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EUROPEAN ATOMIC ENERGY COMMUNITY - EURATOM

FUEL-CLAD CONTACT CONDUCTANCE

by

J. ARRIGHI, C. MUSTACCHI and S. ZANELLA

1966

ORGEL Program

Joint Nuclear Research Center
Ispra Establishment - Italy

Physical Chemistry

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CONTENTS

SUMMARY

An investigation was made of clad-pellet contact conductance for reactor fuel elements. Standard plane geometry correlations were shown to be unreliable whereas suitable cylindrical geometry measurements both in- and out-of-pile were accounted for by theoretical and experimental analysis. Both the gas conduction and the metal-to-metal conduction components were studied using as parameters the elastic and plastic properties of the materials in contact, contact pressure, surface finish and nature of the fluid in the gap.

Particular care was given to the case of Uranium Monocarbide and Sintered Aluminium Powder as pellet and clad.

= LIST OF FIGURES =

LIST OF SYMBOLS

a   -   radius of the contact spot, cm

C   -   cold assembly clearance pellet-clad, cm

C_o -   enrichment of fuel/natural U enrichment

C'   -   operating assembly clearance pellet-clad, cm

D   -   diameter of curvature of ridge, cm

d   -   density of fuel

E   -   Young's modulus, kg/cm^2

F   -   Thermal flux through pellet-sheath, W/cm^2

G_o -   nominal radial clearance UC-SAP, cm

G_1 -   hot radial clearance UC-SAP, cm

H   -   contact hardness, kg/cm^2

H_1 -   apparent "patch" hardness, kg/cm^2

K   -   harmonic mean conductivity of the two solids in contact, W/cm°C

$$K = \frac{2K_1 K_2}{K_1 + K_2}$$

K_1 and K_2 - Thermal conductivity of two solids, W/cm°C

K_3 -   Xenon thermal conductivity, W/cm°C

K_f -   fluid thermal conductivity, W/cm°C

K_p -   fuel conductivity, W/cm°C

L   -   fluid mean free path, cm

M   -   Atomic or molecular mass, gr

N   -   density of "contact patches", cm^{-2}

n   -   number of contact spots per unit surface, cm^{-2}

P   -   apparent pressure on contacting surfaces, kg/cm^2

P_f -   fluid pressure, atm.

R   -   mean square roughness of contacting surfaces, cm

$$R = \left[\frac{R_1^2 + R_2^2}{2} \right]^{1/2}$$

R_1 and R_2 - arithmetic mean roughness heights, cm

R_t -average roughness amplitude (about four times R_1), cm

Res - UC-SAP contact or gap thermal resistance, cm^2 °C/W

r_o - pellet radius, cm

r_3 - SS container inner radius, cm

s - fractional swelling of fuel pellet

S - SS container thickness, cm

t - clad thickness, cm

T - mean contact temperature, °C

$\overline{T}$ - average clad temperature, °C

$\overline{T}_C$ - pellet centre temperature, °C

T_s - pellet surface temperature, °C

ΔT_1 - gap temperature drop, °C

ΔT_p - pellet center-to-surface temperature drop, °C

U - UC-SAP contact or gap thermal conductance, $W/cm^2 °C$

u - solid-solid contact conductance, $W/cm^2 °C$

u_f - fluid conductance, W/cm^2

u_t - total conductance, $W/cm^2 °C$

$\propto$ - fluid-wall accommodation coefficient

$\propto_c$ - clad thermal expansion coefficient $°C^{-1}$

$\propto_p$ - pellet   "   "   " $°C^{-1}$

β - SAP-heavy water temperature drop, (Cr-Al mV)

δ - mean distance between contact planes, cm

ε_o - radial strain of UC pellet surface

ε_1 - radial strain of SAP inner surface

ϕ - Thermal neutron flux, n/cm^2 sec

λ - wavelength of surface microroughness, cm

ν - Poisson's modulus

1. Scope

Contact heat transfer between solid surfaces is a major operating factor in such systems as bearings, switching contacts and reactor fuel element claddings. In some instances it may become a controlling design parameter.

Study and comparison of the pubblished correlations showed an uncertainty of at least one order of magnitude when faced with the problem of actually assessing a value to a thermal contact conductance.

A three year research program was therefore carried out, involving on one hand a study of possible refinements of the available theories and on the other hand a direct experimental study, inclusive of separate determination of such parameters as surface hardness, geometry, mechanical finish, fluid atmosphere.

A final set of experiments bearing directly on the problem on hand, i.e. the thermal distribution in a SAP-clad UC element was carried out under irradiation in the proper geometry to simulate an actual reactor fuel in operation.

The correlations obtained in the present study are thought to enable the designer to assess a contact conductance value within $\pm 30\%$ with a confidence level in excess of 90%.

2. Theoretical

2.1. General

In most cases, real contact between two surfaces is limited to a minor fraction of the total apparent contact area.

The resistance to heat conduction will therefore be due to the combined effect of the constriction of flux lines through the real contact points and to the distribution of these contact points on the surfaces.

An analytical solution of the Laplace problem relative to two semi-infinite bodies connected through a circle of radius a is known. The resistance is 1/2 ak.

If the semi-spaces are connected by a number of such circles, and if the rest of the boundary transfers heat by conduction and radiation through the fluid, no closed-form analytical solution exists. This constitutes one of the difficulties of even the simplest model. Further, another difficulty concerns the estimate of the number of actual contact spots per unit surface n and the radii of such spots when the bodies are loaded in a given manner.

At any rate, a basis for any theoretical model involves two relations, derived as follows.

The actual pressure on the surface of a single contact point is equal to the contact hardness, an ill-defined property into which we shall go later (Appendix I), yielding the relation:

$$n \cdot \pi \, a^2 \cdot H = P \tag{1}$$

The conductance will be the sum of the parallel conductances of the fluid and the solid spots

$$u = K_f / \delta + n \cdot 2aK \cdot \varphi \tag{2}$$

where the apparent mean fluid thickness δ is a function of surface accomodation effects and geometry whereas the factor φ corrects the solid conductance for the interaction of several contact spots.

Besides the problem of correctly estimating δ, H and φ , which is often an open question, relations (1) and (2) constitute a set of two equations in three unknowns, u, n and a.

The choice of a third relation which makes the system defined constitutes the difference between the theories proposed by different authors.

2.2. Previous knowledge

Cetinkale and Fishenden ref. (2) give the factor ξ of eq. (2) as

$$\xi = \frac{\widetilde{\pi}}{2 \tan^{-1}\left\{\dfrac{1}{a\sqrt{\widetilde{\pi}}\ n}\ \sqrt{1 - \dfrac{K_f}{u\delta}} - 1\right\}} \tag{3}$$

which is readily verified to approach unity when the radius and the density of contact points is small.

The third relation, experimentally determined in ref. (2) can be reduced in this case to

$$n = 3.5 \times 10^{-8}\ \frac{(\lambda_1 + \lambda_2)^3}{a^5} \tag{4}$$

Combining (1), (2), (3) and (4) we obtain the required conductance correlation

$$u = \frac{K_f}{\delta}\ +\ \frac{206\ K}{\lambda_1 + \lambda_2}\ \frac{\left[P/H\right]^{4/3}}{\tan^{-1}\left\{\sqrt{\dfrac{1 - K_f/u\delta}{P/H}} - 1\right\}} \tag{5}$$

Laming Ref (4) studies the case of surfaces endowed with parallel ridges, which have therefore a constant number of contact spots and arrives to the following:

$$\xi = \frac{1}{1 - 1.41\left[\dfrac{P}{H}\ \dfrac{1}{1 - K_f/u\delta}\right]^{1/2}} \tag{6}$$

where

$$n = \frac{1}{\lambda_1\ \lambda_2} \tag{7}$$

However, he explains the discrepancies between his
theory and measurements by allowing for a variable
contact hardness empirically estimated as:

$$H = \frac{1.8 \times 10^4}{(\lambda_1 \lambda_2)^{1/2} \, P^{1/2}} \tag{8}$$

independent of the nature of the solid involved (alumi-
num, brass, steel). Combining (1), (2), (6), (7) and
(8), Laming's correlation can be expressed as:

$$u = \frac{K_f}{\delta} + \frac{.00842 \quad P^{3/4} \, K}{(\lambda_1 \lambda_2)^{1/4} \left[1 - \frac{.0105 \, P^{3/4}(\lambda_1 \lambda_2)}{\sqrt{1 - K_{f/u\delta}}} \right]^{1/4}} \tag{9}$$

Ross and Stoute ref. (6), following a model of Boescho-
ten and Van der Held ref. (7) and modifying an empirical
correlation by Ascoli and Germagnoli ref. (8), assume a
pressure-independent radius of contact points given by

$$a = .5 \, R^{1/2} \tag{10}$$

whereas ξ is taken as unity, for moderate pressures.

Combining (1), (2) and (10) and rounding off constants

$$u = \frac{K_f}{\delta} + \frac{K \, P}{.5R^{1/2} \, H} \tag{11}$$

Fenech and Rohsenow ref. (9) discount the possibility
of giving a general correlation between n and a. They
suggest a graphical determination to be made on the re-
corded surface profile to obtain n versus pressure.

However their results point out to a $p^{.7}$ dependence of n. Their formula can be simplified, in the case of surface roughnesses of the same order, to

$$u = \frac{K_f}{\delta} + \frac{2.13 \ K \ \sqrt{n} \ (P/H)^{1/2}}{(1-P/H)(1-K_f/K) \left[\dfrac{4.26 \ \sqrt{n} \ \delta}{(P/H)^{1/2}} + 1 \right]} \tag{12}$$

It is to be noticed that the datum δ appears in (12) even in the case $K_f = 0$. In order to compare solid-solid conductances estimated by means of the different formulas, we shall use two assumptions which will give an estimate of δ an n to use in (12).

The "mean distance between surfaces" will vary between a value of the order of the sum of the amplitudes of the surface rugosities for zero pressure, to a value zero, when the pressure will equal the hardness.

Although a $P^{1/2}$ dependence of δ is likely to give a better description of the physical situation, for the purpose of comparing (12) to the other expressions, we shall assume a linearity, within a limited range, of δ vs. P. On the other hand, the amplitude of surface rugosities is well approximated by 4 times the arithmetic mean R. We shall use therefore an expression

$$\delta \cong 8 \ R(1-\frac{P}{H}) \tag{12a}$$

As to n, assuming $n = \alpha P^{.7}$ and supposing that when the pressure is half the hardness we attain the maximum number of contact points, i.e. $1/\lambda^2$, it will be:

$$\frac{1}{\lambda^2} = \alpha \ (\frac{P}{2})^{.7}$$

and

$$n = \frac{2 \cdot 7}{\lambda^2} \left(\frac{P}{H}\right)^{\cdot 7} \qquad (12b)$$

Inserting (12a) and (12b) into (12) we obtain the approximate expression:

$$u = \frac{2.715 \ K \ \left(\frac{P}{H}\right)^{\cdot 85}}{\lambda\left(1 - \frac{P}{H}\right) \left[\dfrac{43.4 \ (1-P/H) \ R}{\lambda (P/H)^{0.15}} + 1\right]} \qquad (12c)$$

Kragel'skii and Demkin ref. (10) studied the effect of work hardening and surface waviness on the true contact area, but the exponent of pressure in their correlations has an uncertainty of one order of magnitude. However, they attained the important conclusion that linearity between area of contact and load does not proof the existence of a fully plastic contact.

Having briefly reviewed the main available correlations, let us now compare them by applying them to a concrete example.

Assuming a SAP-UC contact with $R_1 = R_2 = 5$ microns, $K_f << K$, $\lambda_1 = \lambda_2 = 300$ microns, $K = .384$ W/cm°C, $H = 2,856$ Kg/cm^2, figure 1 shows the contact conductances to be expected by means of the different correlations.

Plots obtained with our model, described in paragraph 2.3, span a range of $D = 50$ to 400 cm and $N = 3$ cm^{-2}.

The estimates based on the different correlations span a full order of magnitude, lowering the confidence level to an unacceptable value to the designer.

2.3. Suggested model

Our out-of-pile set of measurements had originally the scope of selecting and rifining the existing correlations. However, with equal surface finish and the same materials, our experiments, reported further on, showed a large scatter of results, ranging anywhere from curve (1) to curve (2) of fig. 1. It was therefore apparent that more parameters than those used so far ought to be introduced into a correlation to explain such an erratic behaviour.

The most obvious difference between two supposedly "equal" contacts, as mentioned above, was the existence of lumping zones with high contact point density, due to waviness of surfaces, a fact which was already pointed out in ref. (10) where the matter was however not quantitatively accounted for.

As shown in ref. (11), the density of contact spots on a single patch is well correlated by

$$n = \frac{P}{H_1} \quad x \quad \frac{.06}{R_t^2} \quad (\frac{H_1}{H})^{.825} \tag{13}$$

Combining (1), (2) and (13) we have

$$u = 2 \sqrt{\frac{.06}{\pi}} \quad \left\{ \quad K \quad \frac{P}{R_t} \quad \frac{1}{H^{.9125} H_1^{.0875}} \right. \tag{14}$$

If, on the other hand the constriction resistances of the patches and of the a-spots is taken into account, the following is obtained, ref. (11)

$$u = \frac{\sqrt{\widetilde{\pi}} \quad K \left(\frac{P}{H_1} \right)^{1/2}}{R_t \, \tan^{-1} \left[\left(\frac{H}{H_1} \right)^{1/2} - 1 \right] + \frac{\tan^{-1} \left[\left(\frac{H_1}{P} \right)^{1/2} - 1 \right]}{N^{1/2}}} \qquad (15)$$

The factor $\mathcal{C}$ in formula (14) is derived by comparing (14) with (7.8) of ref. (11):

$$\mathcal{C} = \frac{\widetilde{\pi}}{2 \, \tan^{-1} \left[\left(\frac{H}{H_1} \right)^{1/2} - 1 \right]} \qquad (16)$$

We considered in this derivation H_1 as the macroscopic hardness of the patch. It is in fact an average pressure on the patch, which for the case of a load on the patch lower than the yield strength of the softer material, will be given by the Hertz law

$$H_1 = .662 \, F^{2/3} \left(\frac{P}{ND^2} \right)^{1/3} \qquad (17)$$

Figure 2 shows a plot of the metal to metal conductance in the case of SAP-Armco iron, $R_1 = R_2 = 5 \, \mu m$ $\lambda_1 = \lambda_2 = 300 \, \mu m$, $K = .797$ W/cm°C and $H = 2,856$ kg/cm^2. Our curves (5) and (6) are plotted with $N = 3$ and D from 50 to 400 cm.

Fig. 1 and 2 make it obvious that for the case of a nominally "plane" contact, no correlation can be expected to hold if the parameters D and N do not appear in the geometrical description of the surface.
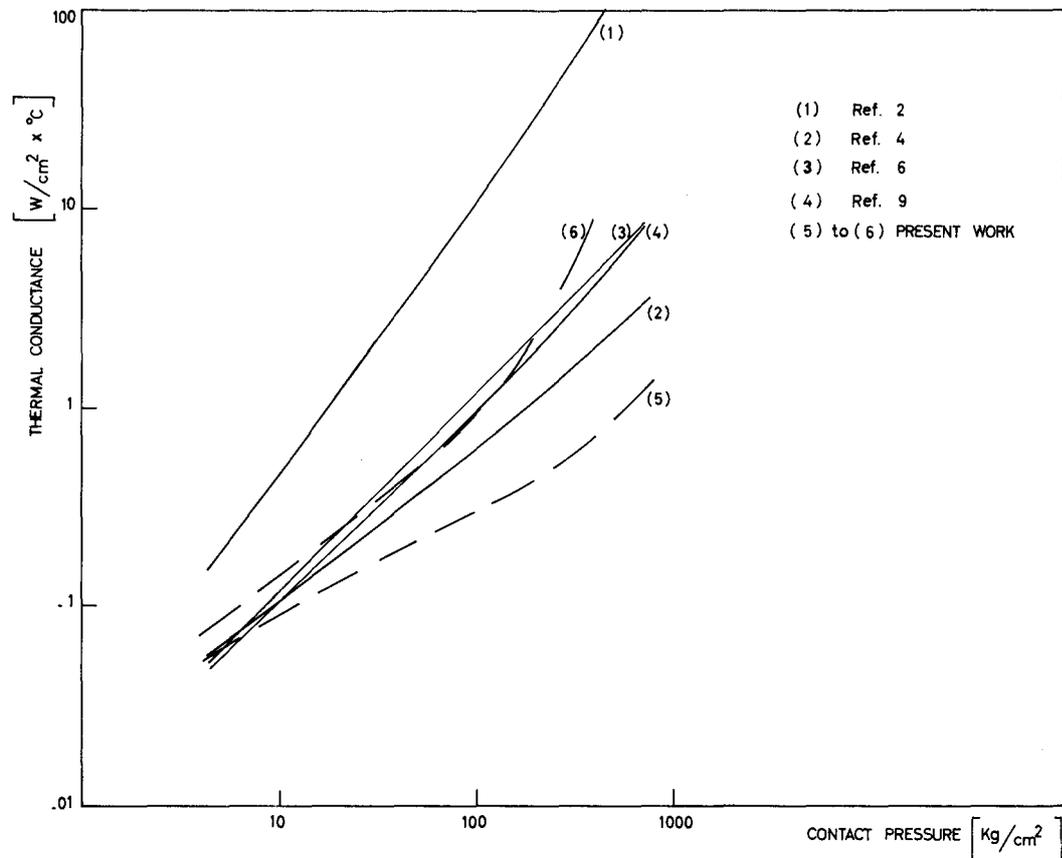
FIG. 1

URANIUM MONOCARBIDE _ SAP CONDUCTANCE
COMPARISON OF CORRELATIONS
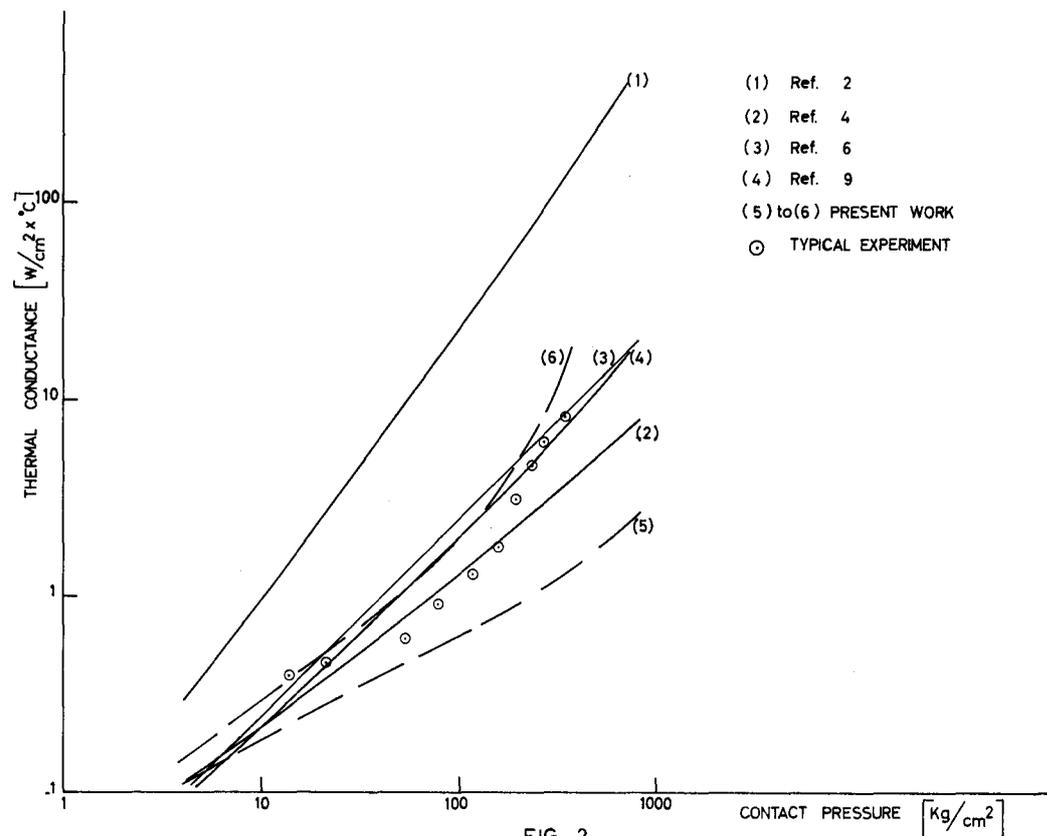
FIG. 2

ARMCO IRON -SAP CONDUCTANCE
COMPARISON OF CORRELATIONS

3. Experimental
===============

3.1. Experimental apparatus

The test assembly consisted in a press, a coil heater placed vertically on top of the test samples and a cooled S.S. plate. (see Fig. 3).

The press was made by a lever loaded with lead bricks at different positions to increase the pressure gradually. A strain gauge head measured the applied loads. An Armco cylinder transmitted both pressure and heat flux to the specimens.

The maximum total load on the test section was 500 kg, the minimum, 4 kg (weight of the measurement head and the Armco cylinder).

A nichrome heater rated 1 KW was coiled in a cylindrical refractory brick and enclosed in a S.S. box to constitute the heater. The power input to this heater was about 200 Watt in most runs.

The cooling system consisted into two cylindrical boxes. The larger one was located under the specimen support whereas a smaller cooled box prevented the heating of the pressure gauge-head.

The cooling rate was regulated by varying the water flow through the lower cooler.

Ten Chromel-Alumel thermocouples were inserted in holes (0.8 - 1 mm dia.) up to the center of the Armco and S.A.P. cylinders, (see fig.4). All thermocouples were connected through a switch to a recorder and to a precision bridge. Temperatures drops across contactes were derived by extrapolation of the temperature profiles.

A 12.8 mm. dia S.A.P. sample was pressed between
two Armco cylinders and yielded, for each pressure, a
high and a low temperature contact.

Contacting surfaces were prepared by various me-
ans as described later and roughnesses were measured
with a surface analizer before each test.

FIG. 3

TEST ASSEMBLY

PRESS

LOAD GAUGE

COOLED BOX

HEATER

UPPER CONTACT

LOWER CONTACT

SPECIMENS

WATER _COOLED SUPPORT
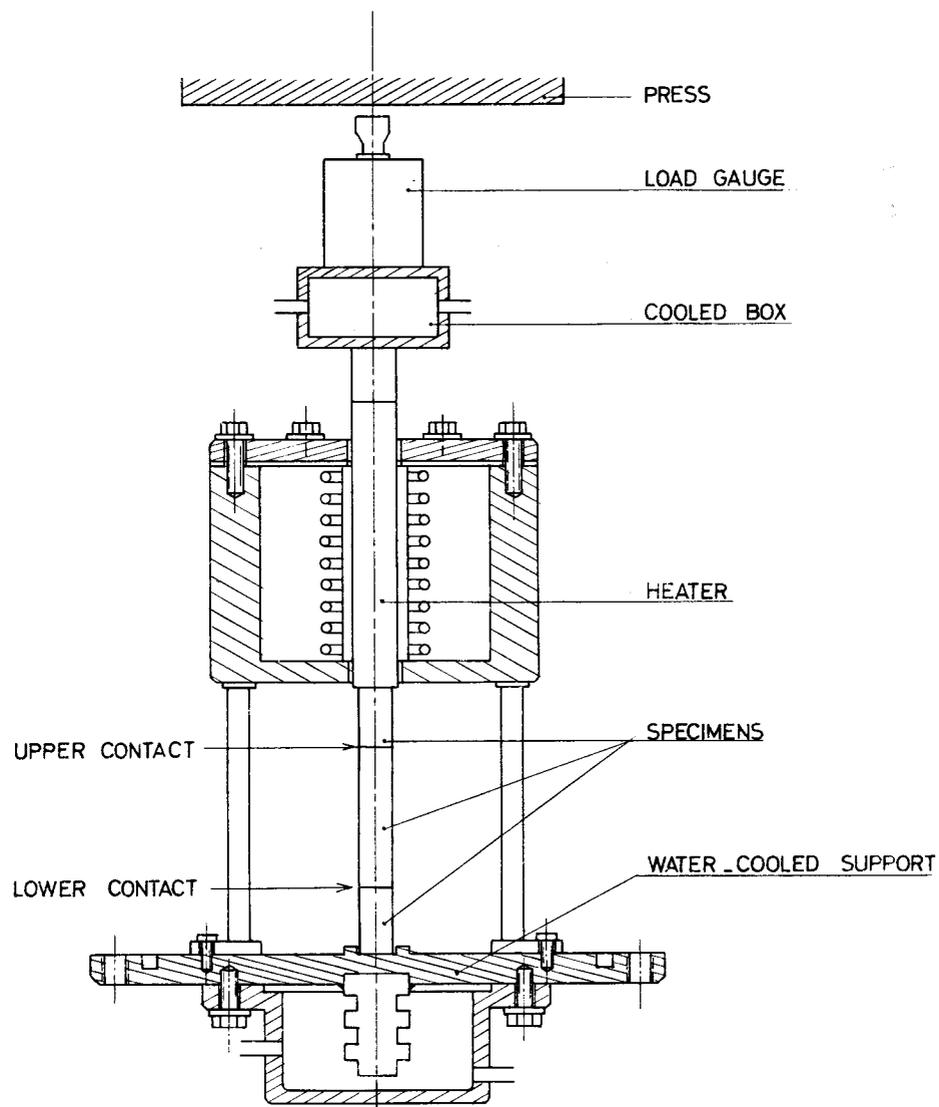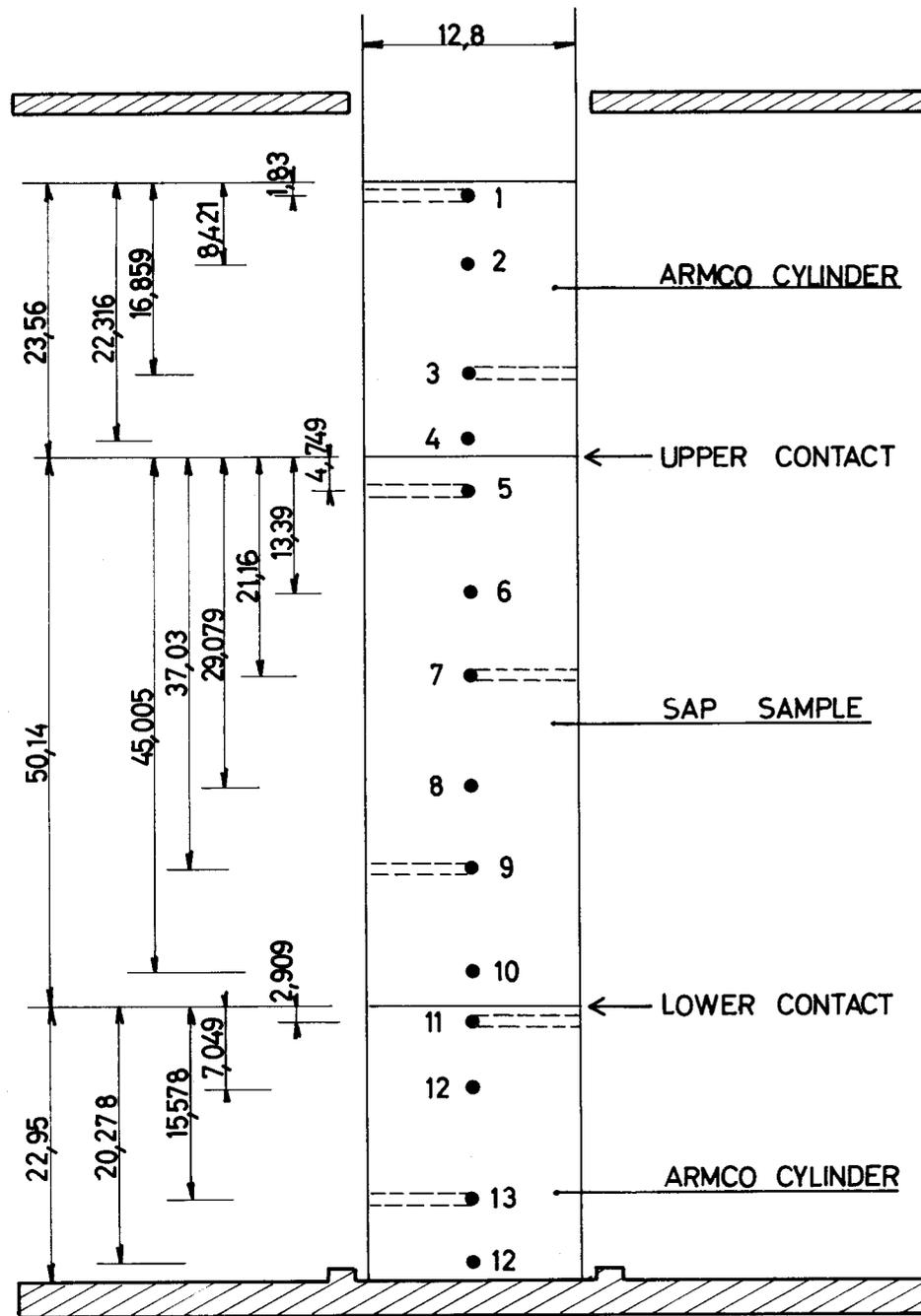
FIG. 4

SPECIMEN ASSEMBLY AND THERMOCOUPLE POSITIONS

ARMCO CYLINDER

UPPER CONTACT

SAP SAMPLE

LOWER CONTACT

ARMCO CYLINDER

3.2. Surface preparation

12.8 mm dia, bars of S.A.P. and Armco iron were cut with different lengths and the specimens were clamped inside a large diameter (6 cm) S.S. cylinder before polishing, to obtain as large a radius of curvature of the polished sample as possible.

The required surface finish was obtained in two stages. The specimen surface was ground gradually using different abrasive papers in a turning machine and the surface finish recorded was approximately R_t=1-2 μm at the end of the first stage.

The specimen was then transferred to an automatic polishing table using diamond paste. The surface finish recorded was R_t=0.1-0.5 μm.

Rugosities were then generated on the surface as parallel ridges, pyramids and stochastic scratches.

Couples of samples with parallel ridges were assembled with the respective ridges orthogonal to each other to determine by visual inspection the number and the area of the contact points after the runs. Fig. 5 shows two typical surfaces after a run.

A number of runs were carried out with pyramidal asperities pressing on optically flat planes (see fig. 6) and pyramids against pyramids (fig. 7) Prophilograph records however showed that the pyramidal asperities had a very irregular vertex since the machining operation had generated small craters on top of the pyramids and the actual contact was made on the perimeters of such craters. For this reason the machined surfaces were lightly polished before each run.

A third series of samples were hand-roughened with different abrasive papers to obtain stochastic finishes which were shown by the surface analyzer to have values of Rt between 1 μm and 40 μm.
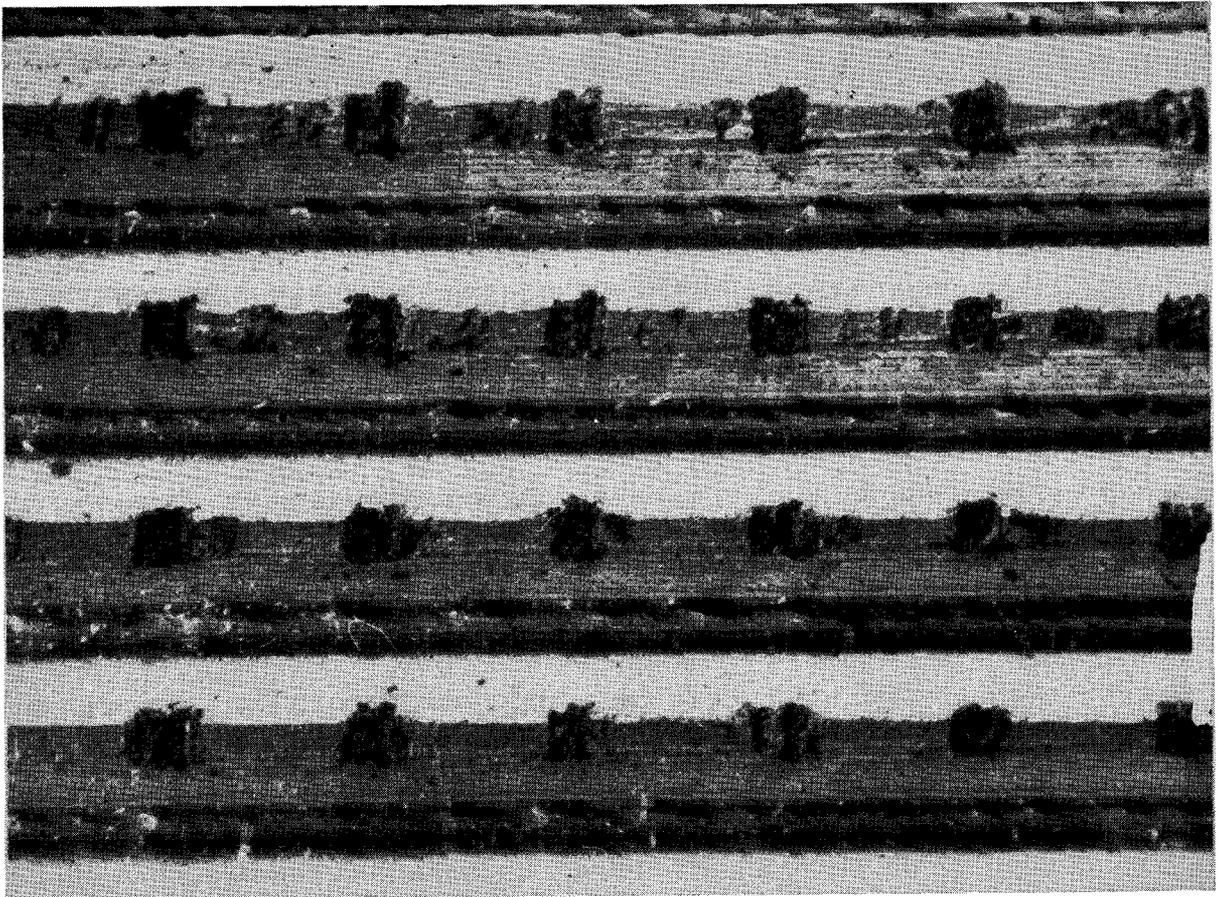
FIG. 5   PARALLEL   RIDGE SURFACES   100 x
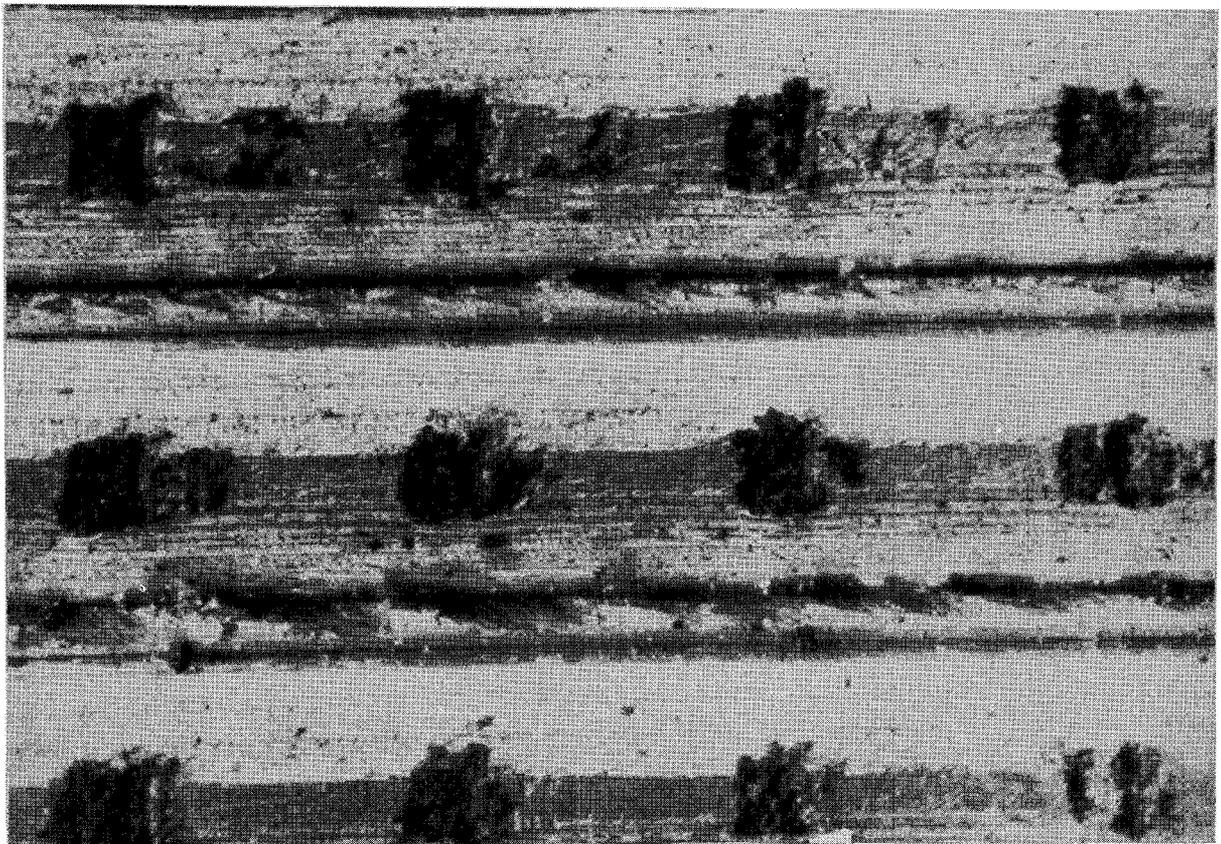
150 x
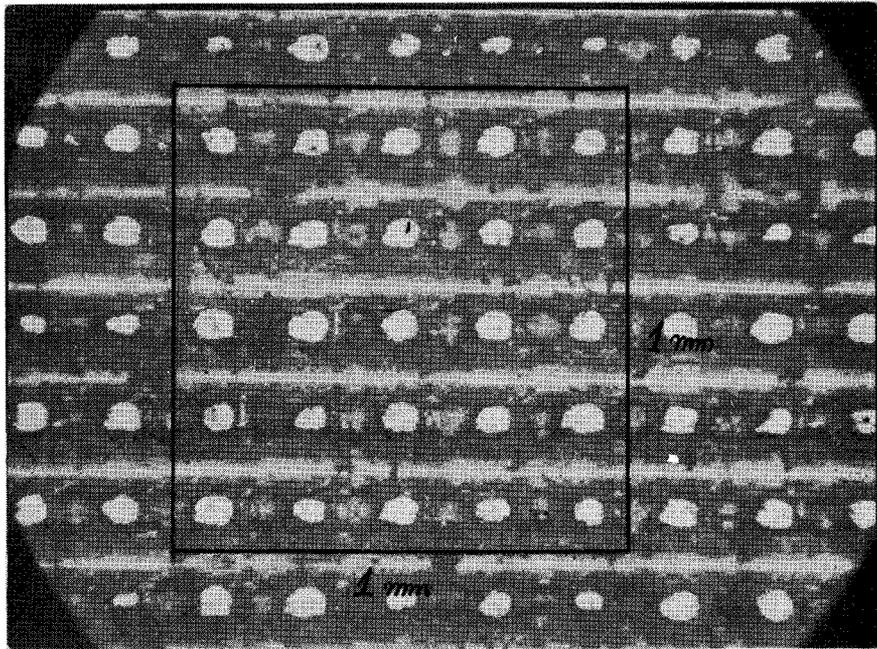
FIG. 6

PYRAMIDAL ASPERITY TEST PLANE

2.42 mm   S = 4,06 mm²

1.679
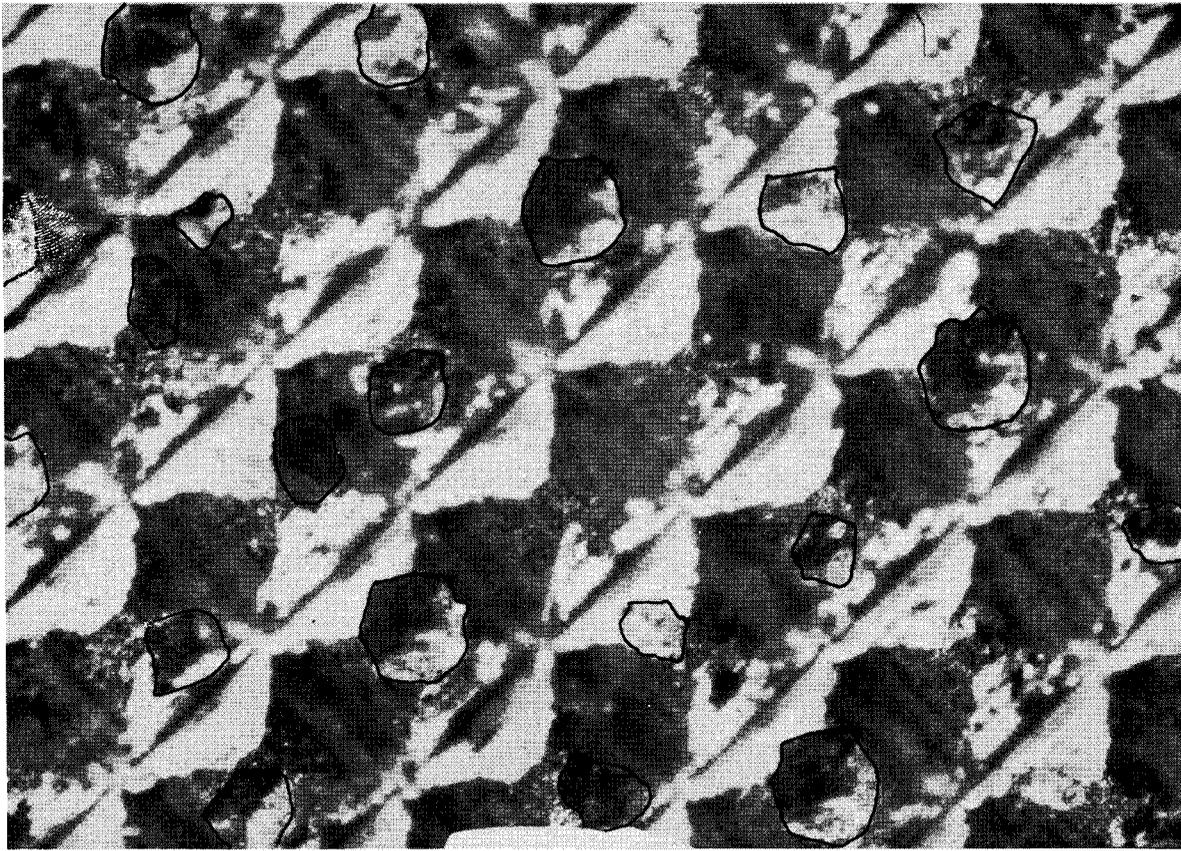
400 x

2,42 mm   S = 4.06 mm²

1.679 mm
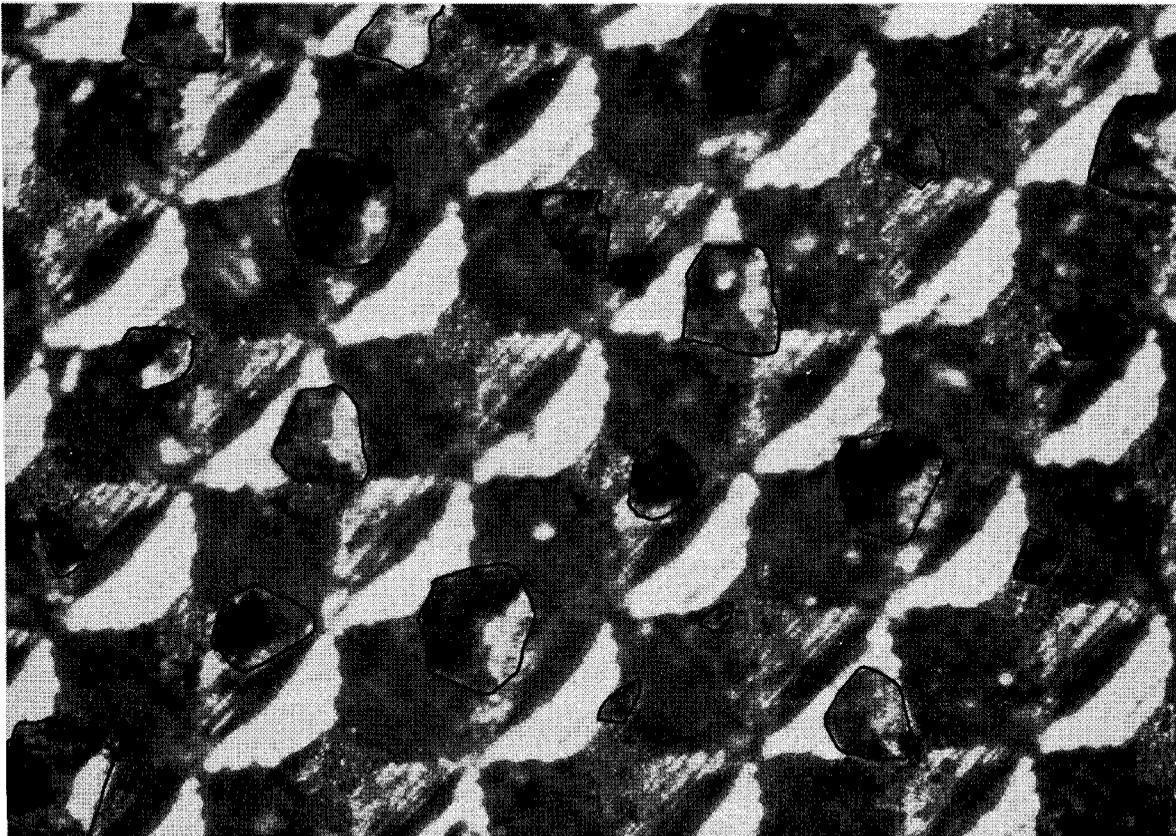
FIG. 7   DOUBLE PYRAMIDAL TEST   400x

3.3. Measurement of the surface finish

The surface finish was characterized by means of a Förster mod. 5815 surface analyzer endowed with a variable reluctance exploring stylus, modulating a carrier frequency, passing a wideband amplifier, a series of integrating operational amplifiers and a recorder.

Refering to fig. 8, several mean roughnesses were determined, defined as follows:

$$R_a \text{ (center-line-average)} = \frac{1}{B-A} \int_A^B \left| f(x) \right| \, dx$$

$$R_t = \text{Average of } (\text{Max } f(x) - \text{Min } f(x))$$

$$Rg = (\text{Root-Mean square}) = \sqrt{\frac{1}{B-A} \int_A^B \left[f(x) \right]^2 \, dx}$$

The stylus would explore a length of about 1 cm on the sample. Curvatures with wavelengths longer than 7 mm would fall out of the pass-band of the system.

Typical values for R_a, and R_t of a series of samples are reported hereafter and show that our runs spanned a range of R_t from about 1 to 30 μm.
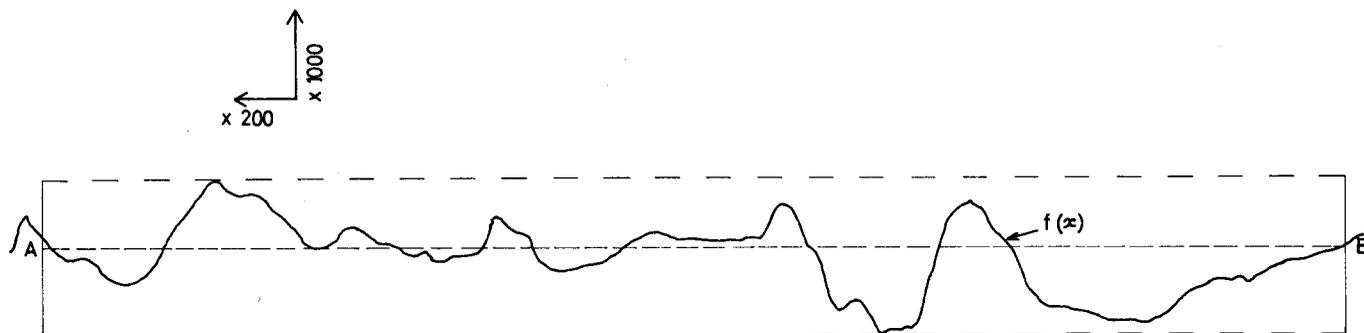
FIG. 8

AMPLIFIED SURFACE PROFILE

SPECIMEN ROUGHNESS

TEST N°	UPPER CONTACT				LOWER CONTACT			
	ARMCO		SAP 7 %		SAP 7 %		ARMCO	
	$Ra(\mu)$	$R_+(\mu)$	$Ra(\mu)$	$R_+(\mu)$	$Ra(\mu)$	$R_+(\mu)$	$Ra(\mu)$	$R_+(\mu)$
29	2.05	13	3.8	13	4.5	20	1.95	14
30	0.60	4.5	7	30	0.15	0.56	0.56	3.2
31	0.60	4.5	0.85	6.5	0.40	2.50	0.56	3.2
32	5.42	24	6.42	25	1	5.5	5.02	28
33	6	24	1.4	8.5	6.4	40	7.15	30
34	0.94	5.5	1.25	9.5	4.25	26	0.15	1.5
35	0.27	1.8	0.22	11.9	3.2	19	0.95	4.9
36	2.6	15	1.7	8	1.9	12	2.6	18
37	3.5	18	4.1	22	4	18	3.7	18
38	0.75	4	1.8	12.5	1.6	3.9	2.75	14.5
39	1.5	7.5	0.45	4.5	2.3	13	1.2	7.3
40	2.7	17	0.53	4.9	2.17	12.5	4.37	22
41	1.1	6.3	2.2	17	2.87	16.75	3.55	24

3.4. Experimental data

In each run the load was increased stepwise from 18 kg to 450 kg and temperature readings were taken at each step.

Contact thermal conductance is:

$$u = \frac{Q}{\Delta T^*} \qquad (18)$$

where Q is the heat flux through the specimen. ΔT^* is the extrapolated temperature discontinuity at the contact. Heat flux through the contact is:

$$Q = \frac{K_1 \Delta T_1}{l_1} \qquad (19)$$

where K_1, ΔT_1 and l_1 are respectively the thermal conductivity, the temperature drop and length of the Armco specimen.

Substituting Eq (18) into Eq (19) we obtain the thermal contact conductance (Watt/cm^2 °C)

$$u = \frac{K_1 \Delta T_1}{l_1 \Delta T^*} \qquad (20)$$

We show hereafter in detail the data measured during a typical run (test n° 26). This specific test was one of the series where a parallel-ridge surface was pressed against another parallel-ridge surface, at right angles.

	Upper contact		Lower contact	
	Armco	SAP 7%	SAP 7%	Armco
wavelength (microns)	806	1200	1195	800
mean width of the peaks "	146	120	119	200
mean depth of the valleys"	450	600	600	450

Thermocouple readings are plotted and extrapolated in fig. 9 to obtain the experimental ΔT^*.

Thermal conductance calculations for each value of the pressure are tabulatd below.

FIG. 9

TYPICAL TEMPERATURE PROFILES

- 28 -

TEST Nº 26 (UPPER CONTACT)

F (kg)	P (kg/cm²)	ARMCO (mV) upper	lower	ΔT ARMCO (mV)	$\overline{T}$ ARMCO (mV)	$\overline{T}$ ARMCO (°C)	K ARMCO (Watt/cm°C)	T SAP (mV)	ΔT CONTACT (mV)	ΔT CONTACT x 1 ARMCO	ΔT ARMCO x K ARMCO	U (Watt/cm²°C)
0.2	0.156	15.58	12.98	2.60	14.28	350	0.518	7.50	5.48	12.80	1.35	0.105
20	15.6	13.83	9.97	3.86	11.90	292	0.552	8.41	1.56	3.66	2.13	0.583
40	31.3	13.34	9.37	3.97	11.35	279	0.560	8.25	1.12	2.63	2.22	0.845
60	46.8	13.13	9.16	3.97	11.14	274	0.563	8.25	0.91	2.14	2.23	1.04
100	78	12.89	8.84	4.05	10.86	267	0.567	8.21	0.63	1.48	2.3	1.55
150	117	12.65	8.48	4.17	10.56	260	0.571	8.08	0.40	0.94	2.38	2.53
200	156	12.50	8.34	4.16	10.42	257	0.573	8.05	0.29	0.68	2.38	3.50
250	195	12.33	8.21	4.12	10.27	253	0.575	8.02	0.19	0.446	2.36	5.30
300	234	12.44	8.30	4.14	10.37	256	0.573	8.16	0.14	0.329	2.37	7.20

TEST Nº 26 (LOWER CONTACT)

F (kg)	P (kg/cm²)	ARMCO (mV) upper	lower	ΔT ARMCO (mV)	$\overline{T}$ ARMCO (mV)	$\overline{T}$ ARMCO (°C)	K ARMCO (Watt/cm°C)	T SAP (mV)	ΔT CONTACT (mV)	ΔT CONTACT x 1 ARMCO	ΔT ARMCO x K ARMCO	U (Watt/cm²°C)
0.2	0.156	2.86	1.95	0.91	2.40	59	0.705	6.72	3.86	9.22	0.64	0.0695
20	15.6	5.52	3.39	2.13	4.45	108	0.665	6.79	1.27	3.03	1.42	0.468
40	31.3	5.55	3.39	2.16	4.47	109	0.664	6.52	0.97	2.32	1.43	0.617
60	46.8	5.66	3.33	2.33	4.49	109	0.664	6.48	0.82	1.96	1.55	0.792
100	78	5.69	3.10	2.59	4.39	107	0.666	6.30	0.61	1.46	1.72	1.18
150	117	5.53	2.82	2.71	4.17	102	0.670	6.06	0.53	1.27	1.82	1.43
200	156	5.56	2.74	2.82	4.15	101	0.671	5.97	0.41	0.98	1.89	1.93
250	195	5.60	2.71	2.89	4.15	101	0.671	5.95	0.35	0.836	1.94	2.32
300	234	5.67	2.69	2.98	4.18	102	0.670	6.02	0.55	0.836	2.00	2.40

Experimental conductances given by Eq. (20)
are plotted in fig. 10 vs. applied pressures. Substitu-
ting in Eq. 9 the harmonic mean conductivities 0.875W/cm°C
and 0.995W/cm°C and the measured wavelengths, the con-
tact conductances, for the upper and the lower contacts
are given by

$$u = \frac{0.0237 \ P^{3/4}}{1-0.00328 \ P^{3/4}} \quad \text{and} \quad u = \frac{0.0254 \ P^{3/4}}{1-0.00328 \ P^{3/4}}$$

u (Watt/cm^2°C) being the contact conductance of the
solids.

These correlations plotted in fig. 10 appear to
yield results lower than the observed experimental con-
ductances. One must notice that the Laming correlations
are based upon the assumption that the microhardness
decreases linearly with the square root of the applied
load, an assumption which was not confirmed by our ob-
servations, which, on the contrary, pointed out to a
constant microhardness, at a given temperature. Infact,
fig. 11 shows a hardness plot vs. applied load with
no apparent variation.

Ref. (4) does not actually carry out microinden-
tation tests, but deduces an "apparent" hardness from
the observed values of metal-metal conductance. Being
there no real evidence of such a variation, we also
plotted in fig. 10 a conductance derived as in Ref. (4)
but under the assumption of a constant microhardness:

$$U \ upper = \frac{0.1495 \ P^{1/2}}{1-0.021 \ P^{1/2}} \quad \text{and} \quad U \ lower = \frac{0.16 \ P^{1/2}}{1-0.0198 \ P^{1/2}}$$

which fit the experimental results no worse that the correlations based on the assumption of a variable H.

Test N° 26 is carried out in air and fluid conductances are neglected because they are one order of magnitude lower than solid-solid conductances, since the fluid thickness is very large. All experimental contact conductances obtained with surfaces endowed with stochastic surface finish are shown in fig. 12.

FIG. 10

RESULTS OF TYPICAL RUN
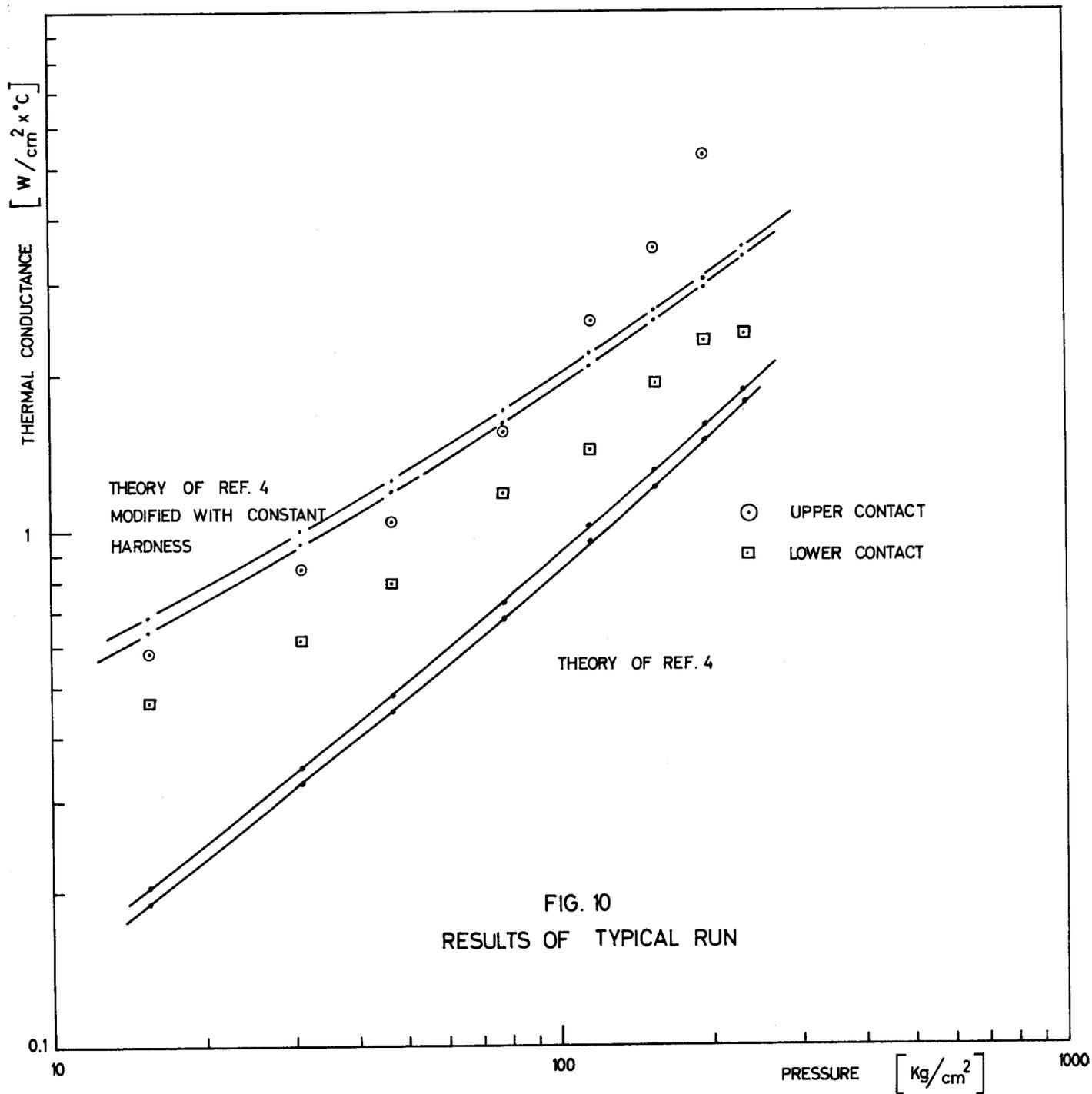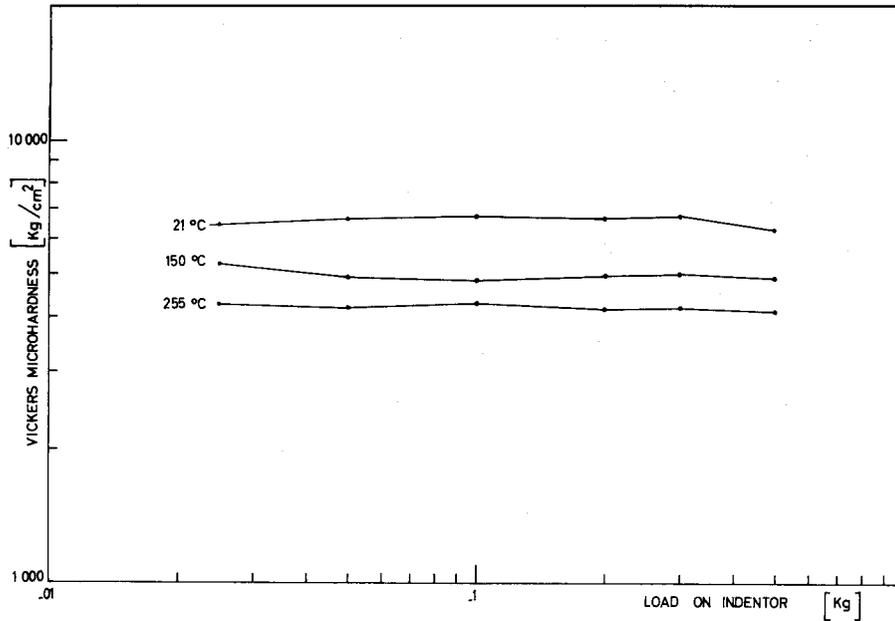
FIG. 11

MICROHARDNESS OF 7% SAP VS LOAD

FIG. 12

SAP - ARMCO IRON CONTACT
CONDUCTANCE IN AIR
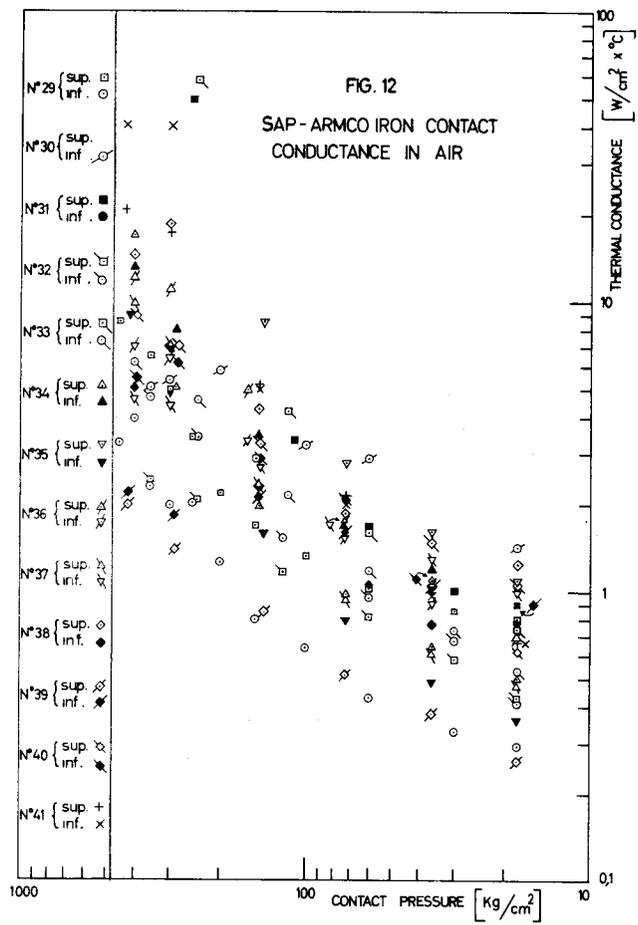
4. Fluid conductance

In any contact, beside the solid-solid contact conductance, there is a parallel conductance due to the fluid enclosed between the contact spots. We have so far accounted for such a conductance by:

$$u_f = \frac{K_f}{\delta}$$

We shall hereafter discuss the most appropriate values to be used for both K_f and δ .

If the distances involved are much larger than the mean free path of the fluid molecules, K_f will be the classical gas conductivity. If this is not so, one must take into account the accommodation effects of the fluid on the solid surfaces either by using formula (2) ref. (12) or by increasing the geometrical distances involved by extrapolation widths, reported in appendix II ref. (12) :

$$u_f = \frac{1}{\dfrac{\delta - 2L}{K_f} + \dfrac{T^{1/2}(1/\alpha_1 + 1/\alpha_2)M^{1/2}}{7.36 \times 10^5 \ P_f}} \tag{21}$$

For fission gases at 1 atm pressure, such extrapolation widths are of the order of 0.5 μm on each side of the gap, suggesting an additional width of 1 μm) to be added to the actual geometrical wall distance.

In the great majority of our tests and in practically all situations concerning a fuel pellet-sheath contact, the voids involved are much larger than the

1 μm above mentioned so that it is reasonable to lump
the extrapolation width into an "effective fluid thick-
ness" δ experimentally determined.

By extrapolation to zero solid-solid contact pres-
sure, we obtained in about 30 runs, values for δ , which
we correlated against $\sum R_t$. The results, tabulated here-
after, where B is the ratio $\delta/\sum R_t$ are to be found at
the end of this paragraph.

The arithmetic mean B was found to be 0.74 with
a mean square deviation 0.58. Discarding all values
of F which fall beyond the mean square deviation, one
finds.

B = 0.67 $\pm$ 0.5 with a confidence level of 90%
It is therefore recommended, for design pur-
poses, to use

$$u_f = \frac{K_f}{2/3 \sum R_t} \tag{22}$$

which is in line with experimental evidence of ref. (4)
and, although it may be affected of a large uncertain-
ty, is infact often negligible whenever a moderate solid-
solid contact is present.

TEST N°	Σ_R (μ)	U_f (W/cm²°C)	δ (μ)	$\bar{E}$	$\Delta=\left\|0.74-\bar{E}\right\|$	Δ^2
29	26	0.403	8.7	0.334	0.406	0.164
	34	0.242	14.5	0.427	0.313	0.098
30	34.5	0.304	11.5	0.334	0.406	0.164
	3.76	0.494	7.08	1.880	1.140	1.300
31	11	0.620	5.65	0.514	0.226	0.051
	5.7	0.248	14.1	2.480	1.740	3.040
32	49	0.279	12.5	0.256	0.484	0.234
	33.4	0.143	24.5	0.735	0.005	—
33	32.5	0.222	15.8	0.486	0.254	0.064
	70	0.120	29.2	0.418	0.322	0.104
34	15	0.475	7.38	0.492	0.248	0.062
	27.5	0.229	15.3	0.556	0.184	0.034
35	3.75	0.586	6	1.610	0.870	0.760
	23.9	0.427	8.2	0.343	0.397	0.158
36	23	0.305	11.5	0.500	0.240	0.057
	30	0.158	22.2	0.740	0	—
37	40	0.242	14.5	0.363	0.377	0.142
	36	0.258	13.5	0.375	0.365	0.133
38	16.5	0.605	5.8	0.351	0.389	0.152
	18.4	0.325	10.8	0.587	0.153	0.023
39	12	0.142	24.7	2.060	1.320	1.740
	10.3	0.274	12.8	1.240	0.500	0.250
40	21.9	0.405	8.65	0.395	0.345	0.119
	34.5	0.157	22.3	0.646	0.094	—
41	23.3	0.340	10.3	0.442	0.298	0.089
	40.75	0.127	27.6	0.678	0.062	—
				19.242		8.938

5. Cylindrical contact

The cylindrical contact typical of a fuel pellet-
clad situation is in many respects a more reproducible
and geometrically definable system than the plane con-
tact discussed in chapter 2.3.

The main reason is that the ridging of the surfa-
ces into contact is not as important as in a plane con-
tact. As soon as a moderate pressure between fuel and
clad is present, the slenderness of the sheath tube is
such to match the sheath and pellet surfaces in such a
way as to avoid the "patching" effect, reducing the si-
tuation to one where the microroughness will be the con-
trolling factor in determining the number of contact spots.

This appears to be true for "thin clads" i.e.
metal clads which do not exced 1 mm thickness. This
fact was verified by optical inspections of pellets che-
mically swelled by oxidation (UC to UO_2) in S.A.P. tubes
endowed with an original elliptical cross-section.

The solid-solid conductance, in such a case will
be given by (ref. 11):

$$U = .434 \frac{K}{R_t} \left(\frac{P}{H}\right)^{.9125} \frac{1}{\tan^{-1}\left[\left(\frac{H}{P}\right)^{1/2} - 1\right]} \qquad (23)$$

using for R_t the larger of the two rugosities, which
in the cases we observed is that of the centerless
ground fuel pellet.

In a series of measurements carried out on the inside of a S.A.P. tube, we found an average $R_t = 2.4$ μm whereas fuel pellets had typical R_t's of 15 to 20 μm.

In the case of UC-SAP with a 400°C clad temperature, $R_t = 17 \times 10^{-4}$cm H = 3,200 kg/cm^2, K = 0.34, δ = 2/3 x x 20 x 10^{-4}and the total conductance is

$$U = \frac{.055 \ P^{.9125}}{\tan^{-1} \left[56.5P^{-1/2} - 1 \right]} + \frac{K_f}{13.3 \times 10^{-4}} \tag{24}$$

A natural –U UC pellet of density 13.4 g/cm^3, exposed to a thermal neutron flux $\emptyset$, will have a heat generation, discounting the effects of gamma heating:

$$2.7 \times 10^{-13}\emptyset \times 13.4 \times \frac{238}{250} = 3.44 \times 10^{-12}\emptyset \ W/cm^3$$

The center-to-surface temperature drop in the UC pellet, with K = 0.15 W/cm °C and a radius r_o, will be

$$\Delta T_{uc} = \frac{3.44 \times 10^{-12}\emptyset \ r_o^2}{4 \times 0.15} = 5.73 \times 10^{-12}\emptyset \ r_o^2 \ °C$$

5.1. Simplified Theory

The hot radial gap will be given by the cold assembly clearance, decreased by the UC swelling and corrected for the differential expansion of UC and SAP, i.e.

$$C' = C - sr_o + r_o(\overline{T} - 20)\alpha_c - r_o(\overline{T}_c - \frac{1}{3}\Delta T_p - 20)\alpha_p$$

On the other hand, $\overline{T}_c - \Delta T_{uc} - \overline{T}$ is the gap temperature drop, equal to the heat flux divided by the conductance, i.e.

$$\Delta T_1 = \frac{3.44 \times 10^{-12}\phi \times \widetilde{\pi} r_o^2}{2\widetilde{\pi} r_o \times u_t} = \frac{172 \times 10^{-12}\phi r_o}{u_t}$$

$$\overline{T}_c = \overline{T} + \Delta T_1 + \Delta T_{uc} = \overline{T} + \frac{1.72 \times 10^{-12}\phi r_o}{u_t} + 5.73 \times 10^{-12}\phi r_o^2$$

whence

$$C' = C + r_o\left[-S - 2 \times 10^{-4} + 10^{-5}\overline{T} - 10^{-17}\phi r_o(\frac{1.72}{u_t} + 3.82 r_o)\right] \quad (25)$$

To completely determine the system, one must express P as a function of the pellet-clad operating interference, as in 8.1. and 8.2. (ref. 11).

Of course, to have an actual contact, it must be $C' < 0$ in eq. 25. The pressure in the case of an interference $-\frac{C'}{r_o}$ will be (ref. 11)

$$P = \frac{- C'/r_o}{\dfrac{1}{E_c}\left[\dfrac{2r_o^2 + t^2 + 2r_o t}{t(t + 2r_o)} + \nu_c\right] + \dfrac{1-\nu_p}{E_p}} \tag{26}$$

For the case UC-SAP

$$E_c = 5 \times 10^5 \ \text{kg/cm}^2 \qquad (\text{ref. } 11)$$
$$E_p = 2.2 \times 10^6 \ " \qquad (\text{ref. } 13)$$
$$\nu_c = 0.4 \qquad (\text{ref. } 15)$$
$$\nu_p = 0.3 \qquad (\text{estimated})$$

Equation 26, using these values and assuming $t \ll r_o$ will become:

$$P = -5 \times 10^5 \ \frac{C't}{r_o^2} \tag{27}$$

Introducing (27) into (24), we obtain, with K_f for fission gases equal $(5 \ Xe + 1 \ K_c)$ estimated to be 0.97×10^{-4} W/cm°C

$$u_t = 0.073 + \frac{8.7 \times 10^3 \left[\dfrac{-C't}{r_o^2}\right]^{0.9125}}{\tan^{-1}\left[8 \times 10^{-2}\left(-\dfrac{C't}{r_o^2}\right)^{-0.5} - 1\right]} \tag{28}$$

with C' given by (25)

For the case $\overline{T} = 400$ °C $\quad$ t = 0.1 cm r_o = 1.5 cm
$$\emptyset = 10^{14} n/cm^2 \times sec$$

formulas (25) and (28) become:

$$C' = C - 1.5 \ s - 7.19 \times 10^{-3} - \frac{3.87 \times 10^{-3}}{u_t}$$

and

$$u_t = \frac{0.073 + 509 \ (-C')^{0.9125}}{\tan^{-1} \left[0.38(-C')^{-.5} -1 \right]} \qquad (30)$$

Combining (29) and (30) one can relate u_t to C
and s and also obtain the operating contact pressure
from (27) i.e.:

$$P = -2.22 \times 10^4 \times C' \qquad (31)$$

such expressions being valid, of course, for $C' < 0$,
that is, for the case of an actual contact between
surfaces.

This simplified theory does not account for the
strain of the clad due to the fuel-clad interference.
It leads to typical results as shown in fig. 13, which
are not conservative for design purposes as far as the
thermal resistance is concerned. The mechanical stress on
the clad will infact yield a lower fuel-clad pressure
and therefore a higher thermal resistence, in most pra-
tical cases.

FIG. 13

THERMAL RESISTANCE
OF UC-SAP

(SIMPLIFIED THEORY)

R_t SAP $= 2 \ \mu m$

R_t UC $= 18 \ \mu m$

UC dia. 30 mm

1 mm SAP clad

FISSION GAS FILLED

$\emptyset \ 10^{14}$ neut./Cm^2 x sec.
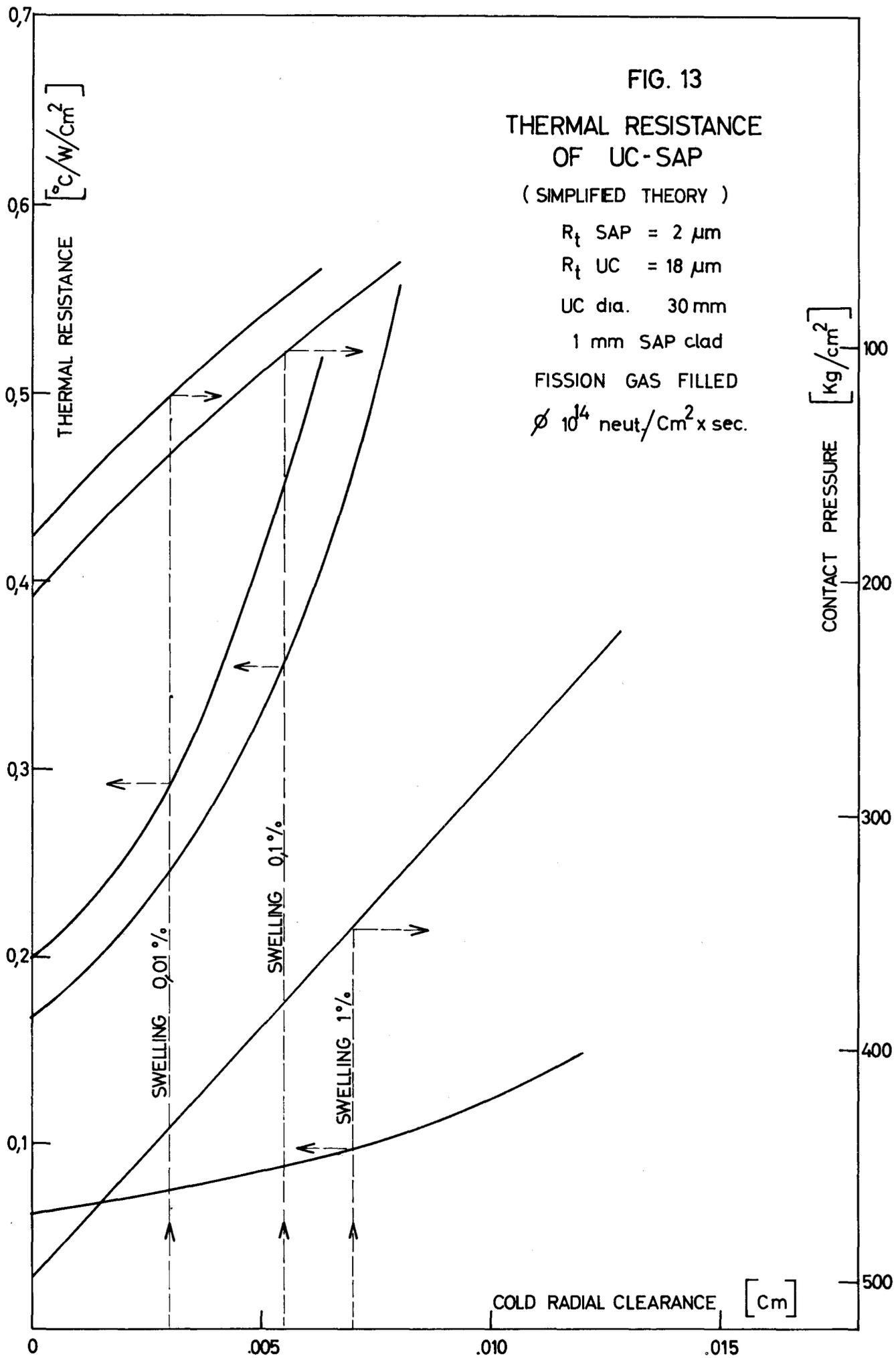
5.2. Complete theory

This model is closer to the actual physical situation than the former. We shall account for the strains of both fuel and clad due to the effects of the contact pressure.

We have thus

$$\mathcal{E}_p = \text{Strain of fuel pellet} = \alpha_p (T_s - 20) + \frac{\alpha_p \, \Delta T_p}{2(1 - \nu_p)} - \frac{P \, r_o}{E_p} (1 - \nu_p)$$

$$\mathcal{E}_c = \text{Strain of clad} = \alpha_c (\overline{T} - 20) + \frac{r_o}{t} \quad \frac{P}{E_c} (1 - \frac{\nu_c}{2})$$

On the other hand, the fuel temperature drop is

$$\Delta T_p = 1.61 \times 10^{-11} \frac{\emptyset \text{ Co } d}{M} \frac{r_o^2}{K_p} \qquad (32)$$

The hot operating radial gap is

$$C' = C - sr_o + r_o(\mathcal{E}_c - \mathcal{E}_p)$$

whence, by substitution

$$C' = C - sr_o + r_o \left[\alpha_c (\overline{T} - 20) + \frac{r_o}{t} \frac{P}{E_c} (1 - \frac{\nu_c}{2}) - \alpha_p (T_s - 20) - \right.$$

$$\left. - \frac{1.61 \times 10^{-11} \alpha_p}{2(1 - \nu_p)} \quad \frac{\emptyset \, C_o d}{M} \quad \frac{r_o^2}{K_p} + \frac{P \, r_o}{E p} (1 - \nu_p) \right] \qquad (33)$$

Interference fit and contact pressure will be related by a simplified version of formula of formula (26), i.e.

$$P = \frac{- C'/r_o}{\frac{1}{E_c}(\frac{r_o}{t} + \nu_c) + (\frac{1-\nu_p}{E_p})} \qquad (34)$$

and, finally, fuel surface and clad temperature are related by

$$T_s = \overline{T} + 3.215 \times 10^{-11} \frac{\emptyset\, C_o d\, r_o}{M\, u} \qquad (35)$$

where u, the total conductance, is

$$u = \frac{K_f}{2/3 \sum R_t} + \frac{.434\, K}{R_t}(\frac{P}{H})^{.9125} \frac{1}{\tan^{-1}\left[(\frac{H}{P})^{1/2} - 1\right]}$$

$$(36)$$

Substituting in (33) the values of C' and T_s from (34) and (35) and solving for P, one obtains the value of the fuel-clad contact pressure during operation:

$$P = \frac{-C/r_o + s - (\overline{T}-20)(\alpha_c - \alpha_p) + 3.215 \times 10^{-11} \frac{\emptyset C_o d r_o \alpha_p}{M}\left[\frac{1}{u} + \frac{r_o}{4K_p(1-\nu_p)}\right]}{\frac{(1-\nu_p)}{E_p}(1+r_o) + \frac{1}{E_c}\left[\frac{r_o}{t}(2 - \frac{\nu_c}{2}) + \nu_c\right]}$$

$$(37)$$

Equations (36) and (37) determine completely both the contact pressure and the conductance between fuel and clad, once the physical dimensions, physical properties thermal flux and enrichment are known. Of course the clad temperature $\overline{T}$ must also be specified.

All properties relating to clads, fuels and filling fluids have been left in symbolic form in (36) and (37) for ease of application to any system of materials, such as UO_2, UC, U metal, clad with Al, Zircaloy, Stainless Steel, Magnox, filled with He, fission gases etc.

For the purpose of illustrating the application of (36) and (37) to pratical situations close of interest to the fuel element designer, a number of computations have been carried out and plotted in fig. (14) to (23). A listing of the computation procedure, in FORTRAN 2 V 3 is shown hereafter, as a help to the fuel element designer. The equivalence of symbols in our text and in the machine program is shown below

TEXT	F2V3	TEXT	F2V3
c	C	r_o	R
C_o	CØ	R_t	RT
d	DENS	ΣR	SRT
E_c	EC	s	S
E_p	EP	t	TT
H	DURC	$\overline{T}$	T
K	CØAR	u	U
K_c	CØNDUC	α_c	ALFAC
K_f	CØNDG	α_p	ALFAP
K_p	CØNDP	ϕ	FI, FØ
M	PESØ	$\left.\begin{array}{c}\nu_c\\\nu_p\end{array}\right\}$	XNU
P	P		

```
      CLAD-PELLET CONDUCTANCE

      DIMENSION S(3),P(8),FO(3)
C     DATI COMUNI
      READ INPUT TAPE 5,10,SRT,RT,T,ALFAP,CO,XNU,EP
   10 FORMAT (7E10.3)
C     DATI CLAD
      READ INPUT TAPE 5,20,ALFAC,EC,DURC,CONDC,TT
   20 FORMAT (5E10.3)
C     DATI GAS
      READ INPUT TAPE 5,30,CONDG
   30 FORMAT (1E10.3)
C     DATI FUEL
      READ INPUT TAPE 5,40,R,DENS,PESO,CONDP
   40 FORMAT (4E10.3)
C     OUTPUT
      WRITE OUTPUT TAPE 6,6
    6 FORMAT (1H ,12HDATI  COMUNI)
      WRITE OUTPUT TAPE 6,61
   61 FORMAT (1H ,1X,8HSOMMA RT,4X,6HMAX RT,50H TEMP CLAD  DIL PELL    AR
     1RICC  POISS PEL YOUNG PEL)
      WRITE OUTPUT TAPE 6,10,SRT,RT,T,ALFAP,CO,XNU,EP
      WRITE OUTPUT TAPE 6,62
   62 FORMAT (1H0,10HDATI  CLAD)
      WRITE OUTPUT TAPE 6,63
   63 FORMAT (1H0,50H  DIL CLAD      YOUNG       DUR     COND      SPESS )
      WRITE OUTPUT TAPE 6,20,ALFAC,EC,DURC,CONDC,TT
      WRITE OUTPUT TAPE 6,64
   64 FORMAT (1H0,8HDATI GAS)
      WRITE OUTPUT TAPE 6,65,CONDG
   65 FORMAT (1H ,6HCONDG=,1E10.3)
      WRITE OUTPUT TAPE 6,66
   66 FORMAT (1H0,9HDATI FUEL)
      WRITE OUTPUT TAPE 6,67
   67 FORMAT (4X,6HRAGGIO,4X,4HDENS,4X,8HPESO MOL,4X,4HCOND)
      WRITE OUTPUT TAPE 6,40,R,DENS,PESO,CONDP
      FO(1)=1.E13
      DO 68 M=2,3
   68 FO(M)=FO(M-1)+5.E13
      DO 69 MO=1,3
      FI=FO(MO)
      WRITE OUTPUT TAPE 6,100,FI
  100 FORMAT(1H ,3HFI=,1E10.2)
      WRITE OUTPUT TAPE 6,5
    5 FORMAT (1H0,1H ,9X,9HPRESSIONE,11X,8HSWELLING,12X,8HCOLD GAP,13X,7
     1HCONDUTT,15X,6HRESIST///)
C     CALCOLO CONDUTTANZA ARMONICA
      COAR=2./(1./CONDP+1./CONDC)
C     DATI DA DEFINIRE
      S(1)=0.
   71 DO 50 J=2,3
   50 S(J)=S(J-1)+5.E-3
      P(1)=1.
   72 DO 60 J=2,8
   60 P(J)=P(J-1)*2.
   73 DO 70 L=1,8
   74 DO 70 N=1,3

C     CALCOLO CONDUTTANZA
   75 U=ATANF(SQRTF(DURC/P(L))-1.)
   76 U=4.34E-1*COAR*(P(L)/DURC)**9.125E-1/(RT*U)
   77 U=U+1.5*CONDG/SRT
C     CALCOLO COLD GAP
   78 C=(R+1.)*(1.-XNU)/EP+(R*(2.-0.5*XNU)/TT+XNU)/EC
   79 C=C*P(L)-S(N)+(T-2.E1)*(ALFAC-ALFAP)
   85 CC=3.215E-11*FI*CO*DENS*R*ALFAP*(1./U+0.25*R/(CONDP*(1.-XNU)))/PES
     1O
   87 C=R*(CC-C)
   88 RES=1./U
      WRITE OUTPUT TAPE 6,80,P(L),S(N),C,U,RES
   80 FORMAT (1H ,5E20.4///)
   70 CONTINUE
   69 CONTINUE
      CALL EXIT
      END(1,0,0,0,0,0,0,0,0,0,0,0,0,0,0)
```

Appendix IV gives the values of the physical constants used in the sample computations and plotted in Fig. (14) to (23). No account has been taken of gamma heating in the fuel and of creep properties of the clad material in such sample computations.

FIG.14

30 mm dia. UC
1mm SAP clad (400 °C)
fission gas-filled
ZERO NEUTRON FLUX
(REACTOR SHUTDOWN)

FIG. 15

30 mm dia. UC (nat.)

1 mm SAP clad (400 °C)

fission gas _ filled

FIG. 16

30 mm dia. UC

1 mm SAP clad (400°C)

He – filled

ZERO NEUTRON FLUX

(REACTOR SHUTDOWN)

FIG. 17

30mm dia. UC
1mm SAP clad (400°C)
He - filled

FIG. 18

30 mm dia. UC (nat.U)
0,5mm Zircaloy-2 clad (400°C)
fission gas-filled

— 10^13 neutron flux
--- 6 x 10^13 " "
-·- 1,1 x 10^14 " "

- 49 -

FIG. 19

30mm dia. UC (nat.U)
0,5mm S.S. clad (400 °C)
fission gas filled
10^13 neutron flux
6 x 10^13 " "
1,1 x 10^13 " "

FIG. 20

30mm dia. UC (nat.U)
0,5mm S.S. clad (400 °C)
He – filled
10^13 neutron flux
6x10^13 " "
1,1x10^13 " "

FIG. 21

30 mm dia. UC (nat.U)
1 mm MAGNOX clad (400 °C)
fission gas filled
10^13 neutron flux
6x10^13 " "
1,1x10^13 " "

FIG. 22

10 mm dia. UO$_2$ (nat.U)

0,5 mm Zirkaloy-2 clad (400 °C)

fission gas filled

— 10^{13} neutron flux

---- 6x10^{13} // //

—·— 1,1 x 10^{14} // //

FIG. 23

10 mm dia. UO$_2$ (nat.U)

0,5 mm S.S clad (400 °C)

fission-gas filled

— 10^{13} neutron flux

---- 6x10^{13} // //

—·— 1,1 x 10^{13} // //

5.3. Plastic Clad

Fig. 14 to 23, plotted according to the formulas of chapter 5.2., are reliable only in the range for which relation (34) holds, i.e. as long as the behaviour of the clad can be accounted for by the laws of elasticity.

It is apparent, however that quite often, a contact pressure P induces a tangential stress in the clad $\frac{Pxr}{t}$ in excess of the elastic limits. In this case one must account for the plastic strain of the clad and modify eq. (37) accordingly.

A plastic deformation of the clad will result in an increase of the inner bore radius and will prevent the contact pressure from increasing beyond certain limits.

The transition from the "elastic" to the "plastic" situation will be controlled by the creep properties of the clad material. We may however study the final thermal situation, which will correspond to a plastically deformed material.

We shall illustrate the above with a concrete case, which will avoid the uncertainty of selecting one or the other description of the strain-stress curves in the plastic state, hardly amenable to generalizations.

With a 7% S.A.P. clad at a temperature of 450°C, it is shown in ref. 11 that the total strain in the plastic region can be approximated by

$$\gamma = 732 - 752 \exp\left(- 1,120\varepsilon\right) \qquad (38)$$

Detracting from the total strain the contribution $\frac{\gamma}{E_c}$ of the elastic strain, we obtain as a

permanent deformation, with $\gamma = Pr_o/t$

$$\varepsilon_o = \frac{1}{1,120} \quad \ln \frac{752}{732 - \frac{Pr_o}{t}} - \frac{Pr_o}{t\,E_c} \tag{39}$$

The hot operating gap from eq. (33) must therefore be increased by an amount $r_o \times \varepsilon_o$, yielding

$$C' = C - sr_o + r_o \left[\alpha_c(\overline{T}-20) + \frac{r_o}{t} \quad \frac{P}{E_c} \left(1 - \frac{\nu_c}{2}\right) - \alpha_p(T_s-20) - \right.$$
$$\left. - \frac{\alpha_p \Delta T_p}{2(1-\nu_p)} + \frac{Pr_o}{E_p} (1-\nu_p) + \frac{1}{1,120} \ln \frac{752}{732 - Pr_o/t} - \right.$$
$$\left. - \frac{Pr_o}{E_c t} \right]$$

which, in combination with (32), (35), (34) and (36) will describe the behaviour of the clad material in the plastic range.

Figs. 24 and 25 show the effect of accounting for the plastic strain of the clad: a small assembly clearance or an interference fit between clad and pellet will eventually lead to a maximum initial operating contact pressure of the order of 50 kg/cm^2 and will prevent the contact resistance from falling below values of 0.95 and 0.35 °C/W/cm^2, respectively, with fission gases and He in the gaps.

Whereas the values of the thermal resistance in the plastic range are time-independent (left scale of figs. 24 and 25), the operating pressures (right scale) will of course decrease with time, since although the density and area of contact spots may remain the same, the operating pressure will be releaved by the creep of the clad material.

FIG 24

30mm dia. UC (nat.U)

1mm SAP clad (450°C)

fission gas-filled

10^{14} neutron flux

FIG 25

30mm dia. UC (nat.U)

1mm SAP clad (450°C)

He-filled

10^{14} neutron flux
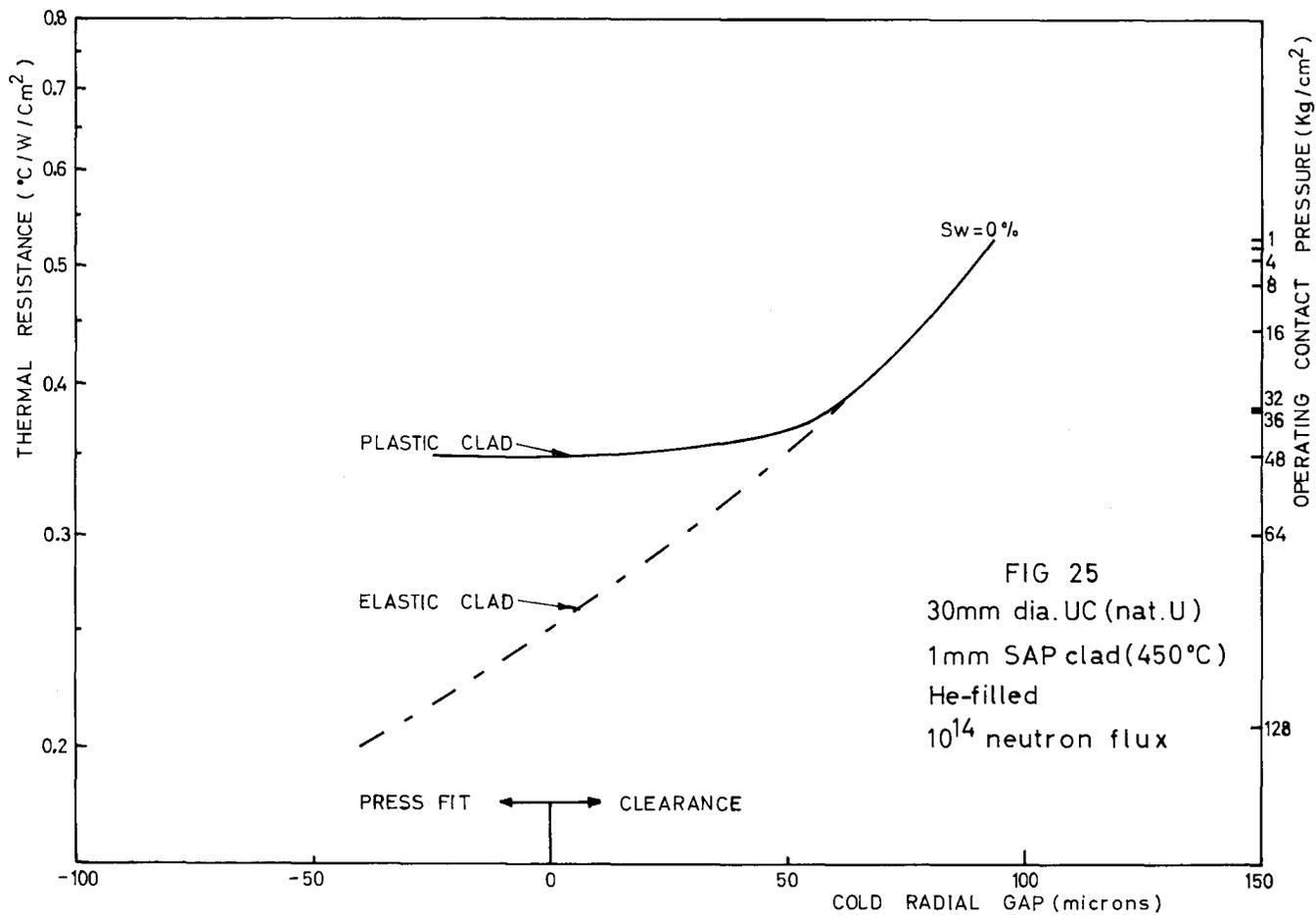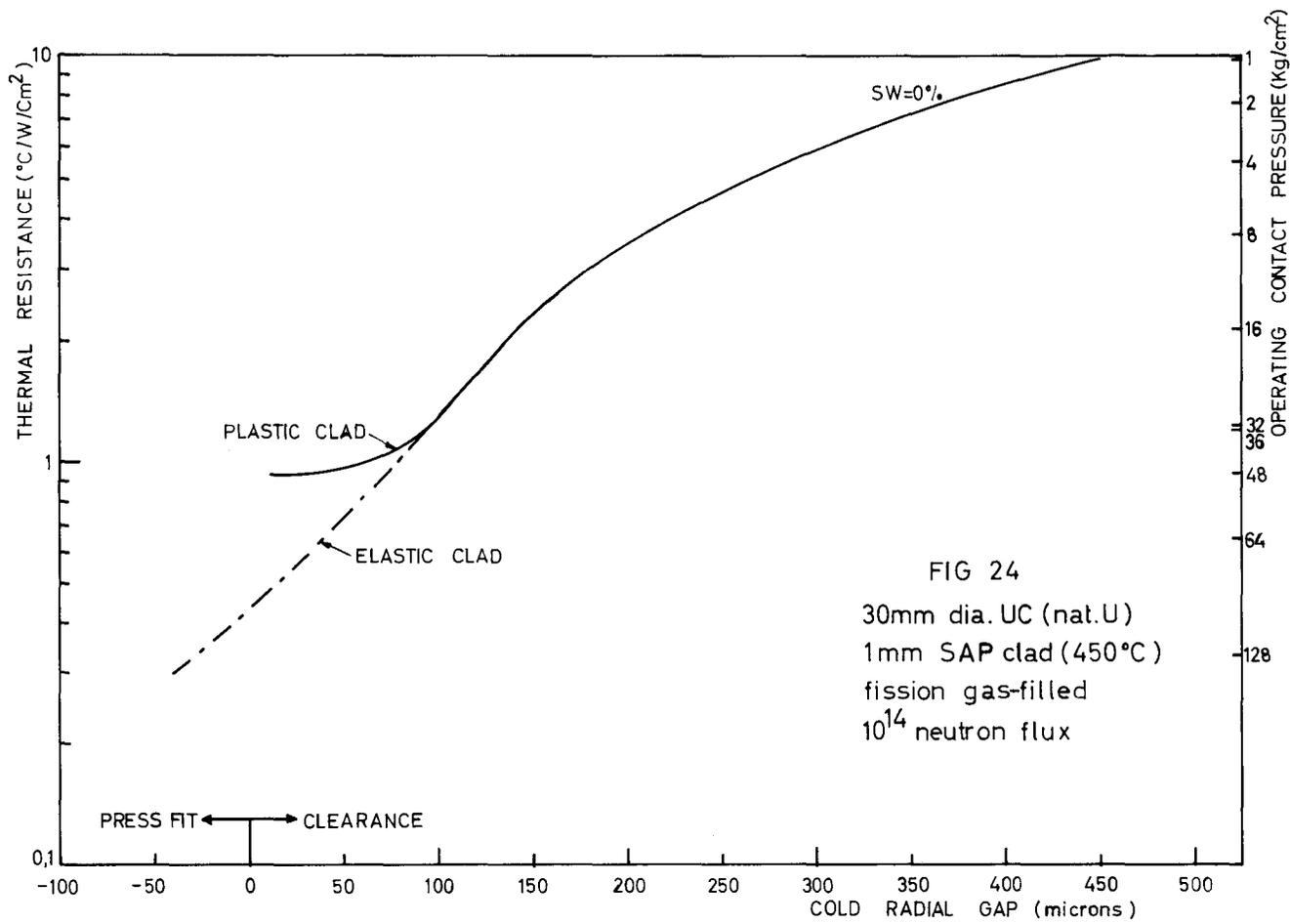
5.4. Out-of-pile experiments

A series of measurements of contact conductance
in cylindrical geometry were carried out on metal pel-
lets loosely inserted or shrink fitted into metal clads.
A number of metals were chosen for the pellet and the
clad, including Aluminium, S.A.P., Armco-iron and Invar,
in order to operate with different expansion coefficients.

Surface roughnesses in the range R_t=5 to 100 μm
were used and axial heat generation was obtained by
means of a W wire. Temperatures of the clad and of two
different radial positions in the pellets were moni-
tored.

Tests were made in vacuo, in air and in argon.

Fig. 26 shows a typical test carried out with an
SAP pellet 17.86 mm O.D. clad in Invar. Differential
thermal expansions were estimated and compared to
gaps deduced from temperature discontinuity at the
contact gap.

The effect of R_t on conductance is clearly seen
to affect the nominal gap by an amount 2/3 R_t, as pre-
viously discussed.
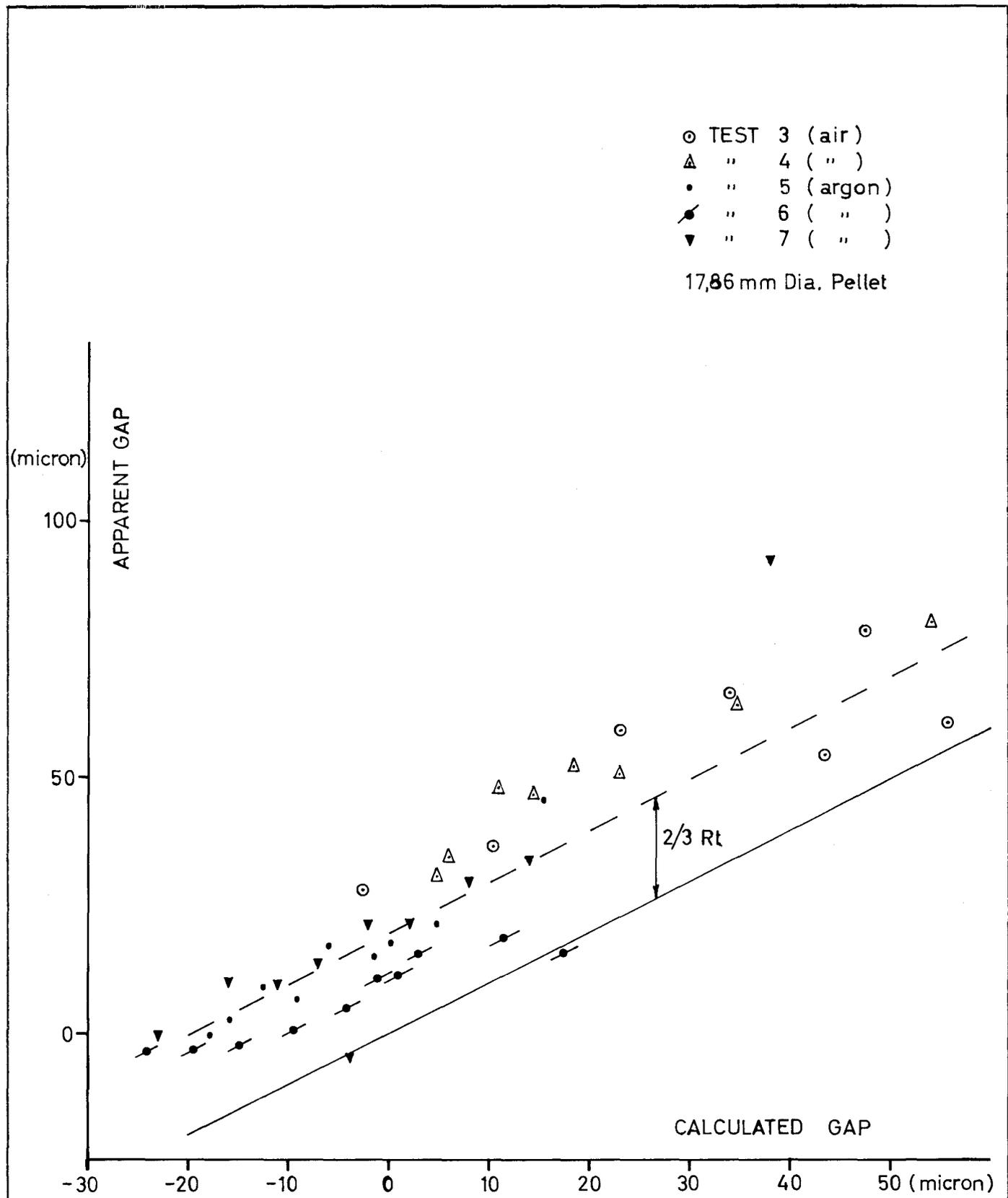
FIG 26

OUT-OF-PILE CYLINDRICAL CONTACT

5.5. In-pile experiments

Two rigs were irradiated by the Physical Chemistry Branch of C.C.R. Ispra to measure the fuel-clad contact conductance. Each rig contained three test assemblies made of Uranium Carbide pellets sheathed in sintered a-luminium powder (S.A.P.) with a Xenon atmosphere.

The first rig was exposed to a flux of 4.5 x 10^{13} n/cm^2 sec. maximum (unperturbed) in channel N°9 of the Ispra 1 reactor.

Nominal Cold radial clearances between fuel and sheath were 0.0023, 0.0046, 0.0104 cm in the first three assemblies and 0.0015 cm in the others.

Operating temperature distributions in the fuel and sheath system were measured during irradiation and indicated fuel temperatures lower than theoretically expected.

To reproduce the thermal flux prevailing in an Orgel reactor, the same rigs were irradiated once again in the central channel at a thermal flux of 10^{14}.

Daily readings of the UC central temperature were plotted as a function of the irradiation time in Figs. 27 and 28.

UC Temperatures dropped some 50,100,150 °C in cans A,B,C of rig 1 during the first full power reactor period (about two weeks).

Heat transfer coefficient between sheath and cooling heavy water was assumed to be constant and the neutron flux was therefore assumed to be well enough correlated to the sheath-water temperature drop.

The best fit over several reactor periods gave an expression for the thermal flux (see appendix III)

$$\emptyset = 0.74\,\beta + 0.1\beta^2$$

Heat generation, temperature distributions and fuel-clad temperature drop were calculated taking into account both the fission heat and the gamma heating.

Gap conductances are given by (App. III)

$$U = \frac{F}{\Delta T_1} = \frac{15.5 \; \emptyset}{Tc-T-45.3 \; \emptyset}$$

Experimental data are shown plotted in Fig. 29, 29a and 30 as thermal resistance versus irradiation time. It is apparent that the thermal resistance underwent a striking decrease throughout the irradiation.

With 0.0023, 0.0046, 0.0104 cm nominal radial gaps in cans A,B,C, of rig N°1 and 0.0015 cm in all three cans of rig. N°2 and since UC and SAP have thermal expansion coefficients of about 10 x 10^{-6}/°C ref. (13) and 21.5 x x 10^{-6}/°C ref. (14), the operating clearances ought to increase during irradiation.

The reasons of the centre temperature decrease can be explained by:
- a) pellet cracks leading to a diametral increase of the pellet,
- b) variation of thermocouple e.m.f. during irradiation,
- c) UC swelling due to fission gases,
- d) chemical transformations in the pellet leading to a "chemical swelling",
- e) creep or rupture of SS container.
- f) Shrinkage of the S.A.P. bore.
- a) Pellet cracks were thought very likely, since examination of the unirradiated pellet stock showed radial cracks caused by relaxation the internal tension generated during cooling after fusion combined with oxidation. A typical picture is shown in Fig. 31.

THERMAL RESISTANCE

RIG. N°1   CAN C

THERMAL   RESISTANCE   VS.
IRRADIATION   TIME   RIG. 1

THERMOCOUPLE
INTERRUPTED

THERMOCOUPLE
RECONNECTED

1° PERIOD

IRRADIATION   TIME   (HOURS)

FIG.29a

FIG. 30

THERMAL RESISTAN
IRRADIATION TIME

CAN   A   .
CAN   B   ●
CAN   C   △
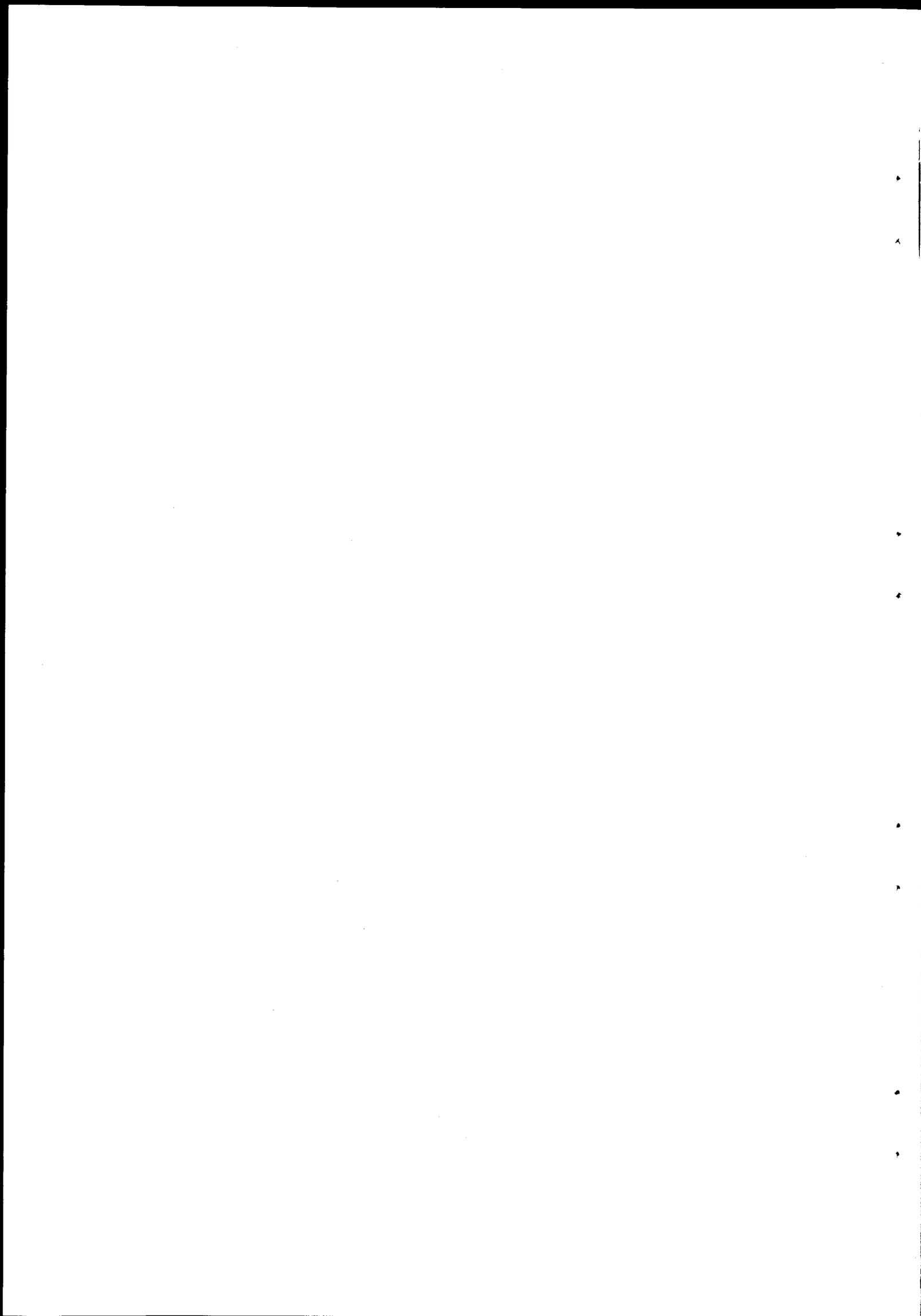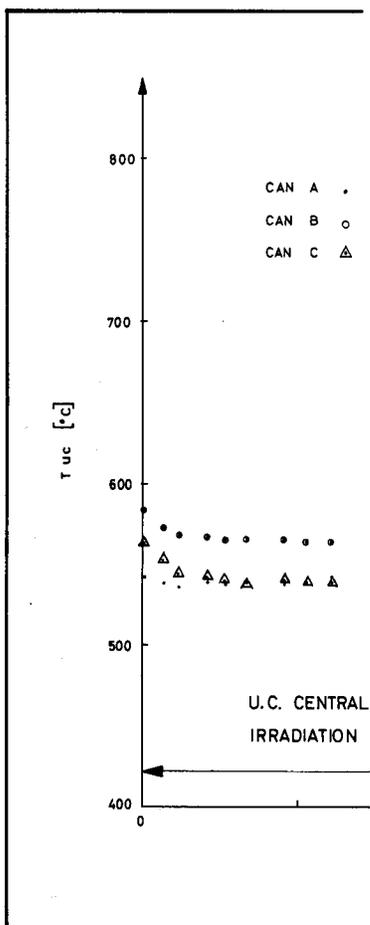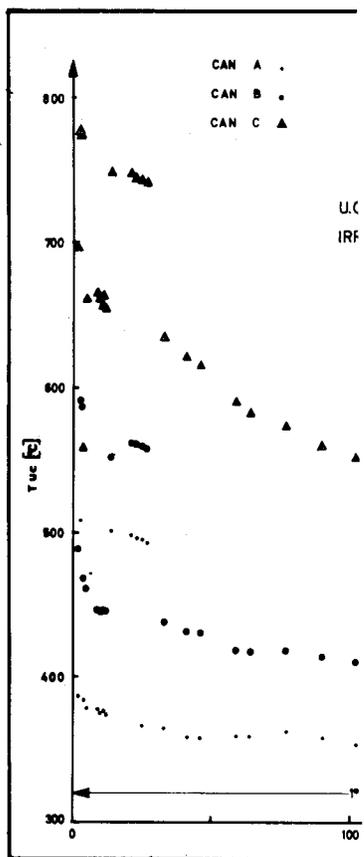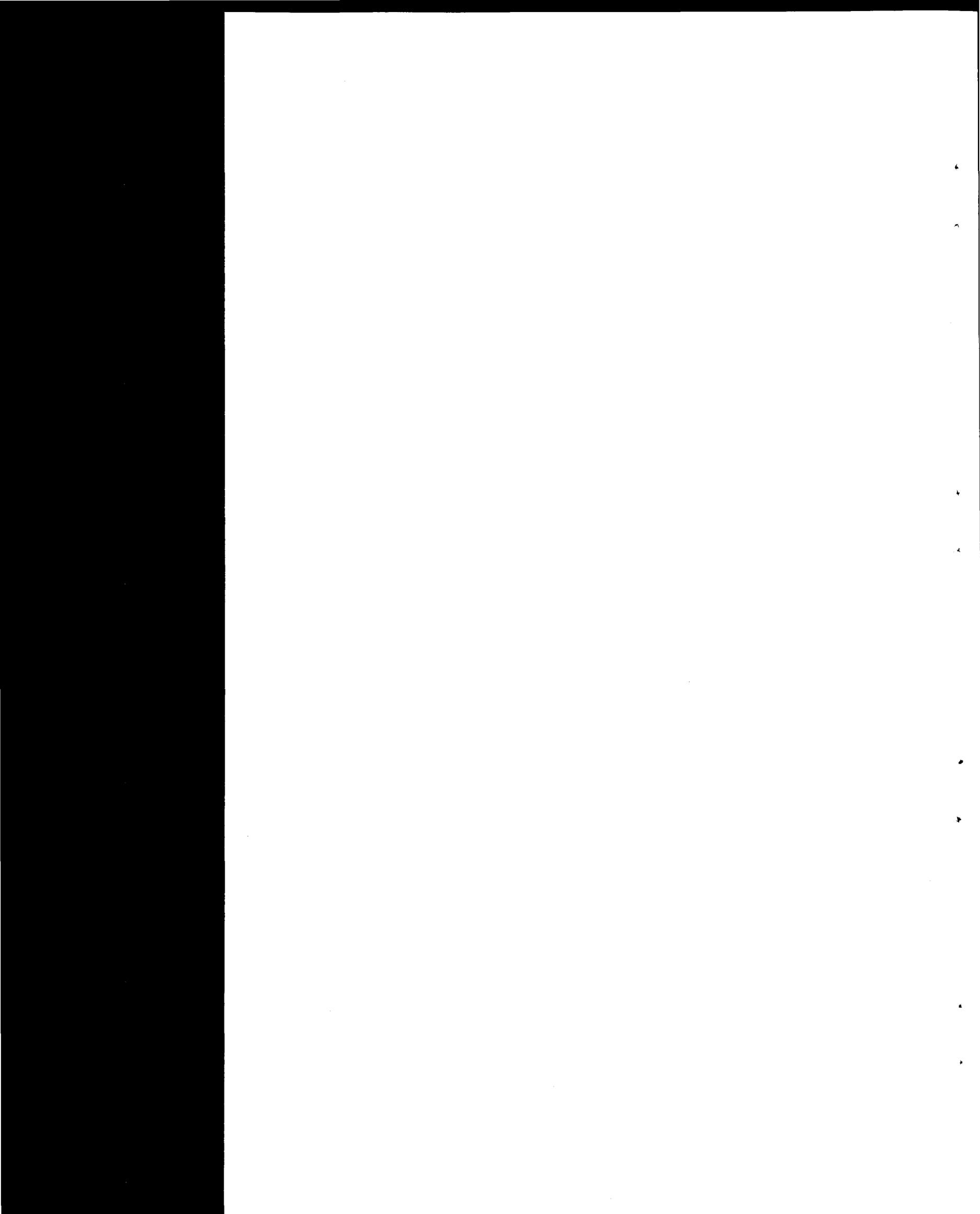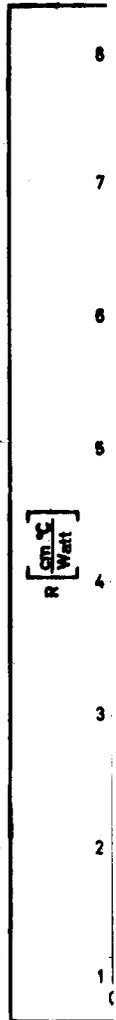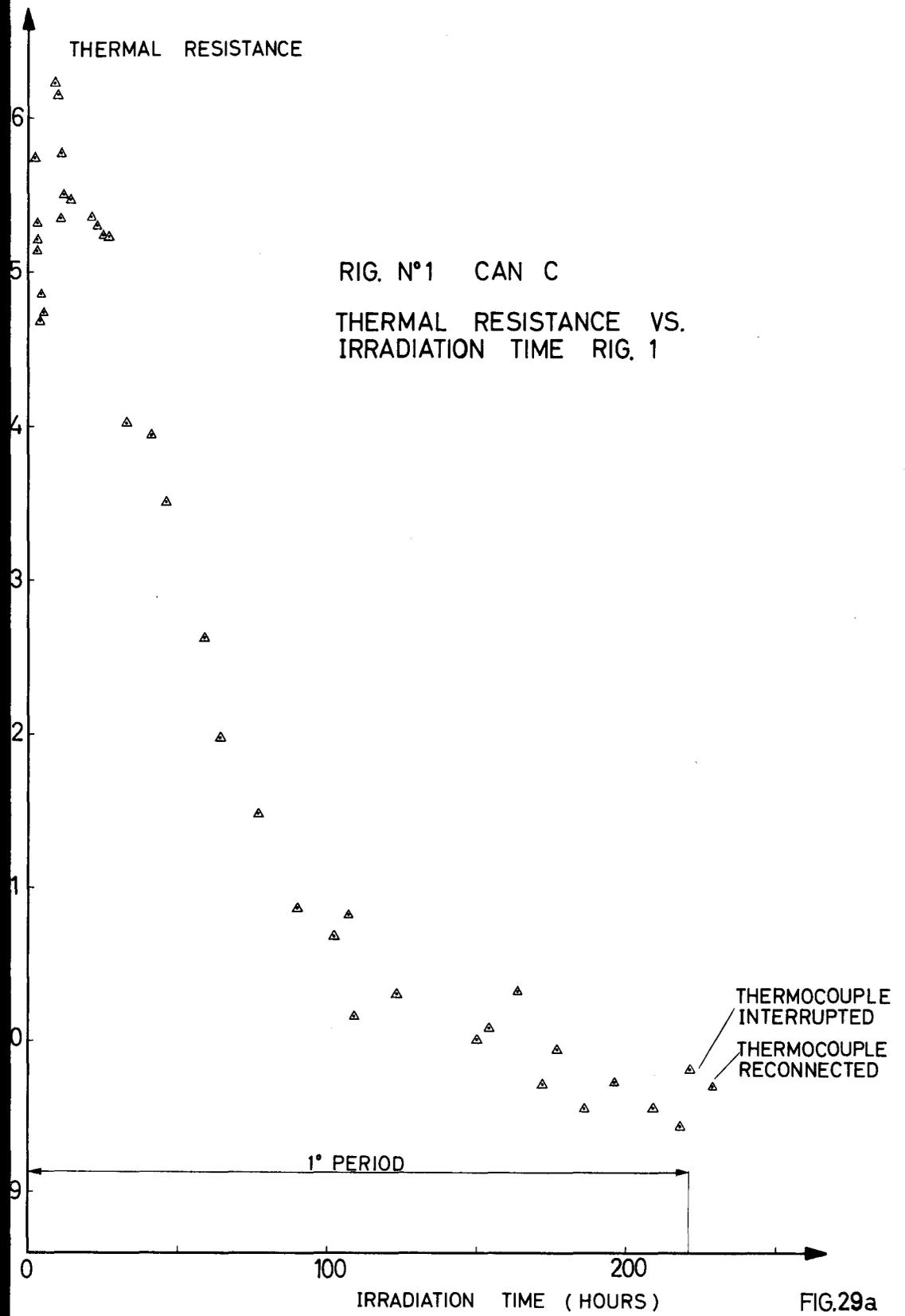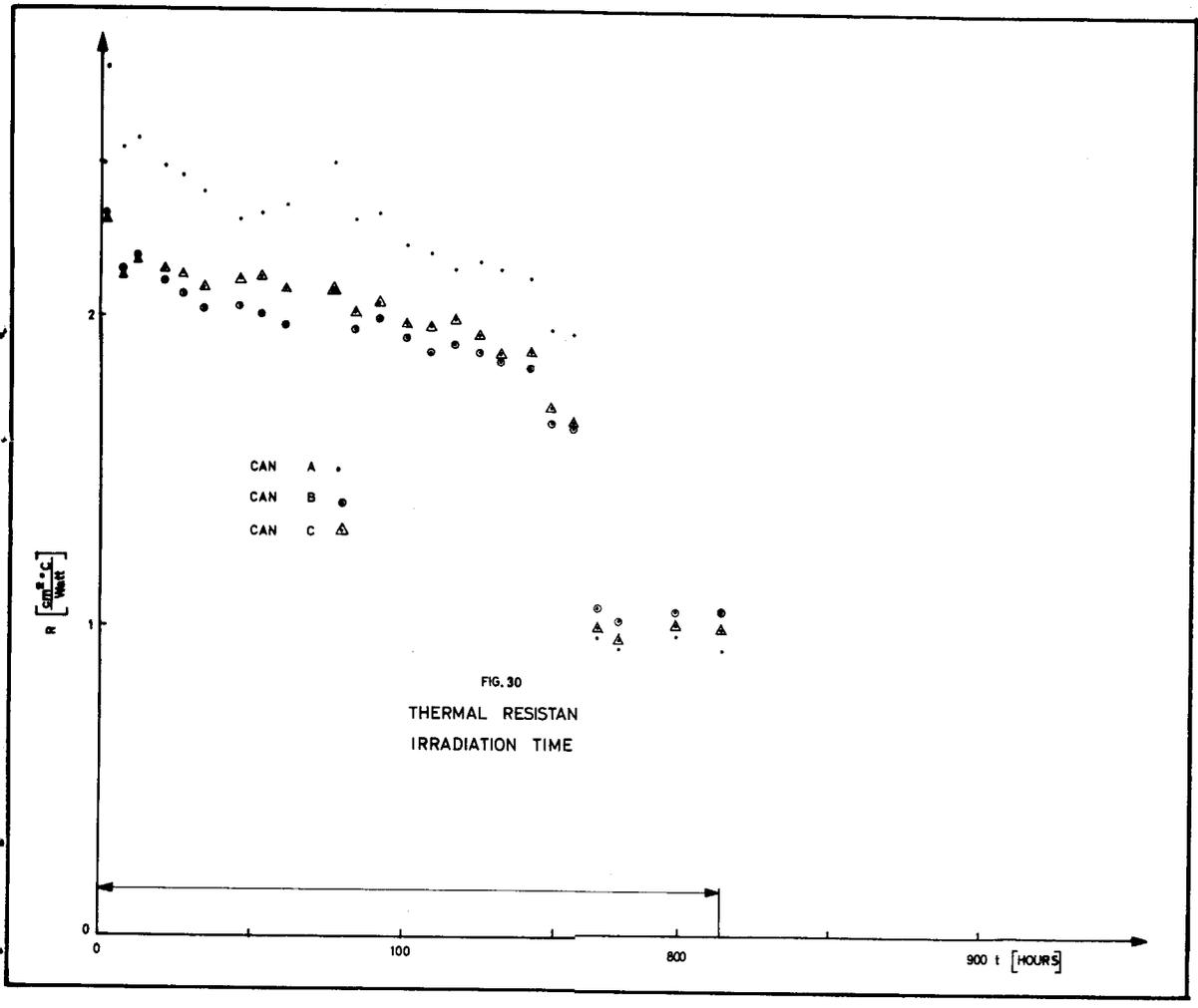
FIG. 31

PELLET RADIAL CRACKS

VOLUME INCREASE $\frac{\Delta V}{V}$ %

ΔV/V = 1,6 % PER ATOM % BU

ΔV/V = 1,0 % PER ATOM % BU

BURNUP (MWD/ TnU · 10⁻⁴)

VOLUME INCREASE FOR ARC-CAST UC
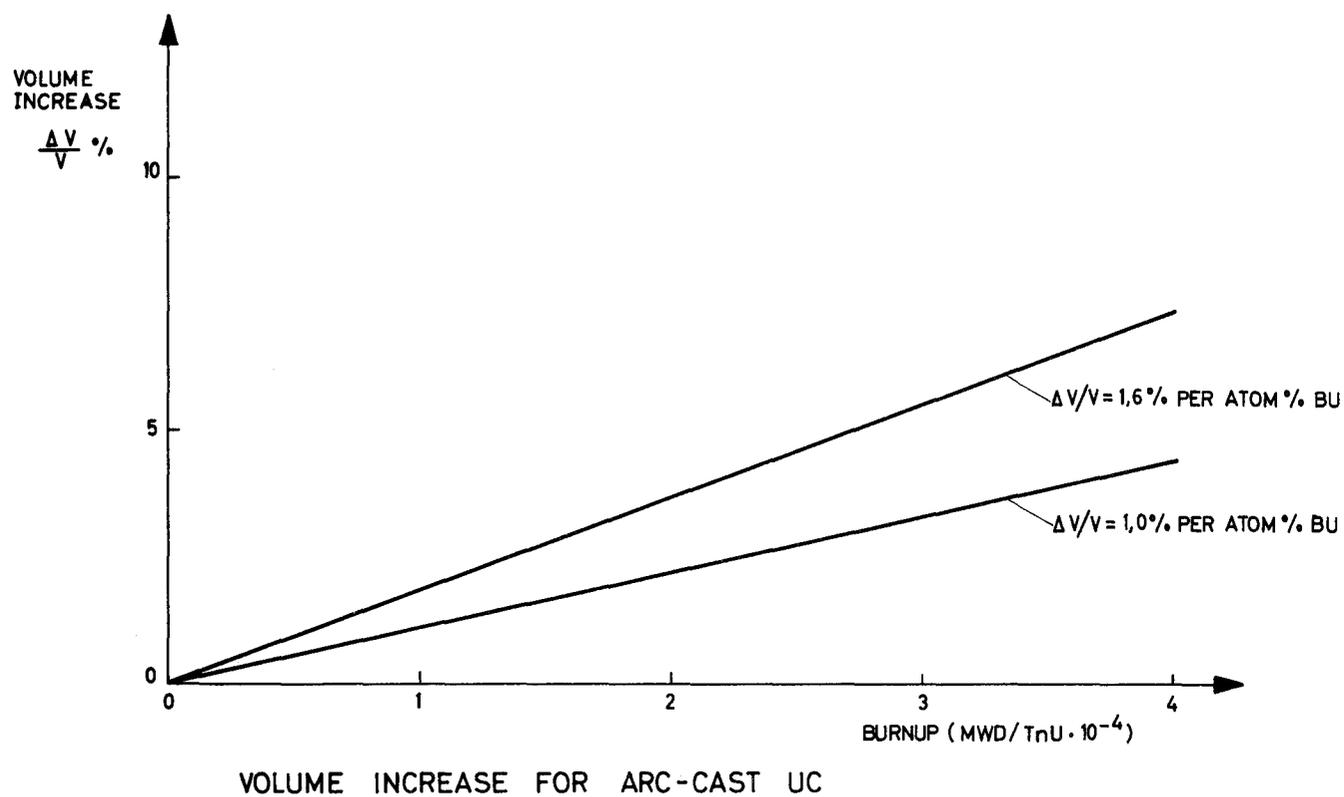
FIG. 32

- b) E.m.f. variations caused by irradiation of Pt-10%
  RhPt thermocouples to an integrated thermal dose
  of 6 x 10^{19} per cycle at approx. 600°C are estima-
  ted to be negligible.

- c) Swelling of the UC pellet due to fission gases was
  unlikely since the operating temperatures and the
  burnups were too low to have any appreciable effect.
  Rough Ref. (19) estimated a volume increase from so-
  lid fission products for UC of 1.0 to 1.6% per atom%
  burnup (1 atom% of U burnup is equivalent to 9400
  $MWD/_{Ton\ U}$) and from Fig. 32 which is plotted for
  reference, he concluded that swelling is caused by the
  cumulation of solid fission products if temperatures
  are lower than 800°C and burnups smaller than
  25.000 $MWD/_{Ton\ U}$. In our case the temperatures were
  lower than 800°C and burnups were 580 $MWD/_{Ton\ U}$
  for Rig N° 2 and 725 $MWD/_{Ton\ U}$ for Rig. N°1. From
  Fig. 32 the volume increase of UC pellet was esti-
  mated to be of the order of 1 to 2%$_0$. Therefore
  swelling due to fission products is negligible.

- d) Presence of a different gas in the initial Xenon
  charge could have occured if some Helium infiltra-
  ted into the capsules through cracks in the welds.
  In this case the conductivity of the gas in the gap
  would gradually increase and the UC pellet tempera-
  ture decrease. The possibility of gas release from
  the SAP clad was rejected because temperatures were
  too low (150°-200°C). However, the possibility re-
  mains of an oxidation of UC to UO_2 due to water or
  oxygen released from the MgO pellets. Interaction
  of UC and UO_2 during irradiation may have led to a
  formation of free U and liberation of CO with sub-
  sequent swelling.

- e) Creep of the SS capsules at a temperature of approx.
  50°C could not occur.

- f) Some shrinkage of the S.A.P. bore by stress relieving un-
  der the combined effect of the temperature and radia-
  tion could occur.

5.6. Post-irradiation examination

The two irradiation rigs were dismantled after a
1- year decay in the hot-cell of the Ispra-1 reactor with
the help of the hot-cell operators.
Scope of the examination was:
1 - A check on the pellet and the bore diameters to esti-
mate the amount of swelling or creep, if any.
2 - A check on the flux by γ activation measurements. The
outer appearance of the UC-SAP assemblies in their
cans showed no obvious deformation or cracks in the
cans. It was impossible to locate the cobalt flux
monitors. Therefore a sampling was made of
- Two S.S. fillings for each can (S.S. contained
.026% Co) which were γ counted (fig.33).
- A few fragments of the UC pellets were thoroughly
examined by M. Bresesti of the Nuclear Chemistry
service to determine burnups.

The latter examinations yielded beside the shape of
the thermal neutron flux (Figs 34, 35 and 36) also the
maximum actual value it attained in rig. n° 1(5.29 x 10^{13})
and n° 2(5.05 x 10^{13}).

It was shown that during the irradiations a conti-
nuous increase of the fission heat generation was due
to Pu formation (11% and 7% contribution of Pu, respec-
tively, for rig n° 1 and 2).

Fig. 37, which shows the flux as estimated from tem-
perature indications checks with the above results, and
shows a flux increase 20 to 25% at the end of irradiation.

The UC pellets were extracted from their S.A.P. clad
by heating the assembly to 300-350°C. The differential ther-
mal expansion of S.A.P. and UC allowed this extraction with
no difficulty in most cases. A slight pressure was exer-
ted on the pellet by means of a vacuum-controlled piston
extractor. The irradiated pellet diameters were measured
in the hot-cell itself by means of a microgage giving
a precision of $\pm$ 1 μm. The S.A.P. bores were also mea-
sured in a plain glove box. The results of these measu-
rements are:
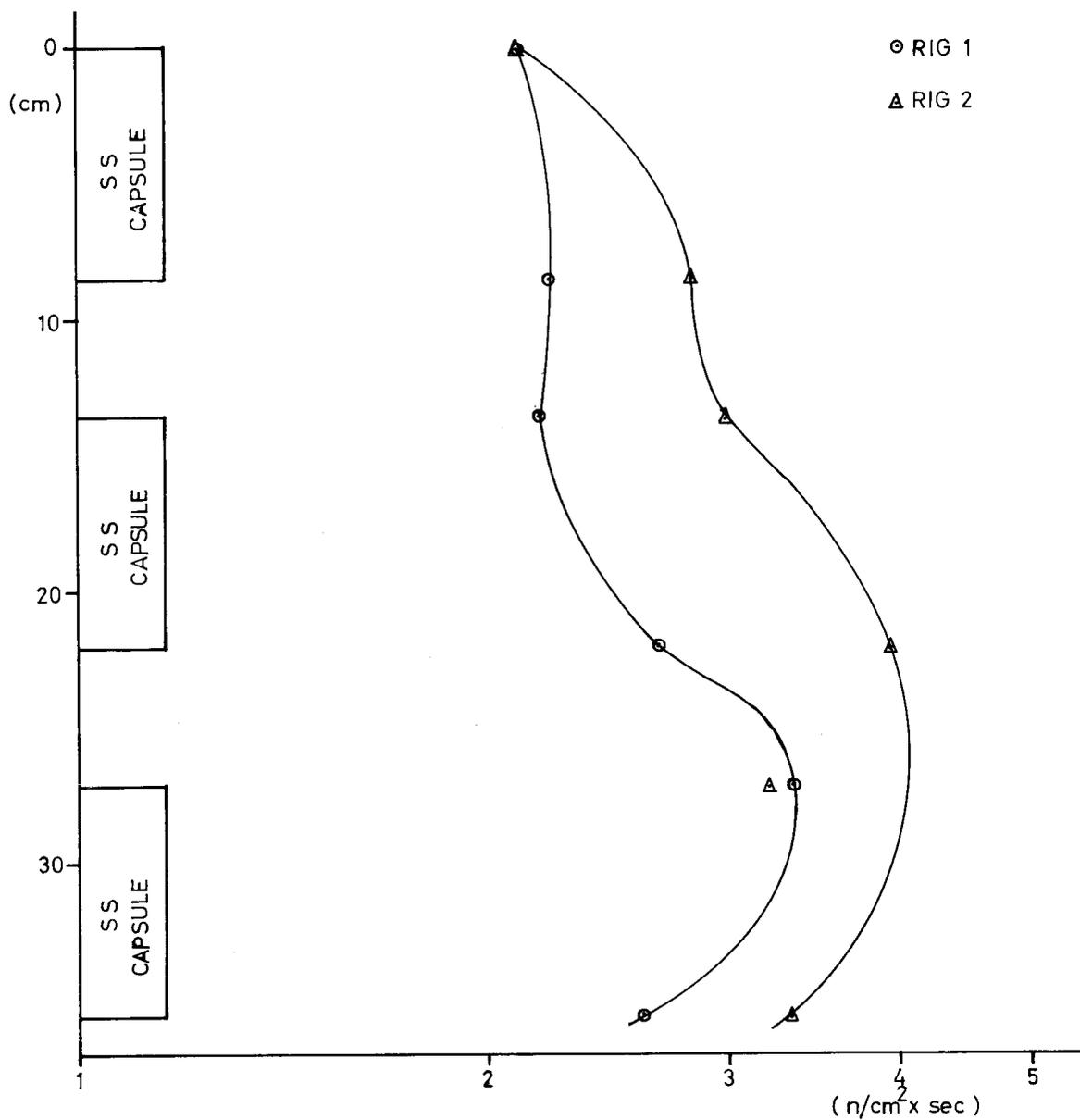
FIG 33

Co-60 ACTIVITIES MEASURED IN S.S. CAPSULES
FROM RIG 1 AND RIG 2
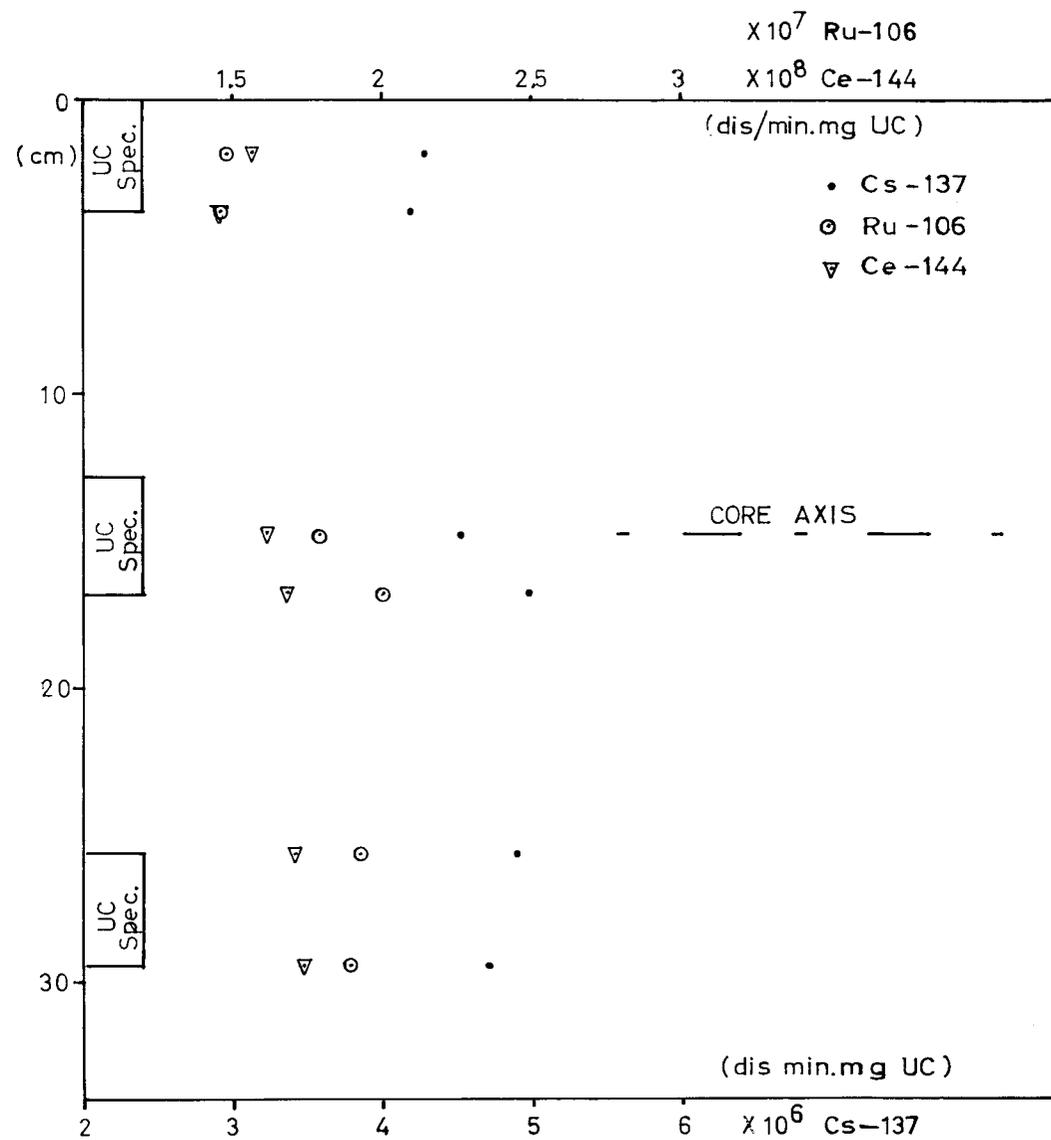
FIG 34

Ru 106, Cs 137, Ce 144 ACTIVITIES MEASURED IN
SAMPLES FROM RIG 1
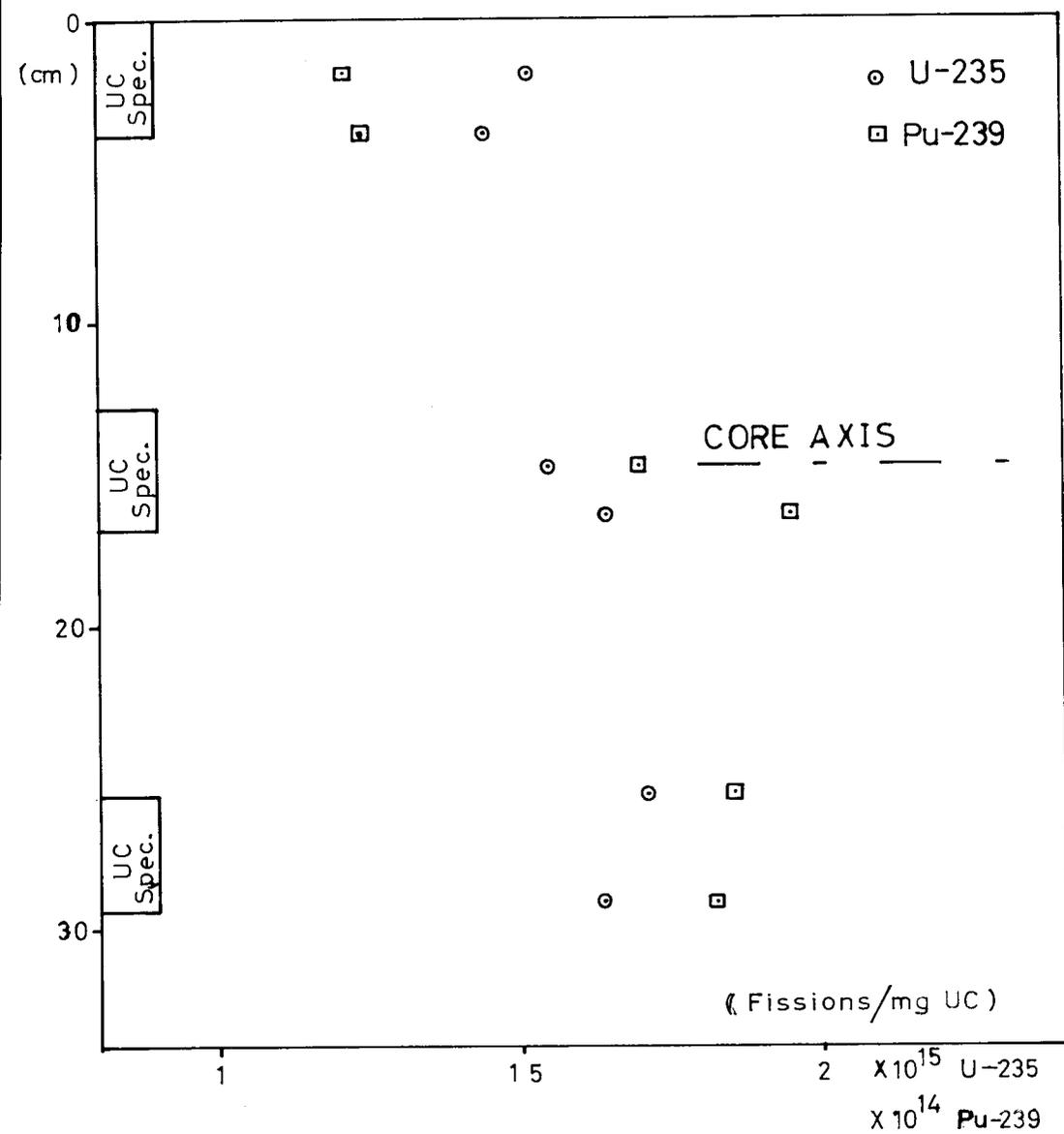
FIG 35

U-235 AND Pu-239 FISSIONS MEASURED IN
SAMPLES FROM RIG 1
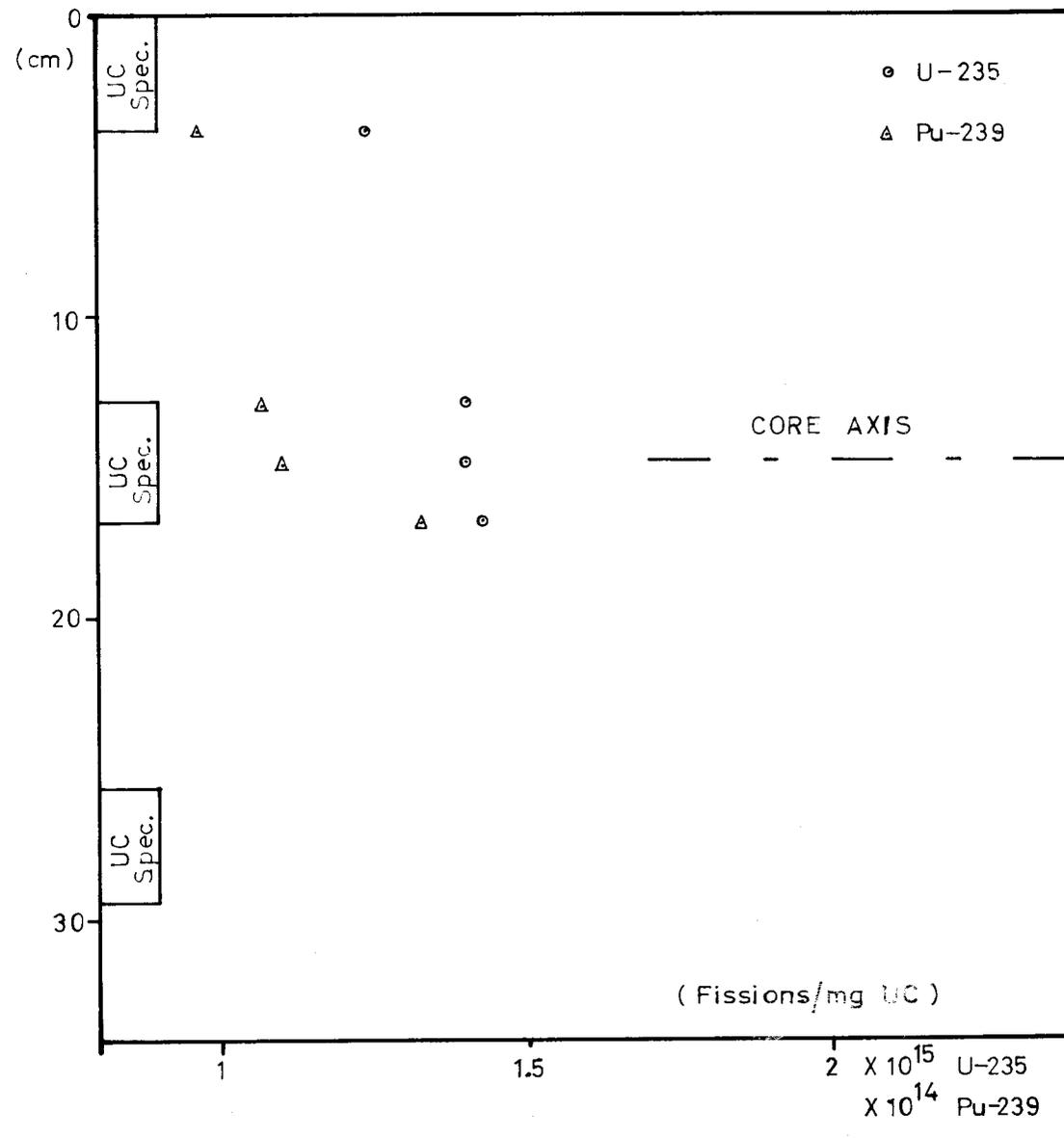
FIG 36

U-235 AND Pu-239 FISSIONS MEASURED IN
SAMPLES FROM RIG 2

CAN A
CAN B
CAN C

THER
IRRA
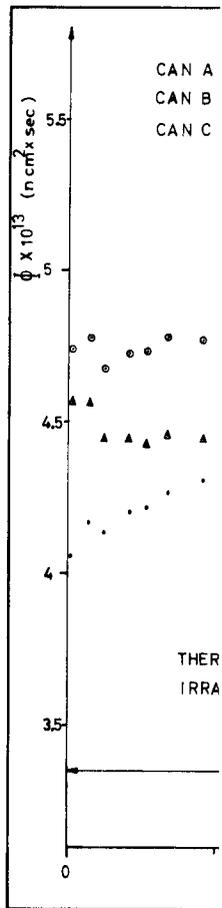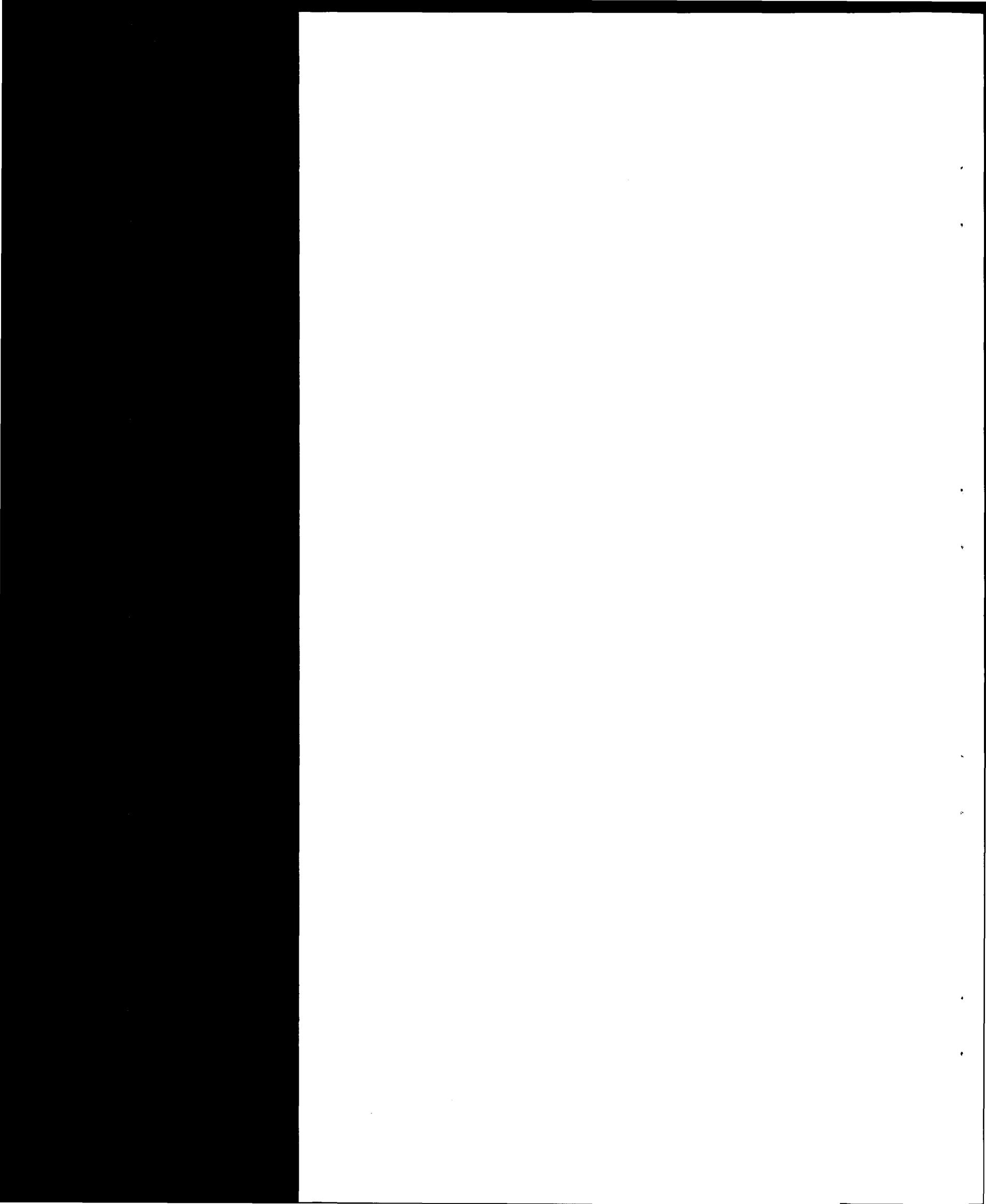
RIG. 1

Nominal (pre-irradiation) measurements

	Pellet n°	Max.Diam. cm.	Min.Diam. cm.	Mean Diam. cm.	Mean Bore Diam.cm.	Diametral Clearance cm.
	1	1.905508	1.904797			
Can A				1.905336	1.909953	0.004622
	2	1.905635	1.905406			
	3	1.906016	1.905127			
Can B				1.905616	1.914842	0.009206
	4	1.906168	1.905154			
	5	1.906524	1.905863			
Can C				1.906092	1.926907	0.020814
	6	1.906168	1.905813			

Post-irradiation measurements

	Pellet n°	Max.Diam.	Min.Diam.	Mean Diam.	Mean Bore	Diametral Clearance
	1					
Can A						
	2		CRACKED			
	3	1.9180	1.9104			
Can B				1.9135 ± 0.0017	1.9103	-0.0032
	4	1.9159	1.9106			
	5	1.9339	1.9102			
Can C				1.91878 ± 0.0036	1.92306	0.00428
	6	1.9273	1.9186			

Swelling of UC was

	Diametral Swelling cm.	Swelling º/º	Max.Irrad. Temperature ºC	Burnup MWD/ton U
Can B	0.0079 ± 0.0017	0.415	650	725
Can C	0.0127 ± 0.0036	0.668	800	725

RIG. 2

Nominal (pre-irradiation) measurements

	Pellet n°	Max.Diam. cm.	Min.Diam. cm.	Mean Diam. cm.	Mean Bore Diam. cm.	Diametral Clearance cm.
Can A	7	1.907667	1.906778	1.906735	1.909673	0.002946
	8	1.906778	1.905711			
Can B	9	1.906270	1.905609	1.906251	1.909445	0.003200
	10	1.906727	1.906397			
Can C	11	1.905762	1.905508			
	12	1.905127	1.904695	1.905478	1.908454	0.003072
	13	1.906016	1.905762			

Post-irradiation measurements

Can A			CRACKED			
Can B	9	1.9248	1.9123	1.917565	1.90493	-0.01263
	10	1.9302	1.9143			
Can C			IMPOSSIBLE TO EXTRACT			

Swelling of UC was:

	Diametral Swelling cm	Swelling %.	Max. Irrad. Temperature °C	Burnup MWD/ton U
Can B	0.0113±0.0040	0.58	580	580

A remakable swelling had taken place. It is very likely that this swelling was due to oxidation during heating for pellet extraction.

S.A.P. bores dicreased by 45 μm for can B, 38 μm for can C of rig n°1 and 45 μm of can B of rig 2. Outer diameter of the clad showed decreases of the order of 5 μm. It is however believed that a permanent strain of the S.A.P. could have occured by strass-relieving even though temperature never exceeded 400°C.

Analysis of the results of the irradiations point out to a few discrepancies:

1 - Contact conductance values were too high on the basis of the nominal cold gaps.
2 - A sudden increase of conductance was seen when shifting rig n°1 from channel n°9 to the central channel.
3 - Contact conductance seemed to increase with time during irradiation.

On the light of the post-irradiation examination, the Causes for the above discrepancies are thought to be:

1 - Cold assembly gaps were smaller than specified.
2 - The sudden increase of conductance was due to the pellet cracks and bore decrease by stress relaxtion.
3 - Increase of conductance during irradiation is due to enhanced fission heat generation because of Pu buildup. A more detailed analysis follows:

Rig n° 2

Gas conductance was
$$U_{gas} = Kf/2/3 \sum R_t = 0.1 \text{ w/cm}^2 \text{ x } °C$$

Inial value or u_{Tot} is about 0.4 W/cm^2°C (see fig. 30)
Therefore $U_{natal} = 0.3$ W/cm^2 x °C
Using formula 8.5 of ref. 11 with a constriction alleviation factor $2/\pi$

$$U_{Metal} = \frac{.434 \; K}{R_t} \cdot \frac{2}{\tilde{\Pi}} \cdot (\frac{P}{H})^{.9125}$$

With $\overline{T}_{SAP}$ = 200 °C $\quad$ K = .276 W/cm°C $\quad$ R_t = 15 x 10^{-4} cm

H= 4,700 kg/cm^2 $\qquad$ U_{metal} = 2.29 x 10^{-2} $P^{.9125}$

and the contact pressure must have been in the neighborhood of 1.4 Kg/cm^2. This low value of the contact pressure shows that the hot gap was closed without an appreciable amount of interference.

Initial clearance (see appendix III) was

$$G_o = G_1 - 1.89 \times 10^{-5} \; \overline{T} + 3.4 \times 10^{-4} - 3.02 \times 10^{-4} \emptyset + 9.53 \times 10^{-6} T_c$$

and with $\overline{T}$=200 °C $\emptyset$=5 $\quad$ T_c= 550 °C $\quad$ G_o=3 μm
The S.A.P. bore must have had a diameter given by:
pellet dia + assembly clearance i.e.

1.90625 + .0006 cm = 1.90685 cm
which checks with the post-irradiation determination of
the S.A.P. bore diameter (1.90493 cm).

At the end of the irradiation, u_{tot} was about 1W/cm^{2}C, corresponding to a contact pressure of about 3.4 Kg/cm^2. Interference fit must have been about 0.12 μm and therefore G_o is again estimated at 3 μm.

Rig n° 1

Gas conductance is again 0.1 W/cm^2°C. Conductance data at the beginning of irradiation are (see fig. 29 and 29a)

	U_{total}	U_{metal}
can A	0.172	0.07
can B	0.149	0.05
can C	0.0625	–

Cans A and B had therefore contact without interference fit at operating conditions.

Estimating G_o as before, with $\overline{T} = 125$ °C, $\emptyset = 2 \times 10^{13}$, $T_c = 390$°C in can A and 490° in can C we obtain

G_o can A = 11 μm

G_o can B = 20.5 μm

whereas for can C the apparent hot gap was:

K gas/U_{total} = 17.6 μm

The portion of gap due to the effect of surface roughness$(\frac{2}{3}\sum R_t)$ was 10 μm. Then, The actual radial hot clearance was 7.6 μm.

With $\overline{T} = 125$ °C, $\emptyset = 2$, $T_c = 700$ °C we estimate
G_o can C = 48 μm

At the end of the irradiation, thermal data were:

	U_{total}	U_{metal}
Can A	0.25	0.15
Can B	0.20	0.10
Can C	0.105	-

which show that the cold gap in cas A and B went pratically unchanged whereas the cold gap in can C is estimated to be 40 μm, with a decrease of abt. 8 μm.

When using in our formulas the derived above, observed conductances during irradiation check quite well with those that were actually observed.

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Appendix I

Measurement of S.A.P. hardness

Hardness is generally understood as the resistance of a body to plastic deformation under a pressure applied to a portion of its surface.

The deformation which determines the load bearing area (sum of all small contact areas), is howerver, elastic up to certain pressure limits. Hardness will therefore be barely definable as the average pressure in a ball indentation test.

It would be incorrect to assume a definite upper pressure limit for the elastic deformation, beyond which plastic yielding takes place, because the cause of deformation is a non-isotropic stress and not a hydrostatic pressure.

The average pressure for plastic flow to start will depend on the initial shape of the contacting surfaces of the two bodies.

Materials in contact will be deformed both elastically and plastically since the local pressure will vary from point to point, generating elastic deformations in some regions and plastic deformation in others.

A relation has been shown to exist between hardness H and yield strength Y

$$H = 3 Y \text{ to } 4 Y$$

However, this relation is valid when all the contact region has yielded plastically. Up to $p = 1.1 Y$ the Hertz law can be assumed to hold (elastic region) and for

$$1.1 Y < p < 3 Y$$

some interpolated pressure-deformation law can be
used to predict the deformed area (Ref.3).

Vickers microhardness tests were used in our
investigation, using a projection microscope tester
having a pyramid diamond identer. Identations were made
with loads of 25, 50, 100, 200, 300 and 500 grams,
at various temperatures between 21 and 330°C for S.A.P.
(4% and 7% Al_2O_3). Vickers hardness was given by the
expression

$$H = \frac{1,854 \; P}{d^2}$$

where

        P is the test weight (grams)
        d is the diagonal length of the pyramidal
          identation (microns)

and was plotted in fig. 38 and 39 as a function of
the temperatures. Experimental measurements are ta-
bulated below.

S.A.P.   4 %

Test weights (gm)		25	50	100	200	300	500
Temperature 25°C							
Diagonal length d(μ)		24.6	35.1	49.6	69.2	86.27	115.4
Hardness H(kg/cm^2)		7080	7510	7550	7750	7470	6950
147°C	d	32.17	44.7	61.42	86.44	106.35	137.1
	H	4475	4635	4925	4970	4900	4940
191°C	d	33.28	47.22	64.90	91.77	110.75	144.57
	H	4170	4160	4400	4410	4540	4440
203°C	d	34.60	49.47	65.52	94.50	117.20	151.35
	H	3865	3785	4330	4140	3770	4050
259°C	d	35.60	51.72	70.87	98.95	127.55	161.57
	H	3645	3470	3690	3795	3425	3550
321°C	d	40.55	56.50	78.00	108.35	135.65	171.10
	H	2145	2900	3050	3155	3025	3160

S.A.P. 7 %

Test weights(gm)		25	50	100	200	300	500
Temperature 21°C							
Diagonal length d(μ)		26.82	37.32	52.38	74.35	90.48	120.70
Hardness H(kg/cm^2)		6440	6650	6770	6710	6800	6360
69°C	d	27.55	38.45	59.62	78.48	93.80	124.50
	H	6100	6270	5210	6020	6330	5980
108°C	d	29.85	39.08	55.85	79.52	96.15	134.50
	H	5200	6070	5940	5870	6020	5120
150°C	d	29.72	43.38	6190	86.32	105.05	137.40
	H	5240	4930	4840	4970	5040	4910
200	d	31.92	41.52	61.12	88.35	108.48	137.80
	H	4540	5370	4960	4750	4740	4880
255°C	d	32.92	47.00	65.60	94.28	115.02	149.58
	H	4270	4200	4310	4170	4210	4140
330°C	d	35.80	53.30	75.88	104.90	130.68	169.48
	H	3610	3260	3220	3370	3260	3230

Appendix II

Number of contact points

A graphical method along the lines suggested by ref. (9) was used to determine the probable number of the contact points. It consisted in an analysis of recorded surface profiles supposing that a rough surface was pressed against a flat plane.

Experimental work was done with four SAP 7% specimens A,B,C,D. Their surfaces were roughened stochastically and their profiles were recorded with the surface analyzer. Surface finishes were as follows:

	A	B	C	D	
R_a (μ)	1.2	0.8	1.1	0.4	1st measurement
R_t (μ)	8	6.5	8	3	
R_a (μ)	1	1.3	0.9	0.35	2nd measurement
R_t (μ)	10	7.5	6	2.8	
R_a (μ)	1.4	1.4	1.2	0.45	3rd measurement
R_t (μ)	8.5	7.5	6.5	3.1	

Our analysis was carried out along 1 m. of profile record, corresponding to a real length of 0.5 cm along the specimen surface.

The initial position was established by a "datum line" which joined the two highest points of the profile. The pressure increase was simulated by a parallel translation of the datum line towards the bottom of the profile. The number of the contact points were counted for each position and the width of the actual contacts was measured and summed.
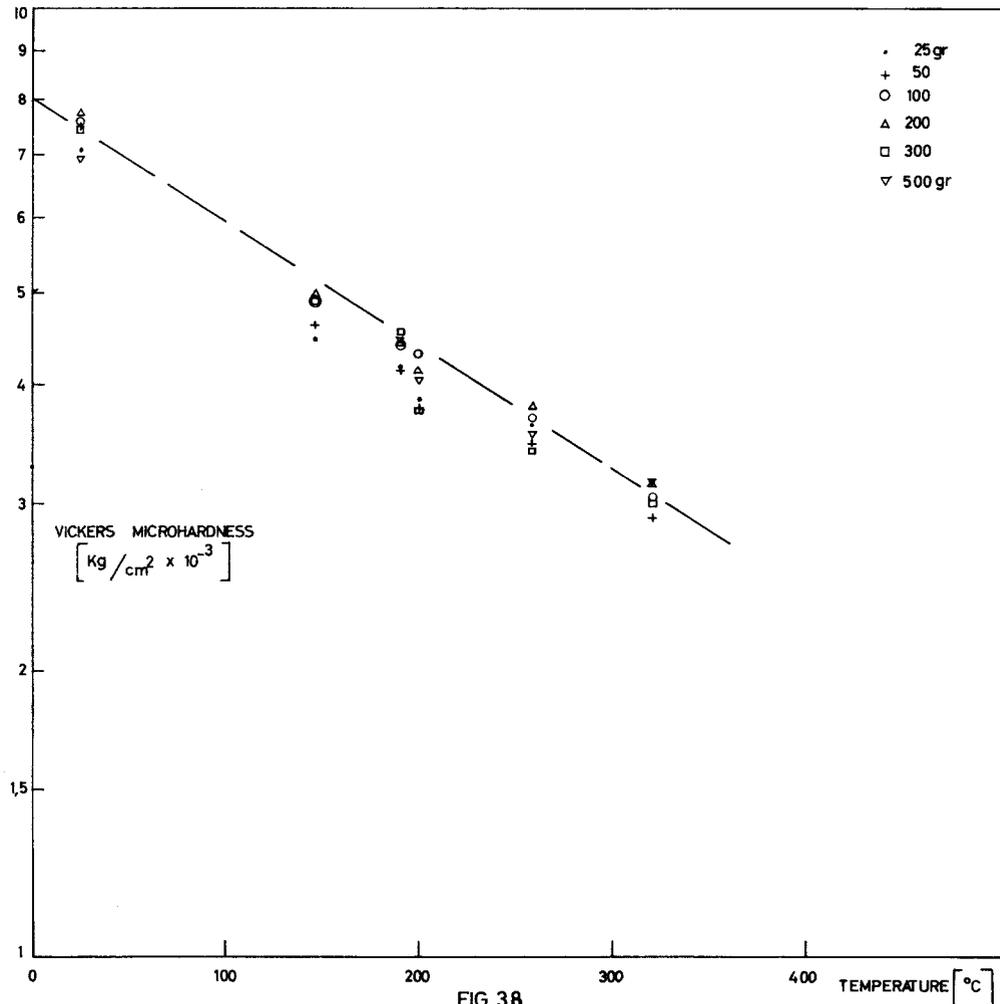
FIG. 38
4% SAP HARDNESS VS TEMPERATURE
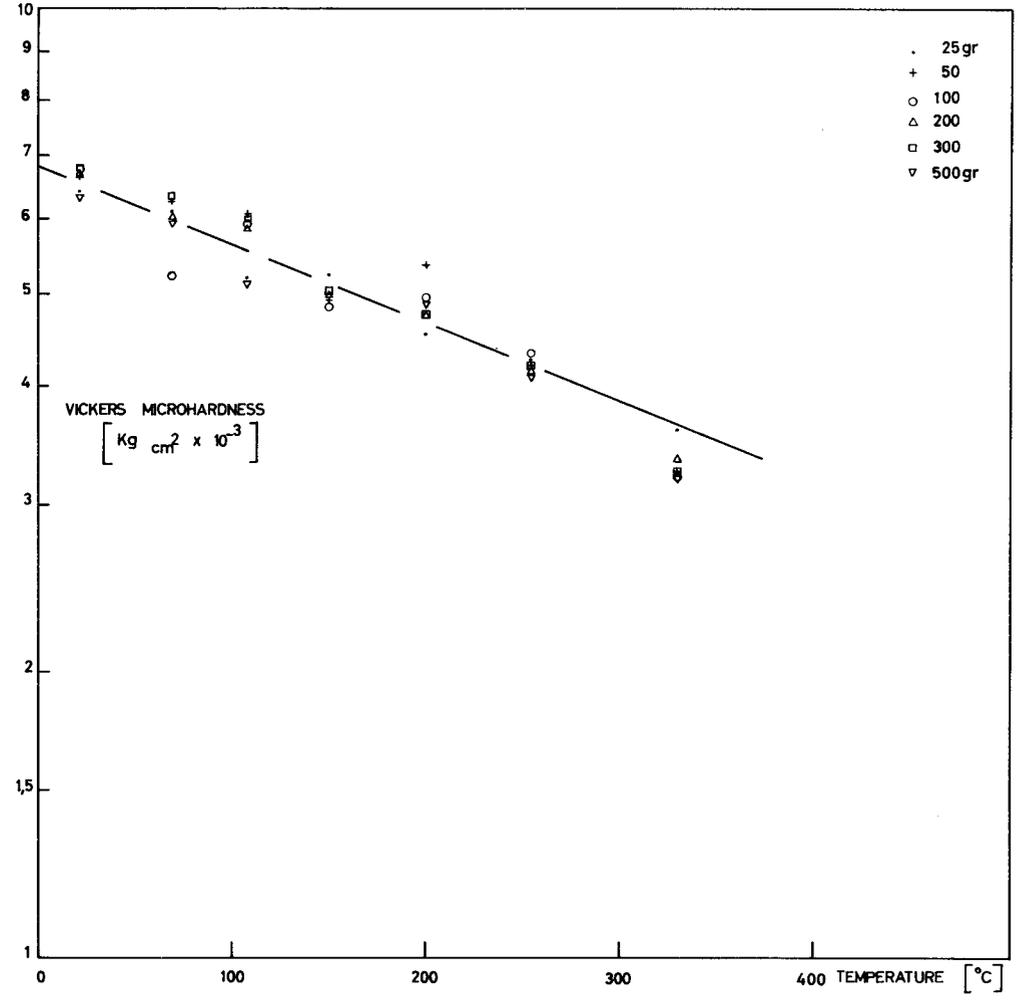
FIG. 39
7% SAP HARDNESS VS TEMPERATURE

To a first approximation, the square of this sum yielded a value for the real contact area, which, multiplied by the hardness, would give the apparent pressure.

In our case the hardness of SAP 7% determined experimentally was 2800 kg/cm^2 at 420 °C.

The number of the contact points per unit area are plotted in fig. 40 versus pressure. The slope of the experimental points is about 2/3 in line with results from ref. (9) and ref. (11).

Another graphical determination was based on ref. (11) to determine the number of the contact points versus pressure for the case of two rough surfaces in contact.

1) A recorded surface profile made on a transparent paper was approached to the record of the other surface profile till first contact was attained. A mean "datum line" was drawn through the contacts.

2) The areas A_1 and A_2 of all empty spaces were measured by a planimeter above and below the datum line.

3) The mean height of the empty space was calculated as

$$\zeta_1 = \frac{A_1}{a} \qquad \text{and} \qquad \zeta_2 = \frac{A_2}{a}$$

where a is the total record length.

4) A parallel to the "datum line" was drawn at a distance 2/3 of the smaller ζ , on the side of the datum line pertaining to the larger of the two ζ .

5) Along this line, the width of the voids was determined and the quantities

$$\alpha_1 = \frac{\sum l_1 \text{ void}}{a} \qquad \alpha_2 = \frac{\sum l_2 \text{ void}}{a}$$

were calculated.

The total volume of the voids on the 3-dimensional surface is

$$Vvoid = 2a \times A - overlap$$

The amount of overlapping is determined from the profile by

$$overlap = \alpha \times a^2 \times \rho$$

where the smaller ρ is chosen.
Substituting

$$Vvoid = aA \ (2-\alpha)$$

The mean height of the voids is calculated as

$$\rho'_1 = \frac{Vvoid}{a^2} = \frac{A_1}{a} \ (2-\alpha_1) \quad \text{and} \quad \rho'_2 = \frac{A_2}{a} \ (2-\alpha_2)$$

6) The relation $\delta = f(\rho')$

The distance δ from the base to the contact point is given by

$$\delta = \frac{\rho'_i}{1 - \dfrac{K_f}{K_i}}$$

where K_f and K_i are fluid and solid thermal conductivity. Since the ratio K_f/K_i is small, one can consider that

$$\rho'_1 \cong \delta_1 \quad \text{and} \quad \rho'_2 \cong \delta_2$$

7) To fluid thermal conductance can be calculated for $\varepsilon = 0$ by
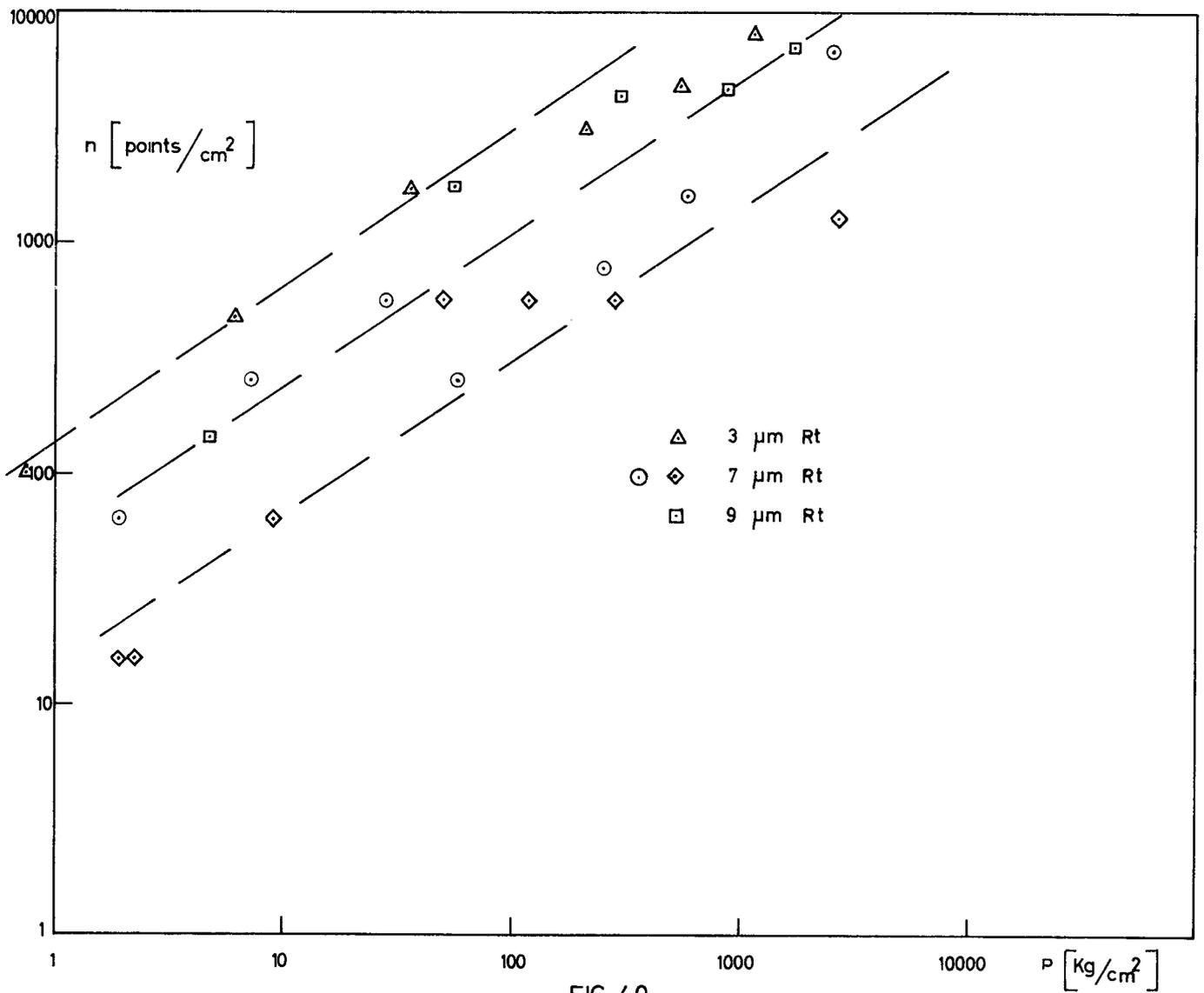
FIG. 40

DENSITY OF CONTACT SPOTS VS PRESSURE
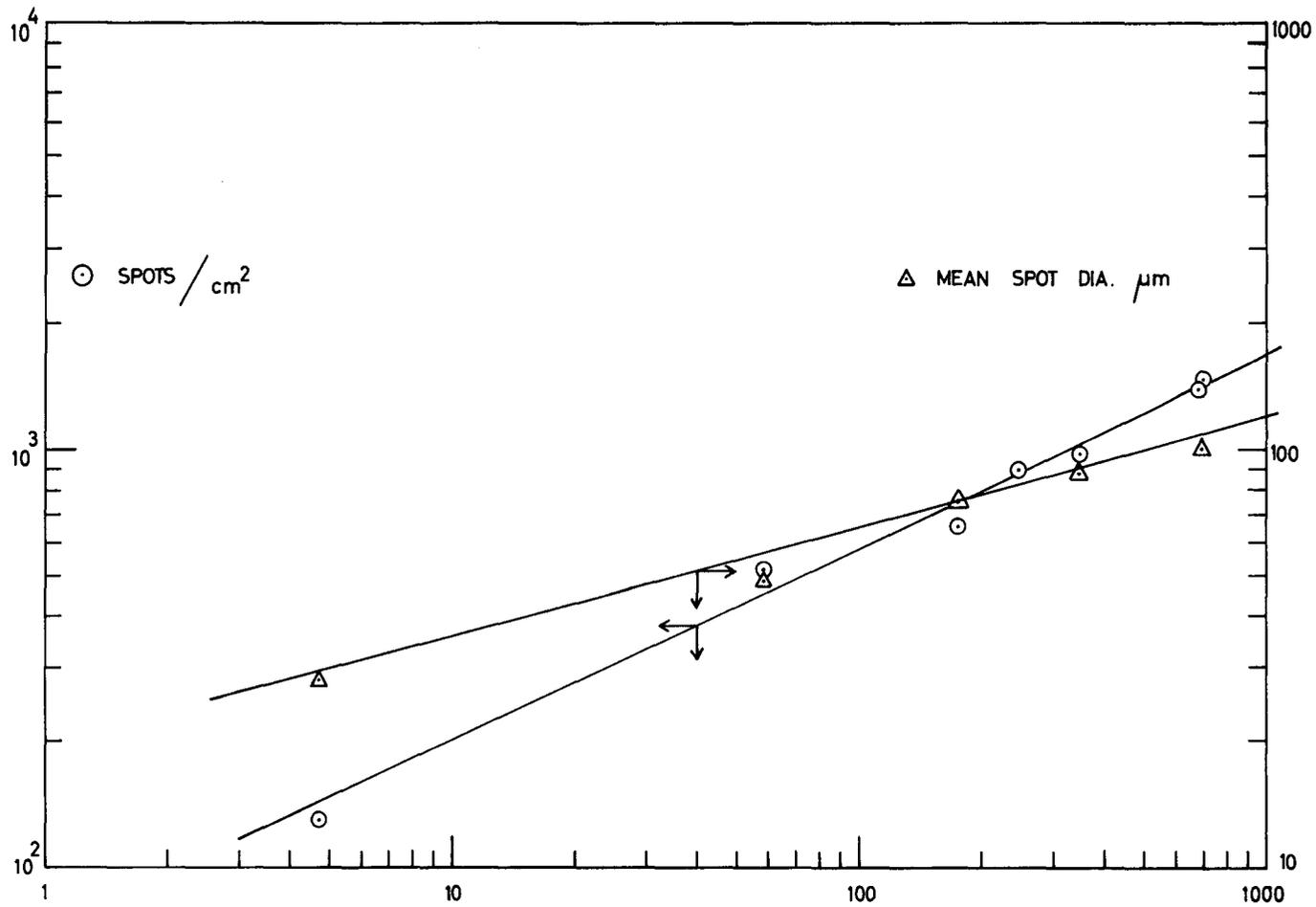
FIG. 41
DENSITY OF CONTACT SPOTS VS PRESSURE
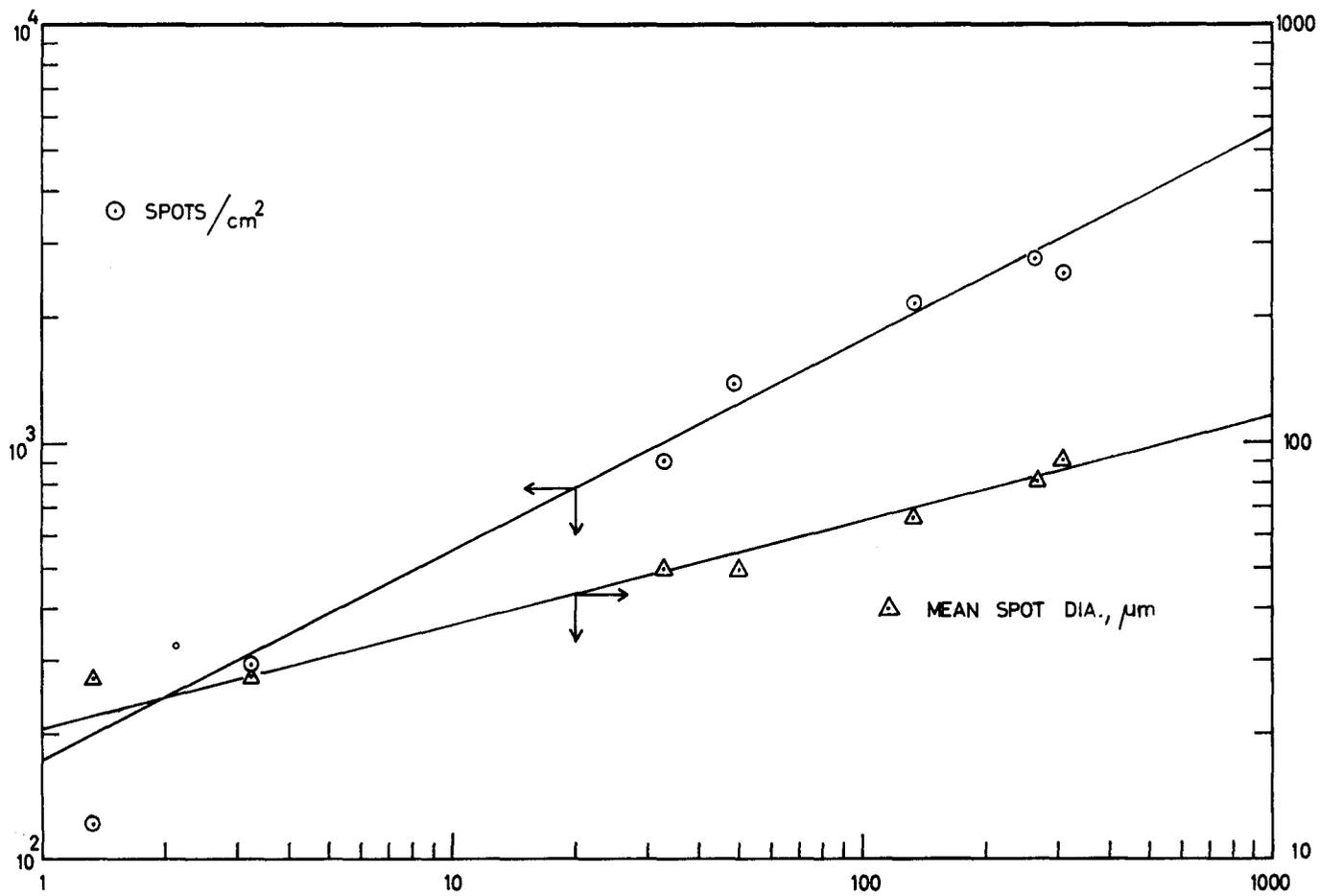
FIG.42
DENSITY OF CONTACT SPOTS VS PRESSURE

$$h = \frac{\dfrac{K_f}{\delta_1 + \delta_2}}{1 - \dfrac{K_f}{\delta_1 + \delta_2}\left(\dfrac{\delta_1}{K_1} + \dfrac{\delta_2}{K_2}\right)} \qquad (\text{Watt/cm}^2 \,^\circ C) \quad (T)$$

where K_1 and K_2 are the thermal conductivity of the two solids.

8) An increase of contact pressure is simulated by approaching the profile records to each other. To increase the randomness of contacts at each approaching step, a relative translation of the two profiles is operated; parallel to the datum line.

At each position of the datum line, the following were determined

        a) Number of contact points N

        b) Width of contacts along the datum line $(\Sigma 1)$
           The number of contact points per unit was given by

$$n = \frac{N^2}{a^2} = \frac{N^2 \times (\text{horizontal amplification})^2}{a^2}$$

The ratio ε^2 between the contact and the total area was

$$\varepsilon^2 = \frac{(\Sigma 1)^2}{a^2}$$

The applied pressure was

$$P = \varepsilon^2 H$$

where H is the hardness of the softer solid.

The method just described was applied to the profiles of test 32 (upper contact).

The surface finish was:

Armco $R_a = 5.42\ \mu m$ $R_t = 24\ \mu m$

SAP $R_a = 6.42\ \mu m$ $R_t = 25\ \mu m$

Assuming

Thermal conductivity of air 4 x 10^{-4} Watt/cm°C at 200°C

"   "   " Armco 0.55   Watt/cm°C at 300°C

"   "   " SAP   2   Watt/cm°C at 200°C

SAP hardness 4600 Kg/cm^2 at 200°C

The thermal conductance of the fluid calculated by Eq. (T) is h = 0.085 Watt/cm^2 °C. The number of contact points per unit area and the mean deameter of the points are plotted vs. the applied pressure in fig. 41.

This graphical method was applied to a UC-SAP surface as well using the recorded profile of a UC pellet and a fuel clad of the Metallurgy Dep't.

With

Thermal conductivity of UC   = 0.2 W/cm°C

"   "   SAP   = 2   W/cm°C

"   "   fluid(fission gases 5 Xe+1 K_r)

$$K = \left[\left(\frac{\sqrt{5}}{\sqrt{5} + \sqrt{1}} + 1.94 \right) + \left(\frac{\sqrt{1}}{\sqrt{1} + \sqrt{5}} \times 3.17 \right) \right] 4.18 =$$

= 0.97 x 10^{-4} Watt/cm°C

SAP HARDNESS = 2900 kg/cm^2 at 400°C

The thermal conductance of the fluid calculated by Eq. (T) is h = 0.0745 W/cm^2°C.

The number of the contact points per unit area and the mean diameter of the points are plotted vs. the applied pressure in fig. 42.

From fig. 41 and 42 it appears that the number of the contact points for unit area increases with a power of the applied pressure close to 0.5.

Appendix III

Data on the in-pile experiments

Description of the test assembly

The rig consited of three assemblies (see in Fig. 43 the rig sketch)
- A stainless steel shield plug tube and a cement shielding.
- The second containment consists of a stainless steel tube attached to the shield plug which carries the specimen can assemblies and a water jacket. Two heater type leak detectors are put at the bottom of the second containment to detect any inward water leak from the water jacket. The water jacket is a water annulus formed by the second containment and the water guide tube.

The water guide aluminium tube is attached to the second containment and the reactor heavy water enters the annulus at the bottom.

- The specimen can assembly consists in three SS capsules joined end to end by connecting posts and located into the second containment.

As shown in Fig. 44 to obtain a total fuel length of 3.81 cm, each can contains two UC pellets(1.906 cm nominal diameter) fitted into a SAP hollow cylinder walls 1.916 cm of inner and 4.93 cm outer nominal diameter contained into a 0.088 cm thick SS can.

The fuel was arc-fused natural uranium carbide with a carbon content 5%$\pm$0.1 weight.

Fuel pellets were produced by centreless grinding and care was taken to check that they conform as nearly as possible to a perfectly circular cross-section.

FIG. 43

SKETCH OF IRRADIATION RIG.

Labels (Fig. 43):
- CEMENT
- HE. TUBE
- THERMOCOUPLES TUBE
- LEAD
- S.S. SHIELD PLUG
- HEAVY WATER OUTLET
- S.S. SECOND CONTAINEMENT
- AL. WATER GUIDE TUBE
- S.S. CAPSULES
- LEAK DETECTORS
- HEAVY WATER INLET
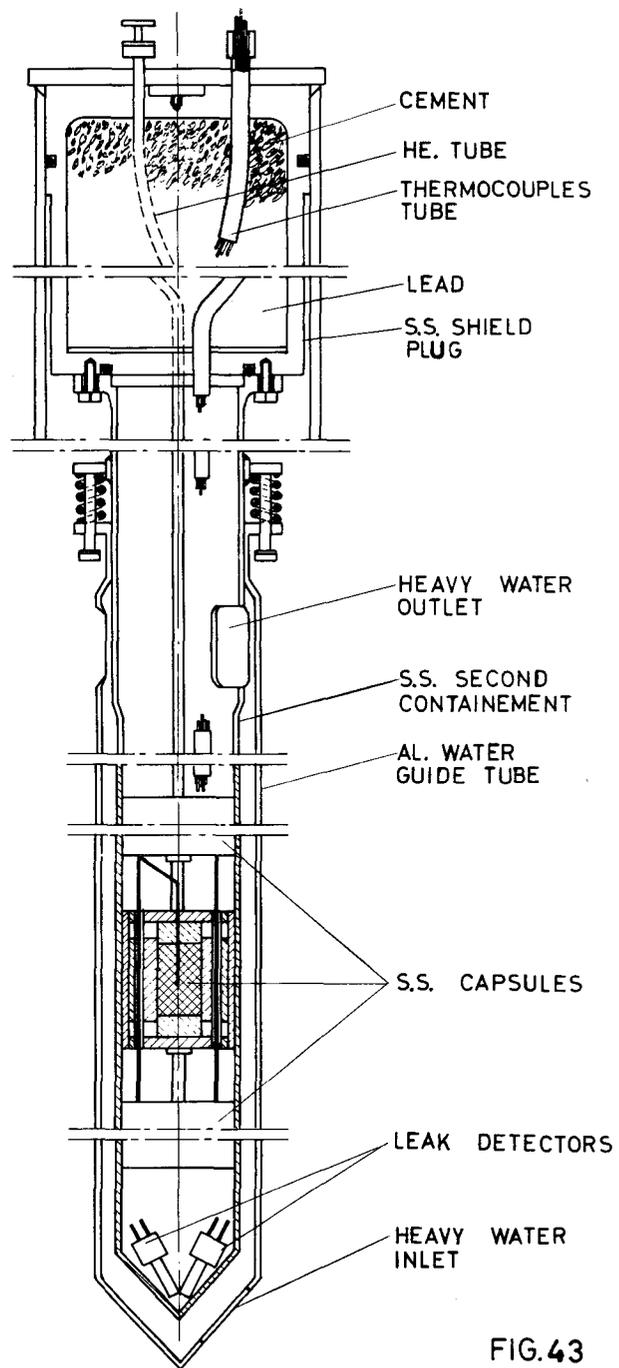
ARRANGEMENT OF THE SPECIMEN CAN

FIG. 44

Labels (Fig. 44):
- THERMAL GUARD PLUGS
- EXPERIMENTAL PELLET
- S.A.P.
- RADIATION SHIELD
- TERMOCOUPLES
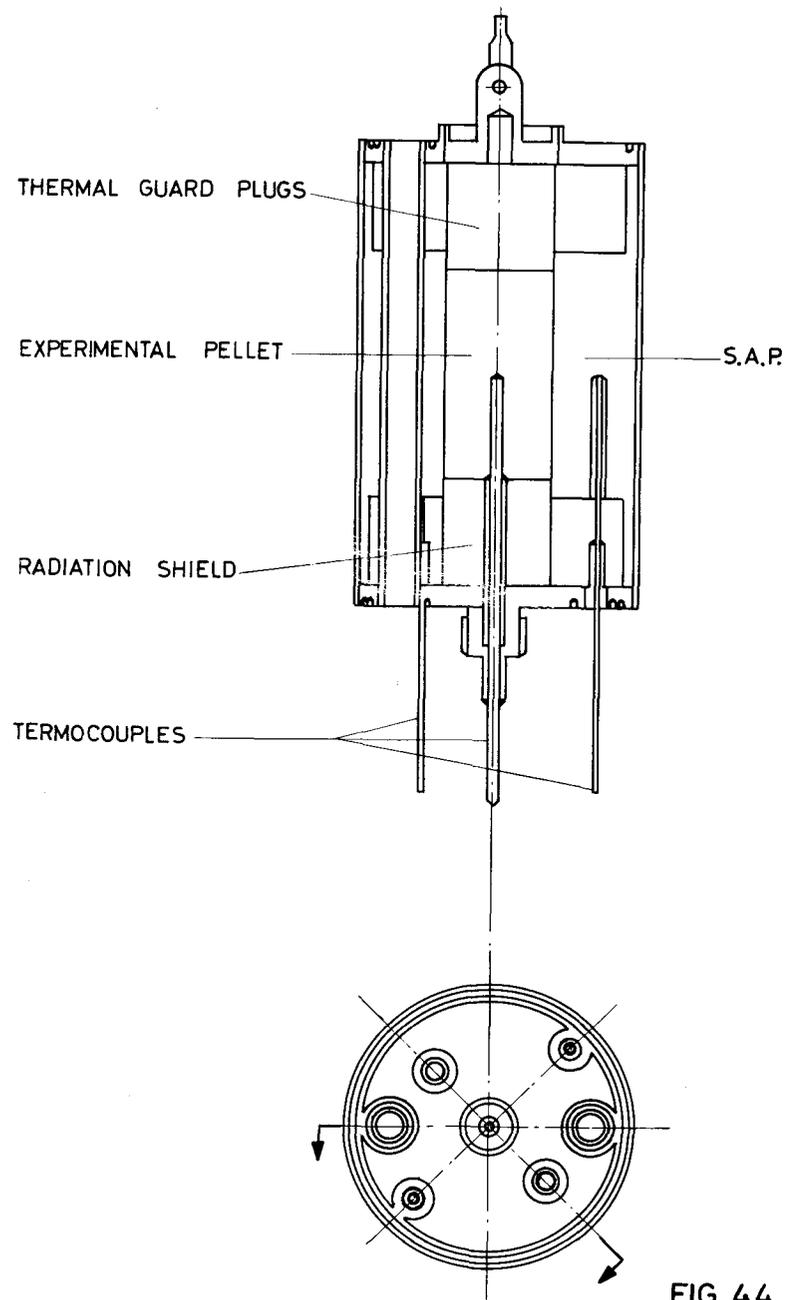
The finished diameter of each pellet was adjusted to satisfy the fuel-sheath clearances required.

Pellets of 98% MgO are located between each end cap of UC pellet to prevent heat losses.

The SS capsule is evacuated and filled with Xenon gas at atmospheric pressure.

Five thermocouples are located in each capsule to determine the radial heat flow through the specimen can.

The centre P_t - 10% Rh - P_t and four chromel-alumel thermocouples which are located in two pairs of holes at different radii, measure respectively the UC and SAP temperature.

Rig. 1

The experiment was irradiated in fuel channel
N° 9 during a given reactor cycle (2weeks) and operated
throughout the period without incident.

Expected values from previous calculations of the
facility showed that under normal operating conditions
the UC temperature would attain 1000°C and the SAP,
400°C approximatively. Infact, the temperature values
were about 600°C in the UC and 150°C in the SAP width
a maximum unperturbed thermal neutron flux of 4.5×10^{13}
n/cm^2 sec.

The low UC temperatures did not permit an expansion
of the UC pellets sufficient to obtain a contact between
UC and SAP. For this reason the rig was later re-irradia-
ted in the central channel to double the thermal neu-
tron flux.

During a second reactor cycle higher center tem-
peratures were achieved, but some thermocouples were
apparently damaged when the assembly was moved.

Infact, the only difficulty encountered was that
the central thermocouple of the can A and C became er-
ratic after 340 hours. The can A thermocouple was recon-
nected by pulling a vacuum in the outer container but
for a short time only.

During a third and fourth reactor cycle the ex-
periment yielded temperatures for a capsule only.

The total irradiation time was 1012 hours with
a thermal neutron flux of 6.9×10^{13} n/cm^2 sec.

Rig. 2

The experiment operated during two reactor
cycles. Total irradiation time was 816 hours
with a thermal neutron flux of 6.9×10^{13} n/cm^{2}sec.

The radial heat flow calculation method utili-
zed the temperature difference in the gap and the
heat thermal flux on the pellet surface to deter-
mine the gap conductance.

The heat generation in UC was known from the
values of the thermal neutron flux and the gamma hea-
ting rate.

The thermal neutron flux has been estimated
under the assumption that the heat transfer coeffi-
cient between SAP sheath and the cooling heavy wa-
ter is constant.

The differences between the mean SAP tempera-
ture and the inlet heavy water temperature during
the raising and falling steps of reactor power are
plotted in Fig. 45 as a function of the thermal
power.

Assuming a maximum unperturbed thermal flux at
the center of the reactor of 10×10^{13} n/cm^2 sec, its
variation law has been obtained by fitting the experi-
mental points with a parabolic expression of flux ver-
sus temperature difference:

$$\emptyset = 0.74\,\beta + 0.1\,\beta^2$$

where

$$\beta = \vartheta + 0.04\ T_r - 0.04\ Tw(mV)$$ is temperature
drop sheath - heavy water and $\vartheta = \dfrac{A+B+C+D}{4}$

The arithmetic mean readings for each can of the
SAP thermocouples mV.

DIFFERENCE BETWEEN SAP TEMPERATURE AND HEAVY WATER TEMPERATURE VS. THERMAL POWER

ΔT SAP-D₂0 mV

MW (FLUX ARBITRARY UNITS)

RIG. N°1

		CHANNEL 9	△
1 PERIOD	CENTRAL	CHANNEL	○
2 PERIOD	CENTRAL	CHANNEL	⊡
3 PERIOD	CENTRAL	CHANNEL	×

RIG N°2

| 1 PERIOD | CENTRAL | CHANNEL | · |
| 2 PERIOD | CENTRAL | CHANNEL | ⊙ |
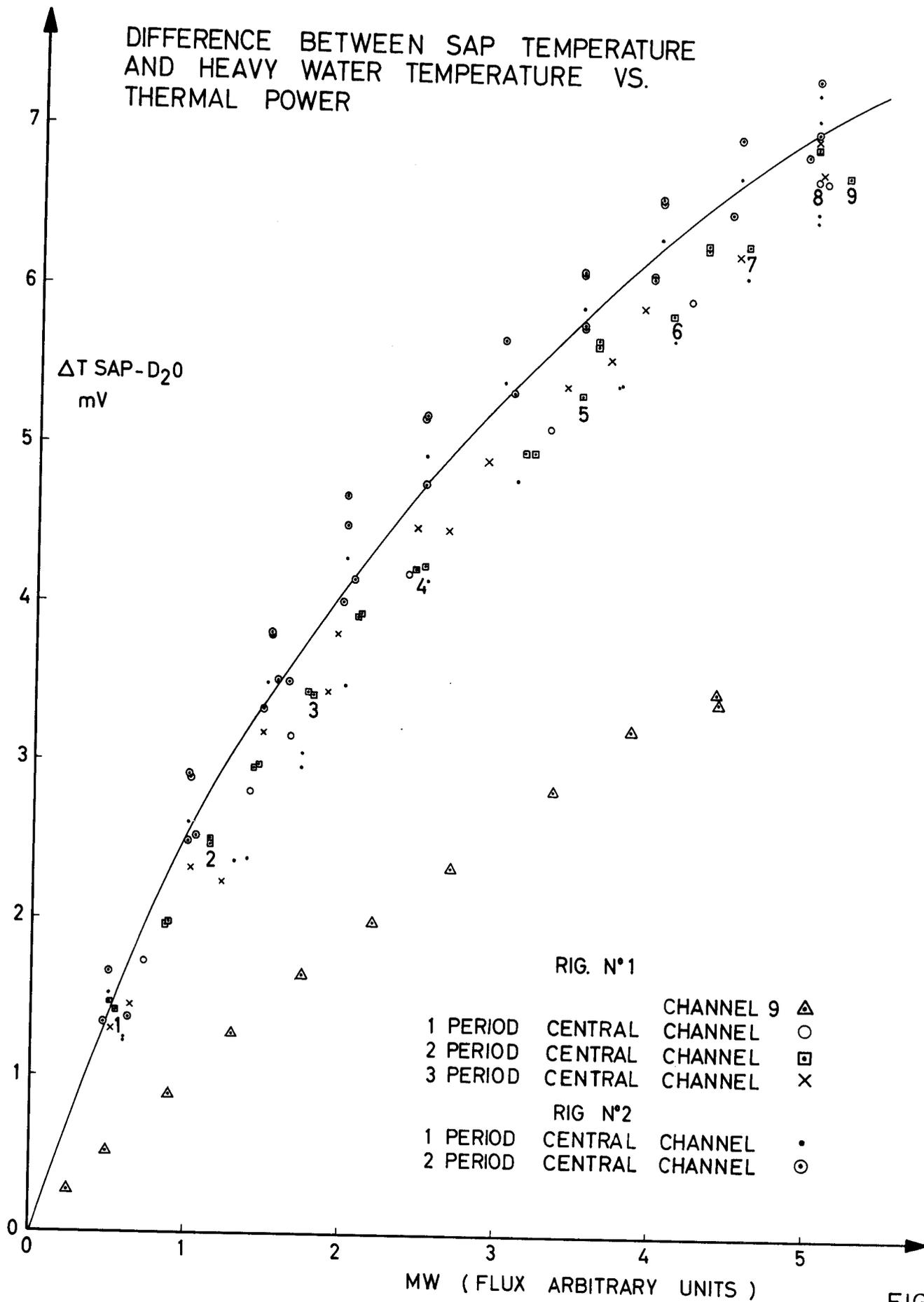
FIG.45

The gamma heating value used in the calcula-
tions has been determined experimentally by means
of the leak detector thermocouple readings. Howe-
ver the leak detector position was about 20 cm be-
low the last capsule in the rig. The gamma heating
in the assemblies was estimated to be about 10 per-
cent higher.

The method was the following:

The electrical supply of the leak detector was
switched off and the temperature increase due to the
gamma heating alone was measured as difference bet-
ween the leak detector and the heavy water temperatures.

The temperature rise ΔT_γ is plotted in Fig. 46
versus leak detector electrical power.

In reactor channel N° 9 with a perturbed neu-
tron flux of 3.66 x 10^{13}n/cm^2sec. the gamma heating
was 0.52 Watt/gr and in the central channel with a
perturbed neutron flux of 8.2 x 10^{13}n/cm^2sec, the
gamma heating was twice as much.

Gamma heating in all materials is assumed to
vary linearly with flux.

In reactor channel N° 9 with a perturbed neu-
tron flux 3.66 x 10^{13}n/cm^2sec, gamma heating is found
from Fig. 46 to be γ = 0.5 Watt/gr - Gamma heating rate
as a function of neutron flux is:

$$\gamma = \frac{0.5}{3.66} \phi = 0.1367 \phi$$
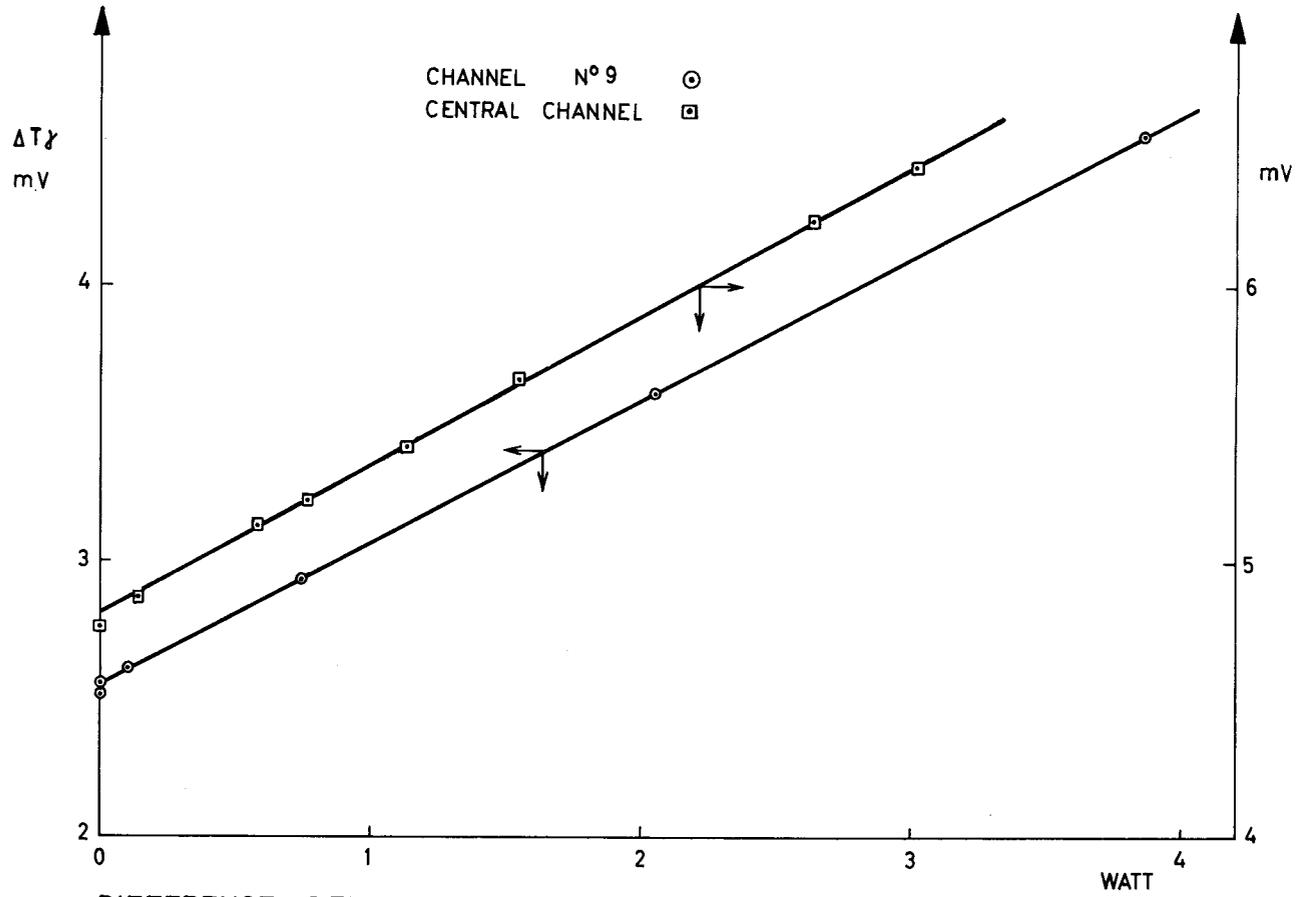
DIFFERENCE BETWEEN LEAK DETECTOR AND HEAVY
WATER TEMPERATURE VS. LEAK DETECTOR POWER

FIG. 46

Temperature difference between surface and centre
of the UC pellet

As noted previously, the UC centre temperature was measured with a Pt - 10% Rh - P_t thermocouple for each capsule.

The temperature difference between UC surface and centre is given by

$$\Delta T_o = \frac{P r_o^2}{4 K_o}$$

Heat generation rate as a function of the neutron flux is:

$$\frac{\emptyset \times 2.7 \times 238 \times \delta_o}{250} + \text{Pot} \, \gamma \times \delta_o = 36.23 \, \emptyset \, \text{Watt/cm}^3$$

Taking $K_o = 0.2$ Watt/cm°C Ref.(13)
and $r_o = 0.953$ cm

$$\Delta T_o = \frac{36.23 \times (0.953)^2}{4 \times 0.2} \emptyset = 41 \, \emptyset \, °C$$

UC surface temperature is:

$$T_o = T_c - \Delta T_o = T_c - 41 \, \emptyset \, °C$$

Temperature drops across SAP

As noted previously, the SAP temperature is measured with two pairs of Chromel/Alumel thermocouples at different radii. Temperature drop across SAP is given by:

$$\frac{- K_2 dT_2}{d\rho} \times 2\tilde{\pi}\rho = Q + \tilde{\pi}(\rho^2 - r_1^2) \times \delta_2 \times \text{Pot} \, \gamma$$

The total heat generated is:

$$Q = 36.23 \, \emptyset \, s_1 s_2 \, \widetilde{\pi} \, r_o^2 \ \text{Watt/cm}$$

where:

$s_1 = 0.9$ a factor accounting for the axial heat loss.

$s_2 = \dfrac{1.5}{1.75}$ a factor accounting for the ratio of the UC length to the SAP length.

Taking $K_2 = 1.45$ Watt/cm°C Ref. (16) and substituting:

$$\frac{-K_2 dT_2}{d\rho} \ 2\widetilde{\pi}\rho = \left[36.23 \times 0.9 \times \frac{1.5}{1.75} \times \widetilde{\pi} \times \overline{0.953}^2 + \right.$$
$$\left. + \widetilde{\pi} \, (\rho^2 - r_1^2) \times 2.8 \times 0.1367 \right] \emptyset$$

$$dT_2 = - \frac{d\rho}{\rho} \, (8.76 \, \emptyset + 0.1329 \, \emptyset \, \rho^2)$$

integrating

$$\Delta T_2 = - 8.76 \, \emptyset \int_{r_1}^{r_2} \frac{d\rho}{\rho} - 0.1329 \, \emptyset \int_{r_1}^{r_2} \rho \, d\rho =$$

$$= - 8.76 \, \emptyset \ln \frac{r_2}{r_1} - \frac{0.1239\emptyset}{2} \, (r_2^2 - r_1^2) =$$

$$= 8.6 \, \emptyset \ \text{°C}$$

SAP inner surface temperature

$$T_1 = \overline{T} + 4.3 \, \emptyset \ \text{°C}$$

SAP outer surface temperature

$$T_2 = \overline{T} - 4.3\ \emptyset\ {}^\circ C$$

Temperature drop across the gap

Temperature drops across the UC/SAP interface is:

$$\Delta T_1 = (T_c - 41\ \emptyset) - (\overline{T} + 4.3\ \emptyset) =$$

$$= T_c - \overline{T} - 45.3\ \emptyset$$

In our case $\Delta T_1 \geqslant 0$. This condition must be always verified.

Heat flux at the gap

The heat flux at the UC outer surface:

$$F = \frac{36.23\ \emptyset \times \widetilde{\pi}\ r_o^2}{2\widetilde{\pi} r_o} = 17.23\ \emptyset\ \text{Watt/cm}^2$$

Multiplying by $s = 0.9$ (axial conduction)

$$F = 15.5\ \emptyset$$

Gap conductance

It is given by the ratio of the gap heat flux and the temperature drops across the gap.

$$U = \frac{F}{\Delta T_1} = \frac{15.5\emptyset}{T_c - \overline{T} - 45.3\emptyset}\quad \text{Watt/cm}^2{}^\circ C$$

Fuel-sheath gaps caused by differential thermal expansion and contraction

In the case of a sheathed fuel, thermal cycling produces internal stresses which can cause cracking if the linear heat rating is not limited to a value below which cracking occurs.

The calculation of the operating gaps at each temperature is made by estimating the thermal expansion of the sheath and fuel material and by assuming that the fuel remains centered within the metal sheathing.

The hot gaps are:

$$G_1 = G_0 + \text{thermal expansion of SAP} - \text{thermal expansion of UC.}$$

UC pellet deformation

In a solid cylinder, radial and tangential stress are given by the expression Ref. (21)

$$\sigma_{rad} = \frac{\alpha E}{1-\nu} \left(\frac{1}{R^2} \int_o^R T \, r \, d \, r - \frac{1}{r^2} \int_o^r T \, r \, d \, r \right)$$

$$\sigma_{tang} = \frac{\alpha E}{1-\nu} \left(-T + \frac{1}{R^2} \int_o^R T \, r \, d \, r + \frac{1}{r^2} \int_o^r T \, r \, d \, r \right)$$

where R and r is the cylinder and generical radius respectively

T is the difference between a local and the surface temperature

F is Young's modulus

ν is Poisson's ratio

α is thermal expansion coefficient.

Temperature difference between centre and surface or at a generical position is respectively:

$$T_c - T_o = \frac{PR^2}{4K} \quad \text{or} \quad T_c - t = \frac{Pr^2}{4K}$$

substituting

$$T = t - T_o = \frac{P}{4 \, K} (R^2 - r^2)$$

where P = heat generation rate

K = thermal conductivity

By substitution

$$\sigma_{tang} = \frac{\alpha E}{1-\nu} \left[-\frac{P}{4 \, K} (R^2 - r^2) + \frac{1}{R^2} \int_o^R \frac{P}{4 \, K} (R^2 - r^2) r dr + \right.$$

$$+ \quad \frac{1}{r^2} \quad \int_0^r \quad \frac{P}{4\,K} \; (R^2 - r^2) \; r\partial r \; \Bigg]$$

Integrating

$$\mathbf{\mathcal{G}}_{tang} \;\; = \frac{P\,\mathbf{E}\,\boldsymbol{\alpha}}{(1-\boldsymbol{\nu})\,4K} \quad (\tfrac{3}{4}\,r^2 - \tfrac{1}{4}\,R^2)$$

At the surfaces for $R = r$

$$\mathbf{\mathcal{G}}_{tang} \;\; = \frac{P\boldsymbol{\alpha}\,ER^2}{8K(1-\boldsymbol{\nu})}$$

The elongation to the stress distribution

$$\boldsymbol{\mathcal{E}} = \frac{\mathbf{\mathcal{G}}(1-\boldsymbol{\nu}^2)}{E} \;\; = \;\; \frac{P\boldsymbol{\alpha}\,R^2(1+\boldsymbol{\nu})}{8K} = \;\; \frac{\boldsymbol{\alpha}\,\Delta T(1+\boldsymbol{\nu})}{2}$$

In the case of UC pellet the total elongation
is the sum of the thermal and the stress elongation

$$\boldsymbol{\mathcal{E}}_0 = \boldsymbol{\alpha}_0 \; (T_0-20) + \frac{\boldsymbol{\alpha}_0 \; \Delta T_0}{2} \; (1+\boldsymbol{\nu})$$

taking $\boldsymbol{\alpha}_0 = 10^{-5} \; {}^\circ C^{-1}$ and $\boldsymbol{\nu} = 0.3$

$$\boldsymbol{\mathcal{E}}_0 = 10^{-5}(T_0+0.65\,\Delta T_0 \; -20)$$

Substituting a temperature values as a function
of thermal neutron flux $\Delta T_0 = 41 \; \emptyset$ and $T_0 = T_c - 41 \; \emptyset$

$$\boldsymbol{\mathcal{E}}_0 = 10^{-15}T_c - 14.3 \times 10^{-5} \; \emptyset \; -2 \times 10^{-4}$$

Stainless steel container deformation

Thermal expansion of the SAP cylinder pressing against the container gives a stress Ref. (21)

$$\sigma = \frac{p\, r_3}{S}$$

The strain due to this stress is:

$$\varepsilon_3 = \frac{\sigma}{E_3} = \frac{p\, r_3}{E_3\, S}$$

The total strain is the sum of the thermal and the stress expansion:

$$\varepsilon_3 = \frac{p\, r_3}{E_3\, S} + \alpha_3(T_3 - 20)$$

Sheath cylinder deformation

Stresses in a thick cylinder pressed by an external pressure are given by the expression Ref. (21)

$$\sigma_{tang} = p\ \frac{b^2(a^2 + r^2)}{r^2\,(b^2 - a^2)} \qquad \text{(compression)}$$

$$\sigma_{rad} = p\ \frac{b^2(r^2 + a^2)}{r^2(b^2 - a^2)} \qquad \text{(compression)}$$

$$\sigma_{long} = 0$$

where b, a, r are outer, inner and generical radius, respectively.

Stress due to the thermal gradient Δ T in the same cylinder are given by the expression Ref.(21)

$$\sigma_{tang.out.} = \frac{\alpha E \Delta T}{2(1-\nu)\ln\frac{b}{a}} \quad (1- \frac{2a^2}{b^2 - a^2} \ln \frac{b}{a}) \quad \text{(traction)}$$

$$\sigma_{tang.inn.} = \frac{\alpha E \Delta T}{2(1-\nu)\ln\frac{b}{a}} \quad (1- \frac{2b^2}{b^2 - a^2} \ln \frac{b}{a}) \quad \text{(compression)}$$

$$\sigma_{long} = \sigma_{tang}$$

$$\sigma_{rad} = 0$$

Composition of strains due to the external pressure and the thermal gradient are given by

$$\varepsilon_x = \frac{\sigma_x}{E} - \frac{\nu \sigma_y}{E} - \frac{\nu \sigma_z}{E}$$

The total elongation of the cylinder outer and inner radii is the sum of the thermal and the composed expansions:

$$\varepsilon_{tot.outer.} = \alpha(T_{out}-20) + \frac{\Delta T \alpha}{2\ln\frac{b}{a}} \quad (1- \frac{2a^2}{b^2 - a^2} \ln \frac{b}{a}) -$$

$$- \frac{p}{E} \frac{a^2+b^2}{b^2-a^2} + \frac{\nu P}{E}$$

$$\varepsilon_{tot.inner.} = \alpha(T_{inn.} - 20) - \frac{\alpha \Delta T}{2\ln\frac{b}{a}}\left(1 - \frac{2b^2}{b^2 - a^2}\ln\frac{b}{a}\right) -$$

$$- \frac{p}{E}\ \frac{2b^2}{b^2 - a^2}$$

From the ratio

$$\frac{\text{SAP radius}}{\text{SS radius}} = \frac{1 + \varepsilon_{tot.}\ SS}{1 + \varepsilon_{tot.out.}\ SAP}$$

Substituting

$$\frac{r_2}{r_3} = \frac{1 + \alpha_3(T_3 - 20) + \dfrac{p\ r_3}{E_3 S}}{1 + \alpha_2(T_2 - 20) + \dfrac{\alpha_2 \Delta T_2}{2\ln\dfrac{r_2}{r_1}}\left(1 - \dfrac{2r_1^2}{r_2^2 - r_1^2}\ln\dfrac{r_2}{r_1}\right) + \dfrac{p}{E_2}\left(\nu_1 - \dfrac{r_1^2 + r_2^2}{r_2^2 - r_1^2}\right)}$$

Solving for the contact pressure

$$p = 1.38\ T_2 + 0.488\ \Delta T_2 - 0.934\ T_3 + 30.7$$

Substituting $T_2, \Delta T_2, T_3$ as a function of neutron flux

$$p = 0.466\ \overline{T} + 7.46\ \phi + 30.7$$

Combining and solving

$$\varepsilon_1 = 1.975 \times 10^{-5}\ \overline{T} + 18.37 \times 10^{-5}\phi - 5.5 \times 10^{-4}$$

The radial hot gap is given by

$$G_1 = G_0 + r_0(\varepsilon_1 - \varepsilon_0) = G_0 + 1.89 \times 10^{-5}\overline{T} - 3.4 \times 10^{-4} + 3.02 \times 10^{-4}\phi - 9.53 \times 10^{-6}T_c$$

UC swelling

Permanent swelling measurements were not possible till the capsules were dismantled. Swelling of the UC pellets was then estimated as a function of the gap thermal resistance, the gas thermal conductivity and the thermal expansions.

The hot clearance can be calculated by the expression.

$$\text{Hot clearance} = R \times K_3$$

where

R = gap thermal resistance $cm^2\,°C/Watt$

K_3 = gas thermal conductivity $Watt/cm\,°C$.

Assuming that the fuel remains centered within the metal sheathing, the hot clearance can be calculated by the difference between the residual clearance due to thermal expansion and the fuel swelling.

The expression

$$\text{hot clearance} = G_1 - \Delta r_o$$

where

G_1 is due to the thermal expansion

Δr_o is the fuel radial swelling.

Then

$$RK_3 = G_1 - \Delta r_o$$

and the fractional radial swelling is given by

$$\frac{\Delta r_o}{r_o} = \frac{G_1 - RK_3}{r_o}$$

Gas thermal conductivity

Since the capsules were filled with Xenon
gas at atmospheric pressure, the thermal conduc-
tivity of Xenon is calculated as a function of the
operating temperatures from

$$K_3 = 5.35 \times 10^{-7} (t + 273)^{.824} \qquad Ref.(17)$$

Experimental data analysis

The elaboration of the experimental data was made with
a 7090 IBM computer code using equations of appendix III.
Symbols used are explained hereafter.

Input data were

TT	= SAP thermocouple readings	mV
GAP	= nominal gap values	cm
TAMB	= room temperatures	°C
TUC	= UC thermocouple readings	°C
TAQ	= inlet heavy water temperature	°C
TEMP	= irradiation time values as referred to a maximum flux of $6.9 \times 10^{13} n/cm^2 sec.$ (hours)	
M	= Test readings number	
N	= specimen can number	
VT(N,M)	= SAP arithmetic mean temperature	mV
T(N,M)	= SAP true mean temperature	°C
DELT(N,M)	= Temperature drop sheath-heavy water	mV
FI(N,M)	= thermal neutron flux	
GAMMA(N,M)	= Gamma heating as a function of the neutron flux	
TUCS(N,M)	= UC pellet surface temperature as a function of the neutron flux	
TS(N,M)	= SAP sheath inner surface temperature as a function of the neutron flux	
POT(N,M)	= Gap heat flux as a function of neutron flux	
COND(N,M)	= thermal gap conductance	$Watt/cm^2 °C$
A(N,M)	= thermal gap resistance	$cm^2 °C/Watt$
GAC(N,M)	= hot gap due to the thermal expansion and the thermal stress	cm
TX	= arithmetic mean temperature between UC and SAP inner surface	
XKX	= Xenon gas thermal conductivity as a function of the operating temperatures	Watt/cm°C
SW(N,M)	= UC pellet radial percent swelling	

The output data included

- thermal neutron flux
- gap heat flux
- thermal gap conductance
- hot gap due to thermal expansions
- UC pellet centre temperature
- SAP sheath arithmetic mean temperature
- thermal gap resistance
- UC pellet radial percent swelling
- irradiation times

$n/cm^2 sec.$
$Watt/cm^2$
$Watt/cm^2 \circ C$
cm
$\circ C$
$\circ C$
$cm^2 \circ C/Watt$

hours

Results are printed as follows

In the first line = Test number and corresponding irradiation time.

In the following three lines = Output data for can A, B and C respectively.

In the last three lines = Input experimental data in the same order

The same code was used to elaborate the data of the second irradiated rig. However the following symbols were changed:

1) TSAP (N,M) = TS (N,M)
2) FLGAP (N,M)= POT (N,M)
3) RES (N,M)= A (N,M)

Comparing the two programs listed hereafter one can see that the gap value is omitted in the input data of the second rig because the three capsules were built with an equal nominal clearance.

ANALISI IRRAGGIAMENTO CONTATTO

```
C      PRIMO RIC
C      LE PROVE DA 1 A 24 SONO LA SALITA,DA 25 A 65 ILPLATEAU,DA 66 A 79
C     1LA DISCESA NEL 9. DA 80 A 92 LA SALITA,DA 93 A 114 IL PLATEAU,DA 1
C     215 A 120 LA DISCESA NEL CENTRALE,PRIMO PERIODO.DA 121 A 134 SALITA
C     3,DA 135 A 155 PLATEAU,DA 156 A 167 DISCESA DEL SECONDO PERIODO.DA
C     4168 A 175 SALITA,DA 176 A 196 PLATEAU,DA 197 A 205 DISCESA TERZO P
C     5ERIODO
C
       DIMENSION TT(5,3,205),GAP(3),TAMB(210),TUC(3,206),TAQ(210),TEMP(21
      10),VT(3,210),T(3,210),DELT(3,210),FI(3,210),GAMMA(3,210),TUCS(3,21
      20),TS(3,210),POT(3,210),COND(3,210),GAC(3,210),A(3,210),SW(3,210),
      3GACVE(3,210)
C
       READ INPUT TAPE 5,10,TT
   10 FORMAT (5E12.4)
       READ INPUT TAPE 5,20,GAP
   20 FORMAT (3E12.4)
       READ INPUT TAPE 5,30,TAMB
   30 FORMAT (5E12.4)
       READ INPUT TAPE 5,40,TUC
   40 FORMAT (6E12.4)
       READ INPUT TAPE 5,50,TAQ
   50 FORMAT (6E12.4)
       READ INPUT TAPE 5,55,TEMP
   55 FORMAT (6F10.2)
       DO 70 M=1,205
       DO 60 N=1,3
       VT(N,M)=(TT(1,N,M)+TT(2,N,M)+TT(3,N,M)+TT(4,N,M))/4.
       T(N,M)=24.4*VT(N,M)+0.976*TAMB(M)
       DELT(N,M)=VT(N,M)-0.04*TAQ(M)+0.04*TAMB(M)
       FI(N,M)=0.74*DELT(N,M)+0.1*DELT(N,M)**2.
       FI(N,M)=0.5*FI(N,M)
   60 CONTINUE
   70 CONTINUE
       DO 65 K=1,3
       FI(K,29)=FI(K,28)
       FI(K,30)=FI(K,28)
       FI(K,31)=FI(K,28)
       DO 64 J=41,45
       FI(K,J)=FI(K,40)
   64 CONTINUE
       FI(K,93)=FI(K,92)
       FI(K,94)=FI(K,92)
       DO 666 J=104,106
  666 FI(K,J)=FI(K,103)
       FI(K,109)=FI(K,108)
   65 CONTINUE
       DO 71 M=1,205
       DO 62 N=1,3
       GAMMA(N,M)=0.1367*FI(N,M)
   66 TUCS(N,M)=TUC(N,M)-41.*FI(N,M)
       TS(N,M)=T(N,M)+4.3*FI(N,M)
       POT(N,M)=15.5*FI(N,M)
       COND(N,M)=POT(N,M)/(TUCS(N,M)-TS(N,M))
       A(N,M)=1./COND(N,M)

       GAC(N,M)=GAP(N)+1.89E-5*T(N,M)-3.4E-4+3.02E-4*FI(N,M)-9.53E-6*TUC(
      1N,M)
   77 TX=(TUCS(N,M)+TS(N,M))*0.5
   73 XKX=5.35E-7*(TX+2.73E2)**0.824
  778 GACVE(N,M)=A(N,M)*XKX
   79 SW(N,M)=(GACVE(N,M)-GAC(N,M))*1.05
  795 SW(N,M)=(-SW(N,M))
   62 CONTINUE
       WRITE OUTPUT TAPE 6,80,M,TEMP(M)
   80 FORMAT (1H0,37X,14HPROVA NUMERO  ,I3,5X,7HTEMPO  ,F8.2,///)
       WRITE OUTPUT TAPE 6,90
   90 FORMAT (1H ,11X,96HFLUSSO NEUTR  FLUSSO TER    CONDUTT       GAP
      1    TEMP CARB    TEMP SAP    RESIS TERM   SWELLING,///)
       WRITE OUTPUT TAPE 6,100,(FI(N,M),POT(N,M),COND(N,M),GAC(N,M),TUC(N
      1,M),T(N,M),A(N,M),SW(N,M),GACVE(N,M),N=1,3)
  100 FORMAT (1H ,11X,9E12.4)
       WRITE OUTPUT TAPE 6,200
  200 FORMAT (1H ,6X,6HMV SAP,17X,3HGAP,11X,3HTUC,6X,4HTAMB,7X,3HTAQ,6X,
      14HTEMP///)
       WRITE OUTPUT TAPE 6,101,((TT(I,N,M),I=1,5),GAP(N),TUC(N,M),TAMB(M)
      1,TAQ(M),TEMP(M),N=1,3)
  101 FORMAT (1H ,5F5.2,E12.4,4F10.2)
   71 CONTINUE
       CALL EXIT
       END(1,0,0,0,0,0,0,0,0,0,0,0,0,0,0)
```

```
      SECONDO RIG

C     1LE PROVE DA 1 A 19 SONO LA SALITA,DA 19 A 63 IL PLATEAU,DA 63 A 72
C     2 LA DISCESA DEL PRIMO PERIODO LE PROVE DA 73 A 76 SONO LA PRIMA SA
C     3LITA,DA 77 A 94 LA SECONDA SALITA,DA 94 A 123 IL PLATEAU,DA 123 A
C     4142 LA DISCESA DEL SECONDO PERIODO
C
      DIMENSION TT(4,3,142),TAMB(144),TUC(3,142),TAQ(144),VT(3,144),T(3,
     1144),FI(3,144),GAMMA(3,144),TUCS(3,144),TSAP(3,144),FLGAP(3,144),C
     2OND(3,144),RES(3,144),GAC(3,144),SW(3,144),TEMP(144),DELT(3,144),G
     3ACVE(3,144)
      READ INPUT TAPE 5,10,TT
10    FORMAT (4E12.4)
      READ INPUT TAPE 5,20,TAMB
20    FORMAT (6E12.4)
      READ INPUT TAPE 5,30,TUC
30    FORMAT (6E12.4)
      READ INPUT TAPE 5,40,TAQ
40    FORMAT (6E12.4)
      READ INPUT TAPE 5,41,TEMP
41    FORMAT (6F10.2)
      DO 50 M=1,142
42    DO 60 N=1,3
      VT(N,M)=(TT(1,N,M)+TT(2,N,M)+TT(3,N,M)+TT(4,N,M))/4.
44    T(N,M)=24.4*VT(N,M)+0.976*TAMB(M)
      DELT(N,M)=VT(N,M)-4.E-2*TAQ(M)+4.E-2*TAMB(M)
      FI(N,M)=0.74*DELT(N,M)+0.1*DELT(N,M)**2.
45    FI(N,M)=0.5*FI(N,M)
60    CONTINUE
50    CONTINUE
      DO 70 M=1,142
62    DO 80 N=1,3
      GAMMA(N,M)=1.367E-1*FI(N,M)
64    TUCS(N,M)=TUC(N,M)-41.*FI(N,M)
      TSAP(N,M)=T(N,M)+4.3*FI(N,M)
66    FLGAP(N,M)=15.5*FI(N,M)
      COND(N,M)=FLGAP(N,M)/(TUCS(N,M)-TSAP(N,M))
68    RES(N,M)=1./COND(N,M)
      GAC(N,M)=1.5E-3+1.89E-5*T(N,M)-3.4E-4+3.02E-4*FI(N,M)-9.53E-6*TUC(
     1N,M)
77    TX=(TUCS(N,M)+TSAP(N,M))*0.5
78    XKX=5.35E-7*(TX+2.73E2)**0.824
778   GACVE(N,M)=RES(N,M)*XKX
79    SW(N,M)=(GACVE(N,M)-GAC(N,M))*1.05
795   SW(N,M)=(-SW(N,M))
80    CONTINUE
      WRITE OUTPUT TAPE 6,85,M,TEMP(M)
85    FORMAT (1H0,37X,14HPROVA NUMERO  ,I3,5X,7HTEMPO  ,F8.2,///)
      WRITE OUTPUT TAPE 6,90
90    FORMAT (1H ,11X,96HFLUSSO NEUTR  FLUSSO TER    CONDUTT       GAP
     1     TEMP CARB     TEMP SAP    RESIS TERM   SWELLING,///)
      WRITE OUTPUT TAPE 6,100,(FI(N,M),FLGAP(N,M),COND(N,M),GAC(N,M),TUC
     1(N,M),T(N,M),RES(N,M),SW(N,M),GACVE(N,M),N=1,3)
100   FORMAT (1H ,11X,9E12.4)
      WRITE OUTPUT TAPE 6,200
200   FORMAT (1H ,6X,6HMV SAP,14X,3HTUC,6X,4HTAMB,7X,3HTAQ,6X,4HTEMP///)
      WRITE OUTPUT TAPE 6,101,((TT(I,N,M),I=1,4),TUC(N,M),TAMB(M),TAQ(M)

     1,TEMP(M),N=1,3)
101   FORMAT (1H ,4F5.2,4F10.2)
70    CONTINUE
      CALL EXIT
      END(1,0,0,0,0,0,0,0,0,0,0,0,0,0,0)
```

Notation for Appendix III

r_o	–	UC pellet radius	– 0.953 cm
r_1	–	SAP inner radius	– 0.958 cm
r_2	–	SAP outer radius	– 2.465 cm
r_3	–	SS container inner radius	– 2.464 cm
ρ	–	Generical radius	
s	–	SS container thickness	– 0.088 cm
α_o	–	UC thermal expansion coefficient	– 10×10^{-6}/°C ref. (13)
α_2	–	SAP " " "	– 21.5×10^{6}/°C ref. (14)
α_3	–	SS " " "	– 14.5×10^{-6}/°C
E_o	–	UC Young's modulus	– 2.2×10^{6} kg/cm^2 ref.(13)
E_2	–	SAP " "	– 0.6×10^{6} kg/cm^2 ref.(14)
E_3	–	SS " "	– 2×10^{6} kg/cm^2
ν_3	–	Poisson's ratio	– 0.3
ν_1	–	SAP " "	– 0.4 ref.(15)
δ_o	–	UC density	– 13.4 gr/cm^3 ref.(13)
δ_2	–	SAP "	– 2.8 gr/cm^3 ref.(14)
T_c	–	UC centre temperature	– °C
T_o	–	UC surface "	– °C
T_1	–	SAP inner surface temperature	– °C
T_2	–	SAP outer " "	– °C
T_3	–	SS inner " "	– °C
$\bar{T}$	–	SAP mean temperature	– °C
t	–	UCT_o-SAP, mean surface temperature	– °C
T_r	–	room temperature	– °C
T_w	–	inlet temperature of heavy water	– °C
ΔT_o	–	temperature difference between centre and surface of UC pellet	– °C

ΔT_2 — temperature drop across SAP — °C

ΔT_1 — temperature drop across UC-SAP interface — °C

G_o — nominal radial clearance UC-SAP — cm

G_1 — hot radial clearance UC-SAP — cm

ε_o — radial deformation of UC pellet-surface

ε_1 — " " of SAP inner surface

ε_2 — " " of SAP outer surface

ε_3 — " " of SS inner surface

p — pressure to SAP-SS container inter face — kg/cm^2

$\emptyset$ — neutron flux — $n/cm^2 sec$

P — power generation in the UC — $Watt/cm^3$

Q — linear heat rating for UC — $Watt/cm$

Pot γ — gamma heating — $Watt/gr$

F — heat flux at UC surface — $Watt/cm^2$

U — contact or gap thermal conductance — $Watt/cm^2 {}^oC$

R — contact or gap thermal resistance — $cm^2 {}^oC/Watt$

K_o — UC thermal conductivity — $Watt/cm {}^oC$ ref. (13)

K_2 — SAP thermal conductivity — $Watt/cm {}^oC$ ref. (16)

K_3 — Xenon gas thermal conductivity — $Watt/cm {}^oC$ ref. (17)

$\dfrac{\Delta r_o}{r_o}$ — swelling of UC pellet

γ — arithmetic mean of SAP readings defined by $\gamma = \dfrac{A+B+C+D}{4}$ mV

β — sheath-heavy temperature drop water defined by

$$\beta = \gamma + 0.04\ T_r - 0.04\ T_w$$

Appendix IV

<u>Physical constants in the sample computations</u>

= Gas Thermal conductivity

	Fission gases	Helium	
Kf	9.7×10^{-5}	2.5×10^{-3}	W/cm°C

= Sum of max. microroughnesses

ΣR_t $\quad 2 \times 10^{-3}$ $\qquad$ cm

= Maximum microroughness

R_t $\quad 1.8 \times 10^{-3}$ $\qquad$ cm

= Pellet radius

	UC	UO_2	
r_o	1.5	0.5	cm

= Fuel Density

	UC	UO_2	
d	13.4	10.2	g/cm^3

= Average fuel Thermal conductivity

	UC	UO_2	
Kp	0.15	0.03	W/cm°C

= Fuel Thermal expansion coefficient

	UC	UO_2	
α_p	10^{-5}	10^{-5}	°C^{-1}

= Poisson's ratio

$$\nu_p = \nu_c = 0.3$$

= Fuel Young's modulus

	UC	UO_2	
E_p	2×10^6	2×10^6	Kg/cm^2

= Clad operating Temperature

$$\bar{T} = 400°C$$

= Microhardness at operating temperature

	SAP	MAGNOX	ZRY2	S.S.	
H	3,200	1,050	2,900	5,400	Kg/cm^2

= Clad Thermal conductivity

	SAP	MAGNOX	ZRY2	S.S.	
K_c	1.7	1.25	0.17	0.16	$W/cm°C$

= Clad Thermal expansion coefficient

	SAP	MAGNOX	ZRY2	S.S.	
α_c	2×10^{-5}	2.6×10^{-5}	1.17×10^{-5}	1.5×10^{-5}	$°C^{-1}$

= Clad Young's modulus

	SAP	MAGNOX	ZRY2	S.S.	
E_c	5×10^5	1.4×10^5	6.44×10^5	1.45×10^6	kg/cm^2

Acknowledgement