- 20. TKCGAP - Thickness of the gap (in inches) between the canister and the insert (if ITYPE=1); Clearance between the fuel assembly and the insert (if ITYPE=2)
 - 21. TKIGAP - Thickness of the gap (in inches) between the insert and the inner shell
 - 22. WTHICK - Minimum thickness of the insert between the outermost canister (or fuel assembly) and the inner shell (inches); If one is analyzing a specific cask whose inside diameter is known, set WTHICK = 0
 - 23. NELEM - Number of assemblies (or canisters) per cask; setting NELEM=0 will cause the code to generate many cases -- first with NELEM=1, then with NELEM=2, NELEM=3, ..., etc., until one or more of the design limits are exceeded
 - 24. CASKID - Inside diameter of the cask (in inches); enter 0 if NELEM=0 (if NELEM=0, the code will calculate CASKID for each case)
 - 25. MISHL - Integer ID number denoting type of material used for the inner shell; possible choices include: 2-carbon steel; 5-aluminum; or 6-stainless steel (usually 6)
 - 26. MOSHL - Integer ID number denoting type of material used for the outer shell; possible choices include: 2-carbon steel; 5-aluminum; or 6-stainless steel (usually 6)
 - 27. MOLIN - Integer ID number denoting type of material used for the outside liner; possible choices include: 2-carbon steel; 5-aluminum; or 6-stainless steel (usually 6)
- Note: Frequently the inner shell, outer shell and outside liner are all stainless-steel. In this particular case, the user may simply punch 3R6 or 3*6 to enter all three parameters (MISHL,MOSHL,MOLIN) at once.
- 28. MFIN - Integer ID number denoting type of material to be used for the external cooling fins in the event that the steady-state heat transfer design analysis shows that fins are needed; possible choices include: 2-carbon steel; 4-copper; 5-aluminum; or 6-stainless steel (usually 6 or 4)
 - 29. TKISHL - Thickness of the inner shell (in inches); must not be zero unless items 30 and 31 are also zero; if all three are zero, the code will use the default values shown in Table A.1

Table A.1
Thickness of Various Structural
Components in Each Type of Cask

TYPE OF CASK	THICKNESS IN INCHES		
	INNER SHELL	OUTER SHELL	OUTSIDE LINER
LEAD	1.5	2.0	.75
U-METAL	.75	2.0	.75
IRON	.375	.375	.75

30. TKOSHL - Thickness of the outer shell (in inches); must not be zero unless items 29 and 31 are also zero; if all three are zero, the code will use the default values shown in Table A.1
31. TKOLIN - Thickness of the outside liner (in inches); must not be zero unless items 29 and 30 are also zero; if all three are zero, the code will use the default values shown in Table A.1
32. MGSULD - Integer ID number denoting type of material used for the gamma shield; possible choices include: 1-lead; 2-carbon steel; 3-depleted uranium metal
- Note: setting MGSULD=0 will cause the code to perform one series of calculations with a Pb gamma shield, another series of calculations with an Fe gamma shield, and a third series of calculations with a depleted U-metal gamma shield. If TKISHL, TKOSHL, and TKOLIN are all entered as zero (3R0 or 3*0), the code will use each set of default values as appropriate.
- Note: in each case, a borated-water neutron shield is used in conjunction with the Pb, Fe, or U-metal gamma shield specified by the user (cf. Fig A.1); the thickness of the neutron shield may, of course, be set equal to zero if so desired (see below).
33. SPFIN - Center-to-center spacing between the fins (in inches); typically 4.0 inches
34. EMISF - Surface emissivity of the fins (dimensionless); $0.0 < \epsilon_f < 1.0$; typically 0.54-0.63 for stainless-steel, or 0.76-0.78 for oxidized copper; entering 0 will cause the code to use a default value of 0.587
35. EMISC - Surface emissivity of the cask body (ie, the outside liner); dimensionless; typically 0.54-0.63 for stainless-steel; entering 0 will cause the code to use a default value of 0.587
36. TPNMAX - Maximum allowable surface temperature ($^{\circ}\text{F}$); the cask will be designed to meet this constraint assuming an ambient temperature of 130°F (TPNMAX usually 250°F)
37. WGHMIX - Maximum allowable weight for the loaded cask (kilo-pounds); typically one would enter 200 to denote a limit of 200000 lbs; If NELEM=0, the code will flag all cases where this limit is exceeded; indeed, it will stop incrementing the number of assemblies whenever this limit is exceeded by more than 20%

*****SHIELDING DATA:**

1. **LTYPE** - Flag denoting the type of cask for which the tabulated shielding data (entered next) is to apply;
 enter 1 if that data applies to a Pb/water cask;
 enter 2 if that data applies to a Fe/water cask;
 enter 3 if that data applies to a U-metal/water cask
2. **NUMPTS** - Number of data entries in this shielding table;
 $1 \leq \text{NUMPTS} \leq 10$; see item 3 below
3. **(NASSYS(I,LTYPE), TKG(I,LTYPE), TKN(I,LTYPE), I=1,NUMPTS)**

NASSYS(I,LTYPE) - number of spent fuel assemblies (or canisters)
 for which the following shielding requirements are applicable

TKG(I,LTYPE) - amount of gamma shielding (in inches) that is required
 whenever one has this many assemblies (**NASSYS**) in this particular type of cask (**LTYPE**)

TKN(I,LTYPE) - amount of neutron shielding (in inches) that is required
 whenever one has this many assemblies (**NASSYS**) in this particular type of cask (**LTYPE**)

Note: Items (1,2,3) should be entered as many times as needed.
 For example: if one wanted to look only at a Pb/water cask, then items (1,2,3) should be entered once [ie, the following data:
 1 3 1 5.60 3.77 8 5.73 4.14 15 5.99 4.41
 would completely describe the neutron and gamma shielding requirements for the Pb/water cask as illustrated in Fig A.6];
 Likewise, if one wanted to look at a Pb/water cask, an Fe/water cask, and a U-metal/water cask, items (1,2,3) should be entered once for the Pb/water cask, again for the Fe/water cask, and again for the U-metal/water cask.

Note: Some recommended data for 1,2,3,5,7 and 10-year-old PWR spent fuel assemblies is given in Table A.2. Used in conjunction with the structural components shown in Table A.1, these thicknesses will reduce the total dose rate down to 10 mrem/hr at a point 10 feet from the centerline.

*****TERMINATION CARD** - Enter a zero somewhere on this card to denote the end of the input for this particular case

Fig. A.6: Neutron and Gamma Shielding Requirements for a Pb Cask Containing 1-Year-Old PWR Spent Fuel (cf. Table A.2)

Table A.2
Optimized Shielding Requirements for Various Types of Casks, as a Function
of the Age of the Spent Fuel and the Number of Assemblies in the Cask

1-YEAR-OLD PWR FUEL ASSEMBLIES				5-YEAR-OLD PWR FUEL ASSEMBLIES			
TYPE CASK	NO.OF ASSYS	THICKNESS (IN INCHES)		TYPE CASK	NO.OF ASSYS	THICKNESS (IN INCHES)	
		G-SHIELD	N-SHIELD			G-SHIELD	N-SHIELD
PB	1	5.00	3.77	PB	1	3.55	3.60
PB	8	5.73	4.14	PB	8	4.19	3.95
PB	15	5.99	4.41	PB	18	4.48	4.07
FE	1	10.91	3.83	FE	1	8.79	3.44
FE	8	12.00	3.91	FE	8	9.76	3.51
FE	15	12.40	3.96	FE	18	10.19	3.57
U	1	3.03	2.98	U	1	2.21	2.54
U	8	3.44	3.52	U	8	2.58	3.09
U	15	3.60	3.73	U	18	2.74	3.35

2-YEAR-OLD PWR FUEL ASSEMBLIES				7-YEAR-OLD PWR FUEL ASSEMBLIES			
TYPE CASK	NO.OF ASSYS	THICKNESS (IN INCHES)		TYPE CASK	NO.OF ASSYS	THICKNESS (IN INCHES)	
		G-SHIELD	N-SHIELD			G-SHIELD	N-SHIELD
PB	1	4.47	3.71	PB	1	3.27	3.54
PB	8	5.18	4.06	PB	8	3.91	3.82
PB	15	5.44	4.24	PB	21	4.27	4.03
FE	1	10.11	3.81	FE	1	8.35	3.49
FE	8	11.18	3.86	FE	8	9.31	3.55
FE	15	11.56	3.90	FE	21	9.84	3.62
U	1	2.72	3.10	U	1	2.05	2.67
U	8	3.13	3.49	U	8	2.40	3.14
U	15	3.27	3.67	U	21	2.61	3.49

3-YEAR-OLD PWR FUEL ASSEMBLIES				10-YEAR-OLD PWR FUEL ASSEMBLIES			
TYPE CASK	NO.OF ASSYS	THICKNESS (IN INCHES)		TYPE CASK	NO.OF ASSYS	THICKNESS (IN INCHES)	
		G-SHIELD	N-SHIELD			G-SHIELD	N-SHIELD
PB	1	4.06	3.73	PB	1	2.98	3.68
PB	8	4.74	4.05	PB	8	3.61	3.93
PB	18	5.05	4.24	PB	21	3.97	4.13
FE	1	9.52	3.68	FE	1	7.90	3.61
FE	8	10.54	3.74	FE	8	8.85	3.66
FE	18	10.99	3.82	FE	21	9.39	3.71
U	1	2.49	2.86	U	1	1.89	2.61
U	8	2.88	3.30	U	8	2.25	3.06
U	18	3.05	3.58	U	21	2.45	3.42

*When used in conjunction with the various structural components shown in Table A.1, the neutron and gamma shielding requirements shown here are sufficient to reduce the total dose down to 10 mrem/hr at a point 10 feet from the centerline of the cask.

APPENDIX B

OPTIMAL PACKING ARRANGEMENTS FOR SQUARE FUEL
ASSEMBLIES INSIDE A CYLINDRICAL SHIPPING CASK

To minimize the overall weight of a spent fuel cask, the fuel assemblies should be arranged in the most compact configuration possible. Figs. B.1(a-e) illustrate the optimal packing arrangements for any given number of assemblies ($N \leq 61$). In each case, the diameter of the smallest circle enclosing the square assemblies is given in terms of the center-to-center pitch between assemblies. (Note that this pitch includes the aluminum or copper insert between the assemblies, and/or any neutron poison that might be present). For quick and easy reference, Table B.1 also gives the diameter/pitch ratio for each of the configurations shown in Figs. B.1(a-e).

As one might suspect, there are certain "magic numbers" associated with the various configurations shown in Figs. B.1(a-e) and Table B.1. Thus, while there is an optimal configuration for a cask containing 15 assemblies and another optimal configuration for a cask containing 18 assemblies, there is no 16 or 17-assembly configuration having a diameter less than that shown for the cask with 18 assemblies. Given the number of assemblies on a fixed pitch, one can use Table B.1 to determine the inside diameter of the cask. Likewise, given the inside diameter of a cask designed to hold a certain number of PWR fuel assemblies, one can use Table B.1 to determine how many BWR assemblies might fit in the same cask.

The optimal packing arrangements shown in Figs. B.1 (a-e) were determined in a logical (or at least semi-logical) fashion by considering several

general types of configurations, determining the "magic numbers" associated with each, and then selecting an optimal subset from the union of all the cases considered.

In the first broad category considered, we assumed that we had many adjacent rows of assemblies, where none of the rows were staggered with respect to the adjacent row(s). Given a vast array of square assemblies arranged in this fashion, one may consider a family of circles of increasing radii whose center may lie (a) at the corner of four neighboring assemblies, (b) at the midpoint directly between two adjacent assemblies, or (c) at the center of a single assembly. By examining the location of the vertices in any quadrant, one can identify a different set of circles which will enclose an integer number of assemblies in each case (a,b,c). Combining these sets, one can then select an optimal subset.

In the second broad category considered, we wanted to account for the fact that some (but not necessarily all) rows of fuel assemblies may be staggered with respect to adjacent rows. To attack this problem, we artificially divided each assembly into four "mini-assemblies". Regardless of whether the centerline of the cask was at the corner between four neighboring assemblies, at the midpoint between two adjacent assemblies, or at the center of a single assembly, it would always lie at the corner between four neighboring "mini-assemblies". We then considered a large family of circles whose radii were defined by the vertices of the various "mini-assemblies" in any given quadrant. Circles enclosing as few as four mini-assemblies and as many as 256 mini-assemblies were considered. This process generated 32 different "magic circles" -- each with its own arrangement of mini-assemblies. Within the silhouette of each of these 32 arrange-

ments, we then sketched as many "full-sized" assemblies as would fit. In some cases we utilized the full silhouette and in some cases we could not. Maximum utilization of this space sometimes required the various rows of assemblies to be staggered, and sometimes it required that they all be aligned. Indeed, 17 of the 32 schemes required the use of staggered assemblies while 15 did not. The 17 staggered patterns then represent a second broad category of near-optimal packing arrangements. Combining these with the patterns previously identified, one can then select an optimal subset.

Lastly, one can identify a third broad category of "intuitively obvious" compact configurations where the centerline of the cask does not lie on the corner between four neighboring assemblies, at the midpoint between two adjacent assemblies, or at the center of a single assembly. Configurations like this with 3, 8, and 13 assemblies are shown in Fig. B.1. Configurations with 5, 7, and 11 assemblies were also considered but later discarded. If $(0,0)$ denotes the coordinates of the lower left-hand corner of such a configuration, and (x,y) denotes the coordinates of the farthest corner, and X denotes the center of the configuration in the x -direction, then the center of the configuration in the y -direction can be calculated as

$$Y = (x^2 + y^2 - 2xX) / (2y) \quad (\text{B.1})$$

and the diameter of the smallest circle enclosing the assemblies is given by

$$D^2 = 4(X^2 + Y^2) \quad (\text{B.2})$$

Combining these configurations with those previously identified in categories 1 and 2, we then selected the optimal subset illustrated in Figs. B.1(a-e).

Table B.1
Minimum Diameter-to-Pitch Ratios for
Dense Arrays of Square Assemblies in
a Circular Cavity (see attached fig)

NO.OF ASSYS	(D/P) ²	NO.OF ASSYS	(D/P) ²
1	2	24	40
2	5	26	41
3	6.65	31	50
4	8	32	52
5	10	34	53
7	13	37	58
8	15.68	39	61
8	17	42	65
10	18	44	68
12	20	46	72
13	22.60	48	74
15	26	52	80
18	32	56	85
21	34	58	89
22	37	61	90

Fig. B.1a: Optimal Packing Arrangements for 1-8 Square Assemblies in a Circular Cask

Fig. B.1b: Optimal Packing Arrangements for 9-22 Square Assemblies in a Circular Cask

$N = 23, 24$
 $\text{DIAM} = (\sqrt{40}) \text{ PITCH}$

$N = 25, 26$
 $\text{DIAM} = (\sqrt{41}) \text{ PITCH}$

$N = 27, 28, 29, 30, 31$
 $\text{DIAM} = (\sqrt{50}) \text{ PITCH}$

$N = 32$
 $\text{DIAM} = (\sqrt{52}) \text{ PITCH}$

$N = 33, 34$
 $\text{DIAM} = (\sqrt{53}) \text{ PITCH}$

$N = 35, 36, 37$
 $\text{DIAM} = (\sqrt{58}) \text{ PITCH}$

Fig. B.1c: Optimal Packing Arrangements for 23-37 Square Assemblies in a Circular Cask

N = 38, 39
DIAM = $(\sqrt{61})$ PITCH

N = 40, 41, 42
DIAM = $(\sqrt{65})$ PITCH

N = 43, 44
DIAM = $(\sqrt{68})$ PITCH

N = 45, 46
DIAM = $(\sqrt{72})$ PITCH

N = 47, 48
DIAM = $(\sqrt{74})$ PITCH

N = 49, 50, 51, 52
DIAM = $(\sqrt{80})$ PITCH

Fig. B.1d: Optimal Packing Arrangements for 38-52 Square Assemblies in a Circular Cask

N=53,54,55,56
DIAM=($\sqrt{85}$) PITCH

N= 57,58
DIAM=($\sqrt{89}$) PITCH

N=59,60,61
DIAM=($\sqrt{90}$) PITCH

Fig. B.1e: Optimal Packing Arrangements for 53-61 Square Assemblies in a Circular Cask

APPENDIX C

DECAY HEAT RATE, NEUTRON AND GAMMA EMISSION RATES, AND THE CORRESPONDING
MULTIGROUP (22n-18 γ) NEUTRON-GAMMA SPECTRA FOR STANDARD PWR AND BWR
SPENT FUEL AT 17 DECAY TIMES RANGING FROM DISCHARGE TO 25 YEARS

As noted in Section IV.A, the ORIGEN-2 code¹⁹ was used to generate the neutron, photon, and decay heat source terms for standard PWR and BWR fuel assemblies at each of 17 decay times (discharge, 120d, 150d, 1y, 2y, ..., 25y). Tables C.1 and C.2 show a concise summary of this data on a "per assembly" basis. Note that each PWR fuel assembly was initially assumed to contain 0.4614 metric tonnes of heavy metal while each BWR assembly was initially assumed to contain 0.1833 metric tonnes of heavy metal.

To make effective use of this data in a multigroup shielding calculation, one must also know the neutron and gamma source spectra. While the gamma spectrum at each decay time may be generated directly from the ORIGEN-2 data, the neutron spectrum must be constructed as shown in Section IV.A.3. This, of course, requires some knowledge of the nuclides responsible for the (α ,n) neutron source at each decay time, the nuclides responsible for the spontaneous fission neutron source at each decay time, and the spectrum corresponding to each nuclide/process. Using the ORIGEN-2 data and several key spectra previously published by Stoddard, et al.^{31,32} at Savannah River, a multigroup (22n-18 γ) spectrum was constructed for the PWR fuel and the BWR fuel at each decay time (cf. Tables C.4 and C.5). The energy group structure used in this construction was the same as that used in the DLC-23/CASK Cross-Section Library²⁷ (cf. Table C.3).

Table C.1

Neutron, Photon, and Decay Heat Source Terms for
Spent PWR Fuel Assemblies at Various Decay Times

FUEL	DECAY HEAT	NEUTRN SRC	GAMMA SRC
<u>AGE</u>	<u>WATTS/ASSY</u>	<u>(N/S)/ASSY</u>	<u>(P/S)/ASSY</u>
DISH	1.017E+06	3.654E+08	5.318E+18
120D	1.109E+04	2.614E+08	9.099E+16
150D	9.514E+03	2.423E+08	7.824E+16
1Y	4.697E+03	1.571E+08	3.990E+16
2Y	2.462E+03	1.098E+08	2.147E+16
3Y	1.533E+03	9.708E+07	1.366E+16
4Y	1.080E+03	9.179E+07	9.817E+15
5Y	8.452E+02	8.816E+07	7.804E+15
7Y	6.369E+02	8.200E+07	5.967E+15
8Y	5.862E+02	7.914E+07	5.500E+15
10Y	5.240E+02	7.374E+07	4.900E+15
13Y	4.708E+02	6.635E+07	4.349E+15
15Y	4.461E+02	6.188E+07	4.079E+15
18Y	4.163E+02	5.577E+07	3.743E+15
20Y	3.993E+02	5.207E+07	3.548E+15
23Y	3.766E+02	4.701E+07	3.287E+15
25Y	3.628E+02	4.395E+07	3.126E+15

*Assumes 0.4614 metric tonnes of heavy metal per
PWR fuel assembly and a burnup of 33,000 MWD/MT

Table C.2

Neutron, Photon, and Decay Heat Source Terms for
Spent BWR Fuel Assemblies at Various Decay Times

FUEL	DECAY HEAT	NEUTRN SRC	GAMMA SRC
<u>AGE</u>	<u>WATTS/ASSY</u>	<u>(N/S)/ASSY</u>	<u>(P/S)/ASSY</u>
DISH	2.776E+05	1.216E+08	1.485E+18
120D	3.215E+03	8.446E+07	2.650E+16
150D	2.766E+03	7.764E+07	2.287E+16
1Y	1.383E+03	4.737E+07	1.184E+16
2Y	7.331E+02	3.077E+07	6.464E+15
3Y	4.620E+02	2.653E+07	4.175E+15
4Y	3.296E+02	2.494E+07	3.047E+15
5Y	2.610E+02	2.394E+07	2.453E+15
7Y	2.003E+02	2.229E+07	1.907E+15
8Y	1.855E+02	2.152E+07	1.767E+15
10Y	1.675E+02	2.008E+07	1.585E+15
13Y	1.519E+02	1.811E+07	1.415E+15
15Y	1.446E+02	1.692E+07	1.330E+15
18Y	1.356E+02	1.528E+07	1.224E+15
20Y	1.304E+02	1.430E+07	1.162E+15
23Y	1.234E+02	1.295E+07	1.077E+15
25Y	1.191E+02	1.212E+07	1.025E+15

*Assumes 0.1833 metric tonnes of heavy metal per
BWR fuel assembly and a burnup of 27,500 MWD/MT

Table C.3
Energy Group Structure for the DLC-23/CASK Cross Section Library

NEUT GRP.	ENERGY RANGE		GAMMA GRP.	ENERGY RANGE	
1	14.92 Mev	12.20 Mev	1	10.00 Mev	8.00 Mev
2	12.20 Mev	10.00 Mev	2	8.00 Mev	6.50 Mev
3	10.00 Mev	8.18 Mev	3	6.50 Mev	5.00 Mev
4	8.18 Mev	6.36 Mev	4	5.00 Mev	4.00 Mev
5	6.36 Mev	4.96 Mev	5	4.00 Mev	3.00 Mev
6	4.96 Mev	4.06 Mev	6	3.00 Mev	2.50 Mev
7	4.06 Mev	3.01 Mev	7	2.50 Mev	2.00 Mev
8	3.01 Mev	2.46 Mev	8	2.00 Mev	1.66 Mev
9	2.46 Mev	2.35 Mev	9	1.66 Mev	1.33 Mev
10	2.35 Mev	1.83 Mev	10	1.33 Mev	1.00 Mev
11	1.83 Mev	1.11 Mev	11	1.00 Mev	.80 Mev
12	1.11 Mev	0.55 Mev	12	.80 Mev	.60 Mev
13	550 Kev	111 Kev	13	.60 Mev	.40 Mev
14	111 Kev	3.35 Kev	14	.40 Mev	.30 Mev
15	3350 ev	583 ev	15	.30 Mev	.20 Mev
16	583 ev	101 ev	16	.20 Mev	.10 Mev
17	101 ev	29 ev	17	.10 Mev	.05 Mev
18	29 ev	10.100 ev	18	.05 Mev	.01 Mev
19	10.100 ev	3.060 ev			
20	3.060 ev	1.120 ev			
21	1.120 ev	.414 ev			
22	.414 ev	.010 ev			

Table C.4
Neutron/Gamma Spectra for Spent PWR Fuel at Various Decay Times

NEUT. GRP	DISH	120D	150D	1Y	2Y	3Y
1	7.895E-05	8.611E-05	8.808E-05	1.024E-04	1.186E-04	1.240E-04
2	6.681E-04	7.295E-04	7.464E-04	8.690E-04	1.008E-03	1.054E-03
3	1.838E-03	2.007E-03	2.053E-03	2.390E-03	2.772E-03	2.899E-03
4	9.267E-03	1.009E-02	1.032E-02	1.197E-02	1.384E-02	1.446E-02
5	2.300E-02	2.516E-02	2.575E-02	3.007E-02	3.497E-02	3.659E-02
6	5.099E-02	5.070E-02	5.062E-02	5.002E-02	4.934E-02	4.910E-02
7	2.284E-01	2.123E-01	2.078E-01	1.756E-01	1.388E-01	1.266E-01
8	1.685E-01	1.581E-01	1.552E-01	1.344E-01	1.108E-01	1.029E-01
9	3.283E-02	3.157E-02	3.122E-02	2.869E-02	2.582E-02	2.487E-02
10	1.342E-01	1.331E-01	1.328E-01	1.305E-01	1.280E-01	1.272E-01
11	1.642E-01	1.737E-01	1.764E-01	1.954E-01	2.171E-01	2.244E-01
12	1.273E-01	1.386E-01	1.417E-01	1.642E-01	1.899E-01	1.984E-01
13	5.870E-02	6.390E-02	6.533E-02	7.570E-02	8.750E-02	9.141E-02
14	6.537E-06	6.131E-06	6.020E-06	5.210E-06	4.305E-06	4.023E-06
15	0.0	0.0	0.0	0.0	0.0	0.0
16	0.0	0.0	0.0	0.0	0.0	0.0
17	0.0	0.0	0.0	0.0	0.0	0.0
18	0.0	0.0	0.0	0.0	0.0	0.0
19	0.0	0.0	0.0	0.0	0.0	0.0
20	0.0	0.0	0.0	0.0	0.0	0.0
21	0.0	0.0	0.0	0.0	0.0	0.0
22	0.0	0.0	0.0	0.0	0.0	0.0
	1.000E+00	1.000E+00	1.000E+00	1.000E+00	1.000E+00	1.000E+00
GAMMA						
GRP						
1	1.734E-09	1.108E-12	1.219E-12	1.779E-12	2.643E-12	3.821E-12
2	5.885E-06	7.217E-12	7.942E-12	1.160E-11	1.725E-11	2.495E-11
3	5.311E-04	4.421E-11	4.865E-11	7.102E-11	1.055E-10	1.526E-10
4	5.292E-04	4.181E-11	4.600E-11	6.715E-11	9.978E-11	1.443E-10
5	2.135E-03	2.604E-06	2.725E-06	3.519E-06	3.287E-06	2.598E-06
6	4.043E-03	3.893E-05	2.615E-05	2.842E-05	2.630E-05	2.062E-05
7	9.274E-03	1.506E-03	1.604E-03	1.868E-03	1.456E-03	9.644E-04
8	1.347E-02	8.461E-04	6.163E-04	6.642E-04	6.167E-04	5.203E-04
9	2.359E-02	3.708E-03	3.943E-03	5.855E-03	7.705E-03	9.249E-03
10	3.348E-02	6.424E-03	7.092E-03	1.076E-02	1.439E-02	1.748E-02
11	6.647E-02	1.643E-01	1.477E-01	5.986E-02	4.420E-02	4.810E-02
12	6.737E-02	1.328E-01	1.247E-01	9.455E-02	1.068E-01	1.256E-01
13	6.940E-02	8.194E-02	8.262E-02	1.046E-01	1.346E-01	1.592E-01
14	3.639E-02	1.193E-02	1.262E-02	1.535E-02	1.491E-02	1.367E-02
15	6.293E-02	2.266E-02	2.402E-02	2.847E-02	2.693E-02	2.427E-02
16	9.935E-02	6.945E-02	6.810E-02	7.119E-02	6.407E-02	5.474E-02
17	1.121E-01	8.459E-02	8.934E-02	1.049E-01	9.981E-02	9.145E-02
18	3.989E-01	4.198E-01	4.376E-01	5.019E-01	4.845E-01	4.547E-01
	1.000E+00	1.000E+00	1.000E+00	1.000E+00	1.000E+00	1.000E+00

Table C.4 (cont)
Neutron/Gamma Spectra for Spent PWR Fuel at Various Decay Times

NEUT. GRP	4Y	5Y	7Y	8Y	10Y	13Y
1	1.253E-04	1.255E-04	1.255E-04	1.254E-04	1.253E-04	1.251E-04
2	1.065E-03	1.067E-03	1.067E-03	1.067E-03	1.066E-03	1.064E-03
3	2.929E-03	2.935E-03	2.934E-03	2.933E-03	2.930E-03	2.926E-03
4	1.461E-02	1.463E-02	1.463E-02	1.462E-02	1.461E-02	1.459E-02
5	3.698E-02	3.705E-02	3.704E-02	3.703E-02	3.699E-02	3.693E-02
6	4.903E-02	4.900E-02	4.896E-02	4.894E-02	4.890E-02	4.883E-02
7	1.236E-01	1.230E-01	1.228E-01	1.229E-01	1.229E-01	1.230E-01
8	1.010E-01	1.007E-01	1.007E-01	1.008E-01	1.009E-01	1.012E-01
9	2.465E-02	2.461E-02	2.462E-02	2.463E-02	2.466E-02	2.471E-02
10	1.271E-01	1.271E-01	1.272E-01	1.272E-01	1.274E-01	1.275E-01
11	2.261E-01	2.265E-01	2.266E-01	2.266E-01	2.266E-01	2.265E-01
12	2.004E-01	2.008E-01	2.008E-01	2.007E-01	2.006E-01	2.003E-01
13	9.234E-02	9.252E-02	9.251E-02	9.247E-02	9.239E-02	9.226E-02
14	3.975E-06	3.986E-06	4.040E-06	4.069E-06	4.133E-06	4.236E-06
15	0.0	0.0	0.0	0.0	0.0	0.0
16	0.0	0.0	0.0	0.0	0.0	0.0
17	0.0	0.0	0.0	0.0	0.0	0.0
18	0.0	0.0	0.0	0.0	0.0	0.0
19	0.0	0.0	0.0	0.0	0.0	0.0
20	0.0	0.0	0.0	0.0	0.0	0.0
21	0.0	0.0	0.0	0.0	0.0	0.0
22	0.0	0.0	0.0	0.0	0.0	0.0
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	1.000E+00	1.000E+00	1.000E+00	1.000E+00	1.000E+00	1.000E+00
GAMMA GRP						
1	5.069E-12	6.129E-12	7.436E-12	7.774E-12	8.102E-12	8.172E-12
2	3.309E-11	4.002E-11	4.855E-11	5.075E-11	5.291E-11	5.336E-11
3	2.024E-10	2.448E-10	2.970E-10	3.105E-10	3.237E-10	3.265E-10
4	1.913E-10	2.314E-10	2.808E-10	2.935E-10	3.060E-10	3.087E-10
5	1.818E-06	1.150E-06	3.812E-07	2.086E-07	6.021E-08	9.860E-09
6	1.433E-05	9.015E-06	2.961E-06	1.614E-06	4.624E-07	7.421E-08
7	5.681E-04	3.043E-04	7.375E-05	3.487E-05	7.689E-06	8.612E-07
8	4.249E-04	3.492E-04	2.646E-04	2.437E-04	2.189E-04	1.942E-04
9	1.035E-02	1.085E-02	1.046E-02	9.909E-03	8.650E-03	6.873E-03
10	1.971E-02	2.074E-02	2.006E-02	1.901E-02	1.659E-02	1.316E-02
11	4.811E-02	4.414E-02	3.189E-02	2.622E-02	1.771E-02	1.062E-02
12	1.385E-01	1.442E-01	1.430E-01	1.403E-01	1.354E-01	1.314E-01
13	1.779E-01	1.890E-01	1.959E-01	1.960E-01	1.949E-01	1.940E-01
14	1.251E-02	1.162E-02	1.065E-02	1.041E-02	1.012E-02	9.899E-03
15	2.223E-02	2.109E-02	2.072E-02	2.095E-02	2.151E-02	2.208E-02
16	4.777E-02	4.371E-02	4.129E-02	4.128E-02	4.160E-02	4.170E-02
17	8.564E-02	8.304E-02	8.395E-02	8.550E-02	8.853E-02	9.159E-02
18	4.364E-01	4.310E-01	4.418E-01	4.502E-01	4.648E-01	4.785E-01
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	1.000E+00	1.000E+00	1.000E+00	1.000E+00	1.000E+00	1.000E+00

Table C.4 (cont)
Neutron/Gamma Spectra for Spent PWR Fuel at Various Decay Times

NEUT.	15Y	18Y	20Y	23Y	25Y
GRP					
1	1.250E-04	1.247E-04	1.246E-04	1.243E-04	1.241E-04
2	1.063E-03	1.061E-03	1.060E-03	1.057E-03	1.056E-03
3	2.922E-03	2.917E-03	2.913E-03	2.907E-03	2.902E-03
4	1.457E-02	1.454E-02	1.452E-02	1.449E-02	1.447E-02
5	3.689E-02	3.682E-02	3.677E-02	3.669E-02	3.663E-02
6	4.879E-02	4.871E-02	4.866E-02	4.857E-02	4.851E-02
7	1.231E-01	1.232E-01	1.233E-01	1.234E-01	1.235E-01
8	1.013E-01	1.016E-01	1.018E-01	1.022E-01	1.024E-01
9	2.475E-02	2.480E-02	2.484E-02	2.491E-02	2.495E-02
10	1.277E-01	1.279E-01	1.281E-01	1.283E-01	1.285E-01
11	2.265E-01	2.264E-01	2.264E-01	2.263E-01	2.263E-01
12	2.001E-01	1.999E-01	1.997E-01	1.993E-01	1.991E-01
13	9.216E-02	9.201E-02	9.190E-02	9.173E-02	9.160E-02
14	4.309E-06	4.428E-06	4.512E-06	4.648E-06	4.746E-06
15	0.0	0.0	0.0	0.0	0.0
16	0.0	0.0	0.0	0.0	0.0
17	0.0	0.0	0.0	0.0	0.0
18	0.0	0.0	0.0	0.0	0.0
19	0.0	0.0	0.0	0.0	0.0
20	0.0	0.0	0.0	0.0	0.0
21	0.0	0.0	0.0	0.0	0.0
22	0.0	0.0	0.0	0.0	0.0
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	1.000E+00	1.000E+00	1.000E+00	1.000E+00	1.000E+00
GAMMA					
GRP					
1	8.096E-12	7.902E-12	7.750E-12	7.503E-12	7.337E-12
2	5.287E-11	5.161E-11	5.060E-11	4.899E-11	4.793E-11
3	3.235E-10	3.157E-10	3.097E-10	2.999E-10	2.933E-10
4	3.059E-10	2.985E-10	2.928E-10	2.836E-10	2.773E-10
5	3.699E-09	1.711E-09	1.454E-09	1.339E-09	1.301E-09
6	2.740E-08	1.296E-08	1.151E-08	1.139E-08	1.160E-08
7	2.397E-07	6.600E-08	4.329E-08	3.079E-08	2.622E-08
8	1.799E-04	1.600E-04	1.480E-04	1.318E-04	1.223E-04
9	5.874E-03	4.652E-03	3.996E-03	3.200E-03	2.774E-03
10	1.124E-02	8.885E-03	7.622E-03	6.092E-03	5.273E-03
11	8.136E-03	6.061E-03	5.254E-03	4.455E-03	4.078E-03
12	1.302E-01	1.297E-01	1.297E-01	1.299E-01	1.300E-01
13	1.941E-01	1.948E-01	1.954E-01	1.963E-01	1.968E-01
14	9.812E-03	9.734E-03	9.706E-03	9.676E-03	9.669E-03
15	2.230E-02	2.245E-02	2.250E-02	2.251E-02	2.251E-02
16	4.146E-02	4.094E-02	4.056E-02	3.995E-02	3.960E-02
17	9.288E-02	9.417E-02	9.478E-02	9.550E-02	9.596E-02
18	4.838E-01	4.884E-01	4.903E-01	4.923E-01	4.933E-01
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	1.000E+00	1.000E+00	1.000E+00	1.000E+00	1.000E+00

Table C.5
Neutron/Gamma Spectra for Spent BWR Fuel at Various Decay Times

NEUT. GRP	DISH	120D	150D	1Y	2Y	3Y
1	7.535E-05	8.176E-05	8.357E-05	9.782E-05	1.164E-04	1.233E-04
2	6.372E-04	6.922E-04	7.077E-04	8.300E-04	9.894E-04	1.048E-03
3	1.753E-03	1.904E-03	1.947E-03	2.283E-03	2.721E-03	2.882E-03
4	8.852E-03	9.590E-03	9.799E-03	1.144E-02	1.358E-02	1.437E-02
5	2.191E-02	2.385E-02	2.439E-02	2.869E-02	3.431E-02	3.637E-02
6	5.113E-02	5.085E-02	5.078E-02	5.017E-02	4.937E-02	4.906E-02
7	2.365E-01	2.220E-01	2.179E-01	1.856E-01	1.435E-01	1.279E-01
8	1.738E-01	1.644E-01	1.618E-01	1.410E-01	1.139E-01	1.040E-01
9	3.347E-02	3.234E-02	3.202E-02	2.950E-02	2.622E-02	2.501E-02
10	1.348E-01	1.338E-01	1.335E-01	1.313E-01	1.285E-01	1.275E-01
11	1.594E-01	1.680E-01	1.704E-01	1.895E-01	2.144E-01	2.236E-01
12	1.216E-01	1.317E-01	1.346E-01	1.571E-01	1.864E-01	1.973E-01
13	5.609E-02	6.074E-02	6.206E-02	7.241E-02	8.591E-02	9.088E-02
14	6.762E-06	6.406E-06	6.307E-06	5.518E-06	4.503E-06	4.148E-06
15	0.0	0.0	0.0	0.0	0.0	0.0
16	0.0	0.0	0.0	0.0	0.0	0.0
17	0.0	0.0	0.0	0.0	0.0	0.0
18	0.0	0.0	0.0	0.0	0.0	0.0
19	0.0	0.0	0.0	0.0	0.0	0.0
20	0.0	0.0	0.0	0.0	0.0	0.0
21	0.0	0.0	0.0	0.0	0.0	0.0
22	0.0	0.0	0.0	0.0	0.0	0.0
	1.000E+00	1.000E+00	1.000E+00	1.000E+00	1.000E+00	1.000E+00
GAMMA						
GRP						
1	2.126E-09	1.173E-12	1.275E-12	1.735E-12	2.418E-12	3.394E-12
2	6.104E-06	7.639E-12	8.305E-12	1.131E-11	1.578E-11	2.217E-11
3	5.234E-04	4.681E-11	5.090E-11	6.926E-11	9.653E-11	1.355E-10
4	5.214E-04	4.426E-11	4.813E-11	6.549E-11	9.127E-11	1.282E-10
5	2.105E-03	2.686E-06	2.813E-06	3.579E-06	3.297E-06	2.566E-06
6	3.986E-03	3.860E-05	2.656E-05	2.884E-05	2.632E-05	2.033E-05
7	9.138E-03	1.479E-03	1.575E-03	1.809E-03	1.391E-03	9.088E-04
8	1.242E-02	8.300E-04	6.146E-04	6.655E-04	6.145E-04	5.154E-04
9	2.240E-02	3.333E-03	3.494E-03	4.886E-03	6.046E-03	6.898E-03
10	3.214E-02	5.713E-03	6.220E-03	8.876E-03	1.117E-02	1.292E-02
11	5.993E-02	1.621E-01	1.452E-01	5.822E-02	4.256E-02	4.570E-02
12	6.384E-02	1.324E-01	1.244E-01	9.517E-02	1.076E-01	1.261E-01
13	6.881E-02	8.312E-02	8.410E-02	1.069E-01	1.371E-01	1.618E-01
14	3.597E-02	1.215E-02	1.285E-02	1.557E-02	1.519E-02	1.403E-02
15	6.358E-02	2.296E-02	2.427E-02	2.851E-02	2.696E-02	2.440E-02
16	1.008E-01	6.872E-02	6.748E-02	7.043E-02	6.334E-02	5.434E-02
17	1.149E-01	8.511E-02	8.981E-02	1.050E-01	1.000E-01	9.208E-02
18	4.090E-01	4.220E-01	4.400E-01	5.040E-01	4.879E-01	4.603E-01
	1.000E+00	1.000E+00	1.000E+00	1.000E+00	1.000E+00	1.000E+00

Table C.5 (cont)
Neutron/Gamma Spectra for Spent BWR Fuel at Various Decay Times

NEUT. GRP	4Y	5Y	7Y	8Y	10Y	13Y
1	1.249E-04	1.253E-04	1.253E-04	1.252E-04	1.251E-04	1.248E-04
2	1.063E-03	1.065E-03	1.065E-03	1.065E-03	1.064E-03	1.062E-03
3	2.922E-03	2.930E-03	2.929E-03	2.928E-03	2.924E-03	2.919E-03
4	1.457E-02	1.461E-02	1.460E-02	1.460E-02	1.458E-02	1.455E-02
5	3.688E-02	3.698E-02	3.698E-02	3.696E-02	3.692E-02	3.685E-02
6	4.897E-02	4.893E-02	4.889E-02	4.887E-02	4.882E-02	4.875E-02
7	1.240E-01	1.231E-01	1.230E-01	1.230E-01	1.231E-01	1.232E-01
8	1.015E-01	1.010E-01	1.010E-01	1.011E-01	1.012E-01	1.015E-01
9	2.472E-02	2.466E-02	2.468E-02	2.469E-02	2.472E-02	2.478E-02
10	1.273E-01	1.273E-01	1.274E-01	1.275E-01	1.276E-01	1.278E-01
11	2.259E-01	2.264E-01	2.265E-01	2.265E-01	2.265E-01	2.264E-01
12	1.999E-01	2.005E-01	2.005E-01	2.004E-01	2.003E-01	2.000E-01
13	9.211E-02	9.236E-02	9.235E-02	9.231E-02	9.222E-02	9.207E-02
14	4.084E-06	4.092E-06	4.152E-06	4.185E-06	4.255E-06	4.370E-06
15	0.0	0.0	0.0	0.0	0.0	0.0
16	0.0	0.0	0.0	0.0	0.0	0.0
17	0.0	0.0	0.0	0.0	0.0	0.0
18	0.0	0.0	0.0	0.0	0.0	0.0
19	0.0	0.0	0.0	0.0	0.0	0.0
20	0.0	0.0	0.0	0.0	0.0	0.0
21	0.0	0.0	0.0	0.0	0.0	0.0
22	0.0	0.0	0.0	0.0	0.0	0.0
	1.000E+00	1.000E+00	1.000E+00	1.000E+00	1.000E+00	1.000E+00
GAMMA GRP						
1	4.421E-12	5.276E-12	6.297E-12	6.553E-12	6.781E-12	6.808E-12
2	2.888E-11	3.446E-11	4.113E-11	4.278E-11	4.430E-11	4.446E-11
3	1.765E-10	2.108E-10	2.516E-10	2.617E-10	2.711E-10	2.721E-10
4	1.669E-10	1.993E-10	2.379E-10	2.474E-10	2.563E-10	2.573E-10
5	1.769E-06	1.105E-06	3.600E-07	1.959E-07	5.606E-08	9.012E-09
6	1.392E-05	8.653E-06	2.796E-06	1.517E-06	4.318E-07	6.932E-08
7	5.282E-04	2.799E-04	6.690E-05	3.154E-05	6.914E-06	7.537E-07
8	4.192E-04	3.444E-04	2.610E-04	2.403E-04	2.153E-04	1.906E-04
9	7.419E-03	7.562E-03	7.091E-03	6.693E-03	5.856E-03	4.733E-03
10	1.402E-02	1.437E-02	1.353E-02	1.277E-02	1.117E-02	9.014E-03
11	4.512E-02	4.099E-02	2.934E-02	2.413E-02	1.638E-02	9.972E-03
12	1.383E-01	1.438E-01	1.427E-01	1.403E-01	1.358E-01	1.321E-01
13	1.801E-01	1.909E-01	1.976E-01	1.977E-01	1.966E-01	1.957E-01
14	1.291E-02	1.204E-02	1.100E-02	1.071E-02	1.033E-02	1.001E-02
15	2.249E-02	2.143E-02	2.108E-02	2.127E-02	2.174E-02	2.222E-02
16	4.775E-02	4.398E-02	4.173E-02	4.167E-02	4.185E-02	4.177E-02
17	8.674E-02	8.447E-02	8.548E-02	8.694E-02	8.967E-02	9.240E-02
18	4.441E-01	4.398E-01	4.501E-01	4.576E-01	4.703E-01	4.819E-01
	1.000E+00	1.000E+00	1.000E+00	1.000E+00	1.000E+00	1.000E+00

Table C.5 (cont)
Neutron/Gamma Spectra for Spent BWR Fuel at Various Decay Times

NEUT. GRP	15Y	18Y	20Y	23Y	25Y
1	1.247E-04	1.244E-04	1.242E-04	1.240E-04	1.237E-04
2	1.060E-03	1.058E-03	1.057E-03	1.054E-03	1.052E-03
3	2.916E-03	2.910E-03	2.905E-03	2.899E-03	2.894E-03
4	1.454E-02	1.451E-02	1.449E-02	1.445E-02	1.443E-02
5	3.680E-02	3.673E-02	3.667E-02	3.659E-02	3.652E-02
6	4.870E-02	4.862E-02	4.856E-02	4.847E-02	4.840E-02
7	1.232E-01	1.234E-01	1.234E-01	1.236E-01	1.237E-01
8	1.017E-01	1.020E-01	1.022E-01	1.026E-01	1.029E-01
9	2.481E-02	2.488E-02	2.492E-02	2.499E-02	2.504E-02
10	1.279E-01	1.282E-01	1.284E-01	1.286E-01	1.288E-01
11	2.264E-01	2.263E-01	2.263E-01	2.262E-01	2.262E-01
12	1.998E-01	1.995E-01	1.992E-01	1.989E-01	1.986E-01
13	9.197E-02	9.180E-02	9.168E-02	9.148E-02	9.134E-02
14	4.450E-06	4.582E-06	4.675E-06	4.825E-06	4.935E-06
15	0.0	0.0	0.0	0.0	0.0
16	0.0	0.0	0.0	0.0	0.0
17	0.0	0.0	0.0	0.0	0.0
18	0.0	0.0	0.0	0.0	0.0
19	0.0	0.0	0.0	0.0	0.0
20	0.0	0.0	0.0	0.0	0.0
21	0.0	0.0	0.0	0.0	0.0
22	0.0	0.0	0.0	0.0	0.0
	1.000E+00	1.000E+00	1.000E+00	1.000E+00	1.000E+00
GAMMA GRP					
1	6.733E-12	6.563E-12	6.433E-12	6.230E-12	6.094E-12
2	4.397E-11	4.287E-11	4.202E-11	4.070E-11	3.981E-11
3	2.691E-10	2.624E-10	2.572E-10	2.492E-10	2.438E-10
4	2.544E-10	2.481E-10	2.432E-10	2.357E-10	2.306E-10
5	3.291E-09	1.451E-09	1.215E-09	1.114E-09	1.082E-09
6	2.582E-08	1.245E-08	1.112E-08	1.103E-08	1.126E-08
7	1.962E-07	4.432E-08	2.665E-08	1.885E-08	1.652E-08
8	1.762E-04	1.565E-04	1.446E-04	1.289E-04	1.196E-04
9	4.109E-03	3.345E-03	2.929E-03	2.416E-03	2.135E-03
10	7.815E-03	6.350E-03	5.553E-03	4.572E-03	4.035E-03
11	7.729E-03	5.834E-03	5.087E-03	4.339E-03	3.980E-03
12	1.310E-01	1.305E-01	1.304E-01	1.305E-01	1.306E-01
13	1.957E-01	1.962E-01	1.967E-01	1.973E-01	1.977E-01
14	9.887E-03	9.768E-03	9.725E-03	9.685E-03	9.668E-03
15	2.238E-02	2.248E-02	2.250E-02	2.250E-02	2.248E-02
16	4.146E-02	4.087E-02	4.045E-02	3.985E-02	3.948E-02
17	9.353E-02	9.466E-02	9.522E-02	9.594E-02	9.637E-02
18	4.862E-01	4.898E-01	4.913E-01	4.928E-01	4.934E-01
	1.000E+00	1.000E+00	1.000E+00	1.000E+00	1.000E+00

APPENDIX D

NEUTRON AND GAMMA SHIELDING REQUIREMENTS AND SUBSEQUENT n/γ DOSE RATES
AS A FUNCTION OF THE INITIAL DESIGN POINT (D_1^i) FOR VARIOUS PB, FE,
AND U-METAL CASKS CONTAINING 1, 3, AND 10-YEAR-OLD PWR FUEL

The purpose of this conceptual design study was to develop a new generation of lead, iron, and uranium-metal shipping casks which have been optimized for the shipment of 1,2,3,5,7 and 10-year-old PWR spent fuel assemblies. A necessary and very important part of this study, therefore, was the accurate determination of the neutron and gamma shielding requirements* for each type of cask as a function of the age of the fuel and the number of assemblies in the cask. Prior to that, however, it was necessary to establish some criteria for determining the relative amounts of neutron and gamma shielding that should be used in each case so as to minimize the overall weight of the loaded cask while reducing the total dose rate down to 10 mrem/hr at a point 10 feet from the centerline. To determine the optimal n/γ split [or the optimal initial design point, D_1^i], a number of zone-width search calculations were performed following the procedure described in Section IV.B.1. Each of these search calculations required a series of 1-D multigroup S_8P_3 shielding calculations to determine the flux distribution throughout the cask and the resulting dose rates 10 feet from the centerline. Indeed, for each type of cask [Pb/water,

* By volume, the neutron shield consisted of 28.5% water (1.0 g/cc), 66.0% ethylene glycol (1.11 g/cc; $\text{HOCH}_2\text{CH}_2\text{OH}$), and 5.5% potassium tetraborate (1.74 g/cc; $\text{K}_2\text{B}_4\text{O}_7 \cdot 8\text{H}_2\text{O}$) made with natural boron. By weight, this common mixture of water and antifreeze contains ~1% boron. While Tables D.1-D.9 refer to the neutron shield simply as "water", it was really this mixture of borated water and antifreeze that was used in the various shielding calculations.

Fe/water, U-metal/water], we performed a series of zone-width search calculations -- each time changing the initial design point (D_Y'), determining the corresponding shielding requirements, and noting the effect of those dimensions on the overall weight of the loaded cask.* Tables D.1 through D.9 show the resulting neutron and gamma shielding requirements and subsequent n/γ dose rates as a function of the initial design point (D_Y') for various Pb, Fe, and U-metal casks containing 1, 3, and 10-year-old PWR fuel. The results for the Pb/water cask containing thirteen 1-year-old PWR fuel assemblies might, for example, be interpreted in the following manner:

*Assuming no neutron source, no neutron shield, and no outside liner, the dose rate 10 feet from the centerline could be reduced to 20 mr/hr by using 6.14 inches of gamma shielding between the inner and outer shell. Given that amount of gamma shielding, and now accounting for the neutron shield and outside liner as well as the presence of both the neutron and gamma sources in the fuel, the dose rate 10 feet from the centerline could be reduced to 10 mr/hr by using 3.34 inches of neutron shielding between the outer shell and the outside liner. The total mass of the loaded cask (assuming the assemblies to be 12.0 feet long) would then be 69.61 metric

* Each cask was assumed to consist of an inner shell, a gamma shield, an outer shell, a neutron shield, and an outside liner as shown in Fig. IV.3. While the dimensions of the neutron and gamma shields were allowed to vary as shown in Tables D.1 through D.9, the thicknesses of the other components were assumed to depend only on the type of cask, as noted in Table II.1. In each case, the dry spent fuel and the (1" thick) aluminum insert were modeled as a large homogenized region on the interior of the cask. The radius of that region (R_1) varied with the number of fuel assemblies, as shown in Tables D.1 through D.9.

tonnes. Ten feet from the centerline, the total gamma dose rate (including primary and secondary gammas) would be 7.95 mr/hr, while the total neutron dose rate would be 2.10 mr/hr. Should the region containing the neutron shield ever be rendered void, the total gamma dose rate 10 feet from the centerline would rise to 11.4 mr/hr while the total neutron dose rate there would rise to 52.9 mr/hr."

Table D.1
 Alternate Sets of Shielding Thicknesses and Subsequent n/γ Dose Rates for a Pb
 Cask Containing 13 1-Year-Old PWR Fuel Assemblies ($R_1 = 58.6$ cm; $L_1 = 12$ ft)

INITIAL DESIGN POINT (MR/HR)	RESULTING GAMMA SHLD THICKNESS (INCHES)	FINAL DESIGN POINT (MR/HR)	RESULTING NEUT. SHLD THICKNESS (INCHES)	TOTAL MASS OF LOADED CASK IN MET. TONNES	NOMINAL GAMMA DOSE 10-FT FROM CENTR-LINE (MR/HR)	NOMINAL NEUT. DOSE 10-FT FROM CENTR-LINE (MR/HR)	ACCIDENTAL GAMMA DOSE 10-FT FROM CENTR-LINE (MR/HR)	ACCIDENTAL NEUT. DOSE 10-FT FROM CENTR-LINE (MR/HR)
5	7.21	10	1.80	76.33	2.69	7.31	2.99	49.8
7	6.95	10	1.96	74.45	3.54	6.43	4.12	50.5
12	6.54	10	2.39	71.65	5.49	4.54	6.93	51.8
20	6.14	10	3.34	69.61	7.95	2.10	11.4	52.9
25	5.97	10	4.11	69.13	8.93	1.17	14.2	53.5
30	5.83	10	5.15	69.21	9.40	0.565	17.0	53.9
37	5.67	10	6.52	69.53	9.82	0.242	20.9	54.4
45	5.52	10	7.98	70.13	9.90	0.109	25.4	54.9
75	5.13	10	11.98	72.31	10.01	0.0173	42.3	56.1
110	4.84	10	15.02	74.44	10.01	0.0052	61.8	57.1
150	4.61	10	17.45	76.39	10.00	0.0022	84.1	57.9

Table D.2
Alternate Sets of Shielding Thicknesses and Subsequent n/γ Dose Rates for an Fe
Cask Containing 13 1-Year-Old PWR Fuel Assemblies ($R_1 = 58.6$ cm; $L_1 = 12$ ft)

INITIAL DESIGN POINT (MR/HR)	RESULTING GAMMA SHLD THICKNESS (INCHES)	FINAL DESIGN POINT (MR/HR)	RESULTING NEUT. SHLD THICKNESS (INCHES)	TOTAL MASS OF LOADED CASK IN MET. TONNES	NOMINAL GAMMA DOSE 10-FT FROM CENTR-LINE (MR/HR)	NOMINAL NEUT. DOSE 10-FT FROM CENTR-LINE (MR/HR)	ACCIDENTAL GAMMA DOSE 10-FT FROM CENTR-LINE (MR/HR)	ACCIDENTAL NEUT. DOSE 10-FT FROM CENTR-LINE (MR/HR)
5	14.46	10	0.38	89.10	2.71	7.24	2.78	12.4
9	13.75	10	0.72	84.73	4.62	5.36	4.93	13.9
13	13.31	10	1.10	82.28	6.29	3.71	7.10	14.9
20	12.80	10	2.00	80.04	8.49	1.49	10.8	16.2
25	12.54	10	2.85	79.34	9.43	0.631	13.4	16.8
30	12.32	10	3.94	79.26	9.81	0.219	16.1	17.4
37	12.08	10	5.41	79.51	9.98	0.0608	19.8	18.1
50	11.72	10	7.69	80.24	10.00	0.0124	26.6	19.2
85	11.10	10	11.70	81.98	10.00	0.0019	44.9	21.1
150	10.45	10	15.91	84.46	10.00	0.0004	78.6	23.4
300	9.66	10	20.91	88.26	9.98	0.0001	155.3	26.5

Table D.3
 Alternate Sets of Shielding Thicknesses and Subsequent n/y Dose Rates for a U-metal
 Cask Containing 13 1-Year-Old PWR Fuel Assemblies ($R_1 = 58.6$ cm; $L_1 = 12$ ft)

INITIAL DESIGN POINT (MR/HR)	RESULTING GAMMA SHLD THICKNESS (INCHES)	FINAL DESIGN POINT (MR/HR)	RESULTING NEUT. SHLD THICKNESS (INCHES)	TOTAL MASS OF LOADED CASK IN MET. TONNES	NOMINAL GAMMA DOSE 10-FT FROM CENTR-LINE (MR/HR)	NOMINAL NEUT. DOSE 10-FT FROM CENTR-LINE (MR/HR)	ACCIDENTAL GAMMA DOSE 10-FT FROM CENTR-LINE (MR/HR)	ACCIDENTAL NEUT. DOSE 10-FT FROM CENTR-LINE (MR/HR)
3	4.45	10	1.03	67.26	1.72	8.36	1.79	25.1
5	4.23	10	1.24	65.05	2.71	7.36	2.94	26.8
10	3.93	10	1.75	62.31	4.94	5.04	5.82	29.2
16	3.72	10	2.50	60.83	7.17	2.82	9.27	31.0
20	3.63	10	3.15	60.40	8.26	1.74	11.6	31.9
25	3.53	10	4.18	60.36	9.13	0.857	14.4	32.8
30	3.45	10	5.30	60.63	9.57	0.436	17.2	33.5
37	3.36	10	6.84	61.27	9.81	0.196	21.2	34.4
45	3.28	10	8.31	61.96	9.96	0.102	25.8	35.3
70	3.09	10	11.93	64.20	10.04	0.0247	40.0	37.2
90	2.98	10	14.05	65.76	9.98	0.0116	52.0	38.5
150	2.76	10	18.09	69.22	10.01	0.0031	85.8	40.9

Table D.4
 Alternate Sets of Shielding Thicknesses and Subsequent n/γ Dose Rates for a Pb
 Cask Containing 15 3-Year-Old PWR Fuel Assemblies ($R_1 = 62.8$ cm; $L_1 = 12$ ft)

INITIAL DESIGN POINT (MR/HR)	RESULTING GAMMA SHLD THICKNESS (INCHES)	FINAL DESIGN POINT (MR/HR)	RESULTING NEUT. SHLD THICKNESS (INCHES)	TOTAL MASS OF LOADED CASK IN MET. TONNES	NOMINAL GAMMA DOSE 10-FT FROM CENTR-LINE (MR/HR)	NOMINAL NEUT. DOSE 10-FT FROM CENTR-LINE (MR/HR)	ACCIDENTAL GAMMA DOSE 10-FT FROM CENTR-LINE (MR/HR)	ACCIDENTAL NEUT. DOSE 10-FT FROM CENTR-LINE (MR/HR)
7	6.00	10	1.58	72.26	3.46	6.57	3.96	35.3
13	5.54	10	2.11	69.26	5.76	4.21	7.23	36.2
19	5.27	10	2.78	67.81	7.62	2.40	10.5	36.8
23	5.13	10	3.36	67.35	8.53	1.52	12.7	37.1
28	4.99	10	4.21	67.18	9.25	0.795	15.4	37.5
34	4.85	10	5.34	67.36	9.68	0.369	18.7	37.8
41	4.72	10	6.61	67.78	9.82	0.172	22.4	38.0
48	4.60	10	7.76	68.29	9.90	0.0928	26.2	38.3
59	4.46	10	9.22	68.97	10.02	0.0459	32.1	38.7
70	4.33	10	10.52	69.71	10.02	0.0259	38.1	38.9
130	3.90	10	15.11	72.83	9.99	0.0044	70.3	40.0

Table D.5
Alternate Sets of Shielding Thicknesses and Subsequent n/γ Dose Rates for an Fe
Cask Containing 15 3-Year-Old PWR Fuel Assemblies ($R_1 = 62.8$ cm; $L_1 = 12$ ft)

INITIAL DESIGN POINT (MR/HR)	RESULTING GAMMA SHLD THICKNESS (INCHES)	FINAL DESIGN POINT (MR/HR)	RESULTING NEUT. SHLD THICKNESS (INCHES)	TOTAL MASS OF LOADED CASK IN MET. TONNES	NOMINAL GAMMA DOSE 10-FT FROM CENTR-LINE (MR/HR)	NOMINAL NEUT. DOSE 10-FT FROM CENTR-LINE (MR/HR)	ACCIDENTAL GAMMA DOSE 10-FT FROM CENTR-LINE (MR/HR)	ACCIDENTAL NEUT. DOSE 10-FT FROM CENTR-LINE (MR/HR)
8	12.45	10	0.47	81.99	4.03	5.94	4.22	11.2
12	12.00	10	0.81	79.41	5.70	4.28	6.27	12.1
19	11.49	10	1.55	76.99	8.00	2.05	9.78	13.1
23	11.28	10	2.14	76.32	8.93	1.13	11.8	13.5
27	11.10	10	2.84	76.01	9.48	0.565	13.8	13.9
32	10.92	10	3.79	75.95	9.80	0.232	16.3	14.3
39	10.70	10	5.09	76.15	9.94	0.0766	19.8	14.8
45	10.54	10	6.08	76.41	9.97	0.0368	22.8	15.2
56	10.30	10	7.61	76.91	10.00	0.0142	28.3	15.8
75	9.99	10	9.66	77.73	9.98	0.0052	37.8	16.6
140	9.32	10	13.96	79.90	9.99	0.0011	69.7	18.5
300	8.51	10	19.08	83.39	9.99	0.0003	147.6	21.0

Table D.6
Alternate Sets of Shielding Thicknesses and Subsequent n/γ Dose Rates for a U-metal
Cask Containing 15 3-Year-Old PWR Fuel Assemblies ($R_1 = 62.8$ cm; $L_1 = 12$ ft)

INITIAL DESIGN POINT (MR/HR)	RESULTING GAMMA SHLD THICKNESS (INCHES)	FINAL DESIGN POINT (MR/HR)	RESULTING NEUT. SHLD THICKNESS (INCHES)	TOTAL MASS OF LOADED CASK IN MET. TONNES	NOMINAL GAMMA DOSE 10-FT FROM CENTR-LINE (MR/HR)	NOMINAL NEUT. DOSE 10-FT FROM CENTR-LINE (MR/HR)	ACCIDENTAL GAMMA DOSE 10-FT FROM CENTR-LINE (MR/HR)	ACCIDENTAL NEUT. DOSE 10-FT FROM CENTR-LINE (MR/HR)
3	3.88	10	0.79	66.34	1.68	8.32	1.74	19.5
6	3.59	10	1.07	63.42	3.13	6.88	3.42	21.2
11	3.34	10	1.57	61.14	5.30	4.70	6.21	22.8
16	3.19	10	2.20	60.05	7.07	2.84	8.97	23.8
20	3.10	10	2.76	59.58	8.22	1.85	11.2	24.4
24	3.02	10	3.57	59.49	9.03	1.03	13.6	25.0
30	2.93	10	4.82	59.74	9.62	0.461	17.0	25.6
37	2.85	10	6.34	60.39	9.78	0.202	20.8	26.3
46	2.76	10	7.88	61.13	9.96	0.0993	25.8	26.9
80	2.55	10	12.04	63.74	9.98	0.0196	44.4	28.7
130	2.36	10	15.68	66.59	9.99	0.0057	71.6	30.3

Table D.7
Alternate Sets of Shielding Thicknesses and Subsequent n/γ Dose Rates for a Pb
Cask Containing 21 10-Year-Old PWR Fuel Assemblies ($R_1 = 71.8$ cm; $L_1 = 12$ ft)

INITIAL DESIGN POINT (MR/HR)	RESULTING GAMMA SHLD THICKNESS (INCHES)	FINAL DESIGN POINT (MR/HR)	RESULTING NEUT. SHLD THICKNESS (INCHES)	TOTAL MASS OF LOADED CASK IN MET. TONNES	NOMINAL GAMMA DOSE 10-FT FROM CENTR-LINE (MR/HR)	NOMINAL NEUT. DOSE 10-FT FROM CENTR-LINE (MR/HR)	ACCIDENTAL GAMMA DOSE 10-FT FROM CENTR-LINE (MR/HR)	ACCIDENTAL NEUT. DOSE 10-FT FROM CENTR-LINE (MR/HR)
5	5.15	10	1.48	77.21	2.43	7.66	2.71	36.4
8	4.84	10	1.70	74.78	3.59	6.40	4.26	37.1
13	4.52	10	2.10	72.50	5.38	4.62	6.88	37.8
20	4.24	10	2.80	70.89	7.36	2.62	10.5	38.5
25	4.09	10	3.40	70.34	8.41	1.63	13.1	38.9
30	3.97	10	4.13	70.18	9.07	0.964	15.7	39.2
37	3.83	10	5.25	70.34	9.55	0.460	19.3	39.5
45	3.71	10	6.43	70.73	9.78	0.229	23.4	39.8
70	3.42	10	9.38	72.13	9.99	0.0537	36.3	40.6
150	2.92	10	14.62	75.64	9.98	0.0069	77.7	41.9

Table D.8
Alternate Sets of Shielding Thicknesses and Subsequent n/γ Dose Rates for an Fe
Cask Containing 21 10-Year-Old PWR Fuel Assemblies ($R_1 = 71.8$ cm; $L_1 = 12$ ft)

INITIAL DESIGN POINT (MR/HR)	RESULTING GAMMA SHLD THICKNESS (INCHES)	FINAL DESIGN POINT (MR/HR)	RESULTING NEUT. SHLD THICKNESS (INCHES)	TOTAL MASS OF LOADED CASK IN MET. TONNES	NOMINAL GAMMA DOSE 10-FT FROM CENTR-LINE (MR/HR)	NOMINAL NEUT. DOSE 10-FT FROM CENTR-LINE (MR/HR)	ACCIDENTAL GAMMA DOSE 10-FT FROM CENTR-LINE (MR/HR)	ACCIDENTAL NEUT. DOSE 10-FT FROM CENTR-LINE (MR/HR)
5	11.35	10	0.40	87.28	2.40	7.58	2.47	13.1
8	10.87	10	0.63	84.19	3.67	6.29	3.92	14.1
13	10.38	10	1.01	81.28	5.53	4.44	6.33	15.3
20	9.94	10	1.67	79.11	7.67	2.38	9.64	16.4
25	9.72	10	2.25	78.34	8.66	1.35	12.0	17.0
30	9.54	10	2.96	77.96	9.28	0.701	14.4	17.5
40	9.25	10	4.46	77.91	9.84	0.194	19.1	18.3
50	9.02	10	5.84	78.21	9.93	0.0715	23.8	18.9
68	8.71	10	7.79	78.82	9.98	0.0231	32.2	19.9
20	8.43	10	9.59	79.51	10.01	0.0102	42.6	20.8
150	7.93	10	12.83	81.09	9.99	0.0031	70.4	22.6
300	7.24	10	17.10	83.69	10.04	0.0009	139.9	25.2

Table 2.9
Alternate Sets of Shielding Thicknesses and Subsequent n/γ Dose Rates for a U-metal
Cask Containing 21 10-Year-Old PWR Fuel Assemblies ($R_1 = 71.8$ cm; $L_1 = 12$ ft)

INITIAL DESIGN POINT (MR/HR)	RESULTING GAMMA SHLD THICKNESS (INCHES)	FINAL DESIGN POINT (MR/HR)	RESULTING NEUT. SHLD THICKNESS (INCHES)	TOTAL MASS OF LOADED CASK IN MET. TONNES	NOMINAL GAMMA DOSE 10-FT FROM CENTR-LINE (MR/HR)	NOMINAL NEUT. DOSE 10-FT FROM CENTR-LINE (MR/HP)	ACCIDENTAL GAMMA DOSE 10-FT FROM CENTR-LINE (MR/HR)	ACCIDENTAL NEUT. DOSE 10-FT FROM CENTR-LINE (MR/HR)
5	3.04	10	1.14	68.23	2.48	7.59	2.74	24.5
8	2.86	10	1.40	66.40	3.75	6.29	4.34	25.8
12	2.72	10	1.78	64.97	5.25	4.72	6.45	26.9
15	2.64	10	2.09	64.29	6.27	3.73	8.05	27.6
20	2.53	10	2.67	63.62	7.66	2.40	10.7	28.4
25	2.45	10	3.42	63.41	8.62	1.40	13.3	29.1
30	2.39	10	4.27	63.52	9.14	0.801	15.9	29.6
37	2.31	10	5.42	63.85	9.67	0.413	19.7	30.3
50	2.20	10	7.38	64.77	9.91	0.158	26.5	31.3
80	2.03	10	10.76	66.90	9.97	0.0399	42.8	32.8
150	1.81	10	15.02	70.24	9.98	0.0089	78.8	35.0

APPENDIX E

WEIGHT OF 1-YEAR-OLD STAINLESS-STEEL-FINNED AND COPPER-FINNED PB, FE,
AND U-METAL CASKS AS A FUNCTION OF THE INITIAL DESIGN POINT (D'_Y)

As noted in Section IV.B, it was necessary to establish some criteria for determining the relative amounts of neutron and gamma shielding that should be used so as to minimize the overall weight of the loaded cask while reducing the total dose rate down to 10 mr/hr at a point 10 feet from the centerline. In the case of the 1-year-old spent fuel, we first established the neutron and gamma shielding requirements for each type of cask [Pb/water, Fe/water, U-metal/water] as a function of the initial design point (D'_Y). Given a finless cask design, one could take this data and easily calculate the weight corresponding to each cask as a function of the initial design point, and graphically determine that value (D'_Y) which minimized the overall weight in each case (cf. Fig. IV.5). Unfortunately, because of the high decay heat load associated with the 1-year-old fuel, these casks do require external cooling fins in order to maintain the outside surface below 250°F while dissipating the decay heat to the environment (at 130°F). Indeed, the fins themselves may often add as much as 10-15,000 lbs. to the weight of the cask -- thus markedly changing the weight per unit length. Designs which emphasized the gamma shielding and had lower initial design points ($D'_Y \lesssim 30$ mr/hr) tended to have longer, thicker fins than those (larger diameter) casks which emphasized the neutron shielding. As a consequence, the whole W vs. D'_Y curve would become sharper and shift somewhat to the right if one were to include the weight of the fins resulting from the steady-state heat transfer design analysis.

To assess the impact of the necessary fins on the optimal n/γ split [i.e. on the optimal initial design point (D_Y')], the shielding thicknesses shown in Tables D.1-D.3 of Appendix D were used as input to the SCOPE code⁷ which then generated a series of more realistic designs^{*} corresponding to the description given in Section II. Tables E.1-E.3 show the weight of several 1-year-old stainless-steel-finned and copper-finned Pb, Fe, and U-metal casks as a function of the initial design point. From these results we see that the stainless-steel-finned casks typically have an optimal initial design point that is somewhat higher than the corresponding copper-finned casks. Because the iron cask can accommodate considerably fewer assemblies, the difference there is not as pronounced. For additional remarks, see Section IV.B.5.

^{*} In addition to having optimized external cooling fins as described in Section V.B, these designs also have an internal cavity length of 171.6 inches so as to accommodate a 13.8-ft. PWR fuel assembly while leaving an additional 6-inch space for an internal axial shock absorber.

Table E.1
Actual Weight of Several 1-Year-Old Stainless-Steel Finned and
Copper-Finned Pb Casks as a Function of the Initial Design Pt.

INITIAL DESIGN POINT (MR/HR)	RESULTING GAMMA SHLD THICKNESS (INCHES)	RESULTING NEUT. SHLD THICKNESS (INCHES)	TOTAL WEIGHT OF FINNED CASKS AS FUNCTION OF INITIAL DESIGN POINT					
			STAINLESS-STEEL-FINNED CASKS			COPPER-FINNED CASKS		
			10-ASSYS	11-ASSYS	12-ASSYS	11-ASSYS	12-ASSYS	13-ASSYS
5	7.21	1.80	196.9	215.0	231.2	193.4	194.7	207.2
7	6.95	1.96	192.6	211.8	226.9	188.8	190.1	202.4
12	6.54	2.39	186.7	205.8	221.1	182.2	183.5	195.5
20	6.14	3.34	181.6	200.8	216.3	177.8*	179.1*	190.8*
25	5.97	4.11	179.8	196.6	211.5	177.3*	177.6*	190.3*
30	5.83	5.15	179.5	196.3	208.8	177.1*	178.5*	190.1*
37	5.67	6.52	177.1*	194.6	207.4	179.1	180.5	192.1
45	5.52	7.98	177.0*	193.0*	205.7	181.6	181.9	194.7
75	5.13	11.98	178.9	193.3*	200.9*	189.2	189.5	202.6
110	4.84	15.02	185.1	194.7	202.1*	195.7	197.4	209.4
150	4.61	17.45	190.8	201.7	203.4	203.2	205.0	217.1

- a) Weight of loaded cask given in 1000-lb units (194.5 = 194,500 lbs).
- b) While the shielding requirements shown here are based on a cask containing 13 1-year-old PWR fuel assemblies (cf. Table D.1), the shielding requirements for casks containing 10 or 11 assemblies will be essentially the same (cf. Table IV.18).
- c) The asterisks in each column denote those designs whose overall weight is very close to the minima for the cask in question.

Table E.2
Actual Weight of Several 1-Year-Old Stainless-Steel-Finned and
Copper-Finned Fe Casks as a Function of the Initial Design Pt.

INITIAL DESIGN POINT (MR/HR)	RESULTING GAMMA SHLD THICKNESS (INCHES)	RESULTING NEUT. SHLD THICKNESS (INCHES)	TOTAL WEIGHT OF FINNED CASKS AS FUNCTION OF INITIAL DESIGN POINT					
			STAINLESS-STEEL-FINNED CASKS			COPPER-FINNED CASKS		
			8-ASSYS	9-ASSYS	10-ASSYS	8-ASSYS	9-ASSYS	10-ASSYS
5	14.46	0.38	197.5	212.3	217.5	196.9	210.8	211.1
9	13.75	0.72	188.5	203.1	209.3	187.2	200.6	202.0
13	13.31	1.10	183.3	197.7	204.0	183.0	195.1	196.4
20	12.80	2.00	177.9*	192.1	198.2	177.6*	190.5*	190.8*
25	12.54	2.85	176.8*	190.6*	195.9*	176.5*	189.4*	189.7*
30	12.32	3.94	177.3*	190.1*	195.3*	177.1*	190.0*	190.3*
37	12.08	5.41	177.9*	190.7*	195.4*	178.8	190.7	192.1
50	11.72	7.69	181.1	194.0	195.4*	182.3	194.2	195.7
85	11.10	11.70	188.1	200.1	200.4	189.9	202.0	202.3
150	10.45	15.91	197.0	209.1	209.4	199.5	211.8	212.1
300	9.66	20.91	210.3	222.6	222.9	213.7	226.2	226.5

- a) Weight of loaded cask given in 1000-lb units (194.5 = 194,500 lbs).
- b) While the shielding requirements shown here are based on a cask containing 13 1-year-old PWR fuel assemblies (cf. Table D.2), the shielding requirements for casks containing 8-10 assemblies will be essentially the same (cf. Table IV.18).
- c) The asterisks in each column denote those designs whose overall weight is very close to the minima for the cask in question.

Table E.3
Actual Weight of Several 1-Year-Old Stainless-Steel-Finned and
Copper-Finned U Casks as a Function of the Initial Design Point

INITIAL DESIGN POINT (MR/HR)	RESULTING GAMMA SHLD THICKNESS (INCHES)	RESULTING NEUT. SHLD THICKNESS (INCHES)	TOTAL WEIGHT OF FINNED CASKS AS FUNCTION OF INITIAL DESIGN POINT					
			STAINLESS-STEEL-FINNED CASKS			COPPER-FINNED CASKS		
			10-ASSYS	11-ASSYS	12-ASSYS	13-ASSYS	14-ASSYS	15-ASSYS
3	4.45	1.03	192.1	214.2	242.4	185.2	200.1	201.4
5	4.23	1.24	187.3	209.3	237.7	179.5	194.1	195.4
10	3.93	1.75	180.7	203.8	230.8	172.7	186.8	188.1
16	3.72	2.50	177.1	198.3	222.7	169.3	183.2	184.5
20	3.63	3.15	172.9	193.5	217.5	167.8*	182.6*	184.0*
25	3.53	4.18	170.8	191.5	212.6	168.3*	182.1*	183.5*
30	3.45	5.30	168.8	187.2	206.1	169.0	183.8	185.2
37	3.36	6.84	166.3	186.5	203.7	171.6	185.4	186.9
45	3.28	8.31	165.5	183.2	199.7	174.7	188.6	190.1
70	3.09	11.93	163.2*	179.0*	191.2*	181.2	196.6	196.8
90	2.98	14.05	164.2*	178.4*	190.8*	187.1	201.1	203.0
150	2.76	18.09	173.4	183.8	190.2*	198.5	213.0	214.8

- a) Weight of loaded cask given in 1000-lb units (194.5 = 194,500 lbs).
- b) While the shielding requirements shown here are based on a cask containing 13 1-year-old PWR fuel assemblies (cf. Table D.3), the shielding requirements for casks containing 10-15 assemblies will be essentially the same (cf. Table IV.18).
- c) The asterisks in each column denote those designs whose overall weight is very close to the minima for the cask in question.

APPENDIX F

OPTIMIZED NEUTRON AND GAMMA SHIELDING REQUIREMENTS FOR PB, FE,
AND U-METAL CASKS AS A FUNCTION OF THE SPENT FUEL'S DECAY
TIME AND THE NUMBER OF ASSEMBLIES IN THE CASK

The purpose of this conceptual design study was to develop a new generation of lead, iron, and uranium-metal spent fuel casks which have been optimized for the shipment of 1,2,3,5,7 and 10-year-old PWR spent fuel assemblies. A necessary and very important part of this study was the accurate determination of the neutron and gamma shielding requirements for each type of cask as a function of the spent fuel's decay time and the number of assemblies in the cask. The purpose of this appendix is to present the rather extensive results summarized in Section IV.C. Note that these results were obtained following the procedure outlined in Section IV.B.1, using the optimal design points (D_Y') given in Tables IV.16 and IV.17. In each case, the total dose rate 10 feet from the centerline of the cask was calculated to be 10 mrem/hr. It should also be noted that each cask was assumed to consist of an inner shell, a gamma shield, an outer shell, a neutron shield, and an outside liner as shown in Fig. II.1.* The thickness of the inner shell, outer shell, and outside liner were assumed to depend on the type of cask, as noted in Table II.1. In each case, the dry spent fuel and the (1" thick) aluminum insert were modeled as a large homogenized region on the interior of the cask. The diameter of that region varied with the number of fuel assemblies as shown in Appendix B.

* By volume, the neutron shield consists of 28.5% water (1.0 g/cc), 66.0% ethylene glycol (1.11 g/cc; $\text{HOCH}_2\text{CH}_2\text{OH}$), and 5.5% potassium tetraborate (1.74 g/cc; $\text{K}_2\text{B}_4\text{O}_7 \cdot 8\text{H}_2\text{O}$) made with natural boron. By weight, this common mixture of water and antifreeze contains ~1% boron. While Tables F.1-F.8 refer to the neutron shield simply as "water", it was really this mixture of borated water and antifreeze that was used in the discrete ordinates shielding calculations.

Table F.1
Optimized Neutron and Gamma Shielding Requirements for Pb, Fe, and U-metal Spent Fuel Shipping Casks, as a Function of the Number of 10-Year-Old PWR Assemblies in the Cask

NUMBER ASSYS.	PB CASK		FE CASK		U-METAL CASK	
	G-SHLD	N-SHLD	G-SHLD	N-SHLD	G-SHLD	N-SHLD
1	2.98	3.69	7.90	3.61	1.90	2.61
4	3.39	3.83	8.52	3.66	2.13	2.86
8	3.61	3.93	8.85	3.66	2.25	3.06
12	3.78	4.04	9.10	3.68	2.34	3.26
18	3.90	4.02	9.27	3.68	2.41	3.33
21	3.97	4.13	9.39	3.71	2.45	3.42
26	4.04	4.25	9.49	3.75	2.49	3.47

*Neutron and gamma shield thicknesses are given in inches

Table F.2
Optimized Neutron and Gamma Shielding Requirements for Pb, Fe, and U-metal Spent Fuel Shipping Casks, as a Function of the Number of 7-Year-Old PWR Assemblies in the Cask

NUMBER ASSYS.	PB CASK		FE CASK		U-METAL CASK	
	G-SHLD	N-SHLD	G-SHLD	N-SHLD	G-SHLD	N-SHLD
1	3.28	3.54	8.35	3.49	2.05	2.67
4	3.69	3.70	8.98	3.50	2.28	2.93
8	3.91	3.82	9.31	3.55	2.40	3.14
12	4.08	3.94	9.56	3.59	2.50	3.34
15	4.15	3.95	9.65	3.59	2.53	3.37
18	4.20	4.01	9.73	3.58	2.56	3.40
21	4.27	4.03	9.84	3.62	2.61	3.49
26	4.35	4.16	9.95	3.68	2.65	3.54

*Neutron and gamma shield thicknesses are given in inches

Table F.3
Optimized Neutron and Gamma Shielding Requirements for Pb, Fe, and U-metal Spent Fuel Shipping Casks, as a Function of the Number of 5-Year-Old PWR Assemblies in the Cask

NUMBER ASSYS.	PB CASK		FE CASK		U-METAL CASK	
	G-SHLD	N-SHLD	G-SHLD	N-SHLD	G-SHLD	N-SHLD
1	3.55	3.60	8.79	3.44	2.21	2.54
4	3.97	3.85	9.42	3.48	2.45	2.85
8	4.19	3.95	9.76	3.51	2.58	3.09
12	4.37	4.07	10.02	3.57	2.67	3.30
15	4.43	4.08	10.11	3.56	2.71	3.33
18	4.49	4.07	10.19	3.57	2.74	3.35
21	4.56	4.21	10.30	3.62	2.79	3.45
24	4.60	4.25	10.35	3.65	2.81	3.44

*Neutron and gamma shield thicknesses are given in inches

Table F.4
Optimized Neutron and Gamma Shielding Requirements for Pb, Fe, and U-metal Spent Fuel Shipping Casks, as a Function of the Number of 3-Year-Old PWR Assemblies in the Cask

NUMBER ASSYS.	PB CASK		FE CASK		U-METAL CASK	
	G-SHLD	N-SHLD	G-SHLD	N-SHLD	G-SHLD	N-SHLD
1	4.06	3.72	9.52	3.68	2.49	2.86
4	4.50	3.97	10.19	3.76	2.74	3.18
8	4.74	4.05	10.55	3.74	2.88	3.30
12	4.92	4.19	10.82	3.79	2.99	3.48
15	4.99	4.21	10.92	3.79	3.02	3.57
18	5.05	4.24	10.99	3.82	3.06	3.58
21	5.12	4.34	11.11	3.87	3.11	3.61

*Neutron and gamma shield thicknesses are given in inches

Table F.3
Optimized Neutron and Gamma Shielding Requirements for Pb, Fe, and U-metal Spent Fuel Shipping Casks, as a Function of the Number of 2-Year-Old PWR Assemblies in the Cask

NUMBER ASSYS.	PB CASK		FE CASK		U-METAL CASK	
	G-SHLD	N-SHLD	G-SHLD	N-SHLD	G-SHLD	N-SHLD
1	4.47	3.71	10.11	3.81	2.72	3.10
4	4.93	3.98	10.81	3.86	2.98	3.32
8	5.18	4.06	11.18	3.86	3.13	3.49
10	5.28	4.13	11.33	3.89	3.18	3.58
12	5.37	4.25	11.46	3.92	3.23	3.65
15	5.44	4.24	11.56	3.90	3.27	3.67
18	5.49	4.24	11.64	3.93	3.31	3.67
21	5.58	4.38	11.77	3.93	3.36	3.76

*Neutron and gamma shield thicknesses are given in inches

Table F.6
Optimized Neutron and Gamma Shielding Requirements for Stainless-Steel-Finned
Pb, Fe, and U-metal Spent Fuel Shipping Casks, as a Function of the Number of
1-Year-Old PWR Assemblies in the Cask

NUMBER ASSYS.	PB CASK		FE CASK		U-METAL CASK	
	G-SHLD	N-SHLD	G-SHLD	N-SHLD	G-SHLD	N-SHLD
1	4.58	8.21	10.67	5.45	2.74	8.54
8	5.31	8.04	11.76	5.36	3.16	8.41
10	5.41	8.01	11.92	5.43	3.22	8.38
12	5.50	8.06	12.05	5.42	3.27	8.44
13	5.52	7.98	12.08	5.41	3.28	8.31

*While fins were not explicitly accounted for in the discrete ordinates shielding calculations, the optimal initial design points (D') are slightly different for stainless-steel-finned and copper-finned casks containing 1-year-old spent fuel. See Sect. IV.B.5 and/or Table IV.16 for additional details.

*Neutron and gamma shield thicknesses are given in inches.

Table F.7
Optimized Neutron and Gamma Shielding Requirements for Bare or
Copper-Finned Pb, Fe, and U-metal Spent Fuel Shipping Casks as a
Function of the Number of 1-Year-Old PWR Assemblies in the Cask

NUMBER ASSYS.	PB CASK		FE CASK		U-METAL CASK	
	G-SHLD	N-SHLD	G-SHLD	N-SHLD	G-SHLD	N-SHLD
1	5.00	3.77	10.91	3.83	3.03	2.98
4	5.48	4.02	11.64	3.88	3.30	3.33
8	5.73	4.14	12.01	3.91	3.44	3.52
10	5.83	4.29	12.17	3.94	3.50	3.63
12	5.92	4.38	12.30	3.97	3.56	3.72
13	5.94	4.40	12.32	3.94	3.57	3.72
15	5.99	4.41	12.40	3.96	3.60	3.73

*While fins were not explicitly accounted for in the discrete ordinates shielding calculations, the optimal initial design points (D_Y') are slightly different for stainless-steel-finned and copper-finned casks containing 1-year-old spent fuel. See Sect. IV.B.5 and/or Table IV.16 for additional details.

*Neutron and gamma shield thicknesses are given in inches.

Table F.8
 An Alternate Set of Neutron and Gamma
 Shielding Requirements for a Copper-
 Finned U-metal Spent Fuel Shipping
 Cask, as a Function of the Number of
 1-Year-Old PWR Assemblies in the Cask

NUMBER ASSYS.	U-METAL CASK	
	G-SHLD	N-SHLD
1	2.82	6.8 ^c
10	3.30	6.86
13	3.36	6.84
15	3.39	6.83

*Neutron and gamma shield thicknesses
 are given in inches.

*An initial design pt (D_Y') of 37 mr/hr
 was used in generating this data. The
 total dose 10 feet from the centerline
 is, of course, still 10 mr/hr. Based
 on some very preliminary calculations,
 this alternate set of shielding data
 was once thought to offer some advan-
 tage relative to that shown in Table
 F.7. More detailed calculations have
 since shown this not to be the case.
 See Sect. VII.A for additional details.

APPENDIX G

EFFECTIVE EMISSIVITY OF THE FINS AND THE CASK

G.1 GENERAL COMMENTS ON EMISSIVITY AND EFFECTIVE EMISSIVITY

Given a single hot surface of area A , the heat lost to the environment by radiation is

$$Q_o = A\epsilon\sigma (T_{\text{SURF}}^4 - T_{\text{ENV}}^4) \quad (\text{G.1})$$

where σ is the Stefan-Boltzmann constant and ϵ is the emissivity of the surface. Indeed, ϵ is a known physical property which has been tabulated for many materials. Akin to that is the reflectivity of the surface ($\rho = 1 - \epsilon$), which is defined as (the ratio of) the thermal energy reemitted from a surface per unit of thermal energy incident on the surface.

Given two identical hot surfaces in close proximity to each other as shown in Fig. G.1(b), Q_o units of thermal energy may be radiated from the first surface, but only $Q_o F_{1 \rightarrow \text{ENV}}$ units actually get radiated directly to the environment -- ie, $Q_o F_{1 \rightarrow 2} = Q_o (1 - F_{1 \rightarrow \text{ENV}})$ units strike the opposite plate. $F_{1 \rightarrow 2}$, known in heat transfer as the "view factor", is the average solid angle of surface 2 as seen from a point on surface 1. For our purposes, it is convenient to think of $F_{1 \rightarrow 2}$ and $F_{1 \rightarrow \text{ENV}}$ as the respective "transmission probabilities." The energy incident on surface 2, of course, has a certain probability of being reflected (ρ), and that reflected energy has a certain probability of escaping to the environment. That which does not escape to the environment ($F_{2 \rightarrow 1} = F_{1 \rightarrow 2}$) will strike surface 1, where some of it may again be reflected, etc., etc. Given Q_o units of thermal energy initially radiated from surface 1, Q units will eventually be transferred to the environment, where

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Fig. G.1: Thermal Radiation From (a) a Single Hot Surface and (b) Two Hot Surfaces in Close Proximity to Each Other

$$Q = Q_o F_{1 \rightarrow \text{ENV}} + Q_o F_{1 \rightarrow 2} \rho F_{1 \rightarrow \text{ENV}} + Q_o F_{1 \rightarrow 2}^2 \rho^2 F_{1 \rightarrow \text{ENV}} + \dots \quad (\text{G.2a})$$

$$= Q_o F_{1 \rightarrow \text{ENV}} [1 + x + x^2 + x^3 + \dots] \quad (\text{G.2b})$$

$$= Q_o F_{1 \rightarrow \text{ENV}} \left(\frac{1}{1-x} \right) \quad \text{where} \quad x = F_{1 \rightarrow 2} \rho = (1 - F_{1 \rightarrow \text{ENV}})(1 - \epsilon) \quad (\text{G.2c})$$

$$= Q_o F \left(\frac{1}{1 - (1 - \epsilon)(1 - F)} \right) = A \left(\frac{\epsilon F}{\epsilon + F - \epsilon F} \right) \sigma (T_{\text{SURF}}^4 - T_{\text{ENV}}^4) \quad (\text{G.2d})$$

$$= A \epsilon' \sigma (T_{\text{SURF}}^4 - T_{\text{ENV}}^4) \quad (\text{G.2e})$$

and the effective emissivity (ϵ') is given by

$$\epsilon' = \frac{1}{\frac{1}{F} + \frac{1}{\epsilon} - 1} \quad \text{and} \quad F = F_{1 \rightarrow \text{ENV}} = (1 - F_{1 \rightarrow 2}) \quad (\text{G.3})$$

Note that the effective emissivity of the surface depends on the geometric configuration as well as the actual emissivity of the material(s) involved.

Plotted as a function of F (cf. Fig. G.2), we see that $\epsilon' \rightarrow \epsilon$ when the plates are far apart and don't see each other. More to the point, we see that $\epsilon' \rightarrow F$ as the plates are pushed closer together and $F = F_{1 \rightarrow \text{ENV}} \rightarrow 0$.

This fact is very important and must be properly accounted for when designing the fins for spent fuel shipping casks.

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Fig. G.2: Behavior of the Effective Emissivity as a Function of the View Factor

G.2 VIEW FACTORS OF INTEREST FOR A CASK WITH CIRCUMFERENTIAL FINS

Consider a cylindrical cask with circumferential fins as shown in Fig. G.3. Focusing on a single segment between two fins, as shown in Fig. G.4, let surface 1 denote that surface through which heat must radiate if it is to reach the environment; let surface 2 denote the cask body having emissivity ϵ_c and reflectivity ρ_c ; and let surfaces 3 and 4 represent the two fins (having emissivity ϵ_f and reflectivity ρ_f). Before we can calculate the effective emissivities of the fins and the cask (cf. Section G.3 and G.4), we must first calculate the various view factors. Letting

$$X = b/a, \quad Y = c/2, \quad (G.4a,b)$$

$$A = Y^2 + X^2 - 1 \quad \text{and} \quad B = Y^2 - X^2 + 1, \quad (G.5a,b)$$

Sparrow and Cess³⁷ give $F_{1 \rightarrow 2}$ and $F_{1 \rightarrow 1}$ as

$$F_{1 \rightarrow 2} = \frac{1}{X} - \frac{1}{\pi X} \left\{ \cos^{-1} \left(\frac{Y}{A} \right) - \frac{1}{2Y} \left[\sqrt{(A+2)^2 - (2X)^2} \cos^{-1} \left(\frac{B}{XA} \right) + B \sin^{-1} \left(\frac{1}{X} \right) - \frac{\pi A}{2} \right] \right\} \quad (G.6)$$

and

$$F_{1 \rightarrow 1} = 1 - \frac{1}{X} + \frac{2}{\pi X} \tan^{-1} \left(\frac{2\sqrt{X^2 - 1}}{Y} \right) - \frac{Y}{2\pi X} \left\{ \frac{\sqrt{4X^2 + Y^2}}{Y} \sin^{-1} \left[\frac{4(X^2 - 1) + \frac{Y^2}{X^2}(X^2 - 2)}{Y^2 + 4(X^2 - 1)} \right] - \sin^{-1} \left(\frac{X^2 - 2}{X^2} \right) + \frac{\pi}{2} \left(\frac{\sqrt{4X^2 + Y^2}}{Y} - 1 \right) \right\} \quad (G.7)$$

Note that $F_{1 \rightarrow 1}$ is not zero since it represents a concave surface. Using symmetry and the various reciprocity relationships, the other view factors that we'll need may be written as

Fig. G.3: Overview of a Spent Fuel Shipping Cask with External Cooling Fins

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Fig. G.4: Nomenclature Used in Calculating the Various View Factors for a Cylindrical Cask with Circumferential Fins

$$F_{1 \rightarrow 3} = 0.5 (1.0 - F_{1 \rightarrow 1} - F_{1 \rightarrow 2}) \quad (G.8)$$

$$F_{3 \rightarrow 1} = F_{1 \rightarrow 3} (A_1 / A_3) \quad (G.9)$$

$$F_{2 \rightarrow 1} = F_{1 \rightarrow 2} (A_1 / A_2) \quad (G.10)$$

$$F_{2 \rightarrow 3} = 0.5 (1.0 - F_{2 \rightarrow 1}) \quad (G.11)$$

$$F_{3 \rightarrow 2} = F_{2 \rightarrow 3} (A_2 / A_3) \quad (G.12)$$

$$F_{3 \rightarrow 4} = 1.0 - F_{3 \rightarrow 1} - F_{3 \rightarrow 2} \quad (G.13)$$

G.3 AN ANALYTIC EXPRESSION FOR THE EFFECTIVE EMISSIVITY OF THE FINS AND THE CASK

To derive an expression for the effective emissivity of a fin, consider the thermal energy initially emitted from the surface of an isothermal fin:

$$Q_0 = A_f \epsilon_f \sigma (T_{\text{SURF}}^4 - T_{\text{ENV}}^4) \quad (G.14)$$

In this case, let us consider the energy initially emitted from surface 3 (see Fig. G.4). Some of that energy ($F_{3 \rightarrow 1}$) may escape directly to the environment through surface 1; some of that energy ($F_{3 \rightarrow 2}$) may be incident on the cask (surface 2) where a certain fraction (ρ_c) will be re-emitted; and some of that energy ($F_{3 \rightarrow 4}$) may be incident on the opposite fin (surface 4) where a certain fraction (ρ_f) will also be reemitted. As shown in Figs. G.4 and G.5, each of these surfaces (2 and 4) may reflect part of that incident energy to the environment (1), back to fin 3, or back and forth between each other. As shown in Fig. G.5, the whole process does become rather complicated in the multi-body system. The figure does try to illustrate, however, how the thermal energy initially emitted from fin 3 can escape directly to the environment, or escape to the environment after being reflected one, two, or three times (etc.). Summing over all these terms, one can write an analytic expression for the total heat emitted from fin 3 which ultimately escapes to the environment:

$$Q_{f1n,3} = \left\{ A_f \epsilon_f F_{3+1} + \left[A_f \epsilon_f F_{3+2} \right] \rho_c F_{2+1} + \left[A_f \epsilon_f F_{3+4} \right] \rho_f F_{4+1} \right. \\ + \left. \left[\left[A_f \epsilon_f F_{3+4} \cup_f F_{4+2} \right] \rho_c F_{2+1} + A_f \epsilon_f \left[F_{3+4} \rho_f F_{4+3} + F_{3+2} \rho_c (2F_{2+3}) \right] \rho_f F_{3+1} \right. \right. \\ + \left. \left. \prod \prod \rho_c F_{2+1} + \prod \prod \rho_f F_{3+1} + \dots + \dots \right\} \cap (T_f^4 - T_{ENV}^4) \quad (G.15)$$

Fig. G.5: Tree-Diagram Showing How Thermal Energy Initially Emitted From the Surface of a Pin (3) May be Reflected Multiple Times Before Ultimately Escaping to the Environment (1)

After writing and examining enough terms, one can see by induction that this expression can be written as:

$$Q_{\text{fin},3} = \left\{ \sum_{i=0}^{\infty} (A_i + B_i) \right\} \sigma (T_f^4 - T_{\text{ENV}}^4) = A_f \epsilon'_f \sigma (T_f^4 - T_{\text{ENV}}^4) \quad (\text{G.16})$$

where the effective emissivity of the fin (ϵ'_f) is defined as

$$\epsilon'_f = \left(\frac{1}{A_f} \right) \sum_{i=0}^{\infty} (A_i + B_i) \quad , \quad (\text{G.17})$$

A_0 and B_0 are defined as

$$A_0 = 0 \quad B_0 = A_f \epsilon_f F_{3 \rightarrow 1} \quad , \quad (\text{G.18a,b})$$

and the remaining coefficients ($A_i, B_i, i = 1, 2, \dots, \infty$) are given by the following recursion relations:

$$A_i = a B_{i-1} \quad , \quad a = \rho_c F_{2 \rightarrow 1} \left(\frac{F_{3 \rightarrow 2}}{F_{3 \rightarrow 1}} \right) \quad (\text{G.18c,d})$$

$$B_i = b B_{i-1} + c A_{i-1} \quad , \quad b = \rho_f F_{3 \rightarrow 4} \quad c = 2 \rho_f F_{3 \rightarrow 1} \left(\frac{F_{2 \rightarrow 3}}{F_{2 \rightarrow 1}} \right) \quad (\text{G.18e,f,g})$$

In practice, this series expression for ϵ'_f typically converges to 6 or 7 significant figures after the first eight or nine terms. Note that ϵ'_f depends on $\epsilon_f, \rho_f, \rho_c$, and the actual dimensions of the system as characterized by the various view factors.

Having derived an expression for the effective emissivity of the fins, the corresponding derivation for the effective emissivity of the cask

body is considerably simpler. Obviously some ($F_{2 \rightarrow 1}$) of the energy initially emitted by the cask body will escape directly to the environment through surface 1; some of that energy ($F_{2 \rightarrow 4}$) may be incident on the one fin (surface 4) where a certain fraction (ρ_f) will be reemitted; and some of that energy ($F_{2 \rightarrow 3}$) may be incident on the other fin (surface 3) where a certain fraction (ρ_f) will also be reemitted. By symmetry, the amount incident on the two fins will be equal. For that energy which is reemitted from fin 3 or 4, we may use the same expression as derived above. The total heat emitted from the cask body (surface 2) which ultimately escapes to the environment may then be written as:

$$Q_{\text{cask},2} =$$

$$= A_c \epsilon_c F_{2 \rightarrow 1} \sigma (T_c^4 - T_{\text{ENV}}^4) + \left\{ \left[A_c \epsilon_c 2F_{2 \rightarrow 3} \right] \rho_f F_{3 \rightarrow 1} + \dots + \dots \right\} \sigma (T_c^4 - T_{\text{ENV}}^4) \quad (\text{G.19a})$$

$$= A_c \epsilon_c F_{2 \rightarrow 1} \sigma (T_c^4 - T_{\text{ENV}}^4) + \left(2\rho_f \frac{\epsilon_c}{\epsilon_f} \frac{A_c}{A_f} F_{2 \rightarrow 3} \right) \left\{ A_f \epsilon_f F_{3 \rightarrow 1} + \dots + \dots \right\} \sigma (T_c^4 - T_{\text{ENV}}^4) \quad (\text{G.19b})$$

$$= A_c \epsilon_c F_{2 \rightarrow 1} \sigma (T_c^4 - T_{\text{ENV}}^4) + \left(2\rho_f \frac{\epsilon_c}{\epsilon_f} \frac{A_c}{A_f} F_{2 \rightarrow 3} \right) A_f \epsilon_f' \sigma (T_c^4 - T_{\text{ENV}}^4) \quad (\text{G.19c})$$

$$= A_c \epsilon_c' \sigma (T_c^4 - T_{\text{ENV}}^4) \quad (\text{G.19d})$$

where the effective emissivity of the cask body (ϵ_c') is defined as

$$\epsilon_c' = \epsilon_c F_{2 \rightarrow 1} + 2\rho_f \left(\frac{\epsilon_c}{\epsilon_f} \right) F_{2 \rightarrow 3} \epsilon_f' \quad (\text{G.20})$$

Note that ϵ_c' depends on ϵ_f , ϵ_c , ρ_f , ρ_c , and the actual dimensions of the system as characterized by the various view factors.

An alternate method for calculating ϵ'_f and ϵ'_c is given in Section G.4. That method, based upon an equivalent electrical circuit, is, no doubt, more familiar to most students of radiation heat transfer. Both methods have been found to give exactly the same result.

G.4 AN ALTERNATE PROCEDURE FOR CALCULATING THE EFFECTIVE EMISSIVITY OF THE FINS AND THE CASK

An electrical analog¹² of the configuration shown in Fig. G.4 is illustrated in Fig. G.6a. To be perfectly analogous, of course, one would need an additional resistor between nodes C and D to represent radiation exchange between the two fins. Due to symmetry, the current through such a branch would be zero, and the component was deleted in the interest of clarity. The object of this circuit analogy, of course, is to reduce the rather nasty circuit shown in Fig. G.6a to a simpler equivalent circuit from which we can back-out the effective emissivity of the fins and the cask. Summing the voltage drops, first around the "A" loop and then around the "B" loop, we obtain two simultaneous equations for the currents i_A and i_B :

$$(I + 2i_A)R_1 + (i_A - i_B)R_5 + i_A R_4 = 0 \quad (G.21a)$$

$$(I + 2i_B)R_2 + i_B R_3 + (i_B - i_A)R_5 = 0 \quad (G.21b)$$

Since the current source (I) ultimately drops out of the expressions for R_{1eq} and R_{4eq} , we may set $I = 1.0$ and rearrange these equations to obtain:

$$(2R_1 + R_4 + R_5) i_A + (-R_5) i_B = (-R_1) \quad (G.22a)$$

$$(-R_5) i_A + (2R_2 + R_3 + R_5) i_B = (-R_2) \quad (G.22b)$$

After solving these two equations for i_A and i_B , we may then calculate the voltage drop across the network

$$\begin{aligned}
 R_1 &= \rho_c / (A_c \epsilon_c) \\
 R_2 &= 1 / (A_c F_{2 \rightarrow 1}) \\
 R_3 &= 1 / (A_f F_{3 \rightarrow 1}) \\
 R_4 &= \rho_f / (A_f \epsilon_f) \\
 R_5 &= 1 / (A_c F_{2 \rightarrow 3})
 \end{aligned}$$

Fig. G.6a: Electrical Circuit Analogy for the Thermal Radiation Problem Posed by the Cylindrical Cask with Circumferential Fins (cf. Fig. G.4)

$$\begin{aligned}
 i_1 &= I + 2i_4 \\
 i_4 &= -i_A
 \end{aligned}$$

Fig. G.6b: Equivalent Electrical Circuit for the Thermal Radiation Problem Posed by the Cylindrical Cask with Circumferential Fins (cf. Fig. G.4)

$$\begin{aligned}
 V_o &= (I + 2 i_A)R_1 + (I + 2 i_B)R_2 \\
 &= (1 + 2 i_A)R_1 + (1 + 2 i_B)R_2
 \end{aligned} \tag{G.23}$$

and the size of the equivalent resistors R_{1eq} and R_{4eq} :

$$R_{1eq} = \frac{V_o}{i_1} = \frac{V_o}{I+2i_A} = \frac{V_o}{1+2i_A} \tag{G.24}$$

$$R_{4eq} = \frac{V_o}{i_4} = \frac{V_o}{-i_A} \tag{G.25}$$

Going back to the analogy with radiation heat transfer, we note that the electrical currents are analogous to heat flow rates and that the driving potential (V_o) is analogous to $\sigma(T_{SURF}^4 - T_{ENV}^4)$. Thus,

$$i_1 = \left(\frac{1}{R_{1eq}} \right) V_o \tag{G.26}$$

is analogous to

$$Q_{cask,2} = A_c \epsilon'_c \sigma(T_c^4 - T_{ENV}^4) \tag{G.27}$$

which implies that

$$\epsilon'_c = \left(\frac{1}{A_c R_{1eq}} \right) \tag{G.28}$$

Likewise,

$$i_4 = \left(\frac{1}{R_{4eq}} \right) V_o \tag{G.29}$$

is analogous to

$$Q_{fin,3} = A_f \varepsilon'_f \sigma (T_f^4 - T_{ENV}^4) \quad , \quad (G.30)$$

which implies that

$$\varepsilon'_f = \left(\frac{1}{A_f R_{4eq}} \right) \quad . \quad (G.31)$$

Note that the effective emissivity of the fins and the cask have both been calculated using this procedure, and using the analytic expressions developed in Section G.3. As noted there, both methods have been found to give exactly the same result.

APPENDIX H

CALCULATION OF FIN EFFECTIVENESS (η)

Fin effectiveness is commonly defined as the ratio of the heat actually transferred to the environment by the fin, to that which would be transferred to the environment if the entire fin were at the same temperature as the base. Given the size and shape of the fin, its thermal conductivity, and the average heat transfer coefficient, one can readily determine the value of η from charts, tables, or simple correlations,³⁸ and then make effective use of the parameter in many engineering calculations. For circumferential fins on a circular cask, plots of η vs $\sqrt{2h_f t_f / k_f}$ are typically given for several values of r_1/r_0 (cf. Ref. 38, page 60). In the SCOPE code, however, the range of parameters to be investigated is not always known *a priori*, and a mathematical means of calculating η was needed.

For reasonably long thin fins, the 1-D heat conduction equation can be used along with an appropriate boundary condition to establish the temperature at every point on the fin (cf. Fig. H.1). Knowing the average heat transfer coefficient (h_f), one can then calculate the actual amount of heat transferred to the environment (Q_{real}) as:

$$Q_{\text{real}} = m_o A_o k_f \left\{ \frac{I_1(m_o r') K_1(m_o r_o) - K_1(m_o r') I_1(m_o r_o)}{I_1(m_o r') K_o(m_o r_o) + K_1(m_o r') I_o(m_o r_o)} \right\} (T_{\text{base}} - T_{\text{amb}}) \quad (\text{H.1})$$

where A_o = area at the base of the fin; as per Fig. H.1, $A_o = (2\pi r_o)(t_f)$

r' = an extrapolated fin radius ($r_1 + y_o$) at which the gradient of the radial temperature distribution is assumed to be zero

$$\text{and } m_o = \sqrt{\frac{2h_f}{t_f k_f}} \quad (k_f = \text{thermal conductivity of the fin material}).$$

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Fig. H.1: Nomenclature Used in Calculating Fin Effectiveness

This expression may be found on page 156 of Ref. 39 or on page 233 of Ref. 40. Note that it is a fairly complicated expression involving modified Bessel functions of the first and second kind. If, on the other hand, the entire fin were at the same temperature as the base, the total heat transferred to the environment ($Q_{\max}$) could be written as simply

$$Q_{\max} = \{A_{\text{tip}} + 2 A_{\text{surf}}\} h_f (T_{\text{base}} - T_{\text{amb}}) \quad (\text{H.2a})$$

$$= \{2\pi r_1 t_f + 2\pi(r_1^2 - r_o^2)\} h_f (T_{\text{base}} - T_{\text{amb}}) \quad (\text{H.2b})$$

In the SCOPE program, η is explicitly calculated as

$$\eta = \left(\frac{Q_{\text{real}}}{Q_{\max}} \right) \quad (\text{H.3})$$

where Eqs. H.1 and H.2 are used in Eq. H.3. Values calculated in this fashion have, in fact, been found to reproduce those shown in Fig. H.2 (taken from page 60 of Ref. 38).

For very short fat fins, however, the use of the 1-D heat conduction equation with a zero gradient at an extrapolated boundary is more questionable, and the above model begins to break down. Just as distressing, the numerator of Eq. H.3 may involve the difference between two large terms, and machine roundoff error can become significant. In that limit, however, the actual fin effectiveness is very close to 1.0 and the following approximation by Dusanberre⁴¹ has been found to be quite good:

$$\eta \approx \frac{1}{1 + \frac{2}{3} \left(\frac{h_f l_f^2}{k_f t_f} \right) \sqrt{\frac{r_1}{r_o}}} \quad , \quad l_f = (r_1 - r_o) \quad (\text{H.4})$$

Indeed, the Dusanberre approximation has been found to be quite good over a fairly wide range of values, as shown in Table H.1:

Fig. H.2: Fin Effectiveness for Circumferential Fins of Rectangular Cross Section

Table H.1: Comparison of the Actual Fin Effectiveness
and That Given By the Dusenberre Approximation

η (Actual; H.3)	.95	.75	.50	.35
η (Dusenberre)	.95	.77	.48	.31

Fins whose heat transfer per unit weight have been optimized will generally have a fin effectiveness near 0.73. In that range, the Dusenberre approximation is still quite good. At present, however, SCOPE uses that approximation only when the fin is so short or so fat that its effectiveness exceeds 98%. Because of its simplicity and the physical insight it provides, the Dusenberre approximation is nevertheless considered quite valuable.

APPENDIX I

METHOD OF CALCULATING THE ΔT ACROSS THE INSERT (2-D SOLUTION)

The innermost region of many spent fuel shipping casks contain an aluminum or copper basket whose purpose it is to hold or "separate" the spent fuel assemblies (cf. Fig. I.1). In addition to physically separating the fuel assemblies, the insert itself also provides a low resistance path by which the decay heat can be conducted outward towards the inner wall of the cask. Because of this, even the innermost fuel assembly "sees" a relatively cool surface to which it can radiate its own decay heat. (Without the insert, of course, the innermost assembly would only see other hot fuel assemblies, and it, itself, would have to be still hotter in order to dissipate its own decay heat). Before one can calculate the maximum fuel pin temperature, he must, of course, determine the temperature of (that part of) the insert surrounding the innermost assembly. Likewise, the designer will want to check the temperature of the insert at that point in order to verify that it is in no danger of melting. Given that one knows the temperature on the outside of the insert (cf. Section V.F), the task then is to calculate the maximum ΔT across the insert. Given a highly conducting material such as aluminum or copper, virtually all of this temperature difference will be due to the heat flow through (i.e. along) the relatively thin web separating the fuel assemblies (cf. Fig. I.1). Typically, this webbing may be anywhere from 0.25" to 2.0" thick.

To calculate the maximum ΔT across the insert, let us consider an electrical analog as shown in Fig. I.2. While the heat flow is analogous to an electrical current, the thermal resistance provided by the insert (i.e. by the webbing) may be considered analogous to an electrical resistor:

Fig. 1.1: Simplified Diagram of a Removable Aluminum Insert Holding 10 Fuel Assemblies

Fig. 1.2: Electrical Network Used to Model the Thermal Resistance of the Insert and the Decay Heat Load Imposed by the Spent Fuel

The decay heat from each assembly is assumed to be equivalent to an 8-amp current source distributed as shown. The thermal resistance offered by each segment of webbing is assumed to be equivalent to a 10-ohm resistor.

Fig. 1.3: Calculated Voltage Distribution at Each Point in the Electrical Network Used to Model the 2-D Heat Conduction Problem Inside the Aluminum or Copper Insert

Assuming the potential along the perimeter to be a constant 10 volts, the network described above would yield this voltage distribution.

the thicker the webbing, the lower the resistance; the greater the pitch between assemblies, the greater the resistance. Lastly, the ΔV across the electrical network may be considered analogous to the ΔT across the insert.

To define the analogy more clearly, consider the heat (q) flowing outward along a small segment of the insert as shown in Fig. I.4. Obviously

$$q = -kA \frac{dT}{dx} = -k(tL) \frac{T_2 - T_1}{x_2 - x_1} = k(tL) \left(\frac{2}{P} \right) (T_1 - T_2) \quad (I.1)$$

Solving for $(T_1 - T_2)$ gives

$$T_1 - T_2 = q \left(\frac{P}{2tLk} \right) \quad (I.2)$$

which, in the electrical model, is analogous to

$$V_1 - V_2 = iR \quad (I.3)$$

$$\text{Thus: } R_{th} = P/(2tLk) \quad (I.4)$$

In the electrical network model shown in Fig. I.2, the decay heat emanating from each side of each fuel assembly is represented as a 2-amp current source (i.e. 8 amps/assembly). Each resistor is assumed to be 10 ohms, and the potential along the perimeter is assumed to be a constant 10 volts. For each of the 26 resistors, one may apply Ohm's law and write an equation of the form

$$\Delta V_k = V_{HI} - V_{LO} = i_k R \quad (26 \text{ equations}) \quad (I.5)$$

For each of the 17 interior nodes, one may also write a current balance equation of the form:

$$\sum_j i_j = 0 \quad (17 \text{ equations}) \quad (I.6)$$

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Fig. I.4: Nomenclature Used to Define the Thermal Resistance of a Small Segment of the Insert

where $Q = 0, 2$, or 4 -amps, depending on the node. (For this particular configuration there happen to be no nodes where $Q = 0$). This coupled system of 43 equations in 43 unknowns (17 nodal voltages and 26 branch currents) may then be solved simultaneously using Gaussian elimination. The resulting voltage distribution throughout the system is shown in Fig. I.3. Proceeding in this fashion, similar network models for each of the 30 different configurations shown in Appendix B have also been analyzed. The maximum nodal voltage calculated for each configuration is given in Table I.1.

Continuing with the present example, we note that if the voltage along the perimeter (V_0) were increased or decreased by 50 or 100 volts, the voltage at each node (V_i) would also increase or decrease by 50 or 100 volts -- ie, $\Delta V_i = (V_i - V_0)$ would remain constant. Likewise, if all the current sources or all the resistances were increased or decreased, the maximum voltage drop ($\Delta V_{MAX} = V_{MAX} - V_0$) would increase or decrease proportionately -- ie, Ohm's law would still apply to the whole system and one could still write $\Delta V_{MAX} \propto IR$, or in this case:

$$\Delta V_{MAX} = (130-10) \left(\frac{I}{8} \right) \left(\frac{R}{10} \right) \quad (I.7)$$

While this characteristic behavior may or may not be obvious at first, a series of numerical calculations for this and other more complicated networks have shown it to be true. Mathematically, of course, the explanation lies in the linear nature of the underlying equations. Indeed, if one were to take the 26 voltage drop equations, write

$$i_k = \left(\frac{1}{R} \right) (V_{HI} - V_{LO}) \quad (I.8)$$

Table I.1
Maximum Nodal Voltage in the Electrical Analog
of the 2-D Heat Conduction Problem for the Var-
ious Insert Configurations Shown in Appendix B

NO.OF ASSYS	V _{max}	NO.OF ASSYS	V _{max}
1	10.0000	24	232.8571
2	30.0000	26	310.4216
3	38.0000	31	365.8975
4	50.0000	32	370.0000
5	30.0000	34	412.6838
7	76.6667	37	395.7141
8 ^a	101.3043	39	485.0764
8 ^b	83.3333	42	516.7627
10	130.0000	44	506.4119
12	116.6667	46	556.5295
13	153.0601	48	595.8389
15	165.0000	52	590.3225
18	237.7922	56	729.4521
21	230.0000	58	734.3955
22	250.0000	61	733.9207

- a) Configuration such that $\text{Dia/Pitch} = \sqrt{15.68}$
b) Configuration such that $\text{Dia/Pitch} = \sqrt{17.00}$

and substitute these expressions into the 17 current balance equations, one would obtain a set of 17 equations in 17 unknowns which could be written in matrix form as

$$\Delta \underline{V} = \underline{R} \underline{Q} = (\underline{R} \underline{A}) (\underline{I} \underline{q}) = (\underline{IR}) (\underline{A} \underline{q}) \quad (I.9a)$$

or

$$\Delta V_i = IR \sum_{j=1}^{17} a_{ij} q_j = IR c_i \quad (I.9b)$$

where I is the total current per assembly (8 amps/assembly)

R is the value of each resistor (10 ohms)

$\underline{A}$ is a matrix, characteristic of the given configuration

and $\underline{q}$ is a vector, characteristic of the given source distribution.

In light of this development, we see that Eq. I.7 can be written more formally as

$$\Delta V_{MAX} = IR \epsilon(N) \quad (I.10)$$

where $\epsilon(N)$, the resistance multiplication factor, is a dimensionless constant which is characteristic of the given configuration and the given source distribution. By analogy, one may also write

$$\Delta T_{MAX} = Q R_{th} \epsilon(N) \quad (I.11)$$

where Q is the total decay heat per assembly (Btu/hr) and R_{th} is the thermal resistance corresponding to any of the 10^{Ω} resistors shown in Fig. I.2. Substituting Eq. I.4 into Eq. I.11, and using Eq. I.10 to eliminate $\epsilon(N)$ yields

$$\Delta T_{MAX} = Q_A \left(\frac{P}{2tLk} \right) \left(\frac{\Delta V_{MAX}}{IR} \right) = Q_A \left(\frac{P/t}{Lk} \right) \left(\frac{V_{MAX} - 10}{160} \right) \quad (I.12)$$

where Q_A = total decay heat per assembly (Btu/hr)

L = length of the insert (feet)

k = thermal conductivity of the insert (Btu/hr)/ft²F

t = effective thickness of the webbing (inches); see footnote *

and P = pitch between assemblies (inches)

As noted previously, the values of V_{MAX} corresponding to each of the insert configurations shown in Appendix B have all been calculated using the type of 2-D network analysis described in this section. For reference, these values are all given in Table I.1. Use of these values in Eq. I.12 will allow one to quickly and accurately calculate the maximum ΔT across the insert.

* In addition to physically separating the fuel assemblies and providing a low resistance path by which decay heat can be readily conducted away from the innermost assemblies, it is often necessary for the designer to embed some type of neutron poison in the insert between the fuel assemblies. This matter is described at some length in Section III. In that section, an inherently subcritical insert design is proposed which would make use of hollow borated stainless-steel tubes embedded within the insert between the fuel assemblies as shown in Fig. I.5. In that event, the thickness (t) to be used in Eq. I.12 is given by $t = t_1 - t_2$ where t_1 is the actual thickness of the insert (between fuel assemblies) and t_2 is the thickness of the embedded poison. See Fig. I.5.

**Fig. I.5: Inherently Subcritical, Removable Aluminum Insert
Designed to Hold 10 PWR Fuel Assemblies**

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APPENDIX J

THE WOOTON-EPSTEIN EQUATION (ORIGINAL UNPUBLISHED DERIVATION)

The Wooton-Epstein Equation is frequently used to calculate the maximum fuel pin clad temperature in a dry spent fuel shipping cask. This semi-theoretical, semi-empirical correlation was used by General Electric in their Consolidated Safety Analysis Report for the IF-300 spent fuel cask,² and has more recently been included in the SCOPE code for Shipping Cask Optimization and Parametric Evaluation.⁷ Unfortunately, the best reference on the correlation is an unpublished paper by R. O. Wooton and H. M. Epstein which they first prepared in August, 1963. Because of the widespread use of the Wooton-Epstein Equation in more recent years, and the lack of an easily obtainable reference, we have (with the permission of the authors) decided to reproduce here a condensed synopsis of their original paper which includes only that information pertaining to natural air convection and radiation cooling of a spent fuel assembly in a horizontal cask.

HEAT TRANSFER FROM A PARALLEL-ROD FUEL ASSEMBLY IN A SHIPPING CONTAINER*

R. O. Wooton and H. M. Epstein

Battelle Memorial Institute, Columbus, Ohio

(August, 1963)

ABSTRACT

The heat transferred by combined radiation and free convection in air was studied for a horizontal parallel-rod fuel assembly. Correlations are developed which permit calculation of the maximum temperatures attained. This data is of interest to designers of spent fuel shipping casks. Fuel assembly powers up to 40 kw were studied.

INTRODUCTION

An investigation has been conducted to study some of the heat-transfer problems encountered in the shipment of irradiated parallel-rod fuel assemblies. In particular, it was necessary to have reliable estimates of the maximum fuel rod surface temperatures attained during shipment. For the complicated geometry of parallel-rod fuel assemblies enclosed by the walls of a shipping cask, experimental data for free convection heat transfer has not previously been available. The experiments described in this paper were designed to furnish the heat-transfer information needed for the shipment of spent fuel assemblies from the Yankee Reactor, and to provide some interesting general information on radiation and free convection heat transfer from parallel-rod fuel assemblies.

In a shipping cask containing several assemblies, each assembly is usually enclosed in a cell of a retaining basket which prevents radial convection between cells. When there are no convection currents between

* This condensed synopsis of their original paper includes only that information pertaining to natural air convection and radiation cooling of a spent fuel assembly in a horizontal cask. Their original paper was of much broader scope. -- J. A. Bucholz

cells, the heat-transfer characteristics inside the cask can be calculated with reasonable accuracy if they are known for a single cell. The experiments in this paper were, therefore, performed for a single assembly enclosed by a steel pipe, which simulates the walls of a basket cell. Four different conditions were investigated experimentally and analytically:^{sa}

- (1) A vertical cask containing water, with flow baffles between the fuel assembly and the cask wall.
- (2) A vertical cask containing water.
- (3) A horizontal cask containing water.
- (4) A horizontal cask containing air.

DESCRIPTION OF THE EXPERIMENT

The simulated fuel assembly used in this experiment was approximately 8 feet long and 7.6 inches square.^{sb} It contained 306 closed stainless-steel tubes, 0.34 inches in diameter, arranged in a square array on 0.422 inch centers.

In the experiment, the fission product decay heat was simulated by electrical resistance heating of the parallel-rod assembly. The total power applied to the assembly was calculated from measurements of the electrical resistance and the current input. The electrical power could

^{sa} In this abridged version of their original paper, only that information pertaining to category (4) has been included.

^{sb} The actual assembly is believed to have measured 17x18, with the 306 rods arranged on a 0.422 inch pitch as noted.

be varied between 0 and about 50 kw. In actual service, the fuel assembly simulated in this study would have a fission product decay heat of about 8 kw after 3 months cooling and would exhibit an axial power distribution with a maximum approximately 50 percent greater than the average power. The decay heat at reactor shutdown would be several times larger. The axial power distribution was not simulated by the electrical heating.

During the experiment, the parallel-rod assembly was centered inside a steel pipe with an inside diameter of 1 foot. The one-foot diameter pipe was placed inside a larger pipe and coolant water passed through the annulus between the two pipes, maintaining a constant temperature at the wall of the one-foot diameter pipe. Thermocouples were placed along the length of the assembly, and temperature data was recorded as a function of the total power supplied to the assembly for the four conditions listed previously.

RESULTS AND ANALYSIS

Experimental Results for Condition 4 - (Natural Air Convection and Radiation Cooling in a Horizontal Cask)

Tests on the assembly under loss of water coolant conditions were performed in the horizontal position. The experimental data are shown in Fig. 1 where the maximum fuel rod surface temperature is plotted as a function of the electrical power supplied to the assembly. The indicated uncertainty in the power measurements for the three points above 1100°F resulted from an uncertainty in the electrical power factor.

Fig. 1: Maximum Fuel Rod Surface Temperature (T_E) as a Function of the Electrical Power Supplied to the Assembly

Analysis and Discussion of Condition 4

The data in Fig. 1 were correlated by an equation of the form

$$Q = \sigma C_1 F_1 A_1 \left[T_E^4 - T_C^4 \right] + C_2 A_1 (T_E - T_C)^{4/3} \quad (1)$$

where the constants C_1 , F_1 , and C_2 are defined below. The first term in the equation accounts for radiative heat transfer and the second accounts for convective heat transfer. The experimental constants F_1 and C_2 were chosen so that the calculated power would be consistently on the low side of the uncertainty in measured power. (Thus, for a known Q , Eq. 1 will conservatively overpredict the maximum fuel rod surface temperature, T_E). From analysis of the experimental data, C_2 was determined to be 0.118, in good agreement with standard correlations for convective heat transfer from single (isolated) cylinders in air at the temperatures encountered in this experiment. The factor F_1 was determined to be 0.512 for the conditions studied. This factor is discussed more completely in the following development.

The geometric constant C_1 had a value of ~ 0.21 for the particular assembly studied. It was determined theoretically as follows: First, consider that the cross section of the assembly is made up of a series of concentric squares. The first square has 17 tubes on a side; the second square has 15 tubes on a side; the third has 13 on a side; the fourth has 11; and so on, until the ninth square has only one tube which is at a maximum temperature T_E . Let us designate the heat-transfer area of the

first square by A_1 and the temperature of the tubes in the first square by T_1 . A similar notation is followed for the other squares. The heat transferred by radiation from Square 2 to Square 1 is designated as Q_{21} , and similar notation is used for the other squares.

The usual expression for the radiant heat transfer between two surfaces is

$$Q_{mn} = \sigma A_m F_{mn} (T_m^4 - T_n^4) \quad (2)$$

where

$$F_{mn} = \left[\frac{1}{f_{mn}} + \left(\frac{1}{\epsilon_m} - 1 \right) + \frac{A_m}{A_n} \left(\frac{1}{\epsilon_n} - 1 \right) \right]^{-1} \quad (3)$$

and the geometric view factor, f_{mn} , is 1.0 for concentric surfaces.

In the development below, the factors F_{mn} are all replaced by a single factor F_1 which is assumed to be emissivity-dependent only.

This is a good approximation, for example, for a series of concentric cylinders where the ratios of successive radii are close to one.

Now, the heat transferred from the outer square to the basket is

$$Q_{10} = \sigma F_1 A_1 (T_1^4 - T_c^4) \quad (4a)$$

where A_1 is the area of an imaginary envelope surrounding the fuel assembly. Likewise, the heat transferred from Square 2 to Square 1 is

$$Q_{21} = \sigma F_1 A_2 (T_2^4 - T_1^4) \quad (4b)$$

Similarly,

$$Q_{32} = \sigma F_1 A_3 (T_3^b - T_2^b) \quad , \quad (4c)$$

$$Q_{43} = \sigma F_1 A_4 (T_4^b - T_3^b) \quad , \quad (4d)$$

•
•
•

and

$$Q_{98} = \sigma F_1 A_9 (T_E^b - T_8^b) \quad . \quad (4i)$$

Dividing through by $\sigma F_1 A_1$ and adding, these equations telescope to yield

$$\frac{1}{\sigma F_1} \left(\frac{Q_{10}}{A_1} + \frac{Q_{21}}{A_2} + \frac{Q_{32}}{A_3} + \dots + \frac{Q_{98}}{A_9} \right) = T_E^b - T_C^b \quad , \quad (5)$$

or

$$\frac{Q_{10}}{\sigma F_1 A_1} \left(\frac{Q_{10}}{Q_{10}} \frac{A_1}{A_1} + \frac{Q_{21}}{Q_{10}} \frac{A_1}{A_2} + \frac{Q_{32}}{Q_{10}} \frac{A_1}{A_3} + \dots \right) = T_E^b - T_C^b \quad . \quad (6)$$

Considering that A_m is proportional to the number of tubes along one side of the square and that Q_{mn} is proportional to the number of tubes inside the square,

$$\frac{A_1}{A_2} = \frac{17}{15} \quad , \quad \frac{A_1}{A_3} = \frac{17}{13} \quad , \quad \dots \quad , \quad \frac{A_1}{A_m} = \frac{N}{N - (2m - 2)} \quad (7)$$

and

$$\frac{Q_{21}}{Q_{10}} = \left(\frac{15}{17}\right)^2, \quad \frac{Q_{32}}{Q_{10}} = \left(\frac{13}{17}\right)^2, \quad \dots, \quad \frac{Q_{mn}}{Q_{10}} = \left(\frac{N - (2m - 2)}{N}\right)^2 \quad (8)$$

Each of the m terms in Eq. (6) may then be written as

$$\frac{Q_{mn}}{Q_{10}} \frac{A_1}{A_m} = \frac{N - (2m - 2)}{N} \quad (9)$$

where N is the number of rods along one side of the fuel assembly.

Substituting this result into Eq. (6) yields a modified arithmetic series whose sum may be written as

$$T_E^4 - T_C^4 = \frac{Q_{10}}{\sigma F_1 A_1} \left[\frac{N + (N - 2) + (N - 4) + \dots + 5 + 3 + 1}{N} \right] \quad (10a)$$

$$= \frac{Q_{10}}{\sigma F_1 A_1} \frac{(N + 1)^2}{4N} \quad (10b)$$

when N is odd. A similar expression can be developed for even N . Thus, the geometric factor C_1 is given by

$$C_1 = \frac{4N}{(N + 1)^2} \quad (N \text{ odd}) \quad (11a)$$

and

$$C_1 = \frac{4}{N + 2} \quad (N \text{ even}) \quad (11b)$$

For the particular assembly studied, $N = 17$ and $C_1 = 68/324 \approx 0.21$.

Since the factor F_1 is assumed to be emissivity dependent only, equation (3) reduces to

$$F_1 = \frac{1}{\left[\frac{1}{\epsilon_E} + \frac{1}{\epsilon_c} - 1 \right]} \quad (12)$$

Since the fuel element (E) and the surrounding canister (c) were both steel, one can assume that $\epsilon_E = \epsilon_c = \epsilon$. Interestingly enough, we note that when an emissivity of 0.677 (which is quite reasonable for steel surfaces at the temperatures encountered in this experiment) is substituted into Eq. (12), it will reproduce the measured value of F_1 which resulted from careful numerical analysis of the correlated data. Equation (12) should therefore be used to calculate F_1 when applying the correlation to non-steel surfaces.

CONCLUSION

Heat-transfer correlations have been presented for a parallel-rod fuel assembly in a horizontal cask containing air. As seen in Fig. 1, the agreement between the calculated and measured temperatures is quite good. The heat-transfer coefficients obtained for use in Eq. (1) are, in general, only somewhat smaller than those predicted by standard correlations for simple geometries. The accuracy of the correlation, and the fact that the heat-transfer coefficients measured are not greatly different from those expected in simpler geometries, suggests that Eq. (1) may be valid over a rather wide range of parameters. However, until more experimental data become available to test the theory, caution should be exercised in extrapolating the given correlation.

APPENDIX K

DERIVATION OF THE TIME-DEPENDENT FINITE-DIFFERENCED
ONE-DIMENSIONAL HEAT TRANSFER EQUATIONS

The purpose of this appendix is to derive the form of the time-dependent finite-differenced one-dimensional heat transfer equations actually solved by the SCOPE program.⁷

A simplified 1-D model of the cask is shown in Fig. K.1. To account for the (often large) heat capacity of the insert without adding undue complexity to the problem, the insert has been modeled as a solid annular region just inside the inner shell. The small gap ($\sim 0.125''$) between the inner shell and the insert has been maintained, as has the overall cross-sectional area of the insert. Prior to (and during) the postulated half-hour fire, the neutron shield is assumed to have been rendered void as a result of the 30-foot drop test.

For illustrative purposes, let us consider a 1-D model wherein we would like to calculate the temperature at each of 13 different nodes, as shown in Fig. K.1. (By changing a DATA statement in the actual code, one can specify any arbitrary number of nodes in any given zone). For the region surrounding each node, one can write a macroscopic heat balance equation of the form:

$$\left[\begin{array}{l} \text{Heat flowing into} \\ \text{the region (through} \\ \text{the left-hand} \\ \text{boundary) per unit} \\ \text{time} \end{array} \right] = \left[\begin{array}{l} \text{Heat flowing out of} \\ \text{the region (through} \\ \text{the right-hand} \\ \text{boundary) per unit} \\ \text{time} \end{array} \right] + \left[\begin{array}{l} \text{Heat being} \\ \text{absorbed by} \\ \text{the region} \\ \text{per unit} \\ \text{time} \end{array} \right]$$

(K.1)

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Fig. K.1: Simplified 1-D Model Used in the Space/Time Transient Thermal Analysis

Looking at the sketch in Table K.2, we may write the following equation for node 1:

$$Q = -k A_{1A} \left. \frac{\partial T}{\partial r} \right|_{1A} + \rho c_p V_{1 \rightarrow 1A} \frac{\partial T_1}{\partial t} \quad (\text{K.2a})$$

$$Q \doteq -k \left[2\pi r_{1A} L \right] \left(\frac{T_2 - T_1}{r_2 - r_1} \right) + \rho c_p \left[\pi (r_{1A}^2 - r_1^2) L \right] \frac{\partial T_1}{\partial t} \quad (\text{K.2b})$$

where Q is the total decay heat rate for all the assemblies in the cask. Note that this might be viewed as a "macroscopic" heat balance equation for the region surrounding node 1.

To eliminate the time-derivative from the resulting equation, we effectively integrate each term over each time interval. To illustrate the procedure used, consider the following first-order equation:

$$\frac{dT}{dt} = a T(t) + s \quad (\text{K.3})$$

$$\Rightarrow \int_{t^n}^{t^{n+1}} \frac{dT}{dt} dt = a \int_{t^n}^{t^{n+1}} T(t) dt + \int_{t^n}^{t^{n+1}} s dt \quad (\text{K.4})$$

$$\Rightarrow (T^{n+1} - T^n) = a I + (\Delta t) s \quad (\text{K.5})$$

$$\text{where } I \equiv (\Delta t) [\theta T^{n+1} + (1-\theta)T^n] \quad , \quad (\text{K.6})$$

and $\theta = 0.0 \Rightarrow$ forward difference (yields explicit soln; not always stable)

$\theta = 1.0 \Rightarrow$ backward difference (yields implicit solution; always stable)

$\theta = 0.5 \Rightarrow$ central difference (implicit solution; usually most accurate)

Likewise, if there are several time-dependent quantities in the equation (as there are in our macroscopic heat-balance equation),

$$\frac{dT}{dt} = a X(t) + b Y(t) + c Z(t) + s \quad (K.7)$$

yields

$$T^{n+1} - T^n = a I_x + b I_y + c I_z + (\Delta t)s \quad (K.8)$$

Looking at Fig. K.1, we see that the equations governing the behavior at many nodes are very similar. For example, the equations for nodes 6 and 8 would be similar since they both represent a point at the interface between two media. For the sake of brevity, we have included the derivation of the finite-differenced heat balance equations only at those nodes which are in some way unique; equations for the others are similar, as noted here:

Node 1: see Table K.2

Node 8: same as for node 6

Node 2: see Table K.3

Node 9: same as for node 2

Node 3: see Table K.4

Node 10: same as for node 3

Node 4: see Table K.5

Node 11: same as for node 4

Node 5: same as node 2

Node 12: same as for node 2

Node 6: see Table K.6

Node 13: see Table K.7 or K.8

Node 7: same as node 2

The resulting nodal equations can then be collected and put into the form of a tridiagonal matrix, as shown in Table K.1. This system of equations must, of course, be solved at each time step during the transient. Note that if one sets $\theta = 0.0$, the system of equations becomes uncoupled and yields a single expression for the "next" temperature at each node. Unfortunately, this forward-differenced solution may become unstable and yield grossly incorrect results after only a few time steps unless one uses an exceedingly small Δt .^{*a} If one sets $\theta = 1.0$, the source vector on the right-hand side simplifies considerably. While the system does require a simultaneous (matrix) solution, this backward-differenced implicit method is stable regardless of the size of the time step. The accumulated error in the solution at any point in time is roughly proportional to (Δt) . If one sets $\theta = 0.5$, one obtains the Crank-Nicolson model^{8,46} which, given constant coefficients, is stable regardless of the size of the time step and generally more accurate than either of the other two schemes [ϵ is proportional to $(\Delta t)^2$]. Because of this, one can use somewhat larger time steps to obtain a specified degree of accuracy.^{*b}

*a One must have $\Delta t < (c)(\Delta r)^2$ where c is a constant depending upon the thermal diffusivity of the surrounding media. Ref. 46 discusses the stability criterion in some detail. For many problems, one may have to use a $\Delta t < 0.0002$ hrs to follow a thermal transient that may not peak for several hours.

*b Indeed, in the SCOPE program, the value of θ is typically set to 0.5 to obtain the Crank-Nicolson solution. Unfortunately, the heat transfer coefficients (h) appearing in the equations for nodes 3, 4, 10, 11, and 13 do depend on temperature, and hence on time (t). Accurate approximation of these parameters will limit the maximum Δt that can be used.^{8,46} Nevertheless, since these are often very slowly varying parameters, the Δt used can (at times) be quite large. In SCOPE, we use a Δt as small as 9 seconds during the rapidly varying portion of the transient and as large as 10 minutes during the more slowly varying portions of the transient ($t > \text{several hours}$). The use of a large, variable time step greatly reduces the overall calculational time. The time steps chosen have typically been able to reproduce the HEATING-5 results for a similar model to within $\pm 1^\circ\text{F}$ at all points in the cask during the entire transient (cf. Section VI.E).

Table K.1

FINAL FORM OF THE TIME-DEPENDENT FINITE-DIFFERENCED ONE-DIMENSIONAL HEAT TRANSFER EQUATIONS

$$(1 + \theta b_1) T_1^{n+1} + (-\theta b_1) T_2^{n+1} = T_1^n + (1 - \theta) [b_1(T_2^n - T_1^n)] + S$$

$$(-\theta a_2) T_1^{n+1} + (1 + \theta a_2 + \theta b_2) T_2^{n+1} + (-\theta b_2) T_3^{n+1} = T_2^n + (1 - \theta) [a_2(T_1^n - T_2^n) + b_2(T_3^n - T_2^n)]$$

$$(-\theta a_3) T_2^{n+1} + (1 + \theta a_3 + \theta b_3) T_3^{n+1} + (-\theta b_3) T_4^{n+1} = T_3^n + (1 - \theta) [a_3(T_2^n - T_3^n) + b_3(T_4^n - T_3^n)]$$

•

•

•

$$(-\theta a_{12}) T_{11}^{n+1} + (1 + \theta a_{12} + \theta b_{12}) T_{12}^{n+1} + (-\theta b_{12}) T_{13}^{n+1} = T_{12}^n + (1 - \theta) [a_{12}(T_{11}^n - T_{12}^n) + b_{12}(T_{13}^n - T_{12}^n)]$$

$$(-\theta a_{13}) T_{12}^{n+1} + (1 + \theta a_{13} + \theta b_{13}) T_{13}^{n+1} = T_{13}^n + (1 - \theta) [a_{13}(T_{12}^n - T_{13}^n) + b_{13}(T_{amb}^n - T_{13}^n)]$$

* Note: T_i^n denotes the temperature at node i at the end of the n^{th} iteration.

As noted earlier, the actual equations coded into the SCOPE program have been generalized to allow for any number of nodes in any zone. (In practice, we have found the 13 equations illustrated here to be quite adequate in most cases). For casks not having an insert, the corresponding nodal equations are deleted from the matrix and the solution proceeds as before. For casks not having a neutron shield and outside liner, the corresponding nodal equations may again be deleted, with the solution again proceeding as before. Likewise, analyses for casks having fins would be carried out using the equation(s) shown in Table K.8, whereas those not having fins would be analyzed using the equation(s) shown in Table K.7. In any event, the resulting tridiagonal system of equations can be solved quickly and easily at each time step using matrix factorization⁴⁵ (i.e., a "direct" solution technique as opposed to an iterative technique). This, combined with the use of a large, variable time step permitted by the Crank-Nicolson differencing scheme, provides for a very fast and efficient means of solving the time-dependent finite-differenced one-dimensional heat transfer equations under the accident and post-accident conditions. More details of the actual solution are discussed in Section VI.

Table K.2

DERIVATION OF TRANSIENT HEAT TRANSFER EQN FOR THE INNERMOST SURFACE

$$Q = -k A_{1A} \left. \frac{\partial T}{\partial r} \right|_{1A} + \rho c_p V_{1 \rightarrow 1A} \frac{\partial T_1}{\partial t}$$

$$Q = -k (2\pi r_{1A} L) \left(\frac{T_2 - T_1}{r_2 - r_1} \right) + \rho c_p \pi (r_{1A}^2 - r_1^2) L \frac{\partial T_1}{\partial t}$$

$$\rho c_p (r_{1A}^2 - r_1^2) \frac{\partial T_1}{\partial t} = \left(\frac{q'}{\pi} \right) \left(\frac{2r_{1A} k}{r_2 - r_1} \right) (T_2 - T_1) \quad \text{where } q' = \left(\frac{Q}{L} \right)$$

$$T_1^{n+1} - T_1^n = \left(\frac{q'}{\pi} \right) w_1 + b_1 \left[\theta T_2^{n+1} + (1 - \theta) T_2^n - \theta T_1^{n+1} - (1 - \theta) T_1^n \right]$$

$$\text{where } w_1 = \frac{\Delta t}{\rho c_p (r_{1A}^2 - r_1^2)} \quad b_1 = \left(\frac{2r_{1A} k}{r_2 - r_1} \right) w_1$$

$$(1 + \theta b_1) T_1^{n+1} + (-\theta b_1) T_2^{n+1} = \left(\frac{q'}{\pi} \right) w_1 + T_1^n + b_1 (1 - \theta) (T_2^n - T_1^n)$$

Table K.3

DERIVATION OF TRANSIENT HEAT TRANSFER EQN WITHIN A GIVEN MATERIAL

$$-k A_{1A} \left. \frac{\partial T}{\partial r} \right|_{1A} = -k A_{2A} \left. \frac{\partial T}{\partial r} \right|_{2A} + \rho c_p V_{1A+2A} \frac{\partial T_2}{\partial t}$$

$$-k (2\pi r_{1A} L) \left(\frac{T_2 - T_1}{r_2 - r_1} \right) = -k (2\pi r_{2A} L) \left(\frac{T_3 - T_2}{r_3 - r_2} \right) + \rho c_p [\pi (r_{2A}^2 - r_{1A}^2) L] \frac{\partial T_2}{\partial t}$$

$$T_2^{n+1} - T_2^n = b_2 \left[\theta T_3^{n+1} + (1 - \theta) T_3^n - \theta T_2^{n+1} - (1 - \theta) T_2^n \right] \\ - a_2 \left[\theta T_2^{n+1} + (1 - \theta) T_2^n - \theta T_1^{n+1} - (1 - \theta) T_1^n \right]$$

$$\text{where } w_2 = \frac{\Delta t}{\rho c_p (r_{2A}^2 - r_{1A}^2)} \quad b_2 = \left(\frac{2r_{2A} k}{r_3 - r_2} \right) w_2 \quad a_2 = \left(\frac{2r_{1A} k}{r_2 - r_1} \right) w_2$$

$$(-\theta a_2) T_1^{n+1} + (1 + \theta a_2 + \theta b_2) T_2^{n+1} + (-\theta b_2) T_3^{n+1}$$

$$= T_2^n + b_2 (1 - \theta) (T_3^n - T_2^n) - a_2 (1 - \theta) (T_2^n - T_1^n)$$

Table K.4

DERIVATION OF TRANSIENT HEAT TRANSFER EQN ON INNER SURFACE OF A VOID

$$-k A_{2A} \left. \frac{\partial T}{\partial r} \right|_{2A} = h_3 A_3 (T_3 - T_4) + \rho c_p V_{2A \rightarrow 3} \frac{\partial T_3}{\partial t}$$

$$-k (2\pi r_{2A} L) \left(\frac{T_3 - T_2}{r_3 - r_2} \right) = h_3 (2\pi r_3 L) (T_3 - T_4) + \rho c_p [\pi (r_3^2 - r_{2A}^2) L] \frac{\partial T_3}{\partial t}$$

$$T_3^{n+1} - T_3^n = -a_3 \left[\theta T_3^{n+1} + (1 - \theta) T_3^n - \theta T_2^{n+1} - (1 - \theta) T_2^n \right]$$

$$-b_3 \left[\theta T_3^{n+1} + (1 - \theta) T_3^n - \theta T_4^{n+1} - (1 - \theta) T_4^n \right]$$

$$\text{where } w_3 = \frac{\Delta t}{\rho c_p (r_3^2 - r_{2A}^2)} \quad a_3 = \left(\frac{2r_{2A}k}{r_3 - r_2} \right) w_3 \quad b_3 = (2r_3 h_3) w_3$$

$$(-\theta a_3) T_2^{n+1} + (1 + \theta a_3 + \theta b_3) T_3^{n+1} + (-\theta b_3) T_4^{n+1}$$

$$= T_3^n - a_3 (1 - \theta) (T_3^n - T_2^n) - b_3 (1 - \theta) (T_3^n - T_4^n)$$

Table K.5

DERIVATION OF TRANSIENT HEAT TRANSFER EQN ON OUTER SURFACE OF A VOID

$$h_3 A_3 (T_3 - T_4) = -k A_{4A} \left. \frac{\partial T}{\partial r} \right|_{4A} + \rho c_p V_{4 \rightarrow 4A} \frac{\partial T_4}{\partial t}$$

$$h_3 (2\pi r_3 L) (T_3 - T_4) = -k (2\pi r_{4A} L) \left(\frac{T_5 - T_4}{r_5 - r_4} \right) + \rho c_p \left[\pi (r_{4A}^2 - r_4^2) L \right] \frac{\partial T_4}{\partial t}$$

$$T_4^{n+1} - T_4^n = a_u \left[\theta T_3^{n+1} + (1 - \theta) T_3^n - \theta T_4^{n+1} - (1 - \theta) T_4^n \right] \\ + b_u \left[\theta T_5^{n+1} + (1 - \theta) T_5^n - \theta T_4^{n+1} - (1 - \theta) T_4^n \right]$$

$$\text{where } w_u = \frac{\Delta t}{\rho c_p (r_{4A}^2 - r_4^2)} \quad a_u = (2r_3 h_3) w_u \quad b_u = \left(\frac{2r_{4A} k}{r_5 - r_4} \right) w_u$$

$$(-\theta a_u) T_3^{n+1} + (1 + \theta a_u + \theta b_u) T_4^{n+1} + (-\theta b_u) T_5^{n+1}$$

$$= T_4^n + a_u (1 - \theta) (T_3^n - T_4^n) + b_u (1 - \theta) (T_5^n - T_4^n)$$

Table K.6

DERIVATION OF TRANSIENT HEAT TRANSFER EQN AT INTERFACE BETWEEN MEDIA

$$-k_B A_{5A} \left. \frac{\partial T}{\partial r} \right|_{5A} = -k_C A_{6A} \left. \frac{\partial T}{\partial r} \right|_{6A} + \left[\rho_B c_{PB} V_{5A \rightarrow 6} + \rho_C c_{PC} V_{6 \rightarrow 6A} \right] \frac{\partial T_6}{\partial t}$$

$$-k_B (2\pi r_{5A} L) \left(\frac{T_6 - T_5}{r_6 - r_5} \right) = -k_C (2\pi r_{6A} L) \left(\frac{T_7 - T_6}{r_7 - r_6} \right) + \left\{ \rho_B c_{PB} [\pi(r_6^2 - r_{5A}^2)L] + \rho_C c_{PC} [\pi(r_{6A}^2 - r_6^2)L] \right\} \frac{\partial T_6}{\partial t}$$

$$T_6^{n+1} - T_6^n = -a_6 \left[\theta T_6^{n+1} + (1 - \theta) T_6^n - \theta T_5^{n+1} - (1 - \theta) T_5^n \right] + b_6 \left[\theta T_7^{n+1} + (1 - \theta) T_7^n - \theta T_6^{n+1} - (1 - \theta) T_6^n \right]$$

$$\text{where } w_6 = \frac{\Delta t}{\rho_B c_{PB} (r_6^2 - r_{5A}^2) + \rho_C c_{PC} (r_{6A}^2 - r_6^2)}$$

$$\text{and } a_6 = \left(\frac{2r_{5A} k_B}{r_6 - r_5} \right) w_6 \quad b_6 = \left(\frac{2r_{6A} k_C}{r_7 - r_6} \right) w_6$$

$$(-\theta a_6) T_5^{n+1} + (1 + \theta a_6 + \theta b_6) T_6^{n+1} + (-\theta b_6) T_7^{n+1} = T_6^n - a_6 (1 - \theta) (T_6^n - T_5^n) + b_6 (1 - \theta) (T_7^n - T_6^n)$$

Table K.7

DERIVATION OF TRANSIENT HEAT TRANSFER EQN ON OUTSIDE SURF OF A FINLESS CASK

$$-k A_{12A} \frac{\partial T}{\partial r} \bigg|_{12A} = h_{13} A_{13} (T_{13} - T_{amb}) + \rho c_p V_{12A \rightarrow 13} \frac{\partial T_{13}}{\partial t}$$

$$-k (2\pi r_{12A} L) \left(\frac{T_{13} - T_{12}}{r_{13} - r_{12}} \right) = h_{13} (2\pi r_{13} L) (T_{13} - T_{amb}) + \rho c_p \left[\pi (r_{13}^2 - r_{12A}^2) L \right] \frac{\partial T_{13}}{\partial t}$$

$$T_{13}^{n+1} - T_{13}^n = -a_{13} \left[\theta T_{13}^{n+1} + (1 - \theta) T_{13}^n - \theta T_{12}^{n+1} - (1 - \theta) T_{12}^n \right] \\ - b_{13} \left[\theta T_{13}^{n+1} + (1 - \theta) T_{13}^n - T_{amb} \right]$$

$$\text{where } w_{13} = \frac{\Delta t}{\rho c_p (r_{13}^2 - r_{12A}^2)} \quad a_{13} = \left(\frac{2r_{12A} k}{r_{13} - r_{12}} \right) w_{13} \quad b_{13} = (2r_{13} h_{13}) w_{13}$$

$$(-\theta a_{13}) T_{12}^{n+1} + (1 + \theta a_{13} + \theta b_{13}) T_{13}^{n+1}$$

$$= T_{13}^n - a_{13} (1 - \theta) (T_{13}^n - T_{12}^n) - b_{13} (1 - \theta) T_{13}^n + b_{13} T_{amb}$$

* Note: To account for direct solar heating of the cask in the post-accident situation, the $b_{13} T_{amb}$ term should be replaced with $b_{13} T_{amb} + (2r_{13} w_{13} q_s^0 / \pi)$, where q_s^0 is the solar heat flux normal to the projected area of the cask ($\sim 900 \text{ W/m}^2$, max) and q_s is the absorptivity of the cask for solar radiation (~ 1.0).

Table K.8

DERIVATION OF TRANSIENT HEAT TRANSFER EQUATION ON OUTSIDE SURFACE OF A CASK WITH CIRCUMFERENTIAL FINS

Let us define

- r_f = outside radius of fin
 s_f = center-to-center fin spacing
 t_f = thickness of fin
 η_f = fin effectiveness

$$\begin{aligned}
 -k A_{12A} \frac{\partial T}{\partial r} \bigg|_{12A} &= h_{cask} A_{cask \text{ body}} (T_{11} - T_{amb}) + \rho_{cask} c_{p,cask} V_{12A-13} \frac{\partial T_{11}}{\partial t} \\
 &+ \eta_f h_{fins} A_{fins} (T_{11} - T_{amb}) + \rho_{fins} c_{p,fins} V_{fins} \frac{\partial T_{11}}{\partial t}
 \end{aligned}$$

$$\begin{aligned}
 -k(2\pi r_{12A} s_f) \left(\frac{T_{11} - T_{12}}{r_{13} - r_{12}} \right) &= h_{cask} (2\pi r_{13} [s_f - t_f]) (T_{11} - T_{amb}) \\
 &+ \eta_f h_{fins} \{ 2\pi (r_f^2 - r_{13}^2) \} (T_{11} - T_{amb}) \\
 &+ [\rho_{cask} c_{p,cask} (r_{13}^2 - r_{12A}^2) s_f + \rho_{fins} c_{p,fins} (r_f^2 - r_{13}^2) t_f] \frac{\partial T_{11}}{\partial t}
 \end{aligned}$$

$$-a'_{11} (T_{11} - T_{12}) = b'_{11} (T_{11} - T_{amb}) + \frac{\partial T_{11}}{\partial t}$$

$$w_{11} = \frac{\Delta t}{\rho_{cask} c_{p,cask} (r_{13}^2 - r_{12A}^2) s_f + \rho_{fins} c_{p,fins} (r_f^2 - r_{13}^2) t_f}$$

$$a_{11} = \left(\frac{2r_{12A} s_f k}{r_{13} - r_{12}} \right) w_{11}$$

$$b_{11} = \{ 2r_{13} [s_f - t_f] h_{cask} + 2 (r_f^2 - r_{13}^2) + r_f t_f \} \eta_f h_{fins} w_{11}$$

$$\begin{aligned}
 T_{11}^{n+1} - T_{11}^n &= -a_{11} [\theta T_{11}^{n+1} + (1 - \theta) T_{11}^n - \theta T_{12}^{n+1} - (1 - \theta) T_{12}^n] \\
 &- b_{11} [\theta T_{11}^{n+1} + (1 - \theta) T_{11}^n - T_{amb}]
 \end{aligned}$$

$$\begin{aligned}
 (-a_{11}) T_{12}^{n+1} + (1 + \theta a_{11} + \theta b_{11}) T_{11}^{n+1} \\
 = T_{11}^n - a_{11} (1 - \theta) (T_{11}^n - T_{12}^n) - b_{11} (1 - \theta) T_{11}^n + b_{11} T_{amb}
 \end{aligned}$$

* Note: To account for direct solar heating of the cask in the post-accident situation, the $b_{11} T_{amb}$ term should be replaced with $b_{11} T_{amb} + (2r_f w_{11} t_f q_s'' / w)$, where q_s'' is the solar heat flux normal to the projected area of the cask ($\sim 900 \text{ W/m}^2$, max) and α_s is the absorptivity of the cask & fins for solar radiation (~ 1.0).

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