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SAFETY ANALYSIS REPORT

on

THE SNAP-27 GROUND SHIPPING CASK

to

**UNITED STATES ATOMIC ENERGY COMMISSION
ALBUQUERQUE OPERATIONS OFFICE**

February 3, 1967

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I. INTRODUCTION

This report presents a safeguards evaluation of the design of the SNAP-27 Ground Shipping Cask developed by Battelle Memorial Institute for General Electric Company, Missiles and Space Division. The purpose of the cask is to transport a single SNAP-27 fuel capsule assembly by common carrier. It is the purpose of this report to show that the cask and its components are designed to meet and surpass the design requirements of General Electric Company Specification NS 0110-07-02-B, AEC Regulations 10 CFR 71 and 10 CFR 72, and ICC Regulations 47 CFR 71-78.

II. SUMMARY

The Ground Shipping Cask is designed to contain a single SNAP-27 Pu-238 fuel capsule assembly for purposes of common carrier transportation. The maximum weight of the loaded cask (including skid) will not exceed

1500 lb. In size, the cask is a 30-inch-diameter cylinder (including fins), 39 in. long, and requires a shipping space of 44 in. in height, 36-in. width, and 48 in. in length. For shipping readiness, the cask is bolted in a vertical position to a structural steel shipping skid. A ring is welded to the cover plug for access to the cavity; a pair of lugs are attached to the external fins for lifting the cask; and four holes are drilled into the skid beams for securing the unit to the transport vehicle. Three sealed charges of metallic particle coolant, funnel, the gas sampling apparatus and used coolant container will be provided in a tool box attached to the shipping skid.

Cask Description

The GSC contains five major components: cask body assembly, cover assembly, center weldment assembly, gage and valve assembly, and shipping structure. These components and the details of design are contained in engineering drawings listed in Table 1. The cask body is almost entirely fabricated of 304 stainless steel, with the internal and external fins being made of copper. The entire external cask and fin surface is plated with 0.001 in. minimum thickness of electroless nickel. The inside surfaces of the cask are machine-finished stainless steel. Figure 1 is a schematic which illustrates the main features of the cask.

The cylindrical cask body is a single 2-inch-diameter shell 36 in. long, of 0.5-inch-thick stainless steel, and designed as a pressure vessel to withstand maximum internal pressure of 75 psi. A mixture of water-ethylene glycol solution fills the annulus formed by the inner cavity and the cask shell, and serves as both a shielding and heat-transfer medium. There are 24 internal copper fins, tangentially adjoined to the

TABLE 1. MAJOR SUBASSEMBLY DRAWINGS

BMI Drawing No.	Drawing Title
SN 0001	Top assembly
SN 0002	Skid
SN 0003	Cover assembly
SN 0004	Upper weldment assembly
SN 0005	Center weldment assembly
SN 0006	Lower weldment assembly
SN 0007	External fin
SN 0008	Gage and valve assembly
SN 0026	Inner cavity weldment
SN 0027	Internal heat fin

FIGURE 1. CROSS SECTION OF SNAP-27 GROUND SHIPPING CASK

inner cask shell and the inner cavity weldment. The bottom plate of the cask is welded to the cylindrical shell and is reinforced with eight gussets welded between the plate and the shell. There are 24 externally welded fins $1/4$ in. x 5 in. x 39 in. for heat dissipation purposes.

The top cover assembly consists of a flat circular plate welded atop a $7-3/8$ -in.-diameter cylinder, measuring $9-3/4$ in. in length. The cylinder is filled with a water-ethylene glycol solution for shielding and cooling purposes. Ten stainless steel studs attach the cover assembly to the cask, and a lifting ring is welded onto the cover plate. The cover assembly, guided by two alignment pins, fits snugly above the fuel basket cavity. Its weight, with coolant-shielding solution, is about 35 pounds.

The center weldment assembly contains as a subassembly the inner cavity weldment. The latter is the specially designed fuel cavity enclosure with internal dimensions $2-3/4$ in. in diameter by 14 in. long. This cavity contains the SNAP-27 fuel capsule assembly. The cavity material is 304 stainless steel; it is positioned and held in place by the 24 internal copper fins. The cavity design is unique in that it has provision for remotely filling and draining the annular cavity (between the fuel capsule and inner cavity weldment) with a metallic particle coolant after the cask enclosure head has been shut and sealed. Access to the inner cavity fuel element coolant is through the tubing attached at the cask top and cask lower side, and forms a completely sealed primary coolant system. Fill and drain access are through simple pipe plugs, and there are provisions at the valves for gas-sampling connections. For draining the metallic particle coolant, special connector equipment will be provided along with a drain receptacle to provide safe removal of the hot coolant.

The secondary coolant (water-ethylene glycol) is filled through a pipe plug opening in the upper weldment assembly to a level within 1 in. of the cask ceiling. To monitor pressure buildup within the cask and to vent any anticipated overpressure, a relief valve-pressure gage combination is mounted in a housing near the top side of the cask. A standpipe, positioned to within 1/2 in. of the cask ceiling, is connected to the valve-gage combination. The gage will read from 30 to 0 in. of mercury vacuum and from 0 to 100 psig. The relief valve poppet will vent at 75 psi. There is no special drain feature for the secondary coolant in either the cask or the cover assembly.

The shipping structure is a base made from standard structural H-beam shapes. These "skids" are designed to transmit the lateral loads encountered during shipment from the top of the casks into the base and in turn to the vehicle bed. There is no serious loading/weight problem that normally exists with most fuel casks.

Heat Transfer

Decay Heat Removal. An important objective of the cask's configuration is to assure that the decay heat generated by the fuel capsule is effectively and safely dissipated. Heat rejection has been integrated into the cask's design in the following manner. The fuel capsule assembly is contained within a loose-fitting stainless steel chamber - the inner cavity weldment. The clearances and voids in the space remaining is filled with the highly heat conductive and fluid-like nickel particle coolant. (The advantages of using metallic particles as the primary coolant is discussed in the coolant section of this report.) A complement of twenty-four

1/16-in.-thick copper sheets are welded on one side to the inner cavity weldment (tangentially), and on the other side to the interior of the cask shell. The annular space between the inner cavity weldment and the shell is filled with the secondary coolant - water-ethylene glycol. The secondary coolant virtually surrounds the fuel magazine, and is also used to fill the cask's cover plug. Lastly, twenty-four 1/4-in.thick copper cooling fins are welded vertically around the periphery of the cask shell. Hence, the cask features a direct conduction path leading from the fuel capsule to the outer surface of the external fins. Heat is also transferred by the normal modes of convection and radiation from the inner cavity weldment and internal fins to the secondary coolant, and thus outward to the cask shell, external fins and into the atmosphere.

Heat Analysis. The cask has been analyzed to predict the key temperature parameters covering all postulated modes of operation and to assure adherence to temperature conditions specified in General Electric Document NS 0110-07-02-B. Under normal operational conditions, the cask will have a surface temperature of 170 F and fuel capsule surface temperature of 345 F on a hot 100 F day. Considering the worst conceived combination of natural environmental conditions, viz., a cask surface emissivity of 0.2 and an ambient temperature of 160 F, the exterior temperature will rise to 230 F and the fuel capsule surface temperature will rise to 445 F. A loss of the primary coolant (only) will not effect the cask surface temperature of 170 F for a 100 F day; however, the fuel capsule temperature will rise to 932 F. A loss of secondary coolant (only), i.e., water-ethylene glycol solution will result in a fuel capsule temperature rise to 417 F for the 100 F day.

Solar Load. Solar conditions are expected to impose a heat load of 390 Btu/hr on the cask for a 100 F ambient condition in a typical Deep South Location. This is not expected to contribute more than 8 percent of the total heat load for normal operating conditions.

Fire. Under AEC regulations, the cask must be analyzed for survival and containment for a potential fire hazard. Calculations indicate a 1475 F 1/2-hour fire will heat the cask's exterior surface to 431 F and the fuel capsule temperature will reach 942 F.

Structural Integrity

The cask has been analyzed to study the maximum structural integrity and to prove conformity with all AEC and ICC regulations. The cask is designed to provide complete protection to the fuel capsule assembly for all credible mechanical damage in normal shipping and storage. The capsule is supported within the cask's inner cavity weldment by a "cushion" of nickel particles which also serve as the primary coolant.

The closure head is bolted to the cask by means of 10 high-strength studs. The studs pass through the 0.5-in.-thick top plate and are welded in place. The cask body - a rolled cylinder - is a welded assemblage of three major sections: upper, center, and lower weldment assemblies. Each assembly is complete, i.e., with the corresponding internal subassembly configuration - fins, gussets, inner cavity, tubing - before the three sections are welded together to form the cask assembly. The 24 external fins are welded on and the cask is complete except for the minor nonstructural finishing touches.

Even though the cask's secondary coolant and cavity is rated at 75 psi, the normal operating pressure is expected to remain below 50 psi. A 1-in. expansion space is maintained between the ceiling and the liquid level in the cask. Analyzed as a pressure vessel, the cask body will have a design factor of safety of 27; the bottom plate, with its eight reinforcing gussets, will have a factor of safety of 2.15 at the center and 5.6 at a point near the gussets. The top plate is gusseted similar to the bottom plate and the same factors of safety can be conservatively assumed. The full penetration butt-welds of the cask girth joint can absorb a 1500 psi longitudinal stress for a 2.33 factor of safety. A beam analysis of the cask, assumed loaded to five times its normal weight and simply supported at its ends, yields a factor of safety of 93.

Trunnions, used as hoisting lugs, are two 1-1/4-in.-diameter pins; each pin is attached to a pair of the external fins and they are located 180 degrees diametrically apart. Each pin can withstand a shear stress of 306 psi for a 16 factor of safety. The pin supports in each fin are more than adequate, with an 83 factor of safety in bearing, 25 in tearout (tension), 15 in tearout (shear) and negligible stress on the longitudinal fin welds.

The four 3/4-in.-diameter bolts attaching the cask to the skid frame were analyzed for a 10 "g" thrusting force load simultaneously with a 5 "g" lateral load and a 2 "g" vertical load. For a maximum bolt loading of 10,500 lb in tension and 4890 lb in shear, the ASTM A325 bolts are rated at 17,670 lb in tension and 6630 lb in shear.

Recent drop tests performed at Oak Ridge National Laboratory verified that the cask wall is sufficiently thick to drop in a 40-in. free-fall onto a 6-in.-diameter bar without puncturing. The cask's capsule containment was also studied by analyzing a 30-ft free-fall drop onto a nonyielding surface. Regarding a free-fall impact on the lid end of the cask, the ten 1/2-in.-diameter retaining bolts can sustain an 18,900-lb static load or 378 times the 50-lb weight of the lid and capsule. The cask need not be counted on for shielding and the resulting gross deformations can be tolerated.

Criticality

An analysis of the SNAP-27 Ground Shipping Cask indicates complete adherence to all regulatory criticality criteria. No criticality problem exists with either a single cask or with two or more casks juxtaposed.

The neutron spectrum of the GSC is thermalized due to the presence of water coolant-shield. If the water were lost, the neutron spectrum would shift to a higher spectrum, thereby resulting in an effective neutron multiplication factor (K_{eff}) of a higher but still subcritical value.

Through the use of a neutron transport computer code, values of K_{eff} and K_{∞} were calculated to be as follows:

K_{eff}	0.49736
K_{∞}	0.49744.

These values take into account the presence of the water shield (i.e., moderator and reflector), and represent (a) a single isolated GSC and (b) an infinite number of GSC's in close contiguity, respectively.

Shielding

Analyses indicate that the SNAP-27 GSC conforms to the shielding requirement of less than 10 mrem/hr at 1 meter from the cask's surface. The total radiation dose rate (neutron plus gamma) was calculated to be 8.4 mrem/hr at the above distance from the surface of the cask.

The neutron dose rate calculation involved neutron multiplication and K_{eff} relationships for configurations well below critical. On the basis of a neutron multiplication of 1.1, combined with various conservative assumptions, the fast neutron dose rate was determined to be 5.1 mrem/hr.

Gamma radiation was found to contribute 3.3 mrem/hr to the total dose rate. The major dose contributors in the gamma spectrum of radiation were the 0.8 mev group (from ^{238}Pu , ^{212}Bi , and ^{208}Tl) with 2.04 mrem/hr and the 2.6 mev (from ^{208}Tl) group with 0.75 mrem/hr. Capture gamma radiation (from hydrogen in the water shield) accounted for 0.4 mrem/hr.

III. OPERATIONAL HANDLING PROCEDURES

The following outline establishes the step-by-step procedures for performing all anticipated handling functions of the GSC. Appropriately, established radiological controls and surveillance safeguards are to be observed concurrently.

General

Prior to any operation, the cask should be visually inspected for cracks, flaws, tamperage; breakage of lock wire seals should be noted. The surface temperature should be checked, and on a loaded GSC, temperatures should range up to 170 F, depending upon ambient conditions.

Loading Procedures

- (1) Inspect the cask for damage, especially if the cask has been in conveyance.
- (2) Weigh the cask, and if the weight deviates more than ± 5 pounds from the labeled empty weight, adjust the quantity of water-ethylene glycol mixture as necessary. Add mixture through the fill opening in the top. The liquid level should be one inch from the top of the cask. Replace fill plug.
- (3) Remove the cask cavity cover.
- (4) Inspect the cask's fuel capsule cavity for foreign particle contamination and condensation. Remove any foreign particle contamination; see Step 7 for condensation removal.

- (5) Remove the plug in the shot fill line and inspect the line for condensation and foreign material. Remove any foreign particle contamination; treat for condensation as outlined in Step 7.
- (6) Remove cover from metallic particle drain valve housing and inspect drain line for foreign materials or condensation. Remove any foreign particle contamination; treat for condensation as outlined in Step 7.
- (7) Eliminate condensation from any part of the GSC cavity by replacing the cover and purging with dry nitrogen or other dry gas through drain and fill tubes. Purge until all moisture has been expelled.
- (8) Test the six roller guides at the top of the cavity for free rotation.
- (9) Close drain line valve.
- (10) Install plug in open end of drain valve.
- (11) Replace cover plate on drain valve housing.

CAUTION - Before proceeding, be assured that all applicable equipment and facilities are completely prepared for any situation that might occur during fuel capsule transfer. When performing capsule loading operation, hold the Flight Handling Tool and fuel capsule at a distance from the operator's body. MINIMIZE RADIATION EXPOSURE BY REMOVING THE FUEL CAPSULE FROM ITS EXISTING LOCATION INTO THE GSC QUICKLY, BUT CAREFULLY.

- (12) Insert fuel capsule into the GSC using Flight Handling Tool. Remove the tool.

CAUTION - Avoid radiation exposure from the capsule end streaming preceding and during cover installation.

- (13) Install the GSC cover.
- (14) Attach the nuts to the 10 bolts in cover and torque each to 40 foot-pounds.
- (15) Install cover plate over cover-lifting ring.
- (16) Select a sealed shot container with an identification tag, "Dry shot ready for use".
- (17) Remove cover from shot fill valve housing. Remove plug from fill line and insert special fill funnel in plug opening.
- (18) Open the shot container and pour the contents through the funnel into the cask.
- (19) Close the shot fill line valve, replace pipe cap, and replace the fill opening plug.
- (20) Replace the valve housing cover.
- (21) Close the empty shot container and tag it "Empty".
- (22) Secure the shot container in its storage position on the shipping skid.
- (23) Record the GSC exterior surface temperature upon reaching thermal equilibrium.
- (24) Wire seal all cover openings.
- (25) The GSC, when mounted on the skid, is ready for shipment.

Unloading Procedures

A prerequisite to the fuel capsule removal and subsequent handling of the GSC is a receiving inspection, including a radiological survey of the GSC. Verification of acceptable results from the receiving

inspection radiological survey is required before this procedure and subsequent test activities can be initiated. Compliance to this procedure is mandatory every time the fuel capsule is to be removed from the GSC. Appropriate radiological controls and surveillance will be maintained by health physics personnel at all times during performance of this procedure.

- (1) Remove cover from valve housing on the top of the GSC.
- (2) Remove cap from street elbow.
- (3) Install absolute filter to street elbow connection.
- (4) Attach gas sampling equipment to filter.
- (5) Open shot fill line valve.
- (6) Check activity of gas sample from shot fill line.
- (7) If air activity is below tolerance, proceed to Step 8. If above tolerance, initiate contamination control procedure.
- (8) Remove pipe plug from drain valve and connect empty shot container to shot drain valve observing proper radiation control procedures.
- (9) Connect air sampler system (see Figure 2) to tubing outlet on empty container attachment device.
- (10) Open shot drain line valve.
- (11) Take air sample from drain line (see Figure 2). If air activity is below tolerance, proceed to Step 12. If above tolerance, initiate contamination control procedure.
- (12) When shot ceases to flow out of drain line, close the shot drain line valve. (Do not operate valve while shot is flowing.)
- (13) Disconnect shot container from the valve. Monitor shot for contamination.

FIGURE 2. SCHEMATIC FOR DRAINING METALLIC PARTICLE COOLANT FROM GSC AND GAS SAMPLING ARRANGEMENT

- (14) If shot is not contaminated, seal the container and tag it "Used Shot" and secure it on its storage position on the shipping skid. If shot is contaminated, use approved procedure for disposal.
- (15) Install pipe plug in drain valve.
- (16) Remove the 10 bolts from the GSC cover.
- (17) Remove the cover plate from the cover-lifting ring.

CAUTION - RADIATION DANGER - From exposure to capsule end streaming during and subsequent to cover removal.

- (18) Lift the cover off the GSC. (The cover weight is 35 lbs.)
- (19) Determine radiation levels above the opened GSC, and verify allowable exposure times for fuel capsule removal.

CAUTION - Before proceeding, ascertain that all applicable equipment and facilities are ready to receive the fuel capsule. When performing Step 20, hold the Flight Handling Tool and the fuel capsule away from the operator's body. MINIMIZE RADIATION EXPOSURE BY MOVING THE FUEL CAPSULE FROM THE GSC TO THE DESIGNATED RECEPTOR QUICKLY, BUT CAREFULLY.

- (20) Fasten the Flight Handling Tool onto the fuel capsule end plate at the three points provided in the end plate and marked on the GSC top with arrows. Remove the fuel capsule from the GSC vertically, and promptly install the fuel capsule into the designated receptor.
- (21) Replace cover on GSC.

IV. HEAT TRANSFER ANALYSIS

The heat-transfer analysis studies the case of a typical SNAP-27 fuel capsule shipment. Besides the normal operating condition, the analysis will cover two hypothetical situations involving a loss-of-coolant problem and a "standard" shipping fire. Figure 3 is included to give points where temperatures are calculated for the heat-transfer model. Table 2 summarizes the temperatures calculated within the cask for the various cases presented.

Decay Heat

The design of the GSC is based on an internal decay heat generation of 1.5 kw (5118 Btu/hr).

Solar Heat

From Marks' Handbook*, the solar heat absorbed by the cask's vertical and horizontal surfaces is defined as:

$$Q_s = 442 \tau [\alpha_H A_H \cos \theta_H + \alpha_V A_V \cos \theta_V] , \quad (1)$$

where

442 = Solar constant

τ = Atmospheric transmittance = 0.6

α = Absorptivity of the surface

A = Projected area of surface, sq. ft.

* Mechanical Engineers' Handbook, Edited by L. S. Marks, Sixth Edition, McGraw-Hill Book Company, New York (1958), "Solar Energy for Heating", (H. C. Hottel) pp 12-114.

FIGURE 3. GSC HEAT TRANSFER MODEL

TABLE 2. CASK TEMPERATURES

Case	T _{ambient} , °F	T ₁ , °F	T ₂ , °F maximum	T ₃ , °F	T ₄ , °F
Normal operation, cask surface emissivity = 0.5	100	170	188	211	345
Normal operation, cask surface emissivity = 0.2	160	230	253	310	445
Loss of primary coolant*	100	170	188	211	932
Loss of secondary coolant*	100	170	224	278	417
Shipping fire	1475 (1/2 hr)	431	321	803	942

* Assumes fuel capsule will be shipped coated.

and

$$\cos \theta = \cos (\phi - \beta - \zeta) ,$$

where

ϕ = Latitude

β = Tilt of the surface from horizontal toward South

ζ = 23.5° at summer solstice.

The solar heat for a warm U.S. location at 27 degrees latitude (Cape Kennedy) is used for conservatism. Therefore,

$$\cos \theta_V = \cos (27 - 90 - 23.5) = 0.0611 ,$$

$$\cos \theta_H = \cos (27 - 0 - 23.5) = 0.998 ,$$

$$A_H = 0.785 \left(\frac{20}{12}\right)^2 = 2.18 \text{ sq. ft.} ,$$

$$A_V = \frac{(20 + 10) \times 36}{144} = 7.5 \text{ sq. ft.} ,$$

$$\alpha_V = 0.85^* ,$$

$$\alpha_H = 0.50^* .$$

The solar heat for the cask is computed as

$$\begin{aligned} Q_S &= 442(0.6)(0.50)(2.18)(0.998) + 442(0.6)(0.85)(7.5)(0.0611) , \\ &= 288 + 102 = 390 \text{ Btu/hr, or about 8 percent of the total} \\ &\text{heat load.} \end{aligned}$$

* The 0.5 surface emissivity (absorptivity) is used for conservatism.
See p 24 for calculation of α_V .

Heat Transfer for Normal Operating Conditions

External Heat Transfer

Two operating cases are considered under normal operation. In the first case, the cask is assumed to be rejecting heat from its surface having an emissivity of 0.5 to 100 F ambient air. In the second case, the cask is assumed to have been subjected to natural environmental conditions which result in a surface emissivity of 0.2. It is further assumed in the second case that the cask surface is rejecting heat to a 160 F environment.

In both cases, the heat is rejected from the outside surface of the cask by radiation and natural convection to air. The heat transferred by radiation is:

$$Q_r = 0.173 \epsilon_r A_r \left[\left(\frac{T_o}{100} \right)^4 - \left(\frac{T_a}{100} \right)^4 \right], \quad (2)$$

and the heat transferred by convection is

$$Q_c = h_c A_c (T_o - T_a), \quad (3)$$

where

ϵ_r = Surface emissivity

A_r = Heat-transfer area for radiation

A_c = Heat-transfer area for convection

T_o = Cask surface temperature

T_a = Ambient temperature

$$h_c = 0.18 (T_o - T_a)^{1/3*}$$

= Convection heat-transfer coefficient.

Experimental testing of this type cask indicates that the ends of the cask will be as hot as the sides. Therefore, heat loss (and solar heating) from the ends is included in the calculation of the surface temperature.

Twenty-four nickel-coated copper fins are attached circumferentially around the cask at a separation adequate to assure isolation of each fin. The convection heat transfer from the fins can, therefore, be analyzed by conventional techniques. The effectiveness of these fins is:

$$\eta = \frac{\tanh b^{**}}{b}, \quad (4)$$

where

$$b = \ell \left(\frac{2h_c}{kY_o} \right)^{1/2} \quad (5)$$

ℓ = Fin height = 5 inches

k = Thermal conductivity of copper ≈ 218 Btu/hr ft F

Y_o = Fin thickness = 1/4 inch.

If the outer surface of the cask is 170 F, the heat-transfer coefficient can be computed as 0.73 and the fin effectiveness is approximately 0.97. The effective heat-transfer area of the sides for convection is:

$$A'_c = \frac{(\pi \times 20 - 24 (0.25))34}{144} + \frac{0.97(24) (10.25) (38.5)}{144},$$

$$= 13.4 + 65 = 78.4 \text{ sq. ft.}$$

* McAdams, W. H., Heat Transmission, Third Edition, McGraw-Hill Book Company, New York, (1954) p 173.

** Jacob, M., Heat Transfer, Volume One, J. Wiley & Sons, New York, (1956) p 236.

The effective heat-transfer area of the top is

$$A_{TOP} = \frac{0.785(20)^2}{144} = 2.18 \text{ sq. ft.}$$

The area available for convection is then

$$A_c = 78.4 + 2.18 = 80.58 \text{ sq. ft.}$$

The finned side of the cask may be considered a cavity-type radiator. According to Williams*, the emissivity of cavity-type radiators is:

$$\epsilon_o = \frac{1}{1 + \frac{s}{S} \left(\frac{1}{\epsilon_r} - 1 \right)}, \quad (6)$$

where

s = Area of opening

S = area of cavity walls

ϵ_r = emissivity of the walls = 0.5

For this cask, ϵ_o is computed as 0.839. The effective radiation heat-transfer area including the top is thus

$$\begin{aligned} \epsilon_r A_r &= \frac{(0.839)(\pi \times 20)(36)}{144} + 0.5 (2.18), \\ &= 13.2 + 1.09 = 14.3 \text{ sq. ft.} \end{aligned}$$

The total heat-transfer capability of the external surface of the cask is

$$Q = Q_r + Q_c, \quad (7)$$

* Williams, C. S., "Discussion of the Theories of Cavity-Type Sources of Radiator Energy", J. Opt. Soc. Am., Volume 51 (May, 1961), p 568.

or for 170 F surface temperature using Equations (2) and (3),

$$Q_r = (0.173)(14.3) [1570 - 980] = 1460.$$

$$Q_c = 0.73 (80.6) (170 - 100) = 4110.$$

$$Q_T = 1460 + 4110 = 5570 \text{ Btu/hr.}$$

5570 Btu/hr is slightly greater than the solar plus decay heat (5508 Btu/hr), hence, the surface temperature will not exceed 170 F in the normal operating case. For the case of a 0.2 surface emissivity, the cask surface temperature is slightly less than 230 F.

Internal Heat Transfer

The internal temperature drop is composed of the temperature drops through the following series thermal resistances: resistance of cask wall, resistance of the biological shield section, resistance of inner stainless steel ring, and resistance of the nickel particle gap.

(1) Temperature Drop Across Cask Wall. The temperature drop across the cask wall is given by

$$\Delta T = \frac{Q}{2\pi KL} \ln (D_2/D_1) , \quad (8)$$

where

Q = Decay heat = 5118 Btu/hr (ignoring any contribution to heat transfer out the cask ends)

K = 9 Btu/hr ft F = average thermal conductivity of stainless steel

D_2 = outer diameter

D_1 = Inner diameter

L = Vertical length over which the heat diffuses.

Substituting numerical values,

$$\begin{aligned}\Delta T &= \frac{5118}{6.28(9)(35/12)} \ln \left(\frac{20}{19.5} \right) \\ &= 0.769 \text{ F.}\end{aligned}$$

(2) Temperature Drop Across Biological Shield Section. The biological shield section consists of a mixture of water-30 v/o ethylene glycol solution sectioned by 24 thin copper fins. To mathematically model the shield section requires the derivation of fin equations, such as in McAdams* using different boundary conditions. Such a complex procedure is not deemed necessary, however, if limiting cases can be used to show the fuel capsule surface temperature remains below a value considered undesirable for normal operation. In this manner, two following cases are calculated. The temperature drop across the section in both cases is given by

$$\Delta T = QR, \quad (9)$$

where

ΔT = Total temperature drop across the biological shield

Q = Total generated heat = 5118 Btu/hr

R = Thermal resistance of the shield section.

Considering the temperature drop across the biological shield and tangential copper bars, the most conservative case is the one in which no credit is taken for the copper internal fins as if they did not exist. For that case:

* McAdams, W. H., Op. Cit., p 268.

R = sum of the thermal resistances to transfer of heat into the water at the inner stainless steel ring and the transfer of heat out of the water at the cask wall.

$$= R_i + R_o.$$

The liquid flows up the inner ring surface, absorbing heat by natural convection and depositing the heat at the cask wall inner surface. Approximating heat transfer from the inner ring to the cask wall as convection between two parallel plates, the film coefficients for the two surfaces are given by*:

$$h_c = 0.13 \left(\frac{K_f}{L} \right) \left[\frac{L^3 \rho_f^2 g \beta_f \Delta T}{\mu_F^2} \left(\frac{c_p \mu}{K} \right) \right]^{1/3},$$

$$= 0.13 \left(\frac{K_f}{L} \right) (N_{Gr} N_{pr})^{1/3}, \quad (10)$$

where

h_c = Film heat-transfer coefficient, $\frac{\text{Btu}}{\text{hr sq. ft. F}}$

K_f = Thermal conductivity of the fluid evaluated at the film temperature, Btu/hr ft F

L = Characteristic length = vertical length of surface in this instance, ft

N_{Gr} = Grashof number computed at T_f , dimensionless

N_{pr} = Prandtl number computed at T_f , dimensionless.

A trial-and-error procedure must be used to compute ΔT .

* McAdams, Op. Cit., p 172.

Trial 1. Assuming that $\Delta T = T_i - T_o = 80$ F, T_i is $170.8 + 80 = 250.8$ F, the bulk temperature of the liquid is computed (as a first approximation) to be the average of the two wall temperatures. Therefore,

$$T_b = 170.8 + 40 = 210.8 \text{ F} = \text{bulk temperature of the liquid.}$$

For the inner ring surface,

$$T_f = T_i - \frac{\Delta T_i}{2} = \text{film temperature for inner ring surface.}$$

$$\Delta T_i = 250.8 - 210.8 = 40 \text{ F} = \text{temperature drop from inner ring surface to the bulk liquid.}$$

Therefore,

$$T_f = 250.8 - (40/2) = 230.8 \text{ F.}$$

From Figure 4, where N_{pr} versus temperature is plotted using weight-averaged properties of a water - 30 percent glycol solution:

$$N_{pr} = 4.4 \text{ at } 230.8 \text{ F. } \sim \text{value} \approx 3 \times \text{value for pure H}_2\text{O}$$

Figure 5 is a plot of $N_{Gr}/L^3 \Delta T$ versus temperature, constructed using weight-averaged properties of a water - 30 percent glycol solution:

$$\begin{aligned} N_{Gr} &= 2.9 \times 10^7 L^3 \Delta T_i \text{ at } 230.8 \text{ F,} \\ &= 2.9 \times 10^7 (1.25)^3 (40) = 2.26 \times 10^9, \end{aligned}$$

$$N_{Gr} N_{pr} = 9.95 \times 10^9,$$

hence,

$$h_{ci} = 0.13 \left(\frac{0.313}{1.25} \right) (9.95 \times 10^9)^{1/3} = 69.8 \frac{\text{Btu}}{\text{hr sq. ft. F}}.$$

$$R_i = \frac{144}{(69.8)(\pi \times 3.25 \times 15)} = 13.47 \times 10^{-3}.$$

FIGURE 4. PLOT OF PRANDTL NUMBER VERSUS TEMPERATURE
For 30% solution H_2O -glycol

FIGURE 5. PLOT OF GRASHOF NUMBER/ ΔT L^3 VERSUS TEMPERATURE

For the cask wall inner surface

$$T_f = T_o + \frac{\Delta T_o}{2} = \text{film temperature for cask wall inner surface,}$$

$$\Delta T_o = 210.8 - 170.8 = 40 \text{ F} = \text{temperature drop from bulk liquid to cask wall inner surface,}$$

therefore,

$$T_f = 170.8 + (40/2) = 190.8 \text{ F.}$$

From the graphs noted, at $T_f = 190.8$,

$$N_{pr} = 7.67.$$

$$N_{Gr} = 1.0 \times 10^7 (2.92)^3 (40) = 9.95 \times 10^9.$$

$$N_{Gr} N_{pr} = 76.4 \times 10^9.$$

$$h_{co} = 0.13 \left(\frac{0.313}{2.92} \right) (76.4 \times 10^9)^{1/3} = 59.2 \frac{\text{Btu}}{\text{hr sq. ft. F}}.$$

$$R_o = \frac{144}{59.2 (\pi \times 19.5 \times 35)} = 1.131 \times 10^{-3}.$$

$$R = (13.47 + 1.131)10^{-3} = 14.60 \times 10^{-3}.$$

For these calculations, the bulk temperature was taken as the linear average of the two wall temperatures. This only relative inaccuracy serves as a starting point for the trial-and-error calculations required. The actual bulk temperature is somewhat lower because the temperature gradient at the two walls differs. Apportioning temperature drops according to film resistances,

$$\Delta T_i = \frac{R_i}{R} \Delta T.$$

Trial 2. For ΔT assumed as 80 F as before,

$$\Delta T_i = \frac{13.47}{14.6} \times 80 = 73.7 \text{ F.}$$

Taking ΔT_i as 70 F and repeating the previous calculations, it is found that

$$R_i = 11.88 \times 10^{-3}.$$

$$R_o = 4.28 \times 10^{-3}.$$

$$R = 16.16 \times 10^{-3}.$$

$$\Delta T_i = 59.8 \text{ F.}$$

Trial 3. Since ΔT_i still does not correspond to its assumed value, the calculation is repeated with $\Delta T_i = 64 \text{ F}$. When 64 F is assumed, the following is computed:

$$R_i = 12.2 \times 10^{-3}.$$

$$R_o = 3.53 \times 10^{-3}.$$

$$R = 15.73 \times 10^{-3}.$$

$$\Delta T_i = 62 \text{ F.}$$

Trial 4. A final calculation is made with ΔT_i assumed equal to 63 F. In this case

$$T_b = 170.8 + 17 = 187.8 \text{ F.}$$

For the inner ring surface

$$T_f = 250.8 - 31.5 = 219.3 \text{ F.}$$

$$N_{pr} = 5.15.$$

$$N_{Gr} = 2.07 (10)^7 (1.25)^3 (63) = 2.54 \times 10^9.$$

$$N_{Gr} N_{pr} = 13.1 \times 10^9.$$

$$h_{ci} = 0.13 \left(\frac{0.313}{1.25} \right) (13.1 \times 10^9)^{1/3} = 76.7 \frac{\text{Btu}}{\text{hr sq. ft. F}}.$$

$$R_i = \frac{144}{(76.7) (\pi \times 3.25 \times 15)} = 12.3 \times 10^{-3}.$$

For the cask wall inner surface,

$$T_f = 170.8 + 8.5 = 179.3.$$

$$\Delta T_o = 80 - 63 = 17.$$

$$N_{pr} = 9.1.$$

$$N_{Gr} = 0.07 (10)^7 (2.92)^3 (17) = 2.96 \times 10^8.$$

$$N_{Gr} N_{pr} = 2.69 \times 10^9.$$

$$h_{co} = 0.13 \left(\frac{0.313}{2.92} \right) (2.69 \times 10^9)^{1/3} = 19.38 \frac{\text{Btu}}{\text{hr sq. ft. F}}.$$

$$R_o = \frac{144}{19.38 (\pi \times 19.5 \times 35)} = 3.47 \times 10^{-3}.$$

Hence,

$$R = 15.77 \times 10^{-3},$$

and checking ΔT_i ,

$$\Delta T_i = \left(\frac{12.3}{15.8} \right) 80 = 62.3 \approx 63 \text{ F.}$$

With reference to Equation (9),

$$\Delta T = 5118 (15.77 \times 10^{-3}) = 80.8 \text{ F.}$$

Since 80 F total ΔT was the assumed value, the solution has been converged on. If no credit is taken for the fins, the bulk temperature of the water within the shielding section is about 188 F and the temperature

change across the shielding is 81 F. The bulk temperature is well below the temperature for which glycol-water solution vaporizes at the relief valve setting (75 psig), hence, no vaporization will occur under normal operating conditions. Furthermore, the ΔT of 81 F is very conservative because no allowance is made for the internal copper fins. In the case of a cask surface temperature of 230 F, the ΔT across the biological shield can be computed similarly. The ΔT in that case is approximately 100 F.

To study internal fin effects, the following calculations are similar to those used for the limiting case where no fins are accounted for. Assuming that $\Delta T = T_i - T_o = 39.2$ F, T_i is $170.8 + 39.2 = 210.0$ F, the temperature drop from the inner ring into the bulk liquid is computed as before by apportioning according to film resistances. Therefore,

$$\Delta T_i = 30.4 \text{ F.}$$

$$T_b = \text{bulk liquid temperature} = 170.8 + 8.8 = 179.6 \text{ F.}$$

For the inner ring surface,

$$T_f = 210 - \frac{30.4}{2} = 194.8 \text{ F.}$$

From Figures 4 and 5,

$$N_{pr} = 7.12.$$

$$N_{Gr} = 1.2(10)^7 (1.25)^3 (30.4) = 7.11 \times 10^8.$$

$$N_{Gr} N_{pr} = 5.06 \times 10^9.$$

$$h_{ci} = 0.13 \left(\frac{0.313}{1.25} \right) (5.06 \times 10^9)^{1/3} = 55.9 \frac{\text{Btu}}{\text{hr sq. ft. F.}}$$

For the cask inner wall surface,

$$T_f = 170.8 + 4.4 = 175.2 \text{ F.}$$

$$\Delta T_o = 8.8 \text{ F.}$$

$$N_{pr} = 9.5.$$

$$N_{Gr} = 0.6 (10)^7 (2.92)^3 (8.8) = 1.311 \times 10^8.$$

$$N_{Gr} N_{pr} = 1.311 (10)^8 (9.5) = 1.247 \times 10^9.$$

$$h_{co} = 0.13 \left(\frac{0.313}{2.92} \right) (1.247 \times 10^9)^{1/3} = 15 \frac{\text{Btu}}{\text{hr sq. ft. F.}}$$

An average h for the internal fins is next computed. At the base of the fin (inner ring surface), the fin heat-transfer coefficient equals h_{ci} (55.9 Btu/hr sq. ft. F). At some point along the fin the temperature-driving force between the fin and the bulk liquid becomes zero. At this point, the fin heat-transfer coefficient is zero, since it is temperature dependent. Hence,

$$\begin{aligned} h_m &= \text{mean fin heat-transfer coefficient} = \frac{55.9 + 0}{2} . \\ &= 27.9 \frac{\text{Btu}}{\text{hr sq. ft. F.}} \end{aligned}$$

Since the point where the driving force becomes zero is not known - (which requires the solution of rigorous mathematical equations describing convection from fins with both ends attached) - it is assumed that this point is at a distance two-thirds of the actual fin height. Using Equation (4) and substituting

$$\ell = 0.5 \text{ ft,}$$

$$K = 218 \text{ Btu/hr ft F,}$$

$$Y_o = 1/16 \text{ inch,}$$

$$b = 0.5 \left[\frac{(2)(27.9)(12)}{(218)(1/16)} \right]^{1/2} = 3.51,$$

the fin effectiveness, η , is calculated as 0.284. The area of the fins is computed as

$$A_f = \frac{2(15 \times 6 \times 24)(0.284)}{144} = 8.51 \text{ sq. ft.}$$

If the effective fin length were taken as the total actual copper length, roughly 9 inches instead of 6 inches, η is computed as 0.190 rather than 0.284. The area of the fins is computed as

$$A_f = \frac{2(15 \times 9 \times 24)(0.190)}{144} = 8.5 \text{ sq. ft.}$$

It is obvious that the decrease in effectiveness offsets the increase in effective fin length. The various resistances can now be computed as

$$R_i = \frac{144}{55.9 (\pi \times 3.25 \times 15)} = 16.9 \times 10^{-3}.$$

$$R_o = \frac{144}{15 (\pi \times 19.5 \times 35)} = 4.47 \times 10^{-3}.$$

$$R_f = \text{fin resistance} = \frac{1}{27.9(8.5)} = 4.2 \times 10^{-3}.$$

The fin resistance and the resistance of the inner ring surface are thermally in parallel, therefore

$$R_h = \frac{1}{\frac{1}{R_i} + \frac{1}{R_f}} = 3.38 \times 10^{-3},$$

and the total resistance to the transfer of heat across the shielding section is

$$R = R_h + R_o = 7.85 \times 10^{-3}.$$

Checking ΔT_i to ascertain that the correct value was chosen,

$$\Delta T_i = \frac{R_i}{R_i + R_o} (\Delta T) = 0.79 (39.2) = 31 \text{ F},$$

which is close enough to the 30.4 F value assumed. From Equation (9)

$$\Delta T = 5118 (7.85 \times 10^{-3}) = 40.2 \text{ F}.$$

Since a total ΔT of 39.2 F was assumed, the solution is considered converged on.

In both of the analyses above, all of the heat was assumed to be transferred out the cask sides. For a short container such as the GSC, the ends will be nearly as hot as the sides and approximately 10 percent of the decay heat is transferred out the ends. If the ends are considered, the temperature drop across the shielding section is reduced by about 4 F.

Based on the above calculations, conclusions can be drawn regarding heat transfer across the biological shield for normal operating conditions. For the conservative analysis, where the effect of internal fins was neglected, temperature drops of 100 F and 81 F were obtained for the cases identified by ambient air temperatures of 160 F and 100 F. For the design analysis, the corresponding temperature drops were 76 F and 36.2 F, respectively.

Temperature Drop Across the Inner Stainless Steel Ring

Using Equation (8) and substituting for the inner stainless steel ring

$$\Delta T = \frac{5118}{6.28(9)(15/12)} \ln \left(\frac{3.25}{2.75} \right).$$

$$= 12 \text{ F.}$$

Temperature Drop Across the Nickel-Particle-Filled Gap

The resistance of the nickel-particle-filled gap is computed from Equation (8). In Equation (8) the thermal conductivity, K , must be replaced with the equivalent conductivity for a particle bed. From experiments carried out at Battelle-Columbus with the GE-MSD fuel capsule simulator and nickel shot of the size and packing expected, K_{eq} is computed as 0.348 Btu/hr ft F over the range of temperatures being considered.* The gap thickness will be about 90 mils. Hence,

$$\Delta T = \frac{5118}{(6.28)(15/12)(0.348)} \ln \left(\frac{D_2}{D_1} \right).$$

$$= 1880 \ln \left(\frac{D_2}{D_1} \right).$$

$$D_2 = 2.75 \text{ in.}$$

$$D_1 = 2.75 - 0.18 = 2.57 \text{ in.}$$

$$\Delta T = 1880 (0.0676) = 127 \text{ F.}$$

* See discussion of K_{eq} in Cooling Media Section.

If the temperature drops for the normal operating cases are summed, the following summary table can be constructed.

TABLE 3. CASK TEMPERATURE FOR NORMAL OPERATING CONDITIONS

Surface Emissivity	Ambient Tempera- ture, F	Cask Surface Temper- ature, F	Maximum Bulk Liquid Temperature, F	Fuel Capsule Surface Temperature, F
0.5	100	170	188	~ 345
0.2	160	230	253	~ 445

GE-MSD has specified that the cask surface temperature shall not exceed 180 F and that the fuel capsule temperature be no greater than 350 F under normal operating conditions. These criteria are, as shown, met. It is also stipulated that the maximum fuel capsule temperature should not exceed 450 F under the conditions for which the outside surface of the cask is fouled. Again this requirement is met. The cask is designed for an operational pressure of 50 psig. The boiling point of glycol-water solution at 50 psig is in excess of 298 F. As shown in the table, neither of the bulk liquid temperatures approach 298 F. Hence, the biological shield will be contained under both of the conditions.

Heat Transfer in the Loss-of-Coolant Case

The loss-of-coolant case is taken to mean that either the primary coolant (metallic particle) or secondary coolant (water-glycol, etc., solution) is lost but both are not lost simultaneously. This

is in keeping with the practice of assuming two accidents do not occur at the same time.

Loss of Primary Coolant

Should all of the metallic particles drain from around the fuel capsule, the heat from the capsule must be rejected to the inner stainless steel ring by:

- (1) radiation
- (2) conduction across an air gap
- (3) natural convection.

Jacob* states that for gaps of less than 1/4 inch, the natural convection heat transfer becomes negligible. Therefore, only Mechanisms 1 and 2 above operate to remove heat from the fuel capsule surface.

Conduction across the air gap can be described by rearranging Equation (8). To compute the thermal resistance of the gap

$$R_c = \frac{2\pi KL}{\ln (D_2/D_1)} \quad (11)$$

The resistance to radiation can be described by

$$R_r = \frac{1}{h_r A_1} \quad (12)$$

where

h_r = radiation heat-transfer coefficient defined in Perry's**

A_1 = surface area of fuel capsule sides.

* Jacob, M., Op. Cit., p 540.

** Perry, John H., Chemical Engineers Handbook - 3rd Edition, McGraw-Hill Book Company, 1950, p 473.

In the normal operating case, which is the case occurring when the cask outer surface is not fouled, the temperature "seen" by the fuel capsule under loss-of-primary-coolant operation is 223 F. Inserting the known geometric parameters, $D_2 = 2.75$ inches, $D_1 = 2.57$ inches, and $L = 1.25$ feet, along with the thermal conductivity of air at ~ 900 F into Equation (10),

$$R_c = \frac{(6.28)(0.031)(1.25)}{\ln \left(\frac{2.75}{2.57} \right)} = 3.6 \frac{\text{F-hr}}{\text{Btu}} .$$

Assuming the capsule surface temperature is 935 F and the capsule surface emissivity is 0.9,

$$h_r = 8.26 \text{ Btu/hr ft}^2 \text{ F} .$$

$$A_1 = \frac{\pi(2.57)(15)}{144} = 0.841 \text{ sq. ft} .$$

$$R_r = 0.144 \text{ F-hr/Btu} .$$

$$R_T = \frac{1}{\left(\frac{1}{R_r} + \frac{1}{R_c} \right)} = \frac{1}{6.95 + 0.28} = 0.1383 \frac{\text{F-hr}}{\text{Btu}} .$$

From Equation (9),

$$\Delta T = 5118 (0.1383) = 709 \text{ F} ,$$

hence, the capsule surface temperature will not exceed 932 F.

The calculation just made is for the case of shipment of a fuel capsule coated with a material of high emissivity. If the fuel capsule is not coated, the surface emissivity of the capsule is 0.3 rather than 0.9. For shipment of the uncoated capsule, the maximum surface temperature would be 1380 F as can be shown using Equations (11) and (12).

Loss of Secondary Coolant

For the case where the secondary coolant is lost, heat must be transferred from the inner stainless steel ring to the cask wall through the copper internal fins and into the air surrounding the fins. For natural convection and conduction through the air, Jacob* gives the following equations:

$$\frac{K_{c,v}}{K} = 0.065 (N_{Gr})_L^{1/3} \left(\frac{H}{L}\right)^{-1/9}, \quad (13)$$

$$Q_{c,v} = \frac{K_{c,v} A_{lm} \Delta T}{L}, \quad (14)$$

where

$K_{c,v}$ = an equivalent conductivity for vertical plates which accounts for both natural convection and conduction.

K = thermal conductivity of air.

$(N_{Gr})_L$ = Grashof number based on plate spacing.

H = vertical height of plate.

L = plate spacing.

$$A_{lm} = \text{log mean area} = \frac{A_1 - A_2}{\ln (A_1/A_2)}.$$

For radiation across the air space, the equation to be used is

$$Q_r = 0.173 \mathcal{J}_{12} A_1 \left[\left(\frac{T_1}{100}\right)^4 - \left(\frac{T_2}{100}\right)^4 \right], \quad (15)$$

* Jacob, M., Op. Cit., p 538.

where

T_1 = inner ring surface temperature,

T_2 = cask wall surface (inner) temperature,

$\mathcal{F}_{12}$ = the fraction of the radiant energy leaving nonblack surface A_1 (the surface of the inner stainless steel ring) and reaching nonblack surface A_2 (the cask wall),

$$\mathcal{F}_{12} = \frac{1}{\left(\frac{1}{\epsilon_1} - 1\right) + \frac{A_1}{A_2} \left(\frac{1}{\epsilon_2} - 1\right) + \frac{1}{\bar{F}_{12}}}, \quad (16)$$

where

ϵ_1 = surface emissivity of the inner stainless steel ring.

ϵ_2 = surface emissivity of the cask wall.

$\bar{F}_{12}$ = the fraction of the radiant energy leaving the inner ring (considered as a black body) which reaches the cask wall.

A_1 = surface area of inner stainless steel ring less copper bar area.

A_2 = surface area (inside) of cask wall less copper bar area.

For the case in which there is a single source and sink and these surfaces cannot see themselves and

$$\bar{F}_{12} = \frac{A_2 - A_1 F_{12}^2}{A_1 + A_2 - 2A_1 F_{12}}, \quad (17)$$

where

F_{12} = the fraction of the radiation leaving black surface A_1 in all directions which is intercepted by surface A_2 .

Assuming that the internal copper bars exchange negligible heat with the gas surrounding them (negligible fin effect), the preceding equations can be used in our case.

The heat transferred through the copper bars by conduction is given by an equation similar to Equation (14). Hence, the total heat removed in the loss-of-the-secondary-coolant case is given by

$$Q = \frac{K_{c,v}}{L} A_{lm} \Delta T + \frac{K_{cu} A_{cu}}{L} \Delta T + 0.173 \mathcal{F}_{12} A_1 \left[\left(\frac{T_1}{100} \right)^4 - \left(\frac{T_2}{100} \right)^4 \right]. \quad (18)$$

By a trial-and-error technique using the numerical information,

$$L \simeq 9 \text{ inches} = 0.75 \text{ ft.}$$

$$A_1 = 0.901 \text{ sq. ft. (area of outside surface of inner ring less fins).}$$

$$A_2 = 6.21 \text{ sq. ft. (area of inside surface of cask wall less fins - 15 inches long)}$$

$$A_{lm} = 2.75 \text{ sq. ft.}$$

$$A_{cu} = 0.16 \text{ sq. ft. (cross sectional area of all copper fins)}$$

$$K_{cu} = 218 \text{ Btu/hr ft F.}$$

$$\mathcal{F}_{12} = 0.439.$$

$$T_2 = 170.8 \text{ F} = 630.8 \text{ R.}$$

The surface temperature of the inner ring is found to be slightly less than 277.8 F. The terms in Equation (18) are computed as 133.2, 4960, and 94.1 Btu/hr, respectively. It is obvious that the only significant mode of heat removal is conduction through the copper bars. In the case of secondary coolant loss without loss of primary coolant, the surface temperature of the fuel capsule is 416.8 F.

Summarizing, the fuel capsule surface temperature in the loss-of-primary-coolant-only case is less than 1400 F for the uncoated capsule and 932 F for the coated capsule; when the secondary coolant alone is lost, the fuel capsule surface temperature is 416.8 F. All of these temperatures are well below the melting range for the Haynes Alloy 25 capsule clad, which has a melting range of 2425-2570 F.

Heat Transfer in a Standard Shipping Fire

A standard shipping fire is that simulated environment inflicted upon the cask, as if it were placed in a large oven (uniform radiant heat source) heated to 1475 F. The heat source, in accordance with AEC regulations, is to have a 0.9 emissivity and the cask surface absorptivity of 0.8 is to be taken to suit the analysis conservatively.

The behavior of the cask in a standard shipping fire can best be quantitatively described by an exact differential equation description of the time-temperature relationships existing within the container throughout the duration of the fire. However, a more plausible approach is to approximate the behavior using finite difference equations. The finite difference technique has been proven a more credible approach.

Heat from the fire can reach the container by the mechanisms of radiation and convection. A heat transfer coefficient for natural convection to the surface can be defined as

$$h_c = 0.19 (T_a - T_s)^{1/3} \quad (1)$$

a coefficient for radiation can be defined as

$$h_r = \frac{0.173 \epsilon_r^{1/4} [T_a^4 - T_s^4]}{T_a - T_s} \quad (2)$$

where

T_a = ambient temperature

T_s = surface temperature of the cask.

The interchange factor, $\mathcal{F}_{12}$, between the radiant heat source (oven) and heat sink (shipping container) is defined in Perry's*:

$$\mathcal{F}_{12} = \frac{1}{\frac{1}{\bar{F}_{12}} + \left(\frac{1}{\epsilon_1} - 1\right) + \frac{A_1}{A_2} \left(\frac{1}{\epsilon_2} - 1\right)} \quad (3)$$

where

$\bar{F}_{12}$ = view factor = 1.0

ϵ_1 = 0.9

ϵ_2 = 0.8

A_1 = cask surface string** area

A_2 = area of the source.

If $A_2 \gg A_1$, the most likely case, $\mathcal{F}_{12} = 0.80$. If $A_2 = 2A_1$, which is the minimum probable value of A_2 , then $\mathcal{F}_{12} = 0.767$. A value of 0.8 will be used.

The cask surface temperature is initially at 170 F and the bulk water temperature under normal operating conditions is a maximum of 188 F. As a first approximation, the inner stainless steel ring surface is assumed adiabatic (that this is essentially true is shown later). Choosing as the increment of time $\Delta\theta = 2$ minutes, the following series of calculations can be made to describe the heat transfer during the fire. Figure 6 identifies the parameters used in the following calculations.

* Chemical Engineers Handbook, Edited by John W. Perry, 3rd Ed., McGraw-Hill Book Company, Inc. (1950), "Heat Transfer", (H. C. Hottel), p 489.

** The string area is defined as the area formed by wrapping a string around the outermost extremities of the object seen by the source. In the case of the GSC this is the surface area computed using the fin tip to fin tip diameter of the container.

FIGURE 6. PARAMETERS USED IN FIRE ANALYSIS

From 0 to 1Δθ (Start of Fire Until Two Minutes Have Lapsed)

$$T_{s \ 0\Delta\theta} = 170 \text{ F}$$

In commonly used transient heat transfer calculations (McAdams,* Dusinberre** and others), the initial ambient condition is chosen as the average of the surface temperature slightly before the step change in ambient conditions, and the ambient temperature just after the change.

Therefore,

$$T_{a \ 0\Delta\theta} = \frac{1475 + 170}{2} = 823 \text{ F} = 1283 \text{ R}$$

where

$$T_{s \ 0\Delta\theta} = 170$$

therefore

$$h_c = 0.19 (823 - 170)^{1/3} = 1.638 \text{ Btu/hr ft}^2 \text{ F}$$

$$h_r = \frac{0.173 (.8) \left[\left(\frac{1283}{100} \right)^4 - \left(\frac{630}{100} \right)^4 \right]}{823 - 170} = 5.40$$

conservatively assuming that heat can enter the cask from all sides and from both ends

$$A_c = A'_c + 2 A_{TOP} = 78.4 + 4.36 = 82.8$$

* McAdams, W. H., Heat Transmission, 3rd Edition, McGraw-Hill, 1954 p 45.

** Dusinberre, Numerical Analysis of Heat Flow, 1st Edition, 1949.

$$h_c A_c = (1.638)(82.6) = 135.3 \frac{\text{Btu}}{\text{hr F}}$$

$$R_c = \left(\frac{1}{135.3}\right) \frac{h_r \text{ } ^\circ\text{F}}{\text{Btu}} = \text{resistance to convection of heat to the cask.}$$

$$A_r = \text{String area} = \frac{\pi \times 30 \times 38.5}{144} + 4.36 = 30 \text{ sq ft}$$

$$h_r A_r = (5.4)(30) = 162 \frac{\text{Btu}}{\text{hr F}}$$

$$R_r = \left(\frac{1}{162}\right) \frac{h_r \text{ } ^\circ\text{F}}{\text{Btu}} = \text{resistance to radiation of heat to the cask}$$

The total resistance to the transfer of heat to the cask outer surface is

$$R_s = \frac{1}{\frac{1}{R_c} + \frac{1}{R_r}} = 3.36 \times 10^{-3} \frac{\text{hr F}}{\text{Btu}}$$

The cask wall thermal resistance can be computed as

$$\begin{aligned} R_w &= \frac{X_w}{K_w A_w} = \frac{(1/4)(144)}{(9.1)(\pi \times 19.75 \times 35.5)(12)} \\ &= 0.15 \times 10^{-3} \frac{\text{hr } ^\circ\text{F}}{\text{Btu}} \end{aligned}$$

Based on the calculations made in the normal operating case

$$h_i \sim 20 \text{ Btu/hr ft}^2 \text{ } ^\circ\text{F} = \text{Typical heat transfer coefficient at inside surface of cask outer wall}$$

$$R_i = 3.36 \times 10^{-3} \frac{\text{hr-}^\circ\text{F}}{\text{Btu}} = \text{resistance to convection of heat into the water from the cask wall}$$

In this case the resistance of the internal copper fin should also be accounted for

$$\begin{aligned} R_{FIN} &= \frac{\ell_{FIN}}{KA_{FIN}} \approx \frac{(9/12)(144)}{218 (1/16 \times 15)24} \\ &= 22 \times 10^{-3} \frac{\text{hr-F}}{\text{Btu}} = \text{resistance to conduction of heat into the internal fins at the cask surface} \end{aligned}$$

The total resistance to the transfer of heat from the cask wall inner surface into the shielding is

$$R_{it} = \frac{1}{\frac{1}{R_i} + \frac{1}{R_{FIN}}} = 2.91 \times 10^{-3} \frac{\text{hr-}^\circ\text{F}}{\text{Btu}}$$

The total resistance to the transfer of heat in from the fire is

$$\Sigma R = R_s + R_w + R_{it} = 6.42 \times 10^{-3} \frac{\text{hr-F}}{\text{Btu}}$$

The total heat transferred into the cask during the time interval 0 to $1\Delta\theta$ is

$$Q_0 \rightarrow 1\Delta\theta = \left(\frac{\Delta T}{\Sigma R} \right) \Delta\theta$$

For the first time increment the total ΔT from the heat source to the liquid in the cask is

$$\Delta T = T_a - T_w = 1283 - 188 - 635^\circ\text{F}$$

therefore

$$Q_0 \rightarrow 1\Delta\theta = \left(\frac{635}{.00642} \right) \left(\frac{2}{60} \right) = 3300 \text{ Btu}$$

The heat transferred in over $\Delta\theta$ must be accumulated in the fins, outer shell of stainless steel, and the liquid. Since the heat capacity of copper (~ 0.09 Btu/lb $^\circ\text{F}$) is an order of magnitude lower than the water and the fins do not have a very significant mass, their thermal capacity is neglected. This means that only the liquid and stainless outer shell are assumed capable of accumulating heat and slightly conservative results will be obtained. The weights of the stainless steel and liquid are 204.5 and 368 lb, respectively. Therefore

$$C = 204.5 C_{P_{ss}} + 368 C_{P_{H_2O}}$$

At these temperatures

$$C_{p_{ss}} = 0.12 \text{ Btu/lb F}$$

$$C_{p_{H_2O}} = 0.88 \text{ Btu/lb F} = \text{weight averaged value for the water 30 v/o glycol solution}$$

hence

$$C = 347 \text{ Btu/F}$$

since the heat from the fire accumulates in the liquid and stainless steel

$$Q_o \rightarrow \Delta\theta = C (T_{w \Delta\theta} - T_w)$$

Where

$$T_{w \Delta\theta} = \text{water temperature at } \Delta\theta$$

$$T_w = \text{water temperature at } \theta = 0$$

solving for $T_{w \Delta\theta}$

$$T_{w \Delta\theta} = 188 + 9.5 = 197.5^\circ\text{F}$$

From $\Delta\theta$ to $2\Delta\theta$ (Two Minutes to Four Minutes After Start of Fire)

For the second time increment the water bulk temperature is taken as that just calculated, therefore

$$T_{w \Delta\theta} = 197.5^\circ\text{F}$$

writing a heat balance on the cask at time $\Delta\theta$

$$(T_{a \Delta\theta} - T_{s \Delta\theta})/R_s = (T_{a \Delta\theta} - T_{w \Delta\theta})/\Sigma R$$

substituting the quantities previously calculated and solving for

$$T_s \Delta\theta,$$

$$T_s \Delta\theta = 494 \text{ F}.$$

For the second time increment the ambient temperature is taken as 1475°F.

Hence, for the interval $\Delta\theta$ to $2\Delta\theta$

$$\begin{aligned} h_c &= 0.19 (1475 - 494)^{1/3} = 1.88 \text{ Btu/hr ft}^2\text{°F} \\ h_r &= \frac{(0.173)(.8)(140,000 - 8260)}{981} = 18.6 \text{ Btu/hr ft}^2\text{°F} \\ R_c &= \left(\frac{1}{155.2}\right) \frac{\text{hr°F}}{\text{Btu}} \\ h_r A_r &= 18.6(30) = 558 \\ R_r &= \left(\frac{1}{558}\right) \frac{\text{hr°F}}{\text{Btu}} \end{aligned}$$

The new outside surface thermal resistance is

$$R_s = 1.403 \times 10^{-3} \frac{\text{hr°F}}{\text{Btu}}$$

The wall and fin resistances are taken as constant at

$$R_w = 0.15 \times 10^{-3} \frac{\text{hr°F}}{\text{Btu}}$$

and

$$R_{FIN} = 22 \times 10^{-3} \frac{\text{hr-°F}}{\text{Btu}}$$

The inside surface temperature of the cask outer wall can be computed in an analogous manner to the outside surface temperature (by proportioning resistances). It is found that during the increment $\Delta\theta$ to $2\Delta\theta$ the inside surface temperature can be almost as hot as the cask surface temperature.

In such a case the water within the cask will be undergoing pool or nucleate boiling. Under this condition the vapor bubbles formed at the surface condense upon being swept into the bulk liquid, which is at a considerably lower temperature and circulating very turbulently and no overall vaporization is occurring yet. For the nucleate boiling condition the heat transfer coefficient is much, much larger than for natural convection. If the coefficient is taken as 1000 (well within the range given in McAdams^{*}) the thermal resistance at the inside surface of the cask outer shell is

$$R_i = \frac{1}{1000 \times \pi \times 19.5 \times 35} = 4.68 \times 10^{-6} \frac{\text{hr F}}{\text{Btu}}$$

and the total inside thermal resistance is

$$R_{it} = \frac{1}{\frac{1}{4.68 \times 10^{-7}} + \frac{1}{22 \times 10^{-3}}} \approx 0$$

and no resistance to heat transfer at the inside surface is exhibited.

If the resistance computed for the second time increment are summed

$$\Sigma R = (1.403 + 0.15 + 0) \times 10^{-3} = 1.553 \times 10^{-3} \frac{\text{hr}^\circ\text{F}}{\text{Btu}}$$

$$\Delta T = \frac{T_a}{1\Delta\theta} - \frac{T_w}{1\Delta\theta}$$

$$\Delta T = 1475 - 198 = 1277$$

$$Q_1 \rightarrow 2\Delta\theta = \left(\frac{1277}{.001553} \right) \left(\frac{2}{60} \right) = 27,300 \text{ Btu}$$

$$C = 349 \text{ Btu/F}$$

* McAdams, op. cit. p 5.

therefore

$$T_{w \ 2\Delta\theta} - T_{w \ 1\Delta\theta} = \frac{27,300}{349} = 78.2$$

$$T_{w \ 2\Delta\theta} = 197.5 + 78.2 = 275.7 \text{ F}$$

From $2\Delta\theta$ to $2.75 \Delta\theta$ (Four Minutes to 5.5 Minutes After Start of Fire)

For the third time increment the water bulk temperature is taken as 275.7°F . At the beginning of the third time increment four minutes have elapsed since the start of the fire. The bulk water temperature is approaching the value at which the relief valve will open (the saturation temperature of water is 320°F at 75 psig - the relief valve setting). For this reason, the time increment is changed to 1.5 minutes.

$$T_{w \ 2\Delta\theta} = 275.7^\circ\text{F}$$

$$(1475 - T_{s \ 2\Delta\theta})/R_s = (1475 - 275.7)/\Sigma R$$

Substituting the quantities previously calculated and solving for $T_{s \ 2\Delta\theta}$

$$T_{s \ 2\Delta\theta} = 395^\circ\text{F}$$

Therefore

$$h_c = 0.19 (1475 - 395)^{1/3} = 4.41 \text{ Btu/hr ft}^2\text{F}$$

$$h_r = \frac{(.173)(.8)(140,000 - 5330)}{1080} = 17.22 \frac{\text{Btu}}{\text{hr ft}^2 \text{ } ^\circ\text{F}}$$

$$h_c A_c = 4.41 (82.6) = 364 \text{ Btu/hr } ^\circ\text{F}$$

$$R_c = \left(\frac{1}{364}\right) \frac{\text{hr } ^\circ\text{F}}{\text{Btu}}$$

$$h_r A_r = 17.22 (30) = 517$$

$$R_r = \left(\frac{1}{517}\right) \frac{\text{hr } ^\circ\text{F}}{\text{Btu}}$$

The new outside surface thermal resistance is

$$R_s = 1.136 \times 10^{-3} \frac{\text{hr } ^\circ\text{F}}{\text{Btu}}$$

The wall resistance is

$$R_w = 0.15 \times 10^{-3} \frac{\text{hr } ^\circ\text{F}}{\text{Btu}}$$

The inside surface of the cask outer shell offers no resistance to heat transfer as was noted previously, hence

$$R_{it} = 0$$

Summing resistances

$$\Sigma R = (1.136 + 0.15 + 0)10^{-3} = 1.286 \times 10^{-3} \frac{\text{hr } ^\circ\text{F}}{\text{Btu}}$$

$$\Delta T = 1475 - 276 = 1199$$

$$Q_{2 \rightarrow 2.75 \text{ } \Delta\theta} = \left(\frac{1199}{.001286}\right) \left(\frac{.75}{60}\right) = 11,670 \text{ Btu}$$

$$C = 349 \text{ Btu/ } ^\circ\text{F}$$

$$T_{w \ 2.75 \Delta\theta} - T_{w \ 2\Delta\theta} = \frac{11,670}{349} = 33.4^{\circ}\text{F}$$

$$T_{w \ 2.75 \Delta\theta} = 276 + 33.4 = 309.4^{\circ}\text{F}$$

From 2.75 $\Delta\theta$ to 3.0 $\Delta\theta$ (5.5 Minutes to 6 Minutes After Start of Fire)

For the fourth time increment the water bulk temperature is taken as 309.4°F.

$$T_{w \ 2.75 \Delta\theta} = 309.4^{\circ}\text{F}$$

$$(1475 - T_{s \ 2.75 \Delta\theta})/R_s = (1475 - 309.4)/\Sigma R$$

substituting the resistances calculated in the previous time increment

$$T_{s \ 2.75 \Delta\theta} = 1475 - \left(\frac{1.136}{1.286}\right)(1165.6) = 446$$

therefore

$$h_c = 0.19 (1475 - 446)^{1/3} = 1.935 \text{ Btu/hr ft}^2\text{ }^{\circ}\text{F}$$

$$h_r = \frac{(.173)(.8)(140,000 - 6720)}{1029} = 17.95 \text{ Btu/hr ft}^2\text{ }^{\circ}\text{F}$$

$$h_c A_c = 1.935 (82.6) = 159.8 \text{ Btu/hr }^{\circ}\text{F}$$

$$R_c = \left(\frac{1}{159.8}\right) \text{ hr }^{\circ}\text{F/Btu}$$

$$h_r A_r = 17.95(30) = 539 \text{ Btu/hr }^{\circ}\text{F}$$

$$R_r = \left(\frac{1}{539}\right) \text{ hr }^{\circ}\text{F/Btu}$$

The new outside surface thermal resistance is

$$R_s = 1.431 \times 10^{-3} \text{ hr } ^\circ\text{F}/\text{Btu}$$

The wall resistance is

$$R_w = 0.15 \times 10^{-3} \frac{\text{hr } ^\circ\text{F}}{\text{Btu}}$$

Summing resistances

$$\Sigma R = (1.431 + 0.15 + 0)10^{-3} = 1.581 \times 10^{-3} \frac{\text{hr } ^\circ\text{F}}{\text{Btu}}$$

$$\Delta T = 1475 - 309 = 1166$$

$$Q_{2.75 \rightarrow 3\Delta\theta} = \left(\frac{1166}{.001581} \right) \left(\frac{25}{60} \right) = 3070 \text{ Btu}$$

$$C = 349 \text{ Btu}/^\circ\text{F}$$

$$T_{w \ 3\Delta\theta} - T_{w \ 2.75\Delta\theta} = \frac{3070}{349} = 8.8^\circ\text{F}$$

$$T_{w \ 3\Delta\theta} = 309.4 + 8.8 = 318.2^\circ\text{F}$$

Hence, at a time after the start of the fire of slightly greater than six minutes the boiling point of the liquid has been reached. For purposes of conservatism, it will be assumed that boiling commences exactly six minutes after the fire begins. The cask surface temperature at six minutes and thereafter, as long as boiling is taking place is

$$T_{s \ 3\Delta\theta} = 1475 - \left(\frac{1.431}{1.581} \right) (1157) = 431^\circ\text{F}$$

Calculations After Boiling Has Begun

The heat input rate during boiling is computed as follows:

$$h_c = 0.19 (1475 - 431)^{1/3} = 1.92 \text{ Btu/hr ft}^2 \text{ } ^\circ\text{F}$$

$$h_r = \frac{(0.173)(.8)[140,000 - 6300]}{1044} = 17.7$$

$$h_c A_c = (1.92)(82.6) = 158.5$$

$$h_r A_r = (17.7)(30) = 531$$

$$R_s = 1.45 \times 10^{-3}$$

$$R_w = 0.15 \times 10^{-3}$$

$$\Sigma R = 1.60 \times 10^{-3}$$

$$\Delta T = 1475 - 321 = 1154$$

$$Q_{in} = \frac{1154}{1.60} \times 10^3 = 721,000 \frac{\text{Btu}}{\text{hr}}$$

when boiling starts the relief valve will open and steam will escape. The steam escaping is rejected to atmospheric pressure and the pressure inside and outside the cask tends to equalize. When the pressure within the cask drops to about 90% of the relief valve setting, the valve reseats and pressure can again build up to a value at which the valve blows again. Assuming the valve blows or "pops" at six minutes the first time:

heat lost by water in cask = heat lost in steam escaping

or

$$W C_p \Delta T = X \lambda$$

Where

W = weight of water in the cask, lb

C_p = specific heat of water in the cask, Btu/lb°F

$\Delta T = T_2 - T_1 = 5^\circ\text{F}$

T_2 = temperature of saturated liquid at 75 psig $\approx 320^\circ\text{F}$

T_1 = temperature of saturated liquid at 68 psig $\approx 315^\circ\text{F}$

X = lbs of water vaporized (100% quality)

λ = heat of vaporization at 320°F

at six minutes

$W = 368 \text{ lb}$

$C_p = 0.88 \text{ Btu/lb}^\circ\text{F}$

$\Delta T = 5^\circ\text{F}$

$\lambda = 895 \text{ Btu/lb}$

therefore

$$X = \frac{(368)(.88)(5)}{895} = 1.81 \text{ lb}$$

The steam released is not 100% quality steam (not dry steam) but is probably close to 90% quality because provision has been made in the cask design to "knock out" entrained liquid. Based on 90% quality the amount of water removed in the "pop" at six minutes is

$$w = \frac{1.81}{.90} = 2.01 \text{ lb}$$

Hence, after the first pop there are $368 - 2 = 366 \text{ lb}$ of water remaining in the cask. The valve is assumed to open and close instantaneously which is a conservative assumption in that it will lead to calculations which show more water to be vaporized in one-half hour than is actually the case.

It is now necessary to compute the amount of time required for the container to heat back up and allow the relief valve to open again. The amount of heat which the cask must absorb can be found by taking the difference in the enthalpy of saturated liquid at 320°F and at 315°F

$$\Delta H = 290.6 - 284.7 = 5.9 \text{ Btu/lb}$$

The cask must absorb

$$q = 5.9 (366) = 2160 \text{ Btu}$$

at a heat input rate of 721,000 Btu/hr the time required is

$$\Delta \theta_p = \frac{2160 \times 3600}{721,000} \approx 10 \text{ seconds}$$

Hence, the valve opens the second time at six minutes and ten seconds after the fire begins. As before, the water lost in the second pop can be computed as

$$X = \frac{(366)(.88)(5)}{895} = 1.80 \text{ lb}$$

$$w = \frac{1.80}{90} = 2.0 \text{ lb}$$

The water remaining in the cask after the second pop is then $366 - 2 = 364 \text{ lb}$.

The liquid loss rate appears to be fairly constant for short-time intervals. The rate of water loss is two pounds/ten seconds or 12 pounds per minute. Assuming that the rate is constant up to ten minutes after initiation of the fire the water remaining then is

$$W = 368 - 4 \times 12 = 320 \text{ lb}$$

for a valve opening occurring at approximately 10 minutes

$$X = \frac{(320)(.88)(5)}{895} = 1.575 \text{ lb}$$

$$w = (1.575/.90) = 1.75 \text{ lb}$$

$$q = 5.9 (320) = 1880 \text{ Btu}$$

$$\Delta\theta_p = \frac{1880 \times 3600}{721,000} = 9.36 \text{ sec}$$

The loss rate at 10 minutes is 1.75 lb/9.36 seconds or 11.2 pounds per minute.

Assuming the loss rate constant at 11.2 lb/min over the time interval 10 to 15 minutes for a valve opening at 15 minutes

$$W = 320 - 5 \times 11.2 = 264 \text{ lb}$$

$$X = \frac{(264)(.88)(5)}{895} = 1.298 \text{ lb}$$

$$w = (1.298/.90) = 1.44 \text{ lb}$$

$$q = 5.9 (264) = 1555 \text{ Btu}$$

$$\Delta\theta_p = \frac{1555 \times 3600}{721,000} = 7.76 \text{ sec}$$

loss rate at 15 minutes = 11.12 lb/min.

Assuming a constant loss rate for the interval 15 to 20 minutes the following can be computed at 20 minutes

$$W = 264 - 5 \times 11.12 = 208.4 \text{ lb}$$

$$X = \frac{(208.4)(.88)(5)}{895} = 1.02 \text{ lb}$$

$$w = (1.02/.90) = 1.132 \text{ lb}$$

$$q = 5.9 (208.4) = 1226$$

$$\Delta \theta_p = \frac{1226 \times 3600}{721,000} = 6.12 \text{ sec}$$

$$\text{loss rate at 20 minutes} = 11.1 \text{ lb/min}$$

For the interval 20 to 30 minutes, since the loss rate is constant the following calculations can be made

$$W = 208.4 - 10 (11.1) = 87.4 \text{ lb}$$

Hence, at the end of one-half hour approximately 87 pounds of water remain in the cask. The maximum water temperature during the fire is 320° and the maximum surface temperature of the cask is 431°F.

In the previous calculations the inner stainless steel ring was assumed adiabatic. The inner ring surface temperature is at a temperature of 206°F for normal operating conditions. The initial temperature of the surface when the fire begins is then 206°F and a temperature driving force exists from the surface to the liquid. This positive driving force only exists for a few minutes after the fire begins--until the bulk water temperature rises to 206°F or greater. The bulk water temperature rises to 206°F in less than two minutes and during this period the decay heat is only about 1% of the total heat being transferred to the water. The assumption that the inner ring is adiabatic over the initial two minutes of the fire is therefore justified.

After the initial two minutes the ring surface temperature must rise to a value at which it can transfer heat to the water which is at 320°F. During the time the ring surface temperature is climbing heat is being abstracted from the water into the ring (and fuel capsule). The important point is that no heat can be transferred from the ring to the liquid until the surface temperature of the ring exceeds 320°F. The time required for the inner ring to reach 320°F can be estimated from the thermal capacitance of the ring, neglecting any contribution to the temperature rise of the ring by heat transfer from the water (which is small). The thermal capacitance of the stainless steel inner ring is

$$W C_p = (10.57)(.12) = 1.27 \text{ Btu/}^\circ\text{F}$$

$$Q = W C_p (T_c - T_i)$$

$$T_c = 320^\circ\text{F}$$

$$T_i = 206$$

hence

$$Q = 144.8 \text{ Btu}$$

must be absorbed by the ring in rising to 320°F. The fuel capsule internal heat generation rate is 5118 Btu/hr, therefore the time required is

$$\frac{144.8}{5118} \times 60 = 1.692 \text{ minutes}$$

At approximately four minutes after the start of the fire the inner ring will have risen to a temperature which is sufficient to transfer heat to the liquid. This means that after four minutes have elapsed the assumption that the inner ring is adiabatic may no longer be a good assumption.

The minimum heat transfer coefficient from the inner ring to the liquid over the 26 remaining minutes of the fire is taken as 10 Btu/hr ft²°F. This is a feasible value for the water glycol vapor mixture and is undoubtedly low. It can be shown by repeating the calculations for vaporization that the decay heat added has a negligible effect on the amount of water vaporized. From the equation

$$Q = hA (T_s - 320)$$

with

$$Q = \text{decay heat} = 5118 \text{ Btu/hr}$$

$$h = 10 \text{ Btu/hr ft}^2\text{°F}$$

$$A = \text{area of inner ring surface} = 1.06 \text{ sq ft}$$

T_s = maximum surface temperature of the inner stainless steel ring. T_s is computed as

$$T_s = \frac{5118}{(1.06)(10)} + 320 = 803^\circ\text{F}$$

The maximum fuel capsule surface temperature which would be attained during a fire, based on the assumption of $h = 10 \text{ Btu/hr sq ft } ^\circ\text{F}$ would be 942 F. The 942 F value is arrived at by adding the temperature drop across the inner ring and particle filled gap (139 F) to the inner ring surface temperature (803 F).

V. SHIELDING ANALYSIS

The purpose of this section is to demonstrate by analysis that the shield design for the SNAP-27 GSC adequately meets the requirements for maximum dose rate as specified in AEC Regulation 10CFR71, ICC Regulations 49CFR 71-78, and GE-MSD Design Specification NS 0010-07-02-B. The dose rate requirements are summarized as follows:

(1) Normal operating conditions:

- (a) The dose rate at any surface of the cask shall not exceed 200 mrem/hr.
- (b) The dose rate at a distance of one (1) meter from the cask's surface shall not exceed 10 mrem/hr.

(2) Accident conditions:

- (a) The dose rate at a distance of three (3) feet from the cask's surface shall not exceed 1 rem/hr.

Shield Description

As noted in the cask illustration, Figure 7, radiation shielding in the SNAP-27 GSC is accomplished by the use of a water-ethylene glycol mixture lined with a stainless steel shell. The side shield consists of an annular section of water-ethylene glycol 8.125 in. thick, and enclosed by concentric inner and outer stainless steel sections, each 0.250 in. thick. The top and bottom shield sections each consist of 8.125 in. of water-ethylene glycol mixture enclosed by a 0.5-in.-thick stainless steel plate.

FIGURE 7. TYPICAL RADIAL SECTION OF RADIATION SHIELDING

The radiation emanating from the sides of the capsule (i.e., direction of most intense radiation) must penetrate the following radial sections:

0.100 in. of nickel particles (~35% void)

0.250 in. of stainless steel

8.125 in. of water-ethylene glycol (70-30) and copper

(0.250 in. average of tangential fin material)

0.250 in. of stainless steel.

For conservatism all calculations neglected any radiation attenuation in the nickel particles, copper tangential fins, and the stainless steel sections.

Sources of Radiation

Various types of radiation must be considered in a calculation of the total dose rate from the SNAP-27 GSC (loaded with the fuel capsule). A summary of the more important radiation types is given in the following tabulation.

<u>Type</u>	<u>Source</u>	<u>Description</u>
n	Fuel	Spontaneous fission
n	Fuel	α , n
γ	Fuel	Fission products
γ	SS-Shell	Radiative Capture, (n, γ)
γ	Shield (H_2O , e.g.)	Radiative Capture, (n, γ)
γ	Fuel	Prompt fission gammas
γ	Fuel	α , γ
γ	Fuel	Decay products
γ	Fuel	Spontaneous fission

Dose Rate Calculations

The following sections include the dose rate calculations for each of the radiation types listed above.

Neutron Dose Rate Determination

Two basic sources of neutron radiation exist within the SNAP-27 fuel capsule assembly. These sources are (a) spontaneous fission of the various plutonium isotopes, and (b) (α , n) reactions resulting from alpha particles which are emitted from the plutonium isotopes (primarily ^{238}Pu) and interact with the light nuclei of the fuel compound. The latter source is the more significant of the two, being responsible for over 90 percent of the total neutron source strength.

For purposes of shield design, the following neutron source strengths were used (based on source data provided by GE-MSD):

Design value	1.12×10^8 neutrons/sec
Maximum value.	1.5×10^8 neutrons/sec.

The above source strengths were considered to be distributed over the neutron energy spectrum shown in Figure 8 (based on data supplied by GE-MSD). To facilitate the calculation of the total neutron dose rate, the spectrum shown in Figure 8 was divided into 12 energy groups as illustrated in Table 4.

FIGURE 8. NEUTRON ENERGY SPECTRUM

TABLE 4. NEUTRON SOURCE STRENGTHS FOR A 12-GROUP STRUCTURE

Group	Energy Range (mev)	Average Energy (mev)	Source Strength (n/sec)
1	0 - 0.5	0.25	2.98×10^6
2	0.5 - 1.0	0.75	6.45×10^6
3	1.0 - 1.5	1.25	1.26×10^7
4	1.5 - 2.0	1.75	2.04×10^7
5	2.0 - 2.5	2.25	2.38×10^7
6	2.5 - 3.0	2.75	2.13×10^7
7	3.0 - 3.5	3.25	1.35×10^7
8	3.5 - 4.0	3.75	5.94×10^6
9	4.0 - 4.5	4.25	2.64×10^6
10	4.5 - 5.0	4.75	1.32×10^6
11	5.0 - 5.5	5.25	7.64×10^5
12	5.5 - 6.0	5.75	3.81×10^5

Each of the 12 energy groups listed in Table 4 are attenuated differently through the water-ethylene glycol shield due to the variation of the attenuation coefficient with incident neutron energy. Data pertaining to the variation of neutron attenuation through water as a function of incident neutron energy were obtained from the published data of Clark,* and are illustrated in Figures 9 through 13. The neutron dose

* Clark, F. M., "Determinations of Shield Requirements for Neutron Sources", ORNL-TM-1655, pp 17-21 (October 5, 1966).

FIGURE 9. DOSE TRANSMISSION FACTOR VERSUS WATER THICKNESS FOR 0.5 MEV NEUTRONS

FIGURE 10. DOSE TRANSMISSION FACTOR VERSUS WATER THICKNESS FOR 1 MEV NEUTRONS

FIGURE 11. DOSE TRANSMISSION FACTOR VERSUS WATER THICKNESS FOR 2 MEV NEUTRONS

FIGURE 12. DOSE TRANSMISSION FACTOR VERSUS WATER THICKNESS FOR 3 MEV NEUTRONS

FIGURE 13. DOSE TRANSMISSION FACTOR VERSUS WATER THICKNESS FOR 5 MEV NEUTRONS

transmission factors were obtained from Figures 9-13 by using a water shield thickness of 8.125 in. and assuming an incident neutron angle of 0 degrees. The conservatism in the latter assumption can be seen by comparing the respective dose transmission values at larger incident angles (see Figure 14).

Using the data from Figures 9-13, a curve of neutron dose transmission factor (at an incidence angle of 0 degrees) versus incident neutron energy was constructed as shown in Figure 15. Through the use of Figure 15 and the relationship,

$$F_{DT} = e^{-\mu t}, \quad (1)$$

where

F_{DT} = neutron dose transmission factor

μ = effective attenuation coefficient

t = water shield thickness = 20.6 cm

values of the attenuation coefficient of water as a function of incident neutron energy were plotted as illustrated in Figure 16.

The neutron source strengths listed in Table 4 together with the corresponding attenuation coefficients for each energy group obtained from Figure 16 were used as input to the SDC^{*} shielding code. The geometry utilized for the radiation source was that of an annular cylinder. Dose rate determinations included (a) dose rates through the side shield, and (b) dose rates through the end shield for locations on the surface of the shield and at one (1) meter from shield's surface. A brief description of the techniques utilized in each of the foregoing cases is given in the following sections.

* Arnold, E. D. and Maskewitz, B. F., "SDC, A Shielding-Design Calculation Code for Fuel-Handling Facilities", ORNL-3041 (March, 1966).

FIGURE 14. RELATIVE NEUTRON DOSE TRANSMISSION FOR VARIOUS NEUTRON ENERGIES AND INCIDENT ANGLES (NORMALIZED TO 1 AT 0 DEGREES INCIDENCE)

FIGURE 15. NEUTRON DOSE TRANSMISSION FACTOR VERSUS INCIDENT NEUTRON ENERGY (AT 8.125" WATER AND 0 DEGREES INCIDENCE)

FIGURE 16. EFFECTIVE ATTENUATION COEFFICIENT VERSUS INCIDENT NEUTRON ENERGY (AT 8.125" WATER AND 0 DEGREES INCIDENCE)

A. Dose Rate Through Side Shield. In this case the radiation dose rate is calculated from an annular cylinder with the shield at the side. The method involves a determination of the difference in dose rate (i.e., net dose rate) from a cylinder of equal specific activity and outside radius from that of a smaller cylinder of a radius corresponding to the inside of the annulus and same source strength, but shielded by the annular thickness of the fuel as well as the shield material. The equation utilized is as follows:

$$D = \frac{KS_v}{4} \left\{ \frac{R_{o1}^2}{(R_1 + t_{c1})} [F(\theta_{21}, \mu t + \mu_c t_{c1}) + F(\theta_{11}, \mu t + \mu_c t_{c1})] - \frac{R_{o2}^2}{(R_2 + t_{c2})} [F(\theta_{22}, \mu t + \mu_c t_{c2} + \mu_c t_s) + F(\theta_{12}, \mu t + \mu_c t_{c2} + \mu_c t_s)] \right\} \quad (2)$$

where

D = dose rate (m rem/hr)

K = a conversion factor (m rem/hr/n/cm²sec)

S_v = isotropic volume source (n/cm³sec)

R_{o1} = large cylinder radius (cm)

R_{o2} = small cylinder radius (cm)

R_1 = distance from large cylinder surface to dose point (cm)

R_2 = distance from small cylinder surface to dose point (cm)

μ = attenuation coefficient for shield material (cm⁻¹)

μ_c = attenuation coefficient for fuel (cm⁻¹)

t = thickness of shield material (cm)

t_{c1} = self-absorption thickness for large cylinder (cm)

t_{c2} = self-absorption thickness for small cylinder (cm)

t_s = thickness of annular fuel material (cm)

$$F(\theta, \mu t) = \int_0^\theta e^{-\mu t \sec \theta} d\theta$$

$$\theta_{21} = \theta_{11} = \tan^{-1} \left(\frac{H/2}{R_1 + t_{c1}} \right)$$

$$\theta_{22} = \theta_{12} = \tan^{-1} \left(\frac{H/2}{R_2 + t_{c2}} \right)$$

H = height of annular cylinder (cm).

B. Dose Rate Through End Shield. In this case the radiation dose rate is calculated from an annular cylinder with the shield at the end. The flux from this geometrical arrangement consists of a) flux emanating from the upper surface of the annulus, and b) flux from the inside surface of the annular cylinder. The first contribution listed above is determined by subtracting the flux equivalent due to a cylinder based on the inner annular dimension from the flux due to a cylinder based on the outer annular dimension. The resulting total dose equation based on an upper limit for truncated cone geometry is as follows:

$$D_1 = \frac{KS_v}{2\mu_c} \left\{ \frac{E_2 \left[(\mu t + \mu_c H \sqrt{1 + (R_o/R)^2}) \right]}{\sqrt{1 + (R_o/R)^2}} - \frac{E_2 \left[\mu t \sqrt{1 + (R_o/R)^2} \right]}{\sqrt{1 + (R_o/R)^2}} \right\}$$

$$- \left. \frac{E_2 [(\mu t + \mu_c H) 1 + (R_i/R)^2]}{1 + (R_i/R)^2} + \frac{E_2 [\mu t 1 + (R_i/R)^2]}{1 + (R_i/R)^2} \right\} \quad (3)$$

where D_1 = dose rate (mrem/hr)

K = a conversion factor (mrem/hr/n/cm²sec)

S_v = isotropic volume source (n/cm³sec)

μ_c = attenuation coefficient of fuel material (cm⁻¹)

μ = attenuation coefficient for shield material (cm⁻¹)

t = thickness of shield material (cm)

H = height of annular cylinder (cm)

R_o = outer annular radius (cm)

R_i = inner annular radius (cm)

$R = R_i + t_c$

t_c = self-absorption thickness of fuel material (cm)

$$E_2(\mu t) = \mu t \int_{\mu t}^{\infty} \frac{e^{-x}}{x^2} dx.$$

The second flux contribution from the inside surface of the annular cylinder is calculated by dividing the annulus into multiple wedges whose total flux equals that of the annulus. A detailed description of this method is given by Arnold*. In its final form, the total flux equation is as follows:

$$D_T = D_1 + D_2$$

where $D_2 = 4 \left(\frac{R_o + R_i}{R_o - R_i} \right) \frac{S_L}{4\pi (R_i + t_c)} [F(\Theta_{2,b}) - F(\Theta, b)]$

* lbid

where R_o = outer annular radius (cm)

R_i = inner annular radius (cm)

S_L = equivalent line source (n/sec-cm)

t_c = distance for equivalent line source from surface of cylinder (cm)

$F(\theta, b)$ = geometric function.

The results of the above calculations indicated that the maximum neutron dose rate occurs at a distance of one (1) meter from the surface of the side shield. Individual group dose rates as well as the total neutron dose rate are shown in Table 5 for the maximum dose rate case. The total neutron

TABLE 5. NEUTRON DOSE RATES AT ONE METER FROM THE SURFACE OF THE SIDE SHIELD

Group	Source Strength Neutrons/sec	Ave. Group Energy, Mev	Neutron Flux n/sec cm/sec	Conversion Fct. mr/hr/nv	Neutron Dose Rate mrem/hr
1	2.98×10^6	0.25	2.17×10^{-2}	0.039	8.96×10^{-4}
2	6.45×10^6	0.75	1.058×10^{-1}	0.092	9.73×10^{-3}
3	1.26×10^7	1.25	5.9×10^{-1}	0.118	6.97×10^{-2}
4	2.04×10^7	1.75	3.285	0.118	3.88×10^{-1}
5	2.38×10^7	2.25	7.44	0.118	8.78×10^{-1}
6	2.13×10^7	2.75	9.68	0.12	1.162
7	1.35×10^7	3.25	8.165	0.128	1.045
8	5.94×10^6	3.75	4.07	0.135	5.5×10^{-1}
9	2.64×10^6	4.25	1.97	0.141	2.78×10^{-1}
10	1.32×10^6	4.75	1.092	0.148	1.615×10^{-1}
11	7.64×10^5	5.25	6.86×10^{-1}	0.154	1.057×10^{-1}
12	3.81×10^5	5.75	3.57×10^{-1}	0.160	5.71×10^{-2}
Total	1.12×10^8				4.6

source strength listed in Table 5 does not include the multiplication, M , due to the noncritical chain reaction of the fuel capsule-cask assembly as related by the following equation:

$$S' = SM = \frac{S}{1-K_{\text{eff}}} , \quad (4)$$

where

S' = multiplied neutron source strength, n/sec

S = neutron source strength before multiplication, n/sec

M = the multiplication of the assembly

K_{eff} = the effective neutron multiplication factor.

Table 6 shows the effect that the subcritical multiplication, M , has on the neutron source strength (and dose rate) as a function of K_{eff} if Equation (4) is assumed valid. The values for K_{eff} less than 0.5 are included in Table 6 since the actual K_{eff} for the fuel capsule-cask configuration will be somewhere in this range.

It should be noted, however, that the neutron source strength is multiplied by the above multiplication factor only if the neutron source is distributed throughout the fuel capsule assembly in the same fashion that fissions are distributed in a critical array (i.e., according to the solution of the fundamental wave equation).^{*} Also, application of the above multiplication factor is probably valid only where the fuel assembly is only slightly subcritical (which is not the present case, i.e., $K_{\text{eff}} < 0.5$).

* Nuclear Engineering Handbook, H. Etherington, ed., "Reactor Calculations", Dietrich, J. R., Sect. 7-3, p 6-111, McGraw-Hill, New York (1958).

TABLE 6. EFFECT OF MULTIPLICATION FACTOR, M, ON NEUTRON SOURCE STRENGTH AND DOSE RATE FOR VARIOUS VALUES OF K_{eff}

K_{eff}	M	Neutron Source Strength, n/sec	Total Neutron Dose Rate, mrem/hr	
			Design Values	Maximum Values ⁽²⁾
0.5	2.	2.24×10^8	9.2	12.3
0.4	1.67	1.87×10^8	7.69	10.3
0.3	1.43	1.6×10^8	6.58	8.82
0.2	1.25	1.4×10^8	5.75	7.71
0.1	1.11 ⁽¹⁾	1.24×10^8	5.1	6.84
No multiplication		1.12×10^8	4.6	6.16

(1) Most probable value.

(2) Based on a nonmultiplied source strength of 1.5×10^8 n/sec.

The only experimental data available on the multiplication of neutrons in subcritical arrays of plutonium fuel-water and plutonium fuel-air configurations are the reported work of Mound Laboratory.* Experimental results indicated a neutron multiplication of 1.29 for a fuel-air array; a similar fuel-water array gave no evidence of neutron multiplication.

On the basis of the above experimental results, a maximum multiplication value of 1.11 was selected for calculational procedures.

* Wolfe, R. A. and Kahle, J. B., "Neutron Multiplication Determination of Plutonium-238 Dioxide", MLM-1340, June 24, 1966.

Gamma Dose Rate Determination

Gamma radiation from the shielded SNAP-27 fuel capsule originates from various sources within the fuel compound as well as from neutron captures in the shield and structural materials. The more important sources of gamma radiation include:

- (1) Prompt spontaneous fission of plutonium isotopes.
- (2) Fission products from spontaneous fission of plutonium.
- (3) Decay of plutonium isotopes and subsequent decay products.
- (4) Interaction of alpha particles with light nuclei in fuel compound.
- (5) Radioactive impurities.
- (6) Neutron capture in shield and structural materials.

The gamma spectrum for the sources (1) - (6) along with group source strengths utilized in the shield calculations are shown in Table 7. These data are representative of a fuel which has aged 2 years since processing (i.e., removal of contaminants, etc.), and were obtained by interpolation of the data from Stoddard.* Radiation data for 2-year post-processing fuel are used because (a) there is a buildup of gamma radiation with time, and (b) the SNAP-27 GSC must meet the dose rate requirements previously outlined for a period of at least 2 years.

* Stoddard, D. H. and Albenesius, E. L., "Radiation Properties of ²³⁸Pu Produced for Isotopic Power Generators", DP-984, p-18 (July, 1965).

TABLE 7. GAMMA RAYS FROM SNAP-27 FUEL CAPSULE (PHOTONS/SEC)*

Energy, mev	From Nuclide Decay			From Spontaneous Fissions, (²³⁸ Pu)	From Fission Products of ²³⁸ Pu	From α -Particle Interaction	Total
	²³⁸ Pu	²¹² Bi	²⁰⁸ Tl				
0.04-0.5	7.86×10^{11}	--	1.12×10^7	9.73×10^6	4.87×10^6	3.18×10^7	7.86×10^{11}
0.5-1.0	1.2×10^9	2.69×10^7	1.31×10^8	5.98×10^6	1.5×10^7	--	1.38×10^9
1.0-2.0	--	4.94×10^6	--	4.12×10^6	3.63×10^6	9.73×10^6	2.24×10^7
2.0-3.0	--	--	1.08×10^8	1.35×10^6	1.31×10^6	2.88×10^6	1.08×10^8
3.0-5.0	--	--	--	5.24×10^5	--	--	5.24×10^5
5.0-7.0	--	--	--	8.60×10^4	--	--	8.60×10^4

At t = 2 years.

An illustration of the gamma radiation buildup with time is given by the presence of ^{212}Bi and ^{208}Tl in Table 7. Radiation from neither of these nuclides (formed from the decay chain of ^{236}Pu) would be present in freshly processed fuel.

A calculation of the fuel product source strength's total gamma dose rate as listed in Table 4 was made by utilizing the SDC* computer code. The code calculation for a dose point located 1 meter from the side surface of the cask (most stringent case) is given in Table 8. For conservatism, the attenuation of gamma rays through the stainless steel sections (0.5-inch total thickness) and tangential copper fins (0.250-inch average thickness transversed) was neglected.

It should be noted that capture gamma radiation due to thermal neutron capture in the secondary coolant (water-ethylene glycol) (i.e., in hydrogen) and stainless steel were not included in Tables 7 and 8. The capture gamma dose rate resulting from neutron captures in hydrogen is only 8 percent of the fast neutron dose rate through the shield as illustrated in Figure 17 taken from Price*. In terms of additional dose rate, the latter contribution from capture gammas amounts to about 0.4 mrem/hr.

Total Dose Rate

On the basis of the neutron and gamma dose rate calculations, the total dose rate at 1 meter from the surface of the GSC is 8.4 mrem/hr based on a value of 1.1 for the neutron multiplication factor (see Table 9).

* Arnold, E. D. and Maskewitz, B. F., "SDC, A Shielding-Design Calculation Code for Fuel-Handling Facilities", ORNL-3041 (March, 1966).

** Price, B. T., Horton, C. C., and Spinney, K. T., Radiation Shielding, Pergamon Press, London, 1957.

TABLE 8. GAMMA DOSE RATES AT 1 METER FROM THE SIDE
SURFACE OF THE GSC

Group	Energy Range, mev	Energy Used, mev	Source Strength, photons/sec	Dose Rate, mrem/hr
1	0.04-0.5	*	7.86×10^{11}	--
2	0.5-1.0	0.8	1.38×10^9	2.04
3	1.0-2.0	2.0	2.24×10^7	0.12
4	2.0-3.0	2.6	1.08×10^7	0.75
5	3.0-5.0	4.0	5.24×10^5	--
6	5.0-7.0	6.0	8.60×10^4	--
Total				2.91

* Broken up into 0.017, 0.043, 0.099, 0.150, and 0.203 kev energy groups
(i.e., for spectrum from ^{238}Pu).

FIGURE 17. RATIO OF GAMMA DOSE TO FAST NEUTRON DOSE
AS A FUNCTION OF WATER THICKNESS

TABLE 9. TOTAL DOSE RATES AT 1 METER FROM THE SIDE
SURFACE OF THE GSC

Radiation Type	Design Value (mrem/hr)	Maximum Value (mrem/hr)
Neutron	5.10	6.84
Gamma *	<u>3.31</u>	<u>3.46</u>
Total	8.41	10.30

* Includes capture gammas..

The dose rates in Table 9 represent maximum values since various conservative steps were taken throughout the calculations. A list of the more significant factors of conservatism is given below:

- (1) Assume a neutron incidence angle of zero (0) degrees (upon the shield).
- (2) Assume a constant shield penetration length of 8.125 in. for all differential segments of the source capsule.
- (3) Neglect radiation attenuation through the copper and stainless steel sections.
- (4) Assume neutron multiplication in the fuel-water configuration.

In the case of the maximum dose rate (i.e., 10.3 mrem/hr), a refined calculation (i.e., including the factors which were neglected for conservatism) indicates that the dose rate is well below 10 mrem/hr.

Dose Rate Under Accident Conditions

In the event that all of the liquid shield material is lost during an accident, the resultant dose rate at one (1) meter from the cask's surface would increase by about a factor of 10. Therefore, the dose rate at a distance of three (3) feet from the cask's surface would be much less than 1 rem/hr (as specified in the regulatory criteria).

VI. CRITICALITY ANALYSIS

In this section it will be shown analytically that the SNAP-27 GSC design adequately meets the criticality requirements outlined in AEC Regulations 10CFR Part 71, ICC Regulations 49CFR71-78, and GE-MSD Design Specification NS 0110-07-02-B. The most stringent of the above criteria are as follows:

- (1) The cask shall be configured in such a manner that under no credible conditions can one cask configuration result in an effective multiplication factor which exceeds 0.5.
- (2) The cask shall be configured in such a manner that under no credible conditions can two or more casks be juxtaposed to permit the formation of a critical configuration.

Neutron Fission Sources

The two major fuel constituents of the SNAP-27 Fuel Capsule Assembly are ^{238}Pu and ^{239}Pu , and either fuel can affect the neutron multiplication factor of the system (i.e., the fuel capsule positioned within the GSC). Due to the difference in the fission cross section of the above plutonium isotopes, each plays a varying role, depending on whether the neutron spectrum is fast or thermal.

In the SNAP-27 GSC design the spectrum is thermalized due to the presence of the water shield. The major contributor to fission in this case is ^{239}Pu because of its high fission cross section for thermal neutrons. The role of the ^{238}Pu is to act as a neutron poison, absorbing high energy neutrons that would otherwise be thermalized and contribute to fissioning of the ^{239}Pu .

Should a loss of secondary coolant (shield water) accidentally occur, the spectrum of the fuel capsule-cask configuration would shift to that of a fast spectrum, increasing the effective neutron multiplication factor (K_{eff}). The reason for the latter phenomenon is that the greater portion of the fuel consists of ^{238}Pu having a relatively high fission cross section for fast neutrons. The ^{239}Pu , in the loss-of-coolant case, acts as a diluent since the number of thermal neutrons available for fission in ^{239}Pu is small in comparison to the fast neutrons available for fission in ^{238}Pu . Although K_{eff} increases in the loss-of-coolant case, the configuration is still subcritical by a safe margin according to the critical mass data published by Carter.*

Calculation of K_{∞} and K_{eff}

The criticality calculations were made with the ANISN** computer code, a one-dimensional discrete-ordinates (S_n) transport code with general anisotropic scattering. Neutron group cross sections used in the ANISN code were generated by the CAM-II cross section library which has a 28-group structure.

* Carter, L. L., "Monte Carlo Calculated Values for the Critical Mass of ^{238}Pu as a Function of Hydrogen to Fuel Atomic Ratios and ^{239}Pu Enrichments, BNWL-149.

** Engle, W. W., Jr., Personal Communication.

Two computations were made for the SNAP-27 GSC, taking into account the presence of the water shield (i.e., thermal system). Values of K_{eff} and K_{∞} were obtained for (a) one (1) isolated GSC, and (b) an infinite number of GSCs in close formation (fin to fin), respectively. The results of the calculations were as follows:

$$K_{\text{eff}} \cdot \cdot \cdot \cdot \cdot 0.49736$$

$$K_{\infty} \cdot \cdot \cdot \cdot \cdot 0.49744$$

As indicated by the small difference in the above multiplication factors, each cask is essentially isolated from the standpoint of fissile material interactions.

VII. STRUCTURAL INTEGRITY ANALYSIS

The structural integrity of the SNAP-27 GSC is designed to surpass all safety standards presently in force. The following analysis therefore compares the cask design with the minimum requirements as set forth in "Rules and Regulations of Packaging of Radioactive Materials for Transport", Title 10 CFR, Part 71 (July 27, 1966), and the ASME Pressure Vessel Code, Section VIII.

In the structural examination which follows, the following outline is used:

- (1) Hoisting Analysis
- (2) Tiedown Analysis
- (3) Pressure Vessel Analysis
- (4) Beam Analysis
- (5) Puncture Analysis
- (6) Impact Analysis

For a detailed examination of the cask, the design drawings listed in Table 1 should be used for reference. Figure 18 is a schematic of the cask model used for analysis. Pertinent information relating to the structural integrity analysis is summarized below.

General Cask Description

- (1) Total Maximum Weight (cask and skid), 1500 lb
- (2) Outer Shell Diameter, 20 in.
- (3) Overall Shell Length, 36 in.
- (4) Design Pressure, 75 psi.

FIGURE 18. GSC STRUCTURAL ANALYSIS MODEL

Material Properties

	Selected Working Stresses, (T-C), psi	Elastic Modulus, psi	Density lb/cu. in.	Coefficient of Thermal Expansion in./in.-F
Stainless Steel	20,000	30×10^6	0.29	9.6×10^{-6}
per	10,000	17×10^6	0.323	9.8×10^{-6}

ar strengths are assumed as 1/2 T-C Strengths.

1. Hoisting Analysis

Total cask weight = 1500 lbs.

(a) Shear on the two lifting lug pins, (1.25 in. diameter)

$$\text{Pin area} = 4 \left(\frac{\pi}{4} \right) (1.25)^2 = 4.9 \text{ sq. in.}$$

$$\tau = \text{shear stress} = \frac{1500 \text{ lbs}}{4.9 \text{ sq. in.}} = 306 \text{ psi, F.S.} = 16$$

(b) Pin Supports. Consider the copper fin with regard to bearing, tearout, and weld strengths.

(1) Bearing. The effective load on each of the four supporting fins is $1/4 (1500) = 375 \text{ lbs.}$

The bearing area, at a minimum is

$$0.25(1.25) = 0.312 \text{ sq. in.}$$

$$\sigma_c = \frac{375}{0.312} = 120 \text{ psi, F.S.} = 83$$

(2) Tearout. Tearout area in tension is

$$0.25(5 - 1.25) = 0.937 \text{ sq. in. at (a)}$$

$$\sigma = \frac{375}{0.937} = 400 \text{ psi, F.S.} = 25$$

Tearout area in shear is

$$0.25(2 \times 2) = 1 \text{ sq. in. at (b)}$$

$$\tau = \frac{325}{1} = 325 \text{ psi, F.S.} = 15$$

- (3) Due to the long length of weld joining the fins to the cask shell, the weld stresses are considered negligible.

2. Tiedown Analysis

Assume Cask C.G. is 2 in. above its mid-height, or 20 in. above bottom. Under 10 g's the thrusting force is 13,000 lbs as shown.

Assuming all four bolts withstand the shear load, the shear load per bolt is 3260 lbs. Assuming two bolts resist the moment, summing moments about (a) gives

$$2 F_B = \frac{13,000(20)}{20},$$

$$F_B = 6500 \text{ lb/bolt tension.}$$

The simultaneous 5g lateral force will add to the bolt loading as follows:

$$\text{Tension, } 1/2(6500) = 3250 \text{ lb.}$$

$$\text{Shear, } 1/2(3260) = 1630 \text{ lb.}$$

Due to the 2 g vertical force each bolt must carry an additional load of

$$2(1500)/4 = 750 \text{ lb.}$$

The maximum bolt loading is then

10,500 lb. Tension

4,890 lb. Shear

ASTM A325 bolts will be used. The allowable loads for 3/4 in. bolts are*

17,670 lb. Tension

6,630 lb. Shear

3. Pressure Vessel Analysis

- (a) The cask body. The cask body will be a rolled cylinder with a 20 in. outer diameter. A full penetration weld will seal the vessel longitudinally. The hoop stress in the weld is:

$$\sigma_h = \frac{pd}{2t} = \frac{75(19.5)}{2(.25)} = 730 \text{ psi, F.S.} = 27$$

- (b) Bottom Plate. Eight reinforcing gussets are welded between the bottom plate and the cylindrical body. Assuming that only the

*Manual of Steel Construction, Sixth Edition, American Institute of Steel Construction, New York (1963) "Connection", pp. 4-3, -4.

outermost inch of the gusset will offer load relief to the bottom plate, the gusset area in tension is $A_g = 8(0.25)1 = 2 \text{ sq. in.}$

To exceed the yield of 2 sq. in. requires more than 40,000 lbs. The total pressure load on the bottom plate is $pA = 75(\pi)10^2 = 23,600 \text{ lbs.}$ Therefore, consider the bottom plate rigidly supported between gussets, and at a distance of 5 in. from the centerline. The unsupported section of the bottom plate, now 10 in. in diameter, may be treated as a simply supported circular disk with a uniformly distributed load on its surface. The maximum principal stresses will be near the center and are given by the following.

$$\sigma_{\max} = \frac{3W}{8\pi mt^2} (3m + 1), *$$

*Roark, "Formulas for Stress and Strain", Fourth Edition, McGraw-Hill, New York (1954) Art. 5, Table X, Case 1.

where

$m = \text{reciprocal of Poisson's ratio} = 1/0.3 = 3.33$

$W = \text{net load on plate} = p \pi (5)^2 = 75(\pi) (5)^2 = 5,900 \text{ lb.}$

$t = \text{plate thickness } 0.5 \text{ in., or}$

$$\sigma_{\max} = 9,300 \text{ psi,}$$

$$\text{F.S.} = \frac{20,000}{9,300} = 2.15.$$

The bottom plate sections between gussets may be analyzed as flat plates with supported edges. The maximum stress is

$$\sigma_{\max} = \frac{0.75 p b^2}{t^2 (1 + 1.61 \alpha^3)}, *$$

$$\frac{b}{a} = \frac{5}{7} = \alpha = 0.714,$$

$$\alpha^3 = 0.364,$$

* Ibid, Case 3.

$$\sigma_{\max} = \frac{0.75 (75) (25)}{(0.5)^2 (1 + 1.61(0.364))} = 3550 \text{ psi},$$

$$\text{F.S.} = \frac{20,000}{3,550} = 5.6.$$

(c) Top Plate. The top plate will be gusseted similarly as the bottom plate. By the reasoning given in Part (b), the gussets will support the concentrated pressure load transmitted by the inner capsule chamber. The calculation of the required plate thickness between gussets parallels that given above in (b).

(d) Girth Joint. Full Penetration Butt-Welds.

$$\text{Hoop stress} = \frac{pd}{2t} = \frac{75(20)}{2(1/4)} = 3000 \text{ psi.}$$

$$\text{Longitudinal stress} = 1/2 \text{ hoop stress} = 1500 \text{ psi.}$$

The joint efficiency for this type of weld, from the ASME Code is 0.7.

$$\text{Shear F.S.} = \frac{5000(0.7)}{1500} = 2.33.$$

4. Beam Analysis

The cask is assumed to be simply supported at its ends and loaded with five times its total weight. The main beam strength of the cask is the 1/4-in. outside shell. Maximum bending stresses occur in the shell at the midpoint of the cask length.

$$\sigma_F = \frac{MC}{I}$$

$$M = \frac{W}{8} = \frac{5(1500)(18)}{8} = 16,900 \text{ in.-lb.}$$

$$C = 20/2 = 10 \text{ in.}$$

$$I = \pi r^3 t = 3.14 (10)^3 (0.25) = 785 \text{ in.}^4$$

Substituting gives

$$\sigma_F = \frac{16,900 (10)}{785} = 215 \text{ psi, F.S.} = 93$$

5. Puncture Analysis

The container is capable of withstanding a free-fall drop of 40 in. onto a 6-in-diameter bar without puncturing. Recent drop tests performed at the Oak Ridge National Laboratory* verify the fact that the cask wall thickness is sufficient. The work has developed and experimentally proved an expression for determining the wall thickness required to resist puncture. For 304 Stainless Steel:

$$t_{REQD} = (2.07 \times 10^{-5})W - (1.306 \times 10^{-10})W^2$$

$$t = \text{in.}$$

$$W = 1500 \text{ lbs.}$$

Substituting gives t_{REQD} is less than 0.01 in., while $t_{actual} = 0.25 \text{ in.}$

*Spaller, A.E., "Structural Analysis of Shipping Casks, Volume 2, Resistance to Puncture", Oak Ridge National Laboratory, Tenn., ORNL-TM-1312, Volume 2, September, 1966.

6. Impact Analysis

Consider a 30-ft free-fall impact onto a non-yielding surface. The nature of the unshielded fuel capsule is such that the dose at one meter from the surface is less than one R. Therefore, the cask need not be counted on for shielding and the gross deformations which may result from a 30 ft free-fall impact may be tolerated. However, containment of the capsule within the cask is assured. The capsule will be contained within the cask as demonstrated by the following calculations.

An impact on the lid end of the cask will be considered as the worst case. It is assumed that the lid bolts must withstand the thrusting force due to the deceleration of the lid and capsule. The weight of the lid, secondary coolant and fuel capsule is approximately 50 lb. The ten 1/2-in. retaining bolts are capable of holding the following static load.

$$F_{TOT} = 10 (.126 \text{ sq in.}) (30,000/2) = 18,900 \text{ lb}$$

This is 378 times the weight of the lid and contents.

VIII. CASK COOLING MEDIA

Primary Coolant

Metallic Particles

The use of metallic particles as a heat transfer medium has been a subject of investigations at BMI. These studies indicate that the use of metallic particles is an excellent method of lowering the ΔT from heat source surfaces to the cask cavity wall. The flow characteristics of spherical particles have also been studied. A packed bed of particles of 35 percent void fraction, which is normal for shipping container use, is quite fluid. For instance, it has been found that a hydraulic head will cause the particles to rise above a discharge outlet.

Metallic Particle Thermal Tests

A test was performed with prototype GSC equipment using a 1500 watt heat source simulator to duplicate the decay heat load from the fuel capsule. The primary purpose of this test was to measure the temperature of the fuel capsule in the environment of the cask and to determine the particle flow characteristics of the metallic particle cooling media. Of secondary interest was the determination of the effective thermal conductivity for the packed bed. The cask cavity was simulated by a structure approximating the materials and geometry of the proposed GSC.

A stainless steel tube 2.704 in. I.D. with a 0.25-in. wall thickness simulated the cask cavity. The bottom of this tube was closed by a stainless steel truncated cone, which contained a 1/2-in. pipe drain tube positioned at a 30° angle from the horizontal. Similarly, at the top of the cavity structure, a fill tube was attached to enter at a 30° angle. This "cavity" assembly was immersed in a low melting-point metallic bath coolant,* providing a controlled ambient temperature.

Temperatures were monitored by ten thermocouples, individually welded to the selected pickup points noted in Figure 19. The thermocouples were .008-inch diameter chromel-alumel wire insulated with MgO and sheathed in .040 in. O.D. stainless steel tubing. (Thermocouples of such small mass can be shown to have negligible effects on the monitored temperature.) The thermocouples were located in planes normal to the axis of the apparatus, and were vertically positioned at the top, bottom, and mid-point of the surface of the simulator. This arrangement placed the key thermocouples (5, 9, and 10, 6) approximately 0.097 inch apart with the spherical nickel particles occupying the annular space.

The temperature at the outside of the cavity wall was maintained at 200 F in all tests by the liquid metal (ASARCO) coolant. The final heat sink was provided by a circulating water cooling coil attached to the outside of the liquid metal containment. The power input was controlled by a voltage-regulated power supply and recorded from a precision voltmeter and ammeter.

* ASARCO (50% Bi, 26.7% Pb, 13.3% Sn, and 10% Cd).

FIGURE 19.

Metallic Particle Thermal Test Schematic (Thermocouple pickup points noted)

The first test was made to determine (1) the operating temperature of the capsule under equilibrium conditions, and (2) the thermal conductivity of the nickel particles at the operating temperature. The capsule temperature reached a value of 308 F under equilibrium conditions. The ΔT through the nickel particles was found to be 117 F (i.e., 308 F - 191 F). By the use of this temperature gradient the equivalent thermal conductivity K of the nickel particle bed was calculated from the following expression

$$K = \frac{Q}{2\pi L (\Delta T)} \ln (D_2/D_1) \quad (1)$$

where

$$Q = 1500 \text{ watts}$$

$$L = 16.5 \text{ inches}$$

$$\Delta T = 117 \text{ F}$$

$$D_2 = 2.70 \text{ inches}$$

$$D_1 = 2.51 \text{ inches}$$

Solving Equation 1 yields $K_{eq} = 0.348 \text{ Btu/hr-ft F}$, the value used in the Heat Transfer Section for calculational purposes.

The second test was to determine the flow characteristics of the nickel particles at the operating temperature of the capsule. Of a 353 cc charge of solid coolant particles, 346 cc flowed freely from the apparatus without agitation. The additional 7 cc were trapped on a ledge formed by

the insertion of the drain pipe into the lower cavity (in the actual design the lower cavity is counterbored, i.e., smooth exit path) section. No particles were found on the simulator's surface following the latter's extraction from the test rig.

The third test was made to determine how the removal of the nickel particles would affect the temperature of the capsule while maintaining the heat input at 1500 watts. The nickel particles were drained from around the fuel capsule, and over a 20-minute period the capsule surface temperature rose from 308 F to an equilibrium temperature of 1104 F. The capsule surface temperature remained at this equilibrium value for more than one hour at which time the test was terminated.

The fourth test was carried out to determine the effect of time at normal operating temperature on the flow and thermal characteristics of the nickel particles. The test was terminated after 14 days of continuous running. No noticeable change of nickel particle flow characteristics or thermal conductivity were noted, nor was the surface of the electrical heat source visibly affected.

Secondary Coolant

Water-Ethylene Glycol

To assure that the liquid shield material will not freeze, expand, and possibly break the new cask open at the specified -65 F low temperature

limit, a 30 volume percent solution of water-ethylene glycol was proposed. Tests were made to determine the physical properties of glycol-water mixtures at low temperatures.

Water-Ethylene Glycol Thermal Tests

An experiment was carried out to determine the expansion and hardness of the solid formed from various water and ethylene glycol mixtures at a temperature of -65 F. The experimental setup was similar to a Beckman Freezing Point Apparatus. Solutions of water and glycol contained in test tubes were lowered into a Dewar flask of dry ice and acetone. A temporary lull in temperature was observed in the mixtures which was regarded as the freezing point. The temperature continued to drop below the freezing point. When the temperature of the mixture was allowed to rise to -65 F, the difference in level for the mixture at room temperature and at -65 F was recorded. The percent volumetric expansion was estimated from the difference in levels. The solids were removed from the test tube and qualitatively tested for hardness. Table 10 lists the various solutions tested:

TABLE 10. LOW-TEMPERATURE STUDIES OF VARIOUS
WATER-ETHYLENE GLYCOL SOLUTIONS

Water-Ethylene Glycol Solution, %	Freeze Point, C	Volume Increase, %	Relative Hardness
0	0	12.3	1
20	- 8	4.4	0.5
25	-10	--	0.4
30	-15	2.75	0.4
35	--	contracted	--

While a 30 v/o water-ethylene glycol solution does expand on freezing by about 2.75 percent, this is much lower than for pure water (12.3 percent). Furthermore, a relative soft slush is maintained down to -65 F for the 30 v/o glycol water solution. Based on these experiments a 30 v/o glycol-water mixture was used in the SNAP-27 GSC design.

SAFETY ANALYSIS REPORT
(Addendum I)

on

THE SNAP-27 GROUND SHIPPING CASK

to

UNITED STATES ATOMIC ENERGY COMMISSION
ALBUQUERQUE OPERATIONS OFFICE

March 13, 1967

BATTELLE MEMORIAL INSTITUTE
Columbus Laboratories
505 King Avenue
Columbus, Ohio 43201

SNAP-27 GSC SAR ADDENDUM

1. A calculation was performed which yields the maximum fuel capsule surface temperature when no water-ethylene-glycol mixture is present in the GSC. The absence of the water-ethylene-glycol mixture is postulated to be due to a puncture of the container during a 30-foot drop prior to the shipping fire.

The worst condition which can be imagined for the calculation is that in which steady state has been reached, i.e., the cask has been sitting in the fire long enough for the outer surface temperature to be close to 1475°F. At steady state, the cask surface must reject heat to a 1475°F environment. The calculation outlined below is identical to those made for the normal operating case except that the water-glycol mixture is not present and different boundary conditions and physical properties are used.

Assuming that the cask surface temperature is 1482°F, it can be shown that natural convection heat transfer is negligible. The heat rejected by radiation is given by

$$Q = 0.173 \epsilon_o A_r \left[\left(\frac{T_s}{100} \right)^4 - \left(\frac{T_a}{100} \right)^4 \right],$$

where

A_r = string area = 30 sq. ft. (p 50 of SAR).

$\epsilon_o \approx 0.8$ (p 24 of SAR).

$T_s = 1942$ R.

$T_a = 1935$ R.

Therefore,

$$\begin{aligned} Q &= 0.173(0.8)(30)(142,000-140,000). \\ &= 8300 \text{ Btu/hr.} \end{aligned}$$

Since $Q > 5118$ (the decay heat), a cask surface temperature of 1482°F is more than enough to reject heat to a 1475°F ambient environment.

The temperature drop across the cask wall is given by Equation (8) in the SAR. Substituting the proper geometric parameters and a thermal conductivity for stainless at 1482°F of $15 \text{ Btu/hr ft } ^{\circ}\text{F}$, the ΔT is approximately 0.5°F . On page 44 of the SAR, it was shown that the only significant means of heat transfer across the biological shield section when the secondary coolant is lost was that due to conduction through the copper bars. Using Equation (18) and neglecting all terms but the copper conduction term

$$\Delta T_{\text{biological shield}} = \frac{QL}{K_{\text{Cu}} A_{\text{Cu}}} = \frac{(5118)(0.75)}{(200)(0.16)} = 120^{\circ}\text{F}.$$

Again using Equation (8) for the ΔT across the inner stainless steel shell, the difference is found to be 7.2°F . The thermal conductivity of the metal-particle-filled gap will actually increase with increasing temperature. However, due to a lack of experimental data, the thermal conductivity of the nickel particles is assumed constant at $0.348 \text{ Btu/hr ft } ^{\circ}\text{F}$. It should be noted that this assumption is conservative. Hence,

$$\Delta T_{\text{gap}} = \frac{5118}{(6.28)(15/12)(0.348)} \ln \left(\frac{2.75}{2.57} \right) = 127^{\circ}\text{F}.$$

The various temperature drops computed are summed and added to the cask surface temperature to yield a maximum fuel capsule surface temperature of 1737°F .

The maximum fuel capsule temperature of 1737°F is still well below the melting range of Haynes Alloy 25 (2425-2570°F). Hence, even in the case of a loss of secondary coolant followed by a shipping fire, the capsule clad will not fail and release radionuclides.

2. The relief valve selected for this application is the same type that has been approved by the AEC (DML) for over 100 spent fuel shipping containers employing water as a primary coolant. Continued use of this type of valve for this purpose, without incident, since 1959 has justified its selection. These valves are also used regularly to vent fossil fuel tanks where the danger from explosion is much greater than in the present case.

The valve is located between fins on the side of the cask and is covered by a box with 0.50-inch stainless steel end plates which are welded to the fins. The construction of this valve is such that any damage to the valve will cause it to leak, thus providing a fail-safe operation. The attached copy of a section through this valve illustrates the protection afforded the plunger by the cap and pressure adjusting screw support. A vertical flow at Position A (see attached sketch) of sufficient force to deform the cap and screw support so as to allow pressure to be exerted on the plunger, would either break the bronze casting or yield the four 1/4"-20 cap screws which attach the unit to the cask body before any pressure would be applied to the plunger.* Either of the above conditions would relieve the cask pressure.

* Ref. Safety Analysis - AEC DLR Docket 70-1039, p 103.

BRONZE RELIEF VALVES FOR LIQUIDS WITH CAP

Set at any single specified pressure
from 1 lb. to 250 lbs.

Orders should specify the Pressure Setting desired as well as the Figure Number. Pressure Setting should be 20% above Working Pressure.

Relief Valve
Male Inlet, Fig. 658

Relief Valve
Male Inlet, Fig. 658

Relief Valve
Female Inlet, Fig. 286

These valves, designed principally for gasoline service, are tight on the discharge side. Frequently, they are installed near the outlet of the main pumps in the pump houses of bulk gasoline stations. When the valve controlling the filling of the tank car or other vessel is suddenly shut off, this valve functions, the discharge going to the suction tank through appropriate piping.

This valve can also be used for other liquids that require a pressure tight discharge. It is frequently used on the discharge side of boiler feed pumps.

It can also be installed on steam cylinders of reciprocation engines to relieve a slug of water which would ordinarily crack a cylinder head.

Bases and all working parts are of bronze, with

the single exception of the steel, cadmium-plated springs.

Seats are integral and do not have a huddling chamber.

Pressure Setting should be 20% above working pressures. Setting can be changed by removing the cap, releasing the locknut on the stem, and adjusting the regulating screw with a wrench. This change should be confined to within 10% of the set pressure.

Designs with both male and female inlets are available, as shown above.

Outlet Size is the same as inlet size.

Marine Service—Sizes $\frac{1}{2}$ to 2 inches may be used on marine service.

LIST PRICES, EACH

Size of Inlet (inches)	$\frac{3}{8}$	$\frac{1}{2}$	$\frac{3}{4}$	1	1 $\frac{1}{4}$	1 $\frac{1}{2}$	2	2 $\frac{1}{2}$	3
Fig. 658, Male Inlet Side Outlet	11.50	12.50	15.00	18.00	22.00	27.00	40.00	61.00	90.00
Fig. 286, Female Inlet Encased Spring	11.50	12.50	15.00	18.00	22.00	27.00	40.00	—	—

DIMENSIONS, IN INCHES

Size of Inlet	$\frac{3}{8}$	$\frac{1}{2}$	$\frac{3}{4}$	1	1 $\frac{1}{4}$	1 $\frac{1}{2}$	2	2 $\frac{1}{2}$	3
Size of Outlet	$\frac{3}{8}$	$\frac{1}{2}$	$\frac{3}{4}$	1	1 $\frac{1}{4}$	1 $\frac{1}{2}$	2	2 $\frac{1}{2}$	3
Over All Height	4 $\frac{1}{2}$	4 $\frac{1}{2}$	5 $\frac{1}{4}$	6 $\frac{1}{4}$	7 $\frac{1}{4}$	8 $\frac{1}{4}$	9 $\frac{1}{4}$	11 $\frac{1}{4}$	13 $\frac{1}{4}$
Center Line to Top of Cap	3 $\frac{1}{4}$	2 $\frac{1}{4}$	3 $\frac{1}{4}$	3 $\frac{1}{4}$	4 $\frac{1}{4}$	4 $\frac{1}{4}$	5 $\frac{1}{4}$	6 $\frac{1}{4}$	7 $\frac{1}{4}$
Center to End, Inlet	1 $\frac{3}{4}$	1 $\frac{1}{8}$	2 $\frac{1}{8}$	2 $\frac{1}{8}$	3 $\frac{1}{8}$	3 $\frac{1}{8}$	4 $\frac{1}{8}$	5	5 $\frac{1}{8}$
Center to End, Side Outlet	1 $\frac{1}{2}$	1 $\frac{1}{4}$	1 $\frac{1}{2}$	1 $\frac{1}{2}$	1 $\frac{1}{2}$	2 $\frac{1}{2}$	2 $\frac{1}{2}$	3 $\frac{1}{8}$	3 $\frac{1}{8}$

3. There is a pressure-relief mechanism for the cask cavity cover. This relief mechanism is a fusible plug manufactured from an indium alloy which will melt at 300°F. It must therefore be assumed that during the fire, the plug will melt and all of the water will escape rapidly from the cask cavity cover. It was shown in the analysis for normal operating conditions (in the SAR) that if heat transfer out the ends of the cask is neglected, a small error of 4°F is incurred, i.e., if heat transfer out the ends is neglected, the capsule surface temperature is calculated to be about 4°F too high. If the water escapes from the cover during a fire, the amount of additional heat received by the capsule will not be significant for two reasons:

- (1) Less than 5 percent of the available surface area of the cavity surrounding the capsule is represented by an end. Since the heat transferred into the capsule is directly proportional to the surface area available for transfer, little heat will enter through the ends.
- (2) The heat from the fire incident on the top of the cask must cross a thermal barrier of 8 inches of air after the water is lost.

Therefore, the answer to Question 3 is that the heat transfer analysis in the SAR is not significantly altered.

4. On page 58 of the SAR, the temperature referred to as 431°F is the temperature at the surface of the cask outer shell (or wall), and not the inner shell. While there is a remote possibility of a vapor blanket forming at the inside surface of the outer shell, the probability of this occurring is probably low because of the turbulent motion of the fluid in the annulus. The turbulent motion is enhanced by the presence of internal fins which have a baffle effect. Furthermore, it would be nonconservative to assume an insulating blanket at the inner surface of the outer shell because such a blanket would effectively insulate against heat transmission from the fire into the water. This would result in less water being boiled off per unit time. The problem of burn-out (destruction of the metal due to vapor blanketing) does not occur for the outer shell inner surface. Burn-out does not occur at fluxes below about 10^6 Btu/hr ft² and the average flux received from the fire is of the order of 10^5 Btu/hr ft².

On page 65 of the SAR, a heat transfer coefficient from the inner shell to the glycol-water mixture of 10 Btu/hr ft² °F was used. While vapor blanketing at the inner shell surface is not likely, an h of 10 corresponds to that for superheated steam (McAdams, p 5) and would, if a vapor blanket did exist adjacent to this surface, account for it. Hence, the 942°F maximum capsule surface temperature is conservative.

5. In the loss-of-primary-coolant case in the SAR, the results for a coated and uncoated capsule were considered. The reason was because in the loss-of-primary-coolant case, the principal method of disposing of the decay heat is by radiation which strongly depends on the emissivity of the capsule surface. For the fire accident environment, the primary coolant is not assumed lost, hence, the primary means of heat transmission from the fuel capsule is conduction which is independent of capsule surface emissivity. Therefore, it is unimportant in the fire accident whether or not the fuel capsule is coated.
6. The first sentence on page 2 of the SAR should read "20-inch diameter" instead of "2-inch diameter".
7. The correct pressure-relief setting is 75 psi.