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KINETIC STUDIES OF HETEROGENEOUS WATER REACTORS

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I. GENERAL SUMMARY OF PROGRESS IN THE QUARTER ENDING 30 SEPTEMBER 1963

During this report period, additional measurements were made of steam-void fraction, temperatures, and pressures in our KEWB capsule experiment. Analysis of these data and previous data indicated that friction in the volume transducer piston was large during most of the transients, although friction measurements before each run had shown the static friction to be negligible. Work during this quarter included the reduction of the dynamic piston friction to negligible magnitudes, thereby permitting the resumption of the experimental program on rapid steam-void formation. Other work on the KEWB capsule experiment included the formulation of a method for calculating the transient heat flux from the fuel disc. The heat flux is important in the interpretation of the measurements. These topics are discussed in detail in the present report.

Also, during this report period we began boiling stability experiments with a 2-foot long rod bundle, consisting of a square array of 16 stainless-steel rods .250" diameter, spaced on .312" centers. The rods are electrically heated in a natural-circulation loop, at one atmosphere pressure. The purpose of the experiment is to explore hydrodynamic instabilities in rod-bundle geometries, and to relate these to our other measurements and analysis on rectangular channels. The primary measurements to be made on the rod bundle are the power-flow transfer functions and the identification of domains of hydrodynamic instability. Power-void transfer functions will also be measured, although the accuracy of these measurements is limited by the X-ray absorption of the rods in the bundle.

We also have done work during this quarter on a survey of theories and data on hydrodynamic instabilities in natural-circulation boiling systems. Numerous theories and models have been published on the spontaneous flow oscillations which frequently arise in natural-circulation boiling loops and reactors. Our survey is an attempt to compare the various theories with each other in terms of the physical assumptions, and to compare their predictions with the available experimental data. We are currently in the process of studying the many publications in this field, and applying the theories to the prediction of hydrodynamic instabilities for conditions where experimental data are available.

Preparation of the apparatus for measurement of flow-void transfer functions in a SPERT IA channel was essentially completed during this report period.

II. KEWB CAPSULE EXPERIMENT DATA

This experiment was described in our September 1962 Quarterly and preliminary results reported in our March 1963 Quarterly. Additional data have been taken and a more complete analysis has been made of the data presented in March.

Figure 1 contains the data from a run made in May. The mass loading of the piston, corresponding to reactor hydrostatic head[1], is 50% greater than in the runs reported in March. The piston friction, discussed in detail in Part III of this report, is smaller than it was in runs reported earlier, but is still of appreciable effect in Fig. 1.

One of our objectives is to measure void formation, under conditions similar to those in the SPERT I destructive transient. Figure 2 is a comparison of a temperature trace from a 3.5 ms KEWB capsule run with two traces from SPERT I destructive transient. This illustrates that it is possible to obtain temperature histories during KEWB capsule experiment runs which are

[1] A detailed explanation of the modeling of hydrostatic head is given in the appendix.

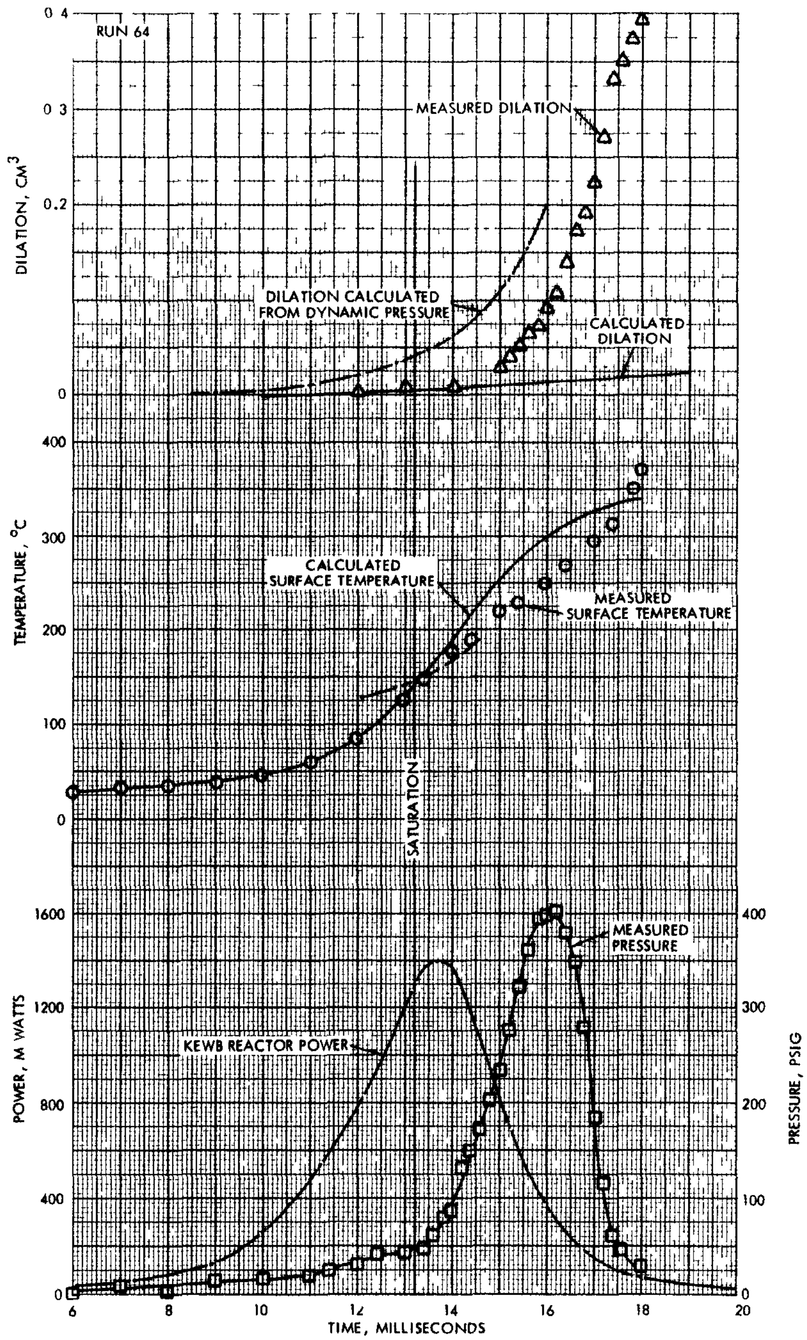

Figure 1. Measurements of Capsule Volume Dilation and Pressure, and Fuel Disc Surface Temperature During a KEWB Power Burst with an Initial Period of 1.52 Milli-seconds. Piston friction had been reduced, relative to earlier measurements, but was still appreciable in this transient.

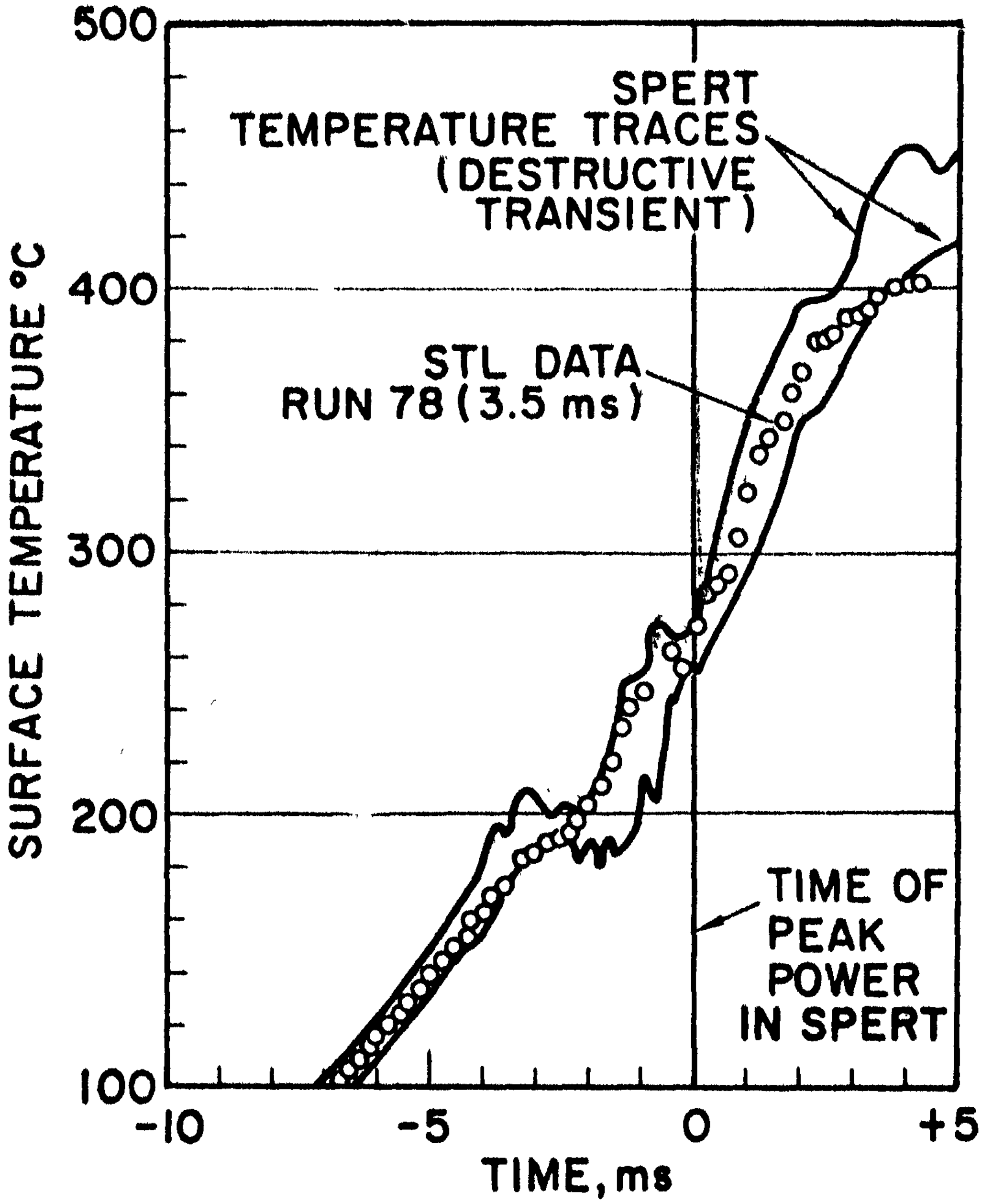

Figure 2. Fuel Surface Temperature History, above 100°C, in a 3.5 ms KEWB Capsule Transient, Compared with Surface Temperature Histories Measured in the SPERT I Destructive Transient as Presented by Feinauer and Brockett ("Some Requirements for Pressure Incidents", Symposium on Reactor Kinetics and Control, U. of Ariz., March 1963).

similar to the SPERT I temperature histories and therefore void data obtained in these experiments should be applicable.

III. PISTON FRICTION

During this report period, routine procedures were developed for the reduction of the experimental data. Analysis of the data obtained to date indicates that piston friction was appreciable during most of the power transients, even though measurements which had preceded each test run had shown that static piston friction was negligible. Much of our effort, during this quarter, was directed toward the elimination of this problem.

Analysis of the pressure and dilation measurements to date has shown that, in general, piston friction was large relative to the net force which accelerated the piston. In a few runs, particularly when the pressure transient reached several hundred psi, the piston friction appeared small but not negligible. It had been expected that the moving friction would be smaller than the static friction, measured as about 1 psi. Instead, moving friction was found to increase with instantaneous pressure. The magnitude of the friction (i.e., the pressure needed to overcome friction) was nearly equal to the pressure, except when the dynamic pressure was several hundred psi. For this reason, it was necessary to defer further KEWB transients until the piston friction could be reduced.

A piston of mass m, area A, position V/A, and friction p_f with applied pressure p, has the equation of motion:

$$(p - p_f)A = \frac{m}{A}\frac{d^2V}{dt^2} \qquad (1)$$

Double integration of this equation from some time, t_o, at which V and p are zero, gives

$$\frac{A^2}{m} \int_{t_o}^{t} dt' \int_{t_o}^{t'} p \, dt - \frac{A^2}{m} \int_{t_o}^{t} dt' \int_{t_o}^{t'} p_f \, dt = V \qquad (2)$$

In Fig. 3 the first term of Eq. (2) is shown both for runs with low and with high pressure rises. The difference between the calculated and measured dilation curves at any time is the double integral of friction to that time. In Fig. 3-a, the friction pressure is almost equal to the total pressure, while in the higher pressure run, shown in Fig. 3-b, the friction pressure is of the order of 1/3 the total pressure.

The capsule was set up in the laboratory. Means were developed for imposing pressure pulses of about the same shape as those formed by the reactor transients. Since modifying the piston was the least difficult change which could be made, it was decided to first attempt to alleviate the problem by this approach. The piston O-ring seal had been originally designed with compression according to manufacturer's recommendations for moving seals. It was found that the O-ring compression could be reduced to a small fraction of this recommendation and still give an adequate pressure seal for our application. Still lower friction was obtained by improving the finish on the bore.

The results of this effort are illustrated in Fig. 4. Part (a) shows data typical of those obtained in the laboratory with essentially the same piston setup as for the data in the March 1963 Quarterly. Practically all the applied force was used in overcoming friction, as was the case for Fig. 3-a. Figure 4-b shows data typical of that obtained after the final improvements had been made. The difference between measured dilation and the calculated double integral is within the

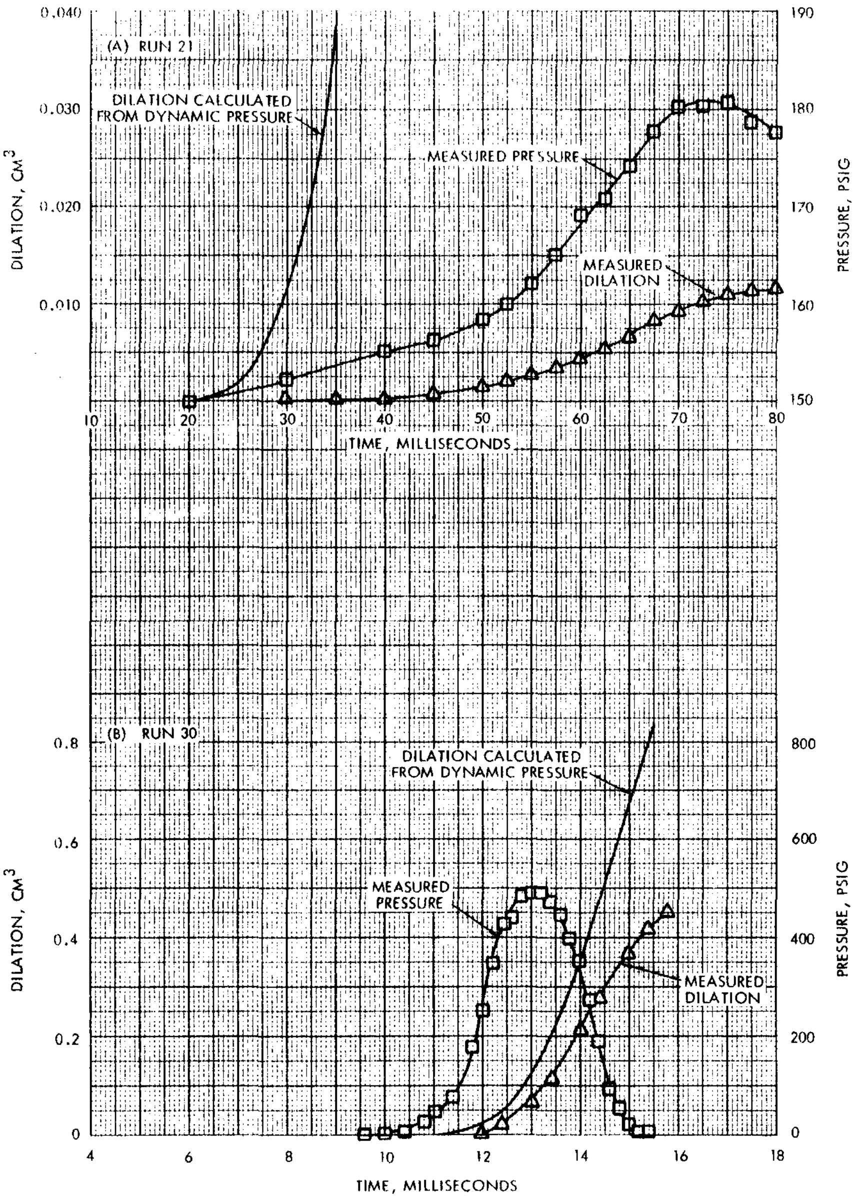

Figure 3. Comparison of Measured Capsule Volume Dilation with that Calculated from Dynamic Pressure During KEWB Reactor Power Bursts with Initial Periods: a) 9.3 Milliseconds and b) 1.41 Milliseconds. Both measurements were prior to modification of piston for reduction in friction.

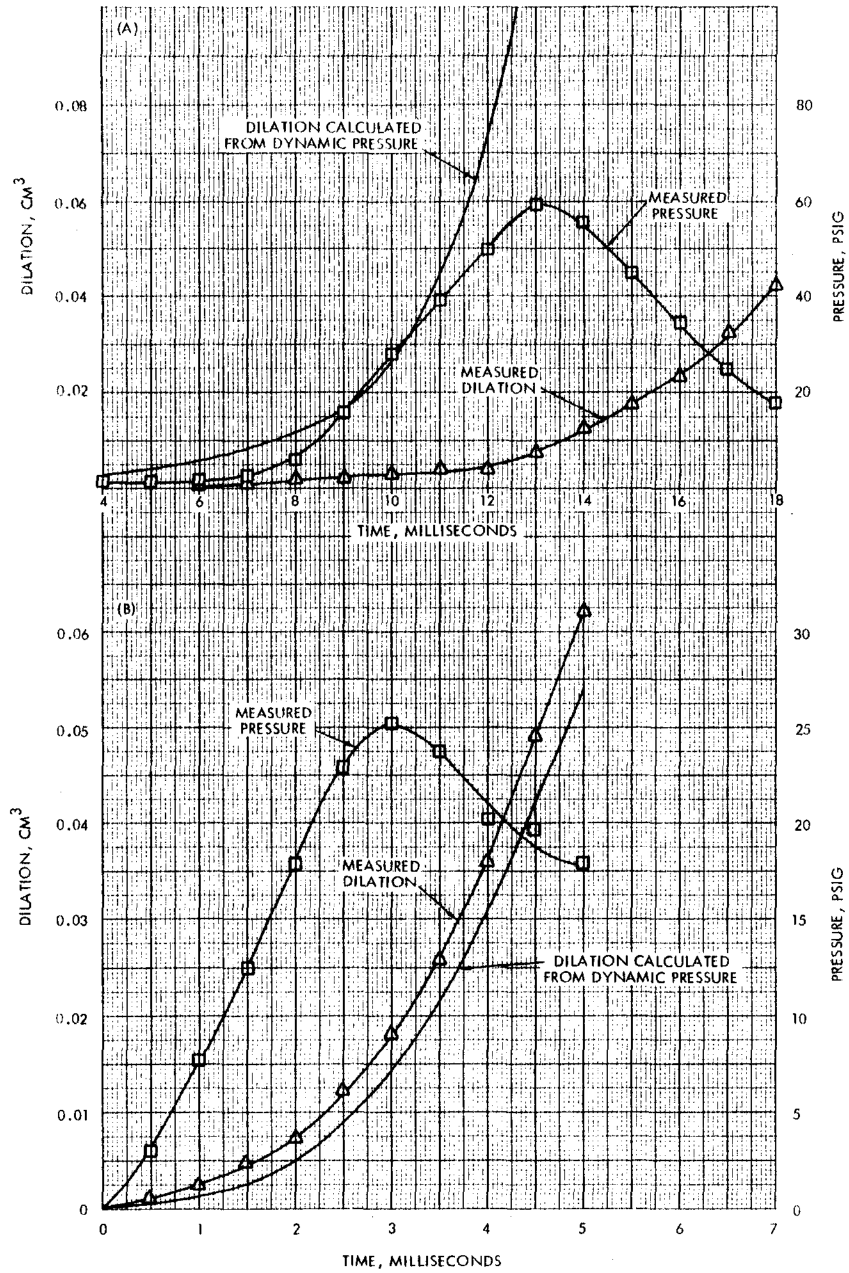

Figure 4. Comparison of Measured Capsule Volume Dilation with that Calculated from Dynamic Pressure: a) Before and b) After the Modification of the Piston and Cylinder. Pressure Pulse Mechanically Produced in the Laboratory.

uncertainties in measurement. The friction is now so small that it cannot be accurately determined. In-pile measurements are being resumed.

III. CALCULATION OF HEAT FLUX AT THE FUEL DISC SURFACE DURING TRANSIENT BOILING IN THE KEWB CAPSULE EXPERIMENT

Knowledge of the transient heat flux is essential to the interpretation of the KEWB capsule data, because the heat flux describes the rate at which energy is made available for steam formation. The measurements available for the heat-flux determination are the power history of the reactor and the surface temperature history of the uranium disc in the capsule. Assuming the relationship between reactor power and fuel disc heat generation rate is known, the heat-flux history is calculable. The first step is the calculation of the surface temperature history which would result from the fission heat release in the meat, on the assumption that the surface is perfectly insulated (zero heat flux). Then the measured temperature history is deducted from this insulated-surface temperature history, and the surface temperature depression thus determined is applied as a boundary condition to the one-dimensional heat conduction equation for the fuel disc. The heat conduction equation is solved for the time varying surface heat flux.

The fuel disc consists of U-10 Mo meat (.0534 cm thick) plated with nickel (.0010 cm) and clad with aluminum (.0076 cm). The nickel layer can be neglected in the heat conduction calculations because of its thinness. The aluminum clad has such high thermal conductivity, relative to the meat and the water, that the clad can be treated as a lumped thermal capacity. A brief calculation will serve to justify this assumption.

Consider the clad as a slab of finite thickness L ($o \leqslant x \leqslant L$, where x is the position variable), thermal conductivity k, and thermal diffusivity α (where $\alpha = k/\rho c$). Let the initial temperature of the slab be zero, and assume that beginning at time zero a constant heat flux Q is imposed on the face of the slab at $x = L$. The other face of the slab ($x = 0$) is perfectly insulated. When the rate of temperature change has become uniform throughout the slab, the slab may be considered as a lumped thermal capacity responding to the imposed heat flux. It will be shown that the time required for the clad to respond as a lumped capacity is negligible in the time scale of our experiments.

The transient temperatures in a slab for the boundary conditions assumed here are given by Carslaw and Jaeger,[1] p. 104, Eq. (1). The solution is

$$T(x,t) = \frac{QL}{k}\left\{\frac{\alpha t}{L^2} + \frac{3x^2 - L^2}{6L^2} - \frac{2}{\pi^2}\sum_{n=1}^{\infty}\frac{(-1)^n}{n^2}e^{-\alpha n^2\pi^2 t/L^2}\cos\frac{n\pi x}{L}\right\} \quad (3)$$

where T(x,t) is the local temperature. We are interested in the time required for the effect at $x = 0$ to essentially reach its asymptotic rate. Therefore, set $x = 0$ in Eq. (3), and differentiate with respect to time.

$$\frac{dT(o,t)}{dt} = \frac{Q}{\rho c L}\left\{1 + 2\sum_{n=1}^{\infty}(-1)^n\, e^{-\alpha n^2\pi^2 t/L^2}\right\} \quad (4)$$

As time increases, the right-hand side of Eq. (4) approaches $Q/\rho c L$, because the infinite series approaches zero with increasing time. The bracketed term on the right in Eq. (4) is one of the theta functions tabulated in Adams' "Smithsonian Mathematical

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[1] Carslaw and Jaeger, Conduction of Heat in Solids, Clarendon Press, 1947.

Formulae and Tables of Elliptic Functions". The values of the properties to be used are α = .86 cm^2/sec (the thermal diffusivity of aluminum) and L = 7.6 x 10^{-3} cm for the clad thickness. Using these values in the argument $\pi^2\alpha t/L^2$ and t = 20 x 10^{-6} sec, we find that the bracketed term equals 0.89, whereas its asymptotic value is unity. Hence, after about 20 microseconds, the clad temperature would be changing at about a uniform rate. Since 20 microseconds is small, relative to the time scale of the experiment (milliseconds), the clad can be treated as a lumped thermal capacity.

Approximating the clad as thermal capacity ρcL, with the temperature T_c, we express the heat flux at the clad-water interface in terms of the heat flux at the meat-clad interface.

$$Q_W = Q_M - \rho cL \frac{d}{dt} T_c \tag{5}$$

where

Q_M = heat flux from meat into clad

Q_W = heat flux from clad into water.

An expression for Q_M is required in order to compute the heat flux into the water for a given clad temperature history. This entails a heat conduction analysis of the meat. Carslaw and Jaeger again provide a solution. Referring to p. 88, Eq. (4) of Carslaw and Jaeger, the temperature in a finite slab with $-\lambda \leqslant x \leqslant \lambda$ (where λ is the half-thickness of the meat) with zero initial temperature and the surfaces kept at temperature Rt for $t > 0$ is:

$$T = Rt + \frac{R(x^2 - \lambda^2)}{2\alpha} + \frac{16R\lambda^2}{\alpha\pi^3} \sum_{n=0}^{\infty} \frac{(-1)^n}{(2n+1)^3} e^{-\alpha(2n+1)^2\pi^2 t/4\lambda^2} \cos\frac{(2n+1)\pi x}{2\lambda} , \tag{6}$$

where α is now the thermal diffusivity of the meat. Differentiating with respect to x, and setting $x = \lambda$, the temperature gradient at the surface is

$$\left.\frac{\partial T}{\partial x}\right|_{x=\lambda} = \frac{R\lambda}{\alpha} - \frac{8R\lambda}{\alpha\pi^2}\sum_{n=0}^{\infty}\frac{1}{(2n+1)^2}\,e^{-\alpha(2n+1)^2\pi^2 t/4\lambda^2} \qquad (7)$$

For brevity, set

$$G(t) = 1 - \frac{8}{\pi^2}\sum_{n=0}^{\infty}\frac{1}{(2n+1)^2}\,e^{-\alpha(2n+1)^2\pi^2 t/4\lambda^2}$$

Then, the heat flux at the surface of the meat, due to a surface temperature ramp Rt, is

$$Q_M = -k_M\left.\frac{\partial T}{\partial x}\right|_{x=\lambda} = -k_M\frac{R\lambda}{\alpha}G(t) \qquad (9)$$

Assuming that the surface temperature is a ramp, Eqs. (5) and (9) yield the total heat flux into the water.

$$Q_W = -k_M\frac{R\lambda}{\alpha}G(t) - \rho cLR = -R\left[\frac{k_M\lambda}{\alpha}G(t) + \rho cL\right] \qquad (10)$$

where

R = rate of rise of surface temperature (linear ramp), ^oC/sec

k_M = thermal conductivity of meat, cal/sec ^oC cm

α = thermal diffusivity of meat, cm^2/sec

Q_W = heat flux from fuel plate into water, cal/cm^2 sec.

The function

$$G(t) = 1 - \frac{8}{\pi^2} \sum_{n=0}^{\infty} \frac{1}{(2n+1)^2} e^{-\alpha(2n+1)^2 t/4\lambda^2}$$

is easily evaluated with a digital computer; it is plotted against its complete argument $\alpha t/4\lambda^2$ in Fig. 5. For the fuel discs used in our KEWB capsule experiments, λ is approximately .027 cm, and the thermal diffusivity, α, is about .043 cm^2/sec.

In this analysis, we have assumed constant thermal properties for the meat, which makes the heat conduction solution linear with respect to R, the assumed rate of change of surface temperature. Therefore, the heat flux due to surface temperature histories of arbitrary shape can be approximated by superimposing the solutions for a sequence of surface temperature ramps, the sum of which approximates the actual surface temperature history. The surface temperature history to be used is not just the measured temperature, but rather, the depression of the measured surface temperature below the surface temperature calculated for an insulated fuel plate with the known heat generation history. The sign of the heat flux in Eq. (10) was arbitrarily made negative for a rising surface temperature (heat flowing into the meat from its environment). Then, a decrease in surface temperature (relative to the calculated zero heat flux surface temperature) will give a positive heat flux, or heat flow from the fuel disc into the environment.

We have programmed the calculation of heat flux for digital computation as part of the data reduction routine for each transient. The heat flux computed for Run 64 (measurements shown in Fig. 1) is plotted in Fig. 6. A marked increase in

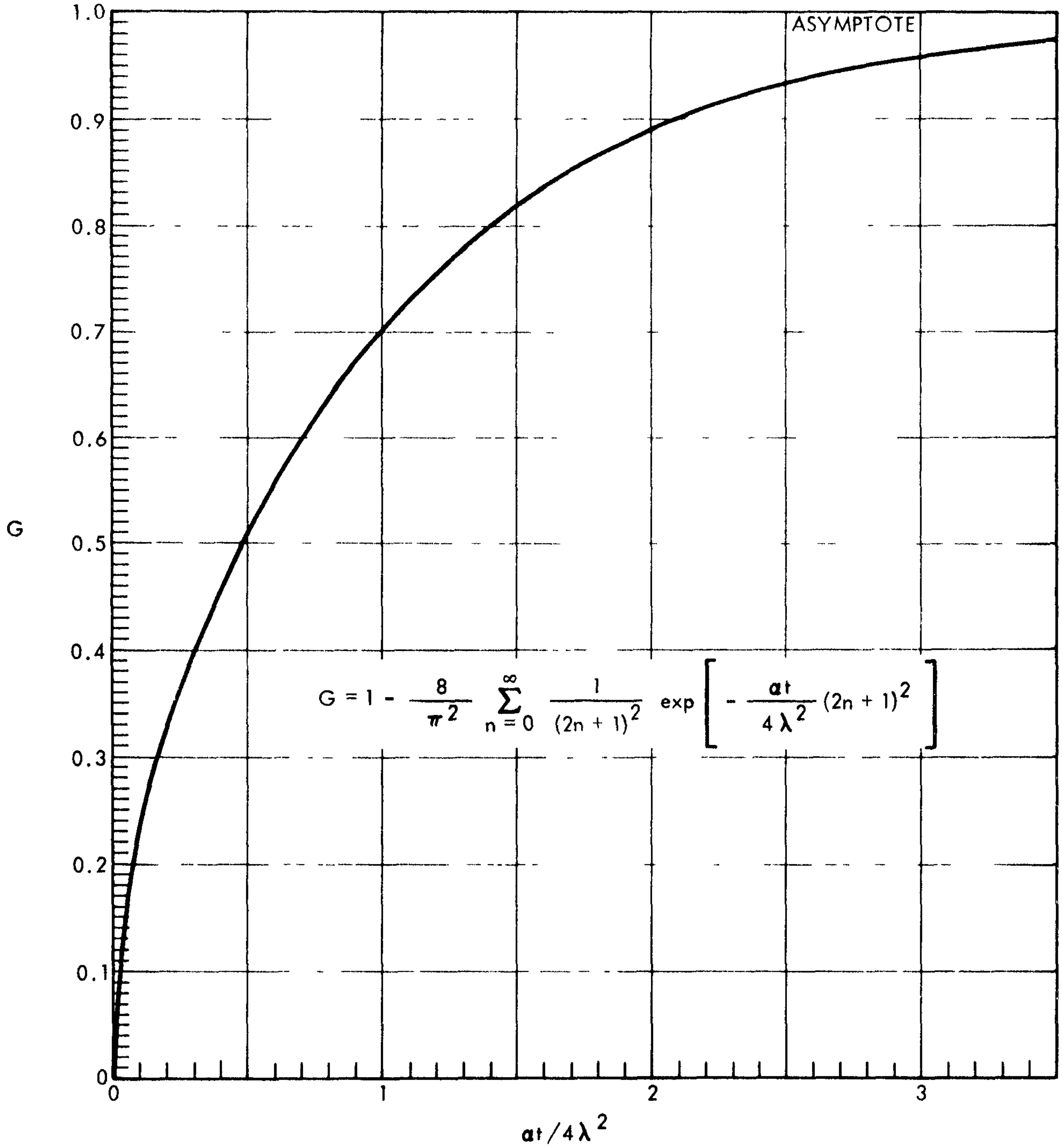
ASYMPTOTE
1.0
0.9
0.8
0.7
0.6
0.5
0.4
0.3
0.2
0.1
0
G
$G = 1 - \frac{8}{\pi^2} \sum_{n=0}^{\infty} \frac{1}{(2n+1)^2} \exp\left[- \frac{at}{4\lambda^2} (2n+1)^2 \right]$
0
1
2
3
$at/4\lambda^2$

Figure 5

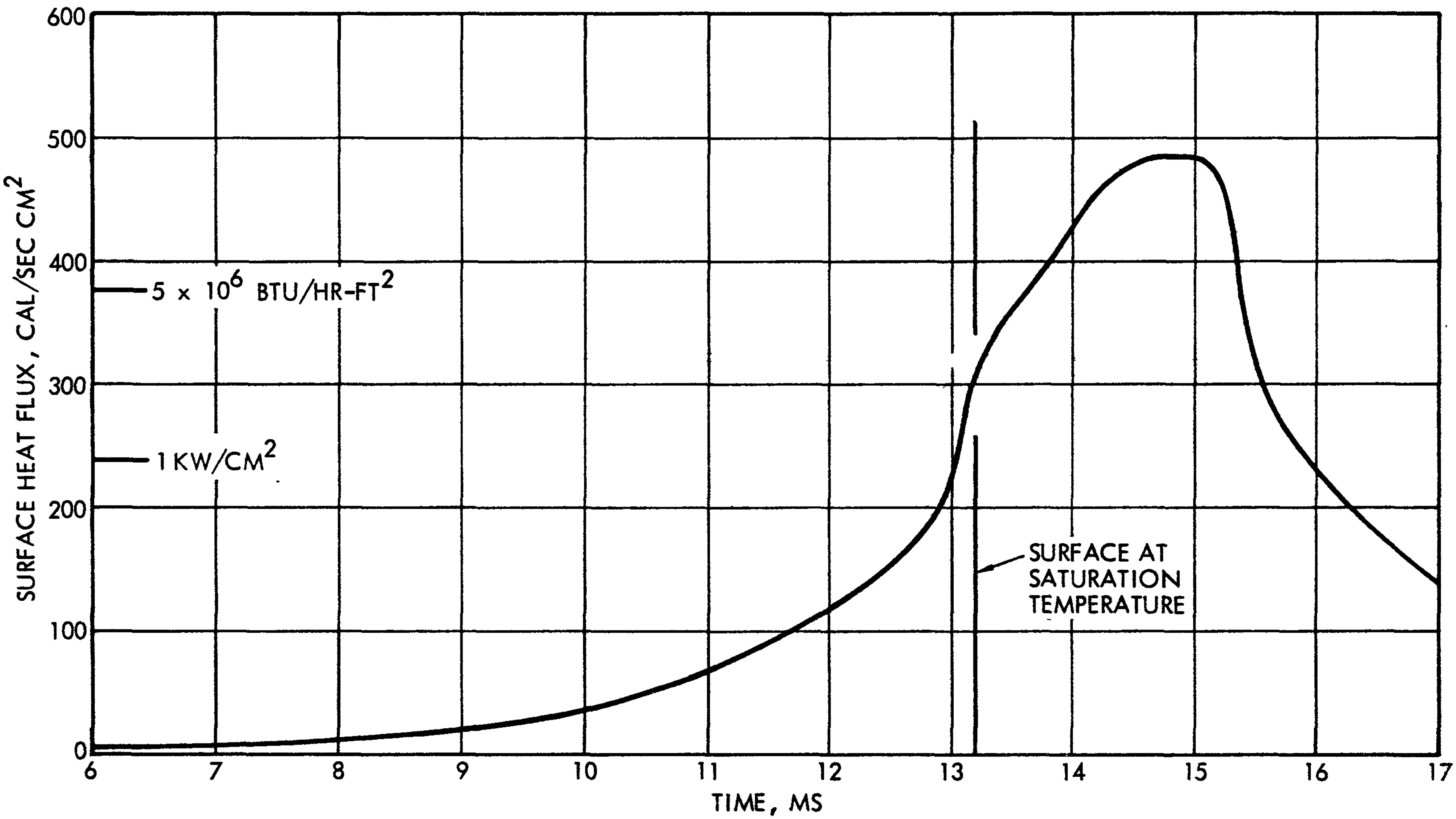

Figure 6. Surface Heat Flux for Run 64, Calculated from Temperature Data Shown in Fig. 1.

heat flux is seen to accompany the onset of boiling. The curve marked "Calculated Surface Temperature" in Fig. 1 is not the zero heat flux temperature history used in the calculation of Fig. 6, but is the surface temperature calculated using the assumption that heat transfer in the water is by conduction only.

The two major assumptions in the heat flux calculation are: (1) the treatment of the fuel disc as a one-dimensional slab and (2) the assumption of constant thermal properties in the meat. The one-dimensional assumption introduces errors due to actual radial and azimuthal variations in uranium loading and neutron flux. The magnitude of this error can be assumed small when there is good agreement between the several surface thermocouples at various positions on one face of the fuel disc. The error due to the assumption of constant thermal properties will depend on the temperature attained in the transient of interest. This is the subject of continuing analysis.

Having the calculated heat flux history for each KEWB capsule transient will be an aid to understanding the processes in transient void formation. For example, the measurements of piston velocity and pressure define the instantaneous rate of mechanical work being done by the boiling process; dividing by the instantaneous heat flux will give the instantaneous thermal efficiency of the gross process of void formation. This may lend insight when data with negligible piston friction are available for study.

APPENDIX

INERTIAL LOADING FOR THE SIMULATION OF REACTOR HYDROSTATIC HEAD

I. ON THE USE OF A LUMPED MASS VERSUS AN ACOUSTICAL LOAD

In this appendix, a modeling law is derived by means of which the lumped mass required to simulate a given reactor hydrostatic head can be calculated. In this derivation, it is assumed that the "round trip" acoustic propagation time* from the core to the surfaces of the reactor vessel and to the free surface of the liquid are short compared with the duration of the pressure transient. For cases where this assumption is not valid, the effects of fluid compressibility must be included in the simulation, and this would preclude using a lumped mass to represent hydrostatic head. In such cases, the experimental capsule would have to be loaded with a long column of water (or other substance having similar acoustic impedance) in place of a lumped mass. For the case of the SPERT I destructive transient, the lumped mass simulation is approximately valid during the initial formation of steam void which shuts the reactor down. This represents an interval of about 5 milliseconds. Lumped mass simulation would not be valid for the later time when a destructive pressure shock occurred with a rise-time of much less than a millisecond. Our KEWB capsule experiment was designed for a study of the earlier interval (the turning of the power burst).

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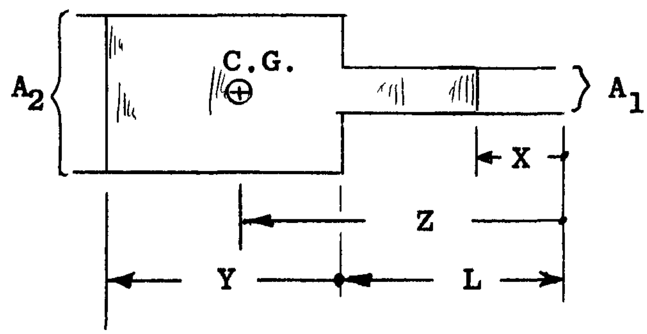
*This propagation time is about 2 milliseconds in SPERT I.

II. EFFECTS OF CHANGES IN CHANNEL CROSS SECTION

Consider an incompressible fluid of density ρ moving from a channel of cross sectional area A_1 into one of area A_2 as illustrated.



The fluid volume, V, is given by

$$V = Y_o A_2 + L A_1 \quad , \tag{A-1}$$

where Y_o is the value of Y when X = 0. To locate the center of gravity, coordinate Z, we balance the moments about X = 0.

$$VZ = YA_2(L + \tfrac{1}{2} Y) + (L - X) A_1(X + \tfrac{L-X}{2}) \quad , \tag{A-2}$$

or simplifying:

$$VZ = A_2LY + \tfrac{1}{2} A_2Y^2 + \tfrac{1}{2} A_1(L^2 - X^2) \quad . \tag{A-3}$$

Differentiating (A-3) with respect to time:

$$V\dot{Z} = A_2L\dot{Y} + A_2Y\dot{Y} - A_1X\dot{X} \quad , \tag{A-4}$$

where a dot over a variable stands for its time derivative. Now, when fluid leaves one section it enters the other and since $Y = Y_o$ when $X = 0$, we have:

$$Y = Y_o + \frac{A_1}{A_2} X \quad . \tag{A-5}$$

Differentiating (A-5) with time:

$$\dot{Y} = \frac{A_1}{A_2} \dot{X} \tag{A-6}$$

Substituting (A-6) into (A-4)

$$V\dot{Z} = A_1 \dot{X} \left[L + Y_o + \left(\frac{A_1}{A_2} - 1 \right) X \right] \quad . \tag{A-7}$$

Differentiating again, with respect to time,

$$V\ddot{Z} = A_1 \ddot{X} \left[L + Y_o + \left(\frac{A_1}{A_2} - 1 \right) X \right] + A_1 \dot{X} \left[\left(\frac{A_1}{A_2} - 1 \right) \dot{X} \right] \tag{A-8}$$

Evaluating (A-8) at $X = 0$, for then L and Y_o describe the fluid dimensions,

$$V\ddot{Z} \Big|_{X=0} = A_1 \ddot{X}(L + Y_o) + A_1 \dot{X}^2 (A_1/A_2 - 1) \tag{A-9}$$

In cases for which

$$\left| \dot{X}^2 \left(\frac{A_1}{A_2} - 1 \right) \right| << \left| \ddot{X}(L + Y_o) \right| \tag{A-10}$$

which is true for the KEWB capsule experiment during the time of piston acceleration, (A-9) reduces to

$$\ddot{Z} \cong \ddot{X} \; \frac{(L + Y_o) \; A_1}{V} \tag{A-11}$$

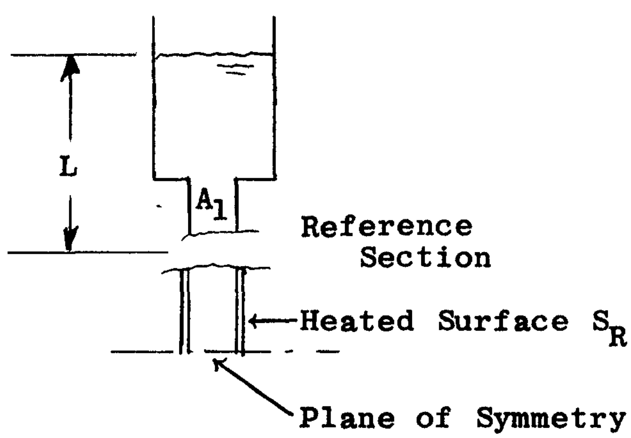
A force, F, acting on surface A_1 will cause the mass, ρV, to be accelerated as $\ddot{Z}$

$$F = \rho V \ddot{Z} = \rho A_1 (L + Y_o) \; \ddot{X} \tag{A-12}$$

The corresponding pressure at A_1 is

$$p = \frac{F}{A_1} = \rho (L + Y_o) \; \ddot{X} \tag{A-13}$$

III. ACCELERATION MODELING



The above illustration represents one channel of a reactor. The surface marked "Reference Section" is that at which the pressure is to be simulated and where $\ddot{X}$ is measured.

Let D be the thermal dilation of the boundary layer due to surface heating (D has units of length). Then,

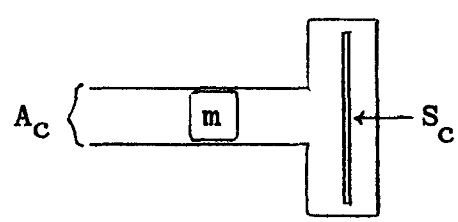
$$\ddot{X} = \frac{S_R}{A_1} \ddot{D} \qquad \text{(A-14)}$$

From (A-13) and (A-14)

$$p = \rho L \frac{S_R}{A_1} \ddot{D} \qquad \text{(A-15)}$$

In the capsule which is to model the reactor, $\ddot{D}$ will be reproduced, and p is to be reproduced as a result of the modeling of the hydrostatic head. The heated surface will be S_c, the area of the fuel disc, and the inertial column L_c. A_c is the piston area, which corresponds to A_1. Then, in order that the capsule have the same loading at the reference section, we must have:

$$\rho L_c \frac{S_c}{A_c} = \rho L \frac{S_R}{A_1} \qquad \text{(A-16)}$$



For the KEWB capsule experiment, the water column is replaced with an equivalent weighted piston of area A_c, and mass given by:

$$m = \rho_w L_c A_c = \rho_w \frac{LS_R}{A_1} \frac{A_c^2}{S_c} , \qquad \text{(A-17)}$$

where

ρ_w - water density

L - reactor hydrostatic head

S_R - effective reactor heat transfer surface per half-channel (assuming pressure relief at both ends of the fuel element).

A_1 - reactor fuel element coolant passage cross section

A_c - piston area in capsule experiment

S_c - heat transfer surface in capsule experiment.

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