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FINAL SUMMARY REPORT
THE DEVELOPMENT AND TESTING OF UO_2
FUEL SYSTEMS FOR
WATER REACTOR APPLICATIONS

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For Period

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Prepared by:

W. P. Chernock
A. J. Anthony
C. E. Burdg
J. J. Koziol
W. J. Mordarski
E. I. Veil
G. Zuromsky

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FOREWORD

The United States and the European Atomic Energy Community (EURATOM) on May 29 and June 18, 1958, signed an agreement which provides a basis for cooperation in programs for the advancement of the peaceful applications of atomic energy. This agreement, in part, provides for the establishment of a Joint U. S. -Euratom Research and Development Program which is aimed at reactors to be constructed in Europe under the Joint Program.

The work described in this report represents the Joint U. S. -Euratom effort which is in keeping with the spirit of cooperation in contributing to the common good by the sharing of scientific and technical information and minimizing the duplication of effort by the limited pool of technical talent available in Western Europe and the United States.

I. INTRODUCTION

This Research and Development Program, sponsored by the United States Atomic Energy Commission and EURATOM, was initiated on July 1, 1961, and was completed on June 30, 1965. Results of earlier work performed on Contract AT(30-1)-2379, as part of the United States Atomic Energy Commission Fuel Cycle Development Program, have been summarized in CEND-88 and CEND-141. CEND-88 includes results of evaluations of economic incentives as well as limited experimental efforts in the following areas: reduction of cladding thickness, swaged elements, vibratory compaction, performance characteristics, rock-riting, zoned pellet enrichments, cored elements, tubular elements, progressive layer assembly, inspection of tube-to-tube joints, and end cap closures. CEND-141 contains a summary of progress of experimental efforts on vibratory compaction, swaging, rock-riting, progressive layer assembly, reduction of cladding thickness and performance characteristics of UO_2 fuel elements.

This report contains a summary of effort performed on Contract AT(30-1)-2863 as part of the Joint United States - EURATOM Research and Development Program. Previous reports of progress under this contract are listed below in the order of their issuance.

<u>Report Number</u>	<u>Period</u>
CEND-149	July to September 1961
CEND-152	October to December 1961
CEND-156	January to March 1962
CEND-157	July 1, 1961 to June 15, 1962
CEND-171	July 1 to September 30, 1962
CEND-172	Period Ending December 31, 1962
CEND-180 (Trip Report)	July 16 to July 30, 1962

<u>Report Number</u>	<u>Period</u>
CEND-178	Period Ending March 31, 1963
CEND-186	June 15, 1962 to July 31, 1963
CEND-199	Period Ending September 30, 1963
CEND-204	Period Ending December 31, 1963
CEND-2863-211	January 1, 1964 to March 31, 1964
CEND-2863-217	April 1 to July 31, 1964
CEND-2863-236	Period Ending March 31, 1965

In addition to the above progress reports, the Joint United States - EURATOM Research and Development Program sponsored a Symposium on Powder Packed Uranium Dioxide Fuel Elements which was held at Combustion Engineering, Inc. on November 30 and December 1, 1961. Twenty-five papers, representing fourteen laboratories, were presented. Summaries of these presentations together with the original presentations are contained in CEND-153.

II. SUMMARY

A. Powder Compaction

The objective of this program on powder compaction was the reduction in overall fuel cycle costs for fuel elements containing UO_2 . A number of compaction methods were evaluated and the results of these investigations are summarized below.

Although the results of economic studies indicated incentives for the use of powder compacted fuel, the fact that the nuclear industry has not yet become heavily committed to this form of UO_2 fuel elements is probably due to the greater current demonstrated experience with pelletized fuel and/or the probability that these lower cost incentives are not being fully realized.

1. Vibratory Compaction

The mechanical aspects of vibratory compaction have been investigated and optimized. As a result of a number of efforts to provide densities above 90% of theoretical, it was concluded that such densities are attainable only under special conditions which result in higher fuel cycle costs and lower fuel powder utilization than conditions required for lower densities. It was concluded that a density in the range of 88 to 89% of theoretical was the optimum density attainable by simple, rapid and economical production techniques and that fuel powder utilization was at a maximum for densities in this range and below. Thus, the range of 88 to 89% of theoretical density represents the highest density commensurate with low fuel cycle costs for elements fabricated by vibratory compaction.

2. Multiple Rod Compactions

Groups of up to thirty-seven rods were simultaneously compacted to a density in the range of 84-87% of theoretical. The compaction time increased as the number of rods increased. However, the per tube compaction time was approximately three to four minutes. The simultaneous compaction of a group of tubes or a cluster offers a potential advantage for the fabrication of second generation fuel element clusters in that the fuel handling time may be minimized.

3. Vibratory Compaction of Nuclear Superheat Critical Fuel Elements

Development work performed as part of this program was utilized as the basis for fabrication of fuel elements of complex geometry for nuclear superheat critical experiments performed as part of the USAEC Nuclear Superheat Program, Contract AT(11-1)-795. In this effort, the potentiality and versatility of vibratory compaction was demonstrated. This effort demonstrated that fuel elements of annular or multi-tube (Figures A-10 and A-11) configurations can be fabricated by vibratory compaction. It was further demonstrated that vibratory compaction, as a method of fuel fabrication, permitted the transfer of fuel from one critical experiment to another with no fuel reprocessing and very little fuel loss. Thus, a total of 579 double annular fuel elements which were used in one critical experiment, were unloaded and this same fuel was reloaded into 105 multi-tube fuel elements.

4. Swaging

Fuel densities of 92-93% of theoretical were reproducibly obtained in fuel elements fabricated by vibratory compaction followed by swaging. Results indicate that rods initially compacted to 84% of theoretical density and reduced in area 12-15% by swaging at room temperature provide the optimum economical fabrication procedure. It appears that this process can become an economic

competitor of pelletized UO_2 .

5. Rock-Riting

A large development effort would be required to establish a feasible process for making UO_2 filled fuel rods in thin-walled cladding by Rock-Riting. However, before such an effort is pursued, economic incentives for Rock-Riting should be demonstrated.

B. Thermal Performance Characteristics of UO_2

The thermal conductivity of UO_2 (at 93.5% of theoretical density) was determined for temperatures up to 2200°C using a high thermal gradient radial heat flow technique. This method was capable of providing, in nominal 0.5 in. diameter rods, maximum fuel temperatures of 2500°C concurrent with cladding surface temperatures close to 100°C . Earlier results on this program indicated a rise in UO_2 thermal conductivity with increasing temperature above 1500°C . However, the latest and what we believe to be the most reliable data do not indicate this rise in conductivity. Detailed analyses of test results and assumptions indicated that, for this method of conductivity measurement, the selection of the proper values for the fuel-to-heater interface conductance strongly influenced the shape of the conductivity-temperature curve at high temperatures. A gap conductance which varies with temperature is the most precise assumption which could be employed. This gap conductance would become very high at the heater temperatures used for the high temperature conductivities determined on this program. Thus, the assumption of near infinite heater-fuel gap conductance most closely simulates true conditions and appears to be justified on the basis of post-test metallographic evaluations indicating firm fuel-heater mechanical attachment. The best value for the thermal conductivity of stoichiometric UO_2 (uncorrected for density) determined* as a result of this

* Plotted data in the body of this report contains the scatterband around the values reported here.

program is 0.033 watts/cm-°C at 1000°C and decreases to a value of 0.024 watts/cm-°C at approximately 1700°C. This latter value is relatively constant to temperatures up to 2200°C. These values are consistent with those obtained by other investigators as a result of in-pile determinations. It is concluded, therefore, that out-of-pile measurements of thermal conductivity made under high thermal gradient conditions can be used as an effective measure of short-term, in-reactor conductivities. There may be no real out-of-pile substitute for thermal conductivity measurements on moderate burnup UO₂ fuels, neither are there in-pile measurements. (In-pile data are generally limited to short irradiation times and very low burnups.) Therefore, we believe that the out-of-pile measurement techniques developed on this program will provide data which are as reliable as those obtained from short-term in-pile measurements.

A correlation between post-test microstructures and thermal conditions in UO₂ fuel rods used for thermal conductivity measurements indicated a dependence of the temperature for the onset of microstructure changes on the thermal gradient within the test element. Onset of small equiaxed grain growth (with average grain diameters of 25 microns) occurred at 1650 to 1730°C at high radial thermal gradients and at 1900 to 2010°C at lower radial thermal gradients. For onset of large blocky grains, temperatures in the range of 1820 to 1910°C and of 2020 to 2120°C were calculated for the onset of large blocky grains in elements with high and lower radial thermal gradients. The low level of lenticular void formation was attributed to the relatively small volume of fuel operating at temperatures approaching 2500°C. Columnar grain growth occurred more predominantly in the specimens with a low radial thermal gradient than in specimens with a high thermal gradient.

C. Collapse Criteria

A program was conducted to develop accurate analytical methods for predicting the collapse pressure of free standing fuel cladding and to establish the limiting factors involved in the selection of thin-walled fuel supported cladding. A series of collapse tests was performed on samples with various diameters and degrees of work hardening to determine the relation of each of these variables to the collapse pressure. The accepted classical criteria for predicting collapse due to external pressure were compared to experimental data and found to be inaccurate in the collapse pressure range of interest. The "Tangent Modulus Criterion" for predicting tube collapse of annealed and hardened tubing was developed and found to be reasonably accurate. It was found that the true collapse pressure, when plotted as a function of the thickness to radius ratio, was approximately a linear function which could best be represented by the "Tangent Modulus Criterion". Furthermore, it was shown that, for tubing with nearly isotropic mechanical properties, axial tensile values can be utilized in the determination of circumferential collapse strength.

An evaluation of a number of criteria important in the design of fuel supported cladding over both vibratory compacted and pelletized UO_2 indicated that uniform collapse occurred in pelletized rods containing 0.010 in. wall, 0.5 in. diameter annealed stainless steel cladding if the diametral gap did not exceed 0.004 in. UO_2 fuel, vibratory compacted to 86% of theoretical density provided adequate fuel support for 0.5 in. diameter, 0.010 in. wall annealed stainless steel cladding at 5000 psi external pressure. Lower density fuel would undoubtedly provide adequate support at the lower pressures encountered in pressurized and boiling water

reactors, although real incentives for densities below a nominal 85% of theoretical even for rods operated at a surface heat flux well above one million BTU/hr-ft² is not obvious at this time.

D. Projection Welding

A method was developed for fabricating stainless steel and Zircaloy clad fuel rod clusters by projection welding spacer wires between rows of filled or empty fuel rods. Electrode tooling and cluster fixturing were developed for the production of square or hexagonal arrays of seven foot long stainless steel clusters containing up to 61 tubes and for small Zircaloy assemblies. The method involves the progressive assembly and projection welding of spacer wires to individual rows of fuel rods. Inspection and repair of all fuel rod-to-spacer joints is possible at all stages of fabrication. A method for simultaneous welding of up to nine joints was developed and was utilized in the fabrication of the test assemblies.

Stainless steel clusters consisting of 37 empty fuel rods, seven feet in length, were able to withstand 3000 temperature reversals with a tube-to-tube temperature difference of 8°C (15°F) (typical of that encountered in power reactor fuel assemblies) without failures of the welds. Tube-to-tube spacing dimensions were normally held to within ± 0.008 cm. High quality, corrosion resistant welds were obtained for Zircaloy tube to Zircaloy spacer wire joint designs by welding in an inert gas filled chamber.

III. DISCUSSION

A. Powder Compaction

1. Introduction

a. Potential for Powder Compaction

The primary objective of the effort at CEND has been a reduction in overall fuel cycle costs. An early analysis of the comparative costs between rods containing vibratory compacted and pelletized UO_2 was presented¹ as part of the Joint United States-EURATOM Research and Development Program Symposium on Powder Packed Uranium Dioxide Fuel Elements. The conclusion section of this paper¹, repeated verbatim below, provides an interesting point for discussion based on current water reactor technology.

"The current economic incentive for vibratory compaction appears to be increasing with advances in technology for both pelletized and powder packed fuels. On this basis, as well as a result of irradiation and performance data being accumulated for powder packed UO_2 fuel elements, vibratory compaction has arrived as a firm competitor for the pelletizing process. Based on the analysis projected into the year 1963, vibratory compaction will remain on the scene for many years in the future."

It is quite clear that vibratory compaction has remained on the scene for many years. However, it has not been utilized extensively as a basic method for fabricating power reactor fuel rods. The foremost deterrents to its more extensive incorporation in the nuclear power industry has been the questionable economic gains which are associated with its use as well as the reluctance of reactor designers to incorporate

new innovations unless specific advantages can be demonstrated. Although irradiation test data for vibratory compacted fuel rods are not scarce, there are a multitude of data for pelletized rods. Analyses of these data does not indicate a clear advantage for one form of oxide over the other. However, the preponderance of irradiation test data for pelletized rods must be viewed as a strong influencing factor for any designer in favor of pellets over vibratory compacted fuel unless a firm economic advantage for vibratory compacted fuel can be established, in the absence of strong technological advantages*.

On the basis of economics alone,** incentives for vibratory compaction were projected¹ for the year 1963. These incentives were based upon a substantial increase in the total volume of fuel business as well as a demonstrated preference of one form of fuel. Thus, the fuel cost projections for pellets assumed that the entire industry would be committed to pellets; a similar assumption was made in the case of vibratory compaction. Under those conditions, net economic incentives for vibratory compaction were demonstrated. The fact that the industry has not become heavily committed to vibratory compaction is due to one or both of the following factors:

- Greater demonstrated experience with pelletized fuel.
- Less actual cost incentives than have been projected on the basis of cost studies.

* Technological advantages will be discussed later in this section.

** In evaluating the economics, a major input is the fabrication cost for the fuel assemblies. However, comparisons should be made on the basis of total fuel cycle costs reflected for cores optimized for pellet rods and rods containing vibratory compacted fuel.

There are firm indications that in the area of fuel recycle, vibratory compaction processes are more economical than pelletizing approaches. However, significantly larger quantities of fuel must be processed and concentrated efforts on economic processing with remote pelletizing must be assessed before firm economic advantages for one of the processes can be established.

From a technological standpoint, the use of vibratory compacted UO_2 becomes advantageous as the nuclear industry advances into designs of higher power densities with the acceptance of molten UO_2 regions in the fuel. In such concepts, the homogenized porosity in a vibratory compacted element can minimize fuel-clad mechanical interaction resulting from the large volume change (approximately 10%) induced from melting. Even here, there is not a clearcut advantage for the vibratory compacted product since a cored pellet or a low density pellet will probably provide equivalent performance. The selection will again have to be made on economic grounds.

Many of the comments made above for the economics of vibratory compaction are equally applicable for swaged UO_2 fuel elements. In this approach, the low cost portion of the vibratory compaction process is combined with small swaging reduction (less than 20% reduction in area) to produce powder packed fuel elements containing fuel packed to about

92% of theoretical density. This packing density is equivalent to the fill density in a typical power reactor fuel rod containing pellets. Thus, the economic penalty of a greater metal/fuel ratio, encountered with vibratory compacted fuel when compared with pellets, is eliminated. However, recent data from VBWR irradiations of oxide fuels have indicated some concern over the performance of elements with a large fuel-clad mechanical interaction. This interaction is built into the "vibswaged" (combination of vibratory compaction and swaging) product and is one of the greatest deterrents to the incorporation of this process in current reactor designs. As with vibratory compaction, sufficient incentives for vibswage rods must be provided to designers before the vibswage elements are seriously considered. To achieve these incentives, and to determine whether VBWR experience can be extrapolated realistically, an extensive irradiation test program is required. On the basis of current technology and current trends, it is unlikely that such a program will be pursued. Should vibswage elements become an important consideration in the future, the program completed as part of this contract will provide the basic parameters for any fuel fabricator wishing to pursue this approach.

b. Program

Early efforts on vibratory compaction were aimed at achieving very high densities. In fact, a review of programs which have been pursued at most laboratories have indicated a competitive spirit aimed at achieving densities higher than have been achieved previously. As the program at CEND advanced, it became apparent that a density-economic balance existed which indicated a density ceiling of about 89% of theoretical for vibratory compaction. Although greater densities were attainable, they were

attainable only at costs* which added greater increments to total fuel cycle costs than the reduction in total fuel cycle costs associated with the increased density. This trend is reflected in the CEND program which is summarized in the next section. A summary of the early state-of-the art is reflected in the following summary of the Symposium on Powder-Packed Uranium Dioxide Fuel Elements. Summaries of other areas associated with vibratory compaction have also been included.

1) General Powder-Packed Fuel Technology

The aim in this portion of the Symposium was to review the economic incentives for considering powder-packed fuels, describe a number of processes for fabricating powder-packed UO_2 fuel elements, and present a new powder preparation process ($\text{UO}_2\text{-ThO}_2$) which may have a significant bearing on the economics of fuel recycle.

The emphasis on powder-packed fuel technology has been motivated primarily by economic factors. The first paper indicated that the economic incentives increase with anticipated advanced in technology for both pelletized and powder-packed UO_2 fuels. A good competitive position for these powder-packed fuels was predicted for many years in the future. This economic advantage was established for a simple rod geometry. Deviations from this geometry would provide an even greater economic incentive for powder-packed fuels; complex geometries offered both economic and technological advantages for the powder-packed fuels.

A review of several of the processes used in fabricating powder-packed fuels was presented in the second paper. The powder preparation methods

* An economic process for providing higher densities (about 92% of theoretical) was developed as part of this contract and consisted of a combination of inexpensive portions of the vibratory compaction process and swaging to low reductions in area.

which were described include those for sintered and ground, fused, high energy impact formed and electrodeposited UO_2 . The latter process is believed to be extremely important in the economic processing of second generation fuel elements. Compaction methods which were reviewed included vibratory compaction, cold swaging, and hot swaging.

The third paper discussed a new development in the preparation of powder ($\text{UO}_2\text{-ThO}_2$) suitable for vibratory compaction or swaging. It is noteworthy that both the powder preparation and the subsequent fuel element fabrication were developed for optimum simplicity. These techniques are particularly applicable to the economic fabrication of second generation fuels which require remote handling. This is an area in which powder-packed fuel technology has a promising future because of greater economic incentives.

2) Vibratory Compaction of UO_2

Vibratory compaction has gained wide attention and acceptance as a result of several features. A partial list of these features includes: low capital investment, ability to fabricate a multitude of shapes without large economic penalties, use of essentially 100% of the powder, low UO_2 inventories, and adaptability and simplicity for remote handling operations required for the economic fabrication of second generation fuel elements.

This section of the Symposium was devoted to descriptions and discussions of processes and process variables utilized in the development of vibratory compaction techniques - utilizing powder fuels. For simplicity, the Summary has been divided into various categories associated with the vibratory compaction process.

Powder Preparation

It has been well established that the ceramic grade powders, because of their high surface activity and low particle density, are not suitable for use in vibratory compaction processes aimed at high bulk densities. For this reason, most investigators have concentrated their efforts on fused UO_2 (a proprietary product of the Spencer Chemical Company) or on arc fused UO_2 (available from a number of vendors). These products have been characterized by a low surface activity, a low surface to volume ratio and high particle densities. Although a cost penalty is associated with the fused products, the economics associated with the increase in attainable density lessen the effect of this cost increase as a deterring factor.

The Sol-Gel and the Salt-Cycle plating processes are being developed to supply powder suitable for vibratory compaction or swaging of second generation fuel elements. The future potential for utilizing either of these processes for first generation fuel elements is not certain at this time.

A new approach to the production of high density powders is the atomic hydrogen, arc-melting process under development at Nukem. The economics of this process, as applied to powder packed fuel technology, have not been assessed to date.

Particle Size Distribution and Attainable Densities

Most investigators are in agreement that a minimum of three particle size fractions are required for the attainment of high bulk fuel densities. Most of the distributions fall within the following limits:

50% - 65% coarse

10% - 35% medium

15% - 30% fine

In general, the coarse particles are greater than 30 mesh, the medium fraction is between 30 and 100 mesh, and the fines are less than 100 mesh in size. Each investigator has shown a preference for different regions within the above framework. Once a screen size range is established, the different methods of comminution in use at different laboratories may cause a variation in the particle size distribution within each range. This factor may be responsible for the apparent difference in results between investigators using similar coarse, medium, and fine fractions. The optimum powder formulation apparently differs for each specific product as a result of variations in cross sectional area of the tube, mode of vibration (electromechanical vs. pneumatic), and complexity of the fuel element design.

A most interesting ternary plot relating particle size fractions and attainable densities was presented by General Electric (Vallecitos). Another interesting feature was the observation by Combustion Engineering that the final particle size distribution after pneumatic compaction approached the same value independent of the initial distribution, within certain limits. Particle comminution during compaction was firmly established. The final density was the same for distributions which were comminuted before compaction and those which were comminuted during compaction. However, the time necessary to reach this density was greater in the latter case. The relative economics of both approaches are being evaluated.

It was generally concluded that densities of 87 to 90% of theoretical in cylindrical fuel elements of approximately 0.5 inch diameter could be attained readily and in a reproducible fashion through the use of careful,

but not necessarily elaborate, techniques. The position within the spectrum of 87-90% of theoretical density depended upon the particular mode of vibration employed, compaction time, and particle size distributions. Efforts to increase density by the use of four or more particle distributions were tempered by considerations of extra costs in comminution (or extra time during compaction if comminution during compaction was to be employed) and the desire to use 100% of the powder.

The majority of investigations have been centered on pneumatic vibrators of various degrees of complexity and sonic electro-mechanical units. The pneumatic units were characterized by low costs, high accelerations, and low (< 100 cps) frequencies; the sonic electro-mechanical units were characterized by high costs, low accelerations, and precise control. High impact, pneumatic units were also used in which the frequency and impact were independently controlled. Frequencies of up to 500 cpm were attained with control to about 10%. The electro-mechanical units permitted continuous acceleration control at any frequency between 5 and 3000 cps.

3) Swaging of UO_2 Fuel Elements

Swaging is considered a competitive process for fuel element fabrication because it is economical, permits good dimensional control, and is capable of yielding high gross fuel densities combined with small variations in local density. In addition, densities in excess of 92% of theoretical are attainable either by swaging to high reductions or a combination of vibratory compaction and swaging.

The three basic methods for swaging UO_2 include cold swaging, hot swaging, and a combination of vibratory compaction and cold swaging. The first two methods utilized low tap densities combined with high reductions

during swaging, whereas the last method provided for a high initial density, by means of economical compaction techniques, and small swaging reductions. It was pointed out that large cold swaging reductions were conducive to the introduction of cladding defects and severe inspection requirements were imposed after fabrication. With some materials, intermediate anneals were required. Hot swaging combined with high reductions has been successfully employed. Although the potential for cladding defects was somewhat reduced, careful control of the process was required. It was generally agreed that the use of high particle density, low surface activity powder was preferable for swaging. In hot swaging, it was pointed out that the fuel loading could include as much as 50% of peroxide type UO_2 powder with little penalty in ultimate density. There was evidence that the void space in hot swaged rods has a higher percentage of closed pores than in cold swaged rods. It was noted that these closed pores could lead to improved fission gas retention properties for the hot swaged fuel rods.

The integrity of the starting tubing is of prime importance in the swaging process because defects tend to enlarge during swaging. In Zircaloy tubing, large grain size and hydriding were found detrimental, and the need for careful process control of tubing intended for heavy swaging reductions was emphasized. Careful process control has permitted the successful swaging to high reductions of Zircaloy clad UO_2 fuel elements.

The problems associated with micro-cracks in the clad wall of UO_2 elements swaged to high reductions has induced several investigators to concentrate their efforts on a combination of vibratory compaction and swaging. In the combined process, the most economical portion of the vibratory compaction cycle is used in the loading and initial densification.

of the fuel rods. When the rods have been compacted to somewhere between 75 and 85% of theoretical density, they are reduced in cross-sectional area from 15 to 25% by swaging. Fuel densities in excess of 92% of theoretical were attained by this technique and the clad integrity was maintained.

Some investigators have encountered tube damage on the inside wall as a result of the use of large particle size fractions. Since these larger particle sizes contributed to higher tap densities, a compromise or optimization is needed for each system under study.

Although a variety of different experimental parameters were employed by different investigators, there was general agreement with respect to the following:

1. With increasing reduction in area, the density increased until a maximum was reached. At this point, there was an undulating decrease and increase sequence in density as a function of reduction in area. The average density did not deviate appreciably from the previously achieved maximum.
2. The maximum density, described above, was different for different tube diameters and types of UO_2 powder.
3. For a given starting density and swaging reduction, lower densities were obtained for larger tube diameters.
4. The variation in local density along the length of the rod was usually within $\pm 1\%$; values as low as $\pm 0.5\%$ have been attained.

Most investigators did not specify a particle size distribution for swaging but merely utilized material below a given mesh size. There was some indication (G.E.) that more consideration should be given to particle size distribution in the swaging process.

Preferential hydriding in Zircaloy-2 was attributed to localized stresses in the cladding and was observed when powder containing large particle size fractions was utilized. Only general hydriding was found in tubes swaged over fine powders.

Investigators who have used both four and two-die swaging equipment generally agreed that better dimensional control can be attained with the four die unit.

4) Performance of Powder Packed Fuel Elements

Although a number of irradiation studies on powder packed fuels are in progress, the relatively small number of irradiation experiments completed to date have indicated adequate in-pile performance. Results were reported for irradiations of UO_2 , $\text{UO}_2\text{-PuO}_2$ and ThO_2 at Hanford, UO_2 and $\text{UO}_2\text{-ThO}_2$ at Oak Ridge and UO_2 at Vallecitos.

Results at Hanford have shown that eight foot long clusters of swaged UO_2 rods could be cycled 200 times with no resultant deleterious effects. Three thousand cycles between 75,000-500,000 BTU/hr-ft² were achieved at Vallecitos on 0.010 inch wall stainless steel clad, swaged UO_2 without change in dimensions of the cladding. Tamp packed rods containing UO_2 at 59% of theoretical density and clad with 0.014 inch thick, Type 304 stainless steel exhibited collapse at 300 psi at 1300°F, predominantly in the void region at the top of the fuel. Evidence of good fuel support with thin cladding was obtained from observations made at Hanford on clusters of powder packed fuel rods with 0.008 inch to 0.015 inch thick stainless steel clad; clad collapse was not observed during irradiation. At Hanford, sintering of $\text{UO}_2\text{-PuO}_2$ fuels occurred at temperatures as low as 200°C which probably contributed to clad support. Considerable credit

was given to the PuO_2 for contributing to the low temperature sintering, and it was questioned whether these results could be applied directly to UO_2 . However, the observation of cladding support at Vallecitos, cited above for 0.010 in. wall, is significant.

Contrary to out-of-pile tests, very little washout of swaged powder was observed during the in-pile operation of a defective, Zircaloy-clad UO_2 rod at Hanford. Corroboration of low washout for in-pile operation of these fuels was obtained at Vallecitos where a 0.015 in. wall swaged rod with a 0.015 in. defect was irradiated in VBWR for three months with an activity release equal to that measured for pellets under equivalent conditions. In addition, a number of PRTR elements have been irradiated, without incident, for several months with pinhole defects in the cladding. As a result of defect tests at Hanford, it was established that Zircaloy-4 was less susceptible to hydrogen pickup than was Zircaloy-2.

Initial activities in fuel recycle were evidenced by the remote fabrication at ORNL of $\text{UO}_2\text{-ThO}_2$ fuels prepared by the Sol-Gel process, and work at Hanford in which irradiated UO_2 was decontaminated by the "salt-cycle" process, vibratory compacted, and re-irradiated. An additional effort at Hanford was the investigation of the irradiation performance of heterogeneously sweetened fuel. U^{235} was utilized as a substitute for Pu. Although surface hot spots were suspected initially, none were observed and fairly good homogenization was revealed through post-irradiation metallography.

Irradiations under the Maritime Program at ORNL were based on swaged and vibratory compacted UO_2 rods with burnups of 11,000 MWD/T. Variables of interest include cold versus hot swaging, fine versus coarse UO_2 , and varying central temperatures. Irradiations for the gas-cooled reactor program at ORNL are aimed at evaluating effects on performance of different linear

heat ratings, high fired versus fused UO_2 , hot swaged versus tamp-packed products and free-standing versus fuel supported tubing. The Fuel Cycle Irradiation program ($\text{UO}_2\text{-ThO}_2$) at ORNL is aimed at a comparison of irradiation performance between Sol-Gel product fuel and fused oxides compacted to densities of 85-90% of theoretical in 39-inch lengths. Burnups up to 50,000 MWD/T at linear heat ratings up to 42,600 BTU/hr-ft (equivalent to over 500,000 BTU/hr-ft (equivalent to over 500,000 BTU/hr-ft²) are anticipated.

Failures at Savannah River of annular Zircaloy-clad swaged UO_2 elements during irradiation were attributed to hydride formation in the cladding. Metallographic evaluation of intact and failed sections indicated that hydride formation had occurred at the fuel-clad interface. Moisture within the powder compacted fuel was suspected as the major source of hydrogen. Subsequently, it was observed that fused and pulverized UO_2 could pick up as much as 20-110 ppm moisture during handling. Hydrogen control was achieved by outgassing the blended powder under vacuum at 1000°C, keeping the oxide dry during subsequent processing by use of inert atmospheres and vacuum storage, and a final baking of the completed fuel tubes at 200°C for up to 36 hours for three foot long elements. It was later found that 300°C was a better temperature for the moisture removal operation. One set of elements fabricated by this new process was irradiated without any evidence of clad failures.

5) Special Techniques

Extrusion

Some of the factors which reduce the bulk density of UO_2 fuel rods containing high density pellets are edge chips, angular ends, hourglassing, and other general surface defects. In addition, a built-in void exists in

any column containing pellets which have been fabricated to the typical non-perpendicular tolerances. Proponents of the extrusion process claim that higher bulk rod densities are possible because there are fewer ends to contain defects since one extruded length is the equivalent of several pellets, therefore, built-in voids in the fuel column are minimized. In addition, the length attainable is a distinct advantage because it minimizes handling costs.

The sintered density of UO_2 extrusions was increased as the particle size of the ceramic grade UO_2 was reduced, resulting in high surface areas in the range of 7-8 m^2/g or more. Plasticizers and binders were added to provide consistency to the material before extrusion. Intimate mixing of these components was imperative for a successful extrusion and subsequent high density. One laboratory included additions of bentonite and TiO_2 as densifiers to increase the sintered oxide density.

Extrusion die design and levels of hydrostatic pressure in the billet were variables which greatly influenced the final sintered density. Extrusions were dried, machined square on the ends, and sintered at 2700 to 3100°F. Sintering cycles varied at different laboratories from 90 minutes (in the case where densifiers are used) to 125 hours. Sintered products were ground to correct for bowing, which in some instances amounted to 1/8 inch in a 10-inch length. In one laboratory as much as 10 to 20% of the UO_2 volume was removed by grinding. Since 20 to 30% of ground UO_2 could be added to the virgin powder for subsequent extrusions, this may not present a problem.

Rods, 12 inches long and 0.6 inch in diameter as well as tubes 10 inches long with an ID of 0.4 inch and an OD of 0.6 inch, have been produced in

the density range of 94-97% of theoretical. In general, the properties of extruded rods were equivalent to those of pellets at the same density.

Gas Pressure Bonding

Gas pressure bonding is being developed by BMI as a competitor for the UO_2 pelletizing process. UO_2 powders were cold pressed or tamp-packed into stainless steel cladding tubes, and the sealed unit was subjected to helium pressures of 10,000 psi at temperatures up to 2300°F. Use of ceramic grade oxide resulted in final densities of 99.5% of theoretical. However, this was accompanied by severe wrinkling of the stainless steel can because of the low initial fuel densities attainable with the ceramic grade powders. A combination of 30 to 60 wt% ceramic grade oxide with fused oxide permitted attainment of higher initial fuel densities, elimination of clad wrinkling, and the attainment of densities in excess of 94% of theoretical.

Ultrasonic Filling

Twenty-four minutes of ultrasonic activation succeeded in increasing the density of a 24 in. long UO_2 fuel rod from a value of 91.1% of theoretical (achieved by pneumatic vibratory compaction) to 91.4%. Incremental loading of a stainless steel tube by ultrasonic filling produced a density of 92.1% of theoretical after 30 minutes of ultrasonic activation. A fifty-fifty mixture of ceramic and fused grades of UO_2 was ultrasonically loaded to a density of 71.9% of theoretical. Damage to the clad wall was not observed. Versatility of the method was evidenced with work on Al_2O_3 , ThO_2 , $\text{ThO}_2\text{-UO}_2$ as well as mixtures containing widely different density components. In the latter case, stratification was not observed.

The program on vibratory compaction performed as part of this contract was initiated on July 1, 1961 and was terminated on July 31, 1963 and is summarized below.

2. Experimental Effort

a. Vibratory Compaction of Rods

1) Introduction

Some work during this period included a review of earlier work in which densities in excess of 92% of theoretical were obtained in short lengths (12 to 18 in.). The aim was to select powder formulations and compaction conditions which were amenable to high densities and which were suitable for economic fabrication. The results of this effort were then applied to the development of fuel elements with fuel supported cladding. Finally, the information obtained in this effort was used in conjunction with a lateral vibratory compaction technique in an effort to obtain high densities in fuel elements of complex geometry.

2) Equipment

During the initial portion of this program, an experimental facility was designed. The floor plan of the area, shown in Figure A-1, indicates the location of the electro-mechanical and pneumatic vibrators, the work tables, glove boxes, and processing equipment. The room is equipped with an integral ventilation and exhaust system which is designed to provide a filtered atmosphere and exhaust while maintaining a room pressure negative to atmospheric.

The processing equipment is housed in eight all-welded stainless steel cubicles which are provided with hinged transparent doors and individually connected to the room exhaust manifold. The evacuation of air through these boxes prevents air-borne contamination during processing. The sieving, grinding and magnetic particle separation equipment is capable of processing approximately two-hundred pound quantities of UO_2 per day of the particle

size groups required for compaction. A typical flow sheet is shown in Figure A-2.

The pneumatic compaction apparatus, shown in Figure A-3, was used early in the program and consists of one standard two-inch vibrator located in the upper position, and one high impact vibrator located in the lower position. In the second approach, the compaction tube, capped at the lower end was attached to a predensification vibrator (either a standard pneumatic or electro-mechanical device) by a mechanical coupling, and the powder was poured into the upper end. Following the predensification step vibration, the charged tube was located on the pneumatic compaction apparatus (Figure A-4) with the capped end attached to the lower vibrator. The upper vibrator ram was lowered onto the partially settled powder with a predetermined compressive load. The application of the compressive loads to the samples was controlled by the vertical movement of the upper vibrator assembly. This motion was controlled by a pneumatic cylinder which permitted the operator to choose a static load ranging from zero to 900-pound force. When the vibrators were operated, the upper vibrator ram remained in contact with the UO_2 while it settled and the pre-selected static load was maintained at all times. A prototype model of a variable impact and frequency vibrator (VIV) which had a greater impact energy was used for all later investigations. Figure A-5 shows this unit in the upper position.

3) Program

During the initial phase of development densities greater than 92% of theoretical were achieved using compaction rams on both ends of the fuel (double ram technique) and small moisture additions of about 1%. The more practical production type process that was subsequently developed eliminated moisture additions and employed a tube, with the bottom end closed, which was

directly connected to the base vibrator, and a top ram which was directly in contact with the fuel during compaction (single ram technique). These elements were 3 ft long and were compacted to densities of about 89 to 90% of theoretical. It could not be clearly concluded from the data whether the decreased density attainable in the 3-ft lengths was due to increased fuel length, a lack of direct compaction of the fuel such as used in the earlier process, a lack of lubrication possibly provided by moisture in the earlier work, lack of homogeneity in the fuel mixtures, a combination of the above factors, or other influencing factors. In order to determine the interaction of these variables, a program was established to compact a series of fuel rods 3 ft in length, using a combination of variables noted above. In addition, elements were compacted with and without restraining dies to determine the effect of a die on ultimate density and density variation along the length.

It was observed that the highest densities achieved in the past were obtained with the following powder formulations shown in Table A-I. Formula 6A, having only three constituent particle sizes, was initially selected as the standard formulation for this evaluation. Formulas 7A and 9A plus further modifications of these formulas are discussed later in the report. It was observed that the unusually high compaction densities were achieved under the following specific conditions:

- (a) Fuel compacts approximated 12 to 18 in. in length and one-half inch in diameter.
- (b) One percent water was added as a wetting agent.
- (c) The double ram technique was used.

TABLE A-I

Densities Achieved During Previous Particle Size Studies

<u>Formulation</u>	<u>Particle Size Distribution wt%</u>					<u>Density Achieved</u>
	<u>6-8</u>	<u>8-12</u>	<u>12-20</u>	<u>20-30</u>	<u>-200</u>	
6A	55	20			25	93.9% T.D.
7A	50	12	20		18	93.0
9A	50	12	20	4	14	94.1

TABLE A-II

Densities Achieved Using Various Compaction Techniques

<u>Rod #</u>	<u>Ram</u>		<u>Pneumatic</u>		<u>Powder Conditions</u>		<u>% T.D.</u>
	<u>Single</u>	<u>Double</u>	<u>Impactor</u>	<u>Vibrator</u>	<u>Dry</u>	<u>Wet</u>	
1	X		X		X		89.1
2	X		X	X	X		89.6
3		X	X		X		89.6
4		X	X	X	X		89.0
5	X		X	X		X	87.1
6		X	X	X		X	85.6

(d) A high energy impactor was attached to the lower ram and a high frequency vibrator to the upper ram.

(e) A 130-pound downward load was applied to the top ram.

a) Evaluation of Process Parameters

A group of specimens was prepared to determine the effects of using moisture additions in 3-ft fuel lengths with the single ram technique. Results indicated fuel densities of 85% of theoretical or less in every case, whereas specimens run under identical conditions at the same time without the benefit of water additions yielded a density as high as 90% of theoretical.

A number of samples with 14-in. fuel lengths were compacted in a die under a variety of conditions as listed in Table A-II. The results confirm previous findings on longer rods and show that neither the wetting agent nor the double ram technique produced higher densities than the single ram dry compactions when the fuel tube is encased in a die. It should be noted that the added mass contributed to the system by the die reduced the amount of impact delivered directly to the fuel.

A second series of elements was compacted to determine the effect of using a restraining die over the fuel rods during compaction. Specimens were compacted using both wet and dry powder mixtures and single and double ram techniques in 3 ft fuel lengths. The resulting density on all specimens was between 89 and 90% of theoretical. It was concluded that although the dies in themselves do not influence the achievement of higher densities, they do permit more rapid compaction times (approximately 3 minutes) because of the higher allowable impact forces. Apparently the deleterious effect of water noted in the previous experiments is overcome by the higher impact forces used with a die. It was concluded that water additions play no important role and

may be detrimental* in some cases.

To further evaluate the effect of a supporting die, two tubes of 9/16 in. OD and 0.038 in. wall thickness were prepared. One of these was encased in a die and compacted with the 6A powder formulation. Both tubes were compacted for three minutes at maximum impact (80 psi on the VIV impactor). The compacted fuel lengths in each tube were approximately 35 in. long. The free standing tube compacted to an apparent density of 91.5% of theoretical and the tube diameter increased approximately 0.008 in. in the region of the ram fuel interface and approximately 0.006 in. in the region of the bottom end plug. The increased volume due to the bulging reduced the actual density to an estimated 90% of theoretical density. The fuel tube encased in the die compacted to 90% of theoretical density with no signs of tube deformation. Both fuel rods were sectioned into 3 in. lengths and a post-compaction particle size sieve analysis was performed. Also determinations of local densities on the 3 in. sections were made. The data are tabulated in Table A-III and Table A-IV. The sections are numbered so that C-1 and F-1 are adjacent to the bottom end plugs which rested on top of the variable impactor, while sections C-11 and F-11 were in contact with the top ram. It is interesting to note that all of the 6-8 mesh particle size was comminuted into particle sizes predominantly in the 8-20 mesh range. Only a very small portion of the fragments appeared in the -60 +200 size range and the amount of -200 size material remained virtually unchanged. Particle size segregation

* Aside from the problem associated with ensuring complete dryness in rods for in-reactor use.

TABLE A-III

POST COMPACTION SIEVE ANALYSIS OF DIE COMPACTED FUEL ELEMENT

Three Inch Segments

Section	Particle Sizes (Tyler Scale)								Percent Deviation from Average								Calculated Density
	8-12	12-18	18-20	20-30	30-60	60-70	70-200	-200	8-12	12-18	18-20	20-30	30-60	60-70	70-200	-200	
C1	47.7	11.9	4.0	5.5	7.2	0.7	3.0	20.0	- 7.7	+20.2	+33.3	+41.0	+46.9	+40.0	+66.7	-17.4	90.2
C2	47.6	11.6	3.5	5.4	6.8	0.2	2.4	22.6	- 7.9	+17.2	+16.7	+38.5	+38.8	- 6.0	+33.3	- 6.6	89.8
C3	51.0	10.9	3.5	3.7	5.3	0.7	2.3	22.6	- 1.4	+10.1	+16.7	- 5.1	+ 8.2	+40.0	+27.8	- 6.6	89.5
C4	48.2	10.6	3.0	3.9	4.8	0.6	1.8	27.0	- 6.8	+ 7.1	0.0	0.0	- 2.0	+20.0	0.0	-11.6	88.3
C5	51.6	9.9	3.0	4.0	5.2	0.7	1.6	24.1	- 0.2	0.0	0.0	+ 2.6	+ 6.1	+40.0	-11.1	- 0.4	88.5
C6	51.6	9.9	3.4	4.1	5.3	0.6	1.8	22.7	- 0.2	0.0	+13.3	+ 5.1	+ 8.2	+20.0	0.0	- 6.2	88.8
C7	52.9	11.0	3.6	4.3	5.9	0.6	2.2	19.6	+ 2.3	+11.1	+20.0	+10.3	+20.4	+20.0	+22.2	-19.0	88.9
C8	55.5	8.4	2.6	3.5	3.6	0.3	1.4	24.8	+ 7.4	-15.2	-13.3	-10.3	-26.5	+40.0	-22.2	+ 2.5	88.3
C9	54.7	7.5	2.0	2.5	2.9	0.5	0.8	29.2	+ 5.8	-24.2	-33.3	-35.9	-40.8	0.0	-55.6	+20.7	90.4
C10	54.2	9.2	2.9	3.5	3.6	0.4	1.7	24.5	+ 4.8	- 7.1	- 3.3	-10.3	-26.5	-20.0	- 5.6	+ 1.2	88.8
C11	53.7	7.6	1.7	2.9	3.0	0.5	1.2	29.5	+ 3.9	-23.2	-43.3	-25.6	-38.8	0.0	-33.3	+21.9	87.9
Average	51.7	9.9	3.0	3.9	4.9	0.5	1.8	24.2									89.0

TABLE A-IV

POST COMPACTION SIEVE ANALYSIS OF COMPACTED FUEL ELEMENT (WITHOUT DIE)

Three inch Segments

Section	Particle Sizes (Tyler Scale)								Percent Deviation from Average								Calculated Density
	8-12	12-18	18-20	20-30	30-60	60-70	70-200	-200	8-12	12-18	18-20	20-30	30-60	60-70	70-200	-200	
F1	49.3	11.9	3.3	3.8	4.6	0.2	2.0	25.1	+14.7	-6.3	-5.7	-28.3	-34.3	-77.8	-33.0	+3.7	88.1
F2	37.2	14.0	4.9	6.9	10.9	1.4	5.7	19.1	-13.5	+10.2	+40.0	+30.2	+55.7	+55.6	+90.0	-21.1	90.1
F3	47.0	12.7	3.0	4.5	5.3	0.7	1.6	25.3	+9.3	0.0	-14.3	-15.1	-24.3	-22.2	-46.7	+4.5	89.0
F4	43.3	13.3	2.7	4.6	5.6	0.4	2.0	25.8	+0.7	+4.7	-22.9	-13.1	-20.0	-55.6	-33.3	+6.6	91.5
F5	44.1	13.0	3.2	5.4	6.5	0.8	2.7	24.3	+2.6	+2.4	-8.6	+1.0	-7.1	-11.1	-10.0	+4.1	90.1
F6	42.4	12.9	3.2	5.4	6.2	0.9	2.9	26.0	-1.4	+1.6	-8.6	+1.9	-11.4	0.0	-3.3	+7.4	89.1
F7	44.7	12.4	3.6	5.1	6.6	1.0	3.1	23.3	+4.0	+2.4	+2.9	-3.8	-5.7	+11.1	-3.3	-3.7	89.0
F8	43.0	12.1	3.8	5.6	7.4	1.0	3.3	23.8	0.0	-4.7	+8.6	+5.7	+5.7	+11.1	-10.0	-1.7	89.2
F9	40.8	11.9	4.3	6.2	9.6	1.3	4.3	21.5	-5.1	-6.3	+22.9	+17.0	+37.1	+44.4	+43.3	-11.2	90.0
F10	47.9	11.2	2.8	3.9	3.3	0.6	1.2	29.1	+11.4	-11.8	-20.0	-26.4	-52.9	-33.0	-60.0	+20.2	88.2
F11	33.8	14.3	4.1	7.3	10.8	1.8	4.6	23.3	-21.4	+12.6	+17.1	+37.7	+54.3	+10.0	+53.0	-3.7	89.3
Average	43.0	12.7	3.5	5.3	7.0	0.9	3.0	24.2									89.5

is apparent. Variations in distribution are shown tabulated in Table A-III and Table A-IV. A larger fraction of the coarse powder remained in the die-restrained tube (51.7%) than in the free standing tube (43%). This can probably be accounted for by the fact that the weight of the die decreased the impact and hence comminution was not as great.

It was concluded that the use of a tube wall supporting die prevents tube wall deformation and that the decreased impact energy, resulting from the increased weight of the test unit, reduces the ultimate density that can be achieved. Thus, a strong but light weight die is required. It was also concluded that use of the single ram technique with dry powder mixtures with the tube supported in a die during compaction results in the highest densities attainable from the processes used. In addition, the attainable density of approximately 89 to 90% of theoretical could only be increased by improved particle size mixtures and carefully controlled loading techniques to attain a maximum of homogeneity in the fuel mixture along the length of the element.

b) Continuous Loading

To minimize segregation caused by loading and also by particle comminution, a variety of ram designs were tried for continuous loading experiments. For this process, the variable impactor was attached to the ram and the tube was supported on a stationary steel plate. The object of the experiment was to load and pack the fuel continuously without disturbing the column of fuel already packed. The loading was synchronized with the rate of ram withdrawal in such a way as to maintain a column of fuel available to the ram configurations. All fuel tubes were free standing (no supporting die). The process was discontinued after it was found that densities in excess of 80% of theoretical could not be attained because of binding between the ram

and tube wall caused by the UO_2 .

c) Compaction with the Variable Impactor Attached to the Ram

In an effort to produce higher densities and to minimize the influence of the weight of the tube supporting die on the impact energy delivered to the fuel and density achieved, several experiments were tried in which the fuel column and supporting die remained stationary and the impact force was applied to the top ram. Table A-V lists the conditions used in each case and the densities achieved. A layer of fuel approximately $1\frac{1}{2}$ in. deep was pulverized to a very fine powder at the ram-fuel interface and was probably detrimental to densification of fuel below this layer. Since the densification process apparently requires a ram bearing on the full fuel cross-section and continuous loading requires the ram area to be reduced in section to allow fuel passage, these two requirements appear incompatible. For this reason work on this concept was terminated.

d) Compaction in Thin Wall Tubing

It had been concluded that a tube supporting die was desirable for the compaction process and it appeared that the compaction of thin walled tubing would present no special difficulties. Therefore, several $\frac{1}{2}$ in. OD stainless steel tubes having 0.015 in. wall thickness were encased in dies and compacted using the 6A formulation (Table A-I) at the maximum impact energy of the impactor.

The first compaction on a 12 in. fuel length produced a density of 88.7% of theoretical with no visible damage to the tube. A second compaction on a 24 in. fuel length produced a density of 88.9% of theoretical. At the ram fuel interface, the tube OD increased approximately 0.0015 in. probably due to improper tube-ram alignment. After this preliminary check, a series of twelve tubes with a 36 in. final fuel length and a 0.015 in. tube

TABLE A-V

Densities Achieved Using Various Vibrator Configurations

<u>Rod #</u>	<u>Test Description</u>	<u>% T.D.</u>
1	VIV only (tube resting on solid base) (tube enclosed in die)	79.6
2	VIV on Ram (vibrator at base of tube) (tube enclosed in die)	82.1
3	VIV on Ram (vibrator at base restricted to upward travel only) (tube enclosed in die)	84.1
4	VIV only (tube resting on solid base) (no tube die)	82.1

wall was compacted. The results are tabulated in Table A-V. All twelve elements were loaded completely before vibration was initiated.

The post-compaction sieve analysis shown on the Table indicates that there was considerable fragmentation of the large particles into medium sized particles but very little generation of fines. It was inferred from these data that the total compaction time in the pneumatic compaction process was primarily controlled by the time required to accomplish comminution. No tube wall bulging was observed on these specimens.

The above observation lead to the speculation that use of the formulation revealed by the post-compaction sieve analysis might eliminate the need for in-place comminution. Thus, the time required to reach high density might be reduced. A secondary benefit, in the form of better fuel utilization, would result from the use of such a formula. Fuel mixture 6-A-1, shown in Table A-VI, was formulated in an attempt to closely approximate the post-compaction sieve analysis typical of the 6-A fuel mixture. The first compaction of mixture 6-A-1 resulted in a 2% drop in density from that obtainable with mixture 6-A. Post compaction sieve analysis of that element showed that a considerable comminution of the coarse fractions was still occurring. In an attempt to minimize comminution during compaction, the initial densification process was performed on both electro-mechanical and small, low impact pneumatic vibrators in an attempt to retain the 6-A-1 formulation for a longer period of time. Final compaction was performed on the pneumatic unit. It was believed that this innovation would succeed in increasing the density to the value attained during compaction of formula 6-A. Results of the initial densification processes indicated densities of 82% of theoretical after three minutes with either type of vibrator. Final pneumatic compaction resulted in densities of approximately 87% after three minutes. It was concluded

TABLE A-VI
RESULTS OF COMPACTION STUDIES

#	Fuel Mix	Densification		Compaction		Sieve ² Size	Details of Fuel Formula Before and After Compaction												1-10 Micron
		time/ den.	type/ ¹ die	time/ den.	type/ die		6/8	8/10	10/ 12	12/ 16	16/ 20	20/ 30	30/ 60	40/ 70	60/ 100	100/ 200	-200	-325	
1	6A	None	-	3/89.0	P/Yes	before	55	20											25
						wt% after													
						wt%													
2	6A	None	-	3/89.5	P/Yes		55	20											25
																			24
3	6A-1	3 min/ 82.0	P/Yes	3 min/ 87.5	P/Yes														25
																			23
4	6A-1	3 min/ 82.0	E/Yes	3 min/ 87.2	P/Yes														25
																			25
5	7A	3 min/ 82.0	P/Yes	3 min. 88.3	P/Yes		50	6	6	10	10								18
6	7A	3 min/ 83.4	E/Yes	3 min/ 89.4	P/Yes		50	6	6	10	10								18
7	7A-1	None	-	3 min/ 87.1	P/Yes		22	11	11	16	10		3		12				25
8	7A-1	3 min/ 81.0	P/Yes	3 min/ 88.1	P/Yes		22	11	11	16	10		3		12				25
9	7A-1	3 min/ 82.9	P/No	3 min/ 87.1	P/Yes		22	11	11	16	10		3		12				25
10	7A-1	3 min/ 84.7	E/Yes	3 min/ 87.9	P/Yes		22	11	11	16	10		3		12				25
11	7A-1	3 min/ 82.0	E/Yes	3 min/ 88.6	P/Yes		22	11	11	16	10		3		12				25
12	7A-1	3 min/ 87.0	E/No	3 min/ 88.1	P/Yes		22	11	11	16		6	2	3	12				25
							18	8	9	13				16	4		3		20
13	8A	3 min/ 86.0	E/No	3 min/ 88.0	P/Yes		24	24			4			24				20	4
14	8A	10 min/ 88.1	E/No	3 min/ 88.1	P/Yes		24	24			4			24				20	4
15	8A	20 min/ 88.3	E/No	3 min/ 88.4	P/Yes		24	24			4			24				20	4
							20	17		8	5	2	19		14			15	

1 - Type E, electromechanical, or P, pneumatic; Die Yes or No. 2-Tyler Sieve Size

that reducing the size of the coarse particles and broadening the range of particle sizes results in lower density.

In order to find a compromise between fuel utilization and attainable density, fuel mixtures 7-A and 7-A-1 were formulated. Densities attained from the densification process on these formulas varied widely depending on whether pneumatic or electro-mechanical techniques were used and whether a die was placed on the tube for this portion of the process. For the three minute densification time employed, highest densities were attained with the electro-mechanical vibrator when no die was used; whereas results with the pneumatic vibrator were approximately the same with or without a die. Despite the wide variance in densities achieved in the densification process, the resultant final densities in the compaction process fell into the predictable range of 87 to 89% of theoretical. The higher densities were obtained with formula 7-A which had the largest coarse fraction.

In an attempt to improve the attainable fuel rod density, the use of ultra-fine particles was investigated. The use of -325 mesh and micron sized particles permitted greater separation between the coarse, medium and fine particle size groups. In theory, this greater separation of groups should result in the attainment of higher densities and some favorable experimental results have been reported in the past. The particular formulation, 8-A, selected for this work was one found in the published literature from the Savannah River Laboratory and the densification process used was similar to that reported by Savannah River. While the densities attained in the densification process were higher than those attained with other formulations, these values were far below those anticipated. Vibration times varying from 3 to 20 minutes were used. However, it appeared that the density had leveled off at 88% after about 10 minutes. Subsequent compaction

on a high energy pneumatic vibrator raised the density 2% on the rod which had already been densified for three minutes but had negligible effect on the rods which had been densified for 10 or 20 minutes. In an effort to insure that segregation of the fines would not limit the achievable densities on these fuel rods, the fines were vacuum dried after ball milling and incremental loading was employed. Fuel rod 13* was loaded in a single fuel charge; fuel rod 14 was loaded in twelve equal increments; and rod number 15 was loaded in twenty-four equal increments. Despite these precautions, visual observations of the fuel removed during subsequent sectioning operations indicated that substantial local segregation of the fines did occur. Equivalent local segregation was observed for all three fuel rods.

Review of Table A-VI leads to the following general observations:

- (1) Less comminution can be expected when electro-mechanical vibrators are used for the initial densification. This, however, seems to have no effect on the final density.
- (2) For the fuel rod sizes under consideration, an increase in the amount or in the particle size of the coarse fraction tends to increase the final density.
- (3) With the limitations of 100% powder utilization and a five to six minute vibration time, densities of about 88-89% of theoretical can be obtained consistently with a rather wide choice of particle size distribution.

* See Table A-VI

- (4) Final fuel rod density is not affected by the use of pneumatic or electro-mechanical vibration techniques for initial densification. Separate initial densification techniques offer no advantage over simple compaction for three minutes with the high impact vibrator.
- (5) The use of -325 mesh or micron size fines does not appear to be warranted on the basis that the same densities can be achieved using -200 mesh fines which are more easily handled and have less tendency to cause segregation.

4) Conclusions

The mechanical aspects of powder packing such as vibrator location with respect to fuel tube and ram, simultaneous loading and packing, variation of table loads, use of single and double ram techniques, and the use of wetting agents or lubricants, have been investigated and optimized earlier in the program (see CEND-172). It is now concluded that 88-89% of theoretical density is the best density value attainable by simple, rapid and economical production techniques for one-half inch diameter, thin-wall stainless steel tubes. Higher densities may be attainable by more sophisticated methods of powder handling and packing. However, use of these techniques leads to higher fuel and fabrication costs, thus over-balancing reductions in fuel cycle costs associated with higher densities. Therefore, the development of such techniques falls outside the scope of this program which is aimed at total fuel cycle cost reduction. Operating within this framework, the work on the development of economical vibratory compaction techniques and powder handling procedures for rod type fuel elements was terminated in the belief that the range of 88 to 89% of theoretical density

represents the highest density consistent with lowest fuel cycle costs for elements fabricated by vibratory compaction methods.

b. Multiple Rod Compactions

1) Introduction

This effort was initiated to develop techniques for the simultaneous compaction of thirty-seven fuel rods in a group with the ultimate object being the compaction of a completely assembled thirty-seven tube, projection welded cluster. Achievement of this objective would reduce the compaction costs and defer the commitment of fuel to the last possible moment in the core fabrication cycle. Preassembly of the tube clusters would permit inspection for spacer defects and dimensional variances, thus permitting rejection of the assembly without incurring the potential fuel losses and labor costs involved in unloading preloaded fuel elements. Although a projection welded cluster was used in this program, the techniques developed are equally applicable to brazed, welded and mechanically assembled fuel clusters.

The initial effort on this program was concerned with the fabrication of a multiple tube pneumatic compaction apparatus and associated alignment fixtures. Major emphasis was first placed on the mechanical design since this greatly affected the vibratory compaction parameters. When the mechanical problems were resolved, emphasis was shifted to the attainment of higher fuel densities equivalent to those for individually compacted tubes for groups of up to thirty-seven tubes. Using the parameters developed for groups of tubes, a thirty-seven tube projection welded cluster was then compacted.

2) Program

A multiple tube pneumatic compaction apparatus shown in Figure A-6 was constructed with provisions for simultaneous compaction of up to sixty-one rods in a hexagonal, or square array. This apparatus can be adapted to use a variety of different vibrator types and will accommodate tubes from two to eight feet in length. A twenty-four inch stroke, double acting air cylinder supports the multiple ram assembly and the positive alignment fixture. The ram assembly grid had sixty-one threaded holes in a hexagonal pattern on 0.625 in. centers into which the rams were screwed.

In all of the experiments, each tube was loaded with the same weight of fused UO_2 containing the previously established economical three particle size distribution shown below:

<u>Particle Size</u>	<u>Wt%</u>
-6 +16	60
-30 +100	15
-200	25

The weight of the charge was calculated to correspond to approximately 37.5 inches of UO_2 at a density of 87% of theoretical. Reproducibility of the size spectrum within the coarse and medium fractions was maintained by actually weighing out fuel charges using the distribution noted below.

<u>Particle Size</u>		<u>Wt%</u>
<u>-6 +16 Mesh Fraction</u>		
-6	+8	37
-8	+10	16
-10	+12	18
-12	+16	29
<u>-30 +100 Mesh Fraction</u>		
-30	+60	76
-60	+100	24

This process of controlling the size spectrum within narrow limits is required because of the depletion of certain sizes during continual reuse in the development effort. During actual production, the incoming fuel would be sampled to insure that the powder size spectrum remained substantially the same and the fuel would only be separated into the coarse, medium, and fine size groups.

Initially a low impact, high frequency pneumatic vibrator (3000 vibrations per minute) was utilized. Type 304 stainless steel tubes, forty-eight inches in length, 0.500 in. in outside diameter, and 0.025 in. wall thickness were fabricated. With this arrangement, a single rod was compacted for nine minutes and attained a density of 84% of theoretical. Thirteen tubes in a hexagonal pattern were then compacted. Densities between 81 and 83% of theoretical were achieved.

In an attempt to increase the density, the high frequency vibrator was replaced with a variable impact vibrator (VIV) which delivers a greater impact. Up to eleven rods were simultaneously compacted. Densities in the range of 84 to 85% of theoretical were obtained.

In these initial experiments, equal fuel charges were loaded into each tube; consequently, the tubes with the smallest diameter had the higher initial fuel columns. Since the top rams were fixed to a common grid plate, the elements with the longest fuel columns were supporting the major portion of the top ram load and the elements with short fuel columns were not being top loaded at all. This disproportionate loading caused several fuel rods to buckle in compression; while other fuel columns, having no load at all, were not densified sufficiently.

The ram assembly was modified to allow for differential compaction heights of the various tubes by spring-loading the top rams as shown in Figure A-7. In addition, the reference tube thickness was changed from 0.025 in. to 0.020 in. and the 0.500 in. outside diameter was maintained.

In the remainder of the experiments, the same weight of fused UO_2 was placed in each tube, a high impact vibrator was used in each compaction, and a top load, varying from twelve to forty pounds was applied on each fuel rod during compaction. Using these conditions, groups of seven, thirteen, nineteen and thirty-seven fuel rods were compacted as the program progressed. A summary of results is presented below.

<u>Number of Tubes</u>	<u>Density Range % T. D.</u>	<u>Compaction Time (Minutes)</u>	<u>Maximum Difference in Fuel Column Heights Within Group (Inches)</u>
1	88	21	--
7	86 - 87	22	0.36
13	85 - 87	40	0.50
19	85 - 86	70	0.46
37	84 - 87	120	1.40

The above time data indicates that, although twenty-one minutes are required to compact one tube, only three to four minutes per tube are required to compact groups of from seven to thirty-seven. The equipment can operate unattended during most of the compaction period. The long running time of the nineteen and thirty-seven tube group was partially attributed to mechanical difficulties in the vibrator, which reduced the impact level.

The density data indicate that there is not a significant decrease in density when compacting a large group of thirty-seven rods when compared to the seven rod group. Likewise, there was only a decrease in density of approximately one to two percent for a seven tube compaction when compared with a single tube compaction. Additional compaction times did not result in increased density. Figure A-8 shows the gross average density of each rod in a thirty-seven rod hexagonal group. It is felt that proper experimental conditions could be established which would permit all the tubes to be in the 86 to 87% T.D. range. Sieve analyses were performed on a representative rod from each group and on the rods with the highest and lowest density in the thirty-seven rod group. Table A-VII shows the sieve analysis of the coarse fraction of representative rods from the various groups. The remainder of the sieve analysis was not included since there were no significant trends in the other particle fractions. The data indicated that, in general, the amount of comminution of the coarse fraction was reduced as the number of rods per compaction was increased. However, this trend may exhibit a bottom threshold between nineteen and thirty-seven rod groups, where the value of the coarse fraction for the thirty-seven rod group is equal to that for the nineteen rod group. The average density appears to decrease with decreasing comminution of the coarse fraction. In each of the experiments a dimensional survey of the outside diameter of each

TABLE A-VII

SIEVE ANALYSIS OF COARSE FRACTION (-6 +16)
OF COMPACTED FUEL ELEMENTS

Sieve Analysis wt% After Compaction							
Particle Size	Prior To Compaction	Single Tube Compaction	Seven Tube Group	Thirteen Tube Group	Nineteen Tube Group	Thirty-Seven Tube Compaction	
						Low Density Tube	High Density Tube
-6 +8	22.2	12.7	15.0	17.3	19.3	17.0	19.0
-8 +10	9.6	7.4	9.4	8.1	9.6	9.0	10.0
-10 +12	11.0	10.2	10.1	10.3	12.1	11.0	11.0
-12 +16	<u>17.2</u>	<u>16.9</u>	<u>17.7</u>	<u>17.5</u>	<u>14.3</u>	<u>18.0</u>	<u>17.0</u>
Total	60.0	47.2	52.2	53.2	55.3	55.0	57.0
Density of Tube (% T.D.)	--	87.9	87.1	86.9	85.8	84.2	87.3

NOTE: Analysis of individual rods selected from each group.

tube was performed. The results show no evidence of bulging or deformation.

Due to the encouraging results obtained in the compaction of thirty-seven tubes, it was decided to compact fuel into a thirty-seven tube projection welded cluster. A seven foot long, sixty-one tube projection welded cluster taken from the welding evaluation program, performed as part of another portion of this program, was cut down to thirty-seven tubes, four feet long. The end of the cluster containing the end plugs was then cast in cerrolow-alloy and machined until all of the end plugs were in one plane. Casting the end in cerrolow-alloy provided a rigid mold which reduced the possibility of stressing the spacer wires during the machining. In a production setup, the end plugs would have been welded into the tubes and the tubes would have been fixtured in such a fashion as to eliminate the above casting and machining operations prior to fabricating the cluster.

The projection welded cluster was compacted using the same conditions as those developed on the simultaneous compaction of thirty-seven individual tubes. The total compaction time was three hours. The density of a representative tube was checked every ten minutes during the compaction process, since it was not feasible to check every tube. The results for this tube are tabulated below:

<u>Time (Minutes)</u>	<u>Percent Theoretical Density</u>
1	74
10	81
30	82.5
40-150	0-0.3 increase every 10 minutes
150-180	0.2

Densities were calculated for each of the tubes after two and a half hours--and then after three hours. The additional half hour of compaction time resulted in an average increase of theoretical density of only 0.2 percent. The group average density of each rod, after 3 hours of compaction time, is shown in Figure A-9. The average density was 84.8 percent. Figure A-9 indicates that there is no apparent dependence of density upon position within the cluster. The average density of the compacted projection welded cluster is approximately one percent lower than the simultaneous compaction of thirty-seven individual tubes.

A visual and metallographic examination indicated that twenty-five percent of the welds in the projection welded cluster failed during compaction. The failures were localized in the area encompassed by tubes 1, 16, 34 and 19 (Figure A-9). It was noted during the compaction that the first visible weld failures were those on tube 16. A majority of the welds still intact have small microcracks at the interface between the wire and tube. If the compaction time was increased, more welds would probably have failed. The wires on tube 16 and the wires on the periphery tubes are subject to a greater deflection since these are in a less rigid condition. Since these wires have a greater deflection, an out-of-phase condition occurs with the result that one tube is deflecting upwards while an adjacent tube is standing fairly rigid. This overstresses the weld and causes failure. After an outer tube fails, the adjacent tube then acts as an outer tube, and the fracture once made, can continue. A possible solution is to weld or braze a wire strap around the periphery tubes.

3) Conclusions

Groups of up to thirty-seven rods can be compacted simultaneously to a density range of 84-87 percent of theoretical. The compaction time increases as the number of rods are increased; however, the per tube compaction time appears in all cases to be approximately three to four minutes. Spring loading of the top rams is necessary to prevent disproportionate bearing loads from developing on the various tubes.

Compaction of a projection welded cluster does not seem feasible, using a high impact vibrator. Electro-mechanical compaction, combined with some type of a band on the periphery tubes, would be a more suitable process. The simultaneous compaction of a group of tubes or a cluster offers a potential advantage for the fabrication of second-generation fuel elements of clusters in that the fuel handling time is minimized.

c. Vibratory Compaction of Nuclear Superheat Critical Fuel Elements

A program consisting of unloading vibratory compacted fuel elements from one nuclear superheat critical experiment and reloading the fuel by vibratory compaction for another critical experiment was completed under Contract AT(11-1)-795 with the Commission. Development work performed under the Fuel Cycle Program and this contract with the U.S.-EURATOM Research and Development Program was vital in the successful conclusion of the above mentioned effort. It is because of the influence of the present U.S.-EURATOM program on the successful completion of this work that a brief summary of the effort is included here. In addition, the effort under the Nuclear Superheat Program represents a demonstration of the potentiality and versatility of vibratory compaction techniques.

A total of 579 double annular fuel elements which had been vibratory compacted in the last quarter of 1960 for use in nuclear superheater critical

experiments were unloaded. The fuel, Spencer fused uranium dioxide, enriched to 2.55% U^{235} , was then reloaded by pneumatic vibratory means to make 105 multi-tube fuel elements (Figures A-10 and A-11).

The prime emphasis of this program was to produce a core loading of these very complex fuel elements at a minimum cost. Several important points were demonstrated as a result of this effort:

- 1) Vibratory compaction as a method of fuel fabrication permits the transfer of fuel from one critical experiment to another, providing the enrichment requirements are not changed, with no fuel reprocessing and very little fuel loss. The total fuel loss during the sequence of unloading and re-loading was less than 0.2%.
- 2) As a result of (1), above, fuel costs to the Commission for the second critical was zero since no additional fuel was required. No time allowance in the production schedule was required for fuel delivery and the fuel is again available for a third critical experiment.
- 3) Fuel elements of a complex geometry can be made by vibratory compaction techniques. Figure A-10 shows a photograph of the multi-tube annular fuel element and Figure A-11 illustrates a cutaway of the same element. The compacted UO_2 filled the space between the large inner and outer tubes and completely surrounded the small superheater tubes. Fabrication of such an element by a technique other than vibratory compaction may be extremely difficult.

Details of the development and production of these elements can be found in the Nuclear Superheat Development Program reports.²

d. Fuel Elements of Complex Geometry

An effort was directed towards the development of methods for the efficient packing of fuel elements of complex geometry to high densities. Because elements of complex geometry are more massive than simple fuel rods, the ordinary means of applying axial vibrations may not be feasible. Most mechanical and electro-mechanical vibrators suitable for vibratory compaction have practical power limitations at levels below those required to densify massive fuel elements although a Large Force MB Vibrator would probably be sufficient. More sophisticated devices which could deliver the power may also produce structural damage in the cladding or end cap welds.

As a possible solution, the application of localized lateral forces for powder densification was studied. To apply such forces, the fuel tube was encased in a die to which a two-inch pneumatic vibrator could be attached anywhere along the length of the die.

The tube die was also designed to remove tube ovality during densification and thereby minimize the out-of-roundness of packed fuel elements.

The fuel element selected for lateral vibratory compaction studies consisted of a stainless inner tube having an outside diameter of 1.385 in. and a wall thickness of 0.035 in. and an outer stainless tube of 2.750 in. outside diameter and a 0.054 in. wall thickness. Both tubes were welded to an end plug to provide a concentric annulus for the fuel. During compaction, a ram supporting a 100-pound load rested on top of the fuel column.

The fuel charges were blended and loaded in 1000-gram batches for the 15 kg loadings and in 2000-gram batches for 28 kg loadings. Compaction was initiated after the entire charge was loaded in the tube.

Table A-VIII below shows the fuel mixtures used, the compaction conditions, density achieved and the post-compaction fuel analysis. Table A-IX tabulates the outer tube ovality measurements made before and after compaction.

Comparison of runs #1 and #2 shows that the coarser particle blend packed more efficiently in the wide 0.625 in. annulus. This probably would not be the case if internal coolant tubes were present in the annulus and the relative free space between clad structures was reduced. Runs 3a and 3b show that changing the location of the vibrator along the length of the tube produced further densification. Hence, in future investigations it may be desirable to energize vibrators either singly along the length of the tube or simultaneously. The post-compaction sieve analysis of all three runs reveals that severe particle comminution did not take place.

1) Conclusions

The densities obtained using lateral vibratory compaction compare favorably to those obtained using axial compaction forces for these large elements. Since it is not necessary to induce particle mobility for densification in the entire column simultaneously, the lateral method is applicable when the mass of the fuel column becomes too large to densify with axially applied loads. If large diameter tubing with very small ovality is used, a lighter weight die encasing the tube for vibrator mounting will suffice. The reduced mass of the die and fuel tube would allow even greater particle mobility for densification with a vibrator of the same power rating. It is probable that use of lateral vibration combined with the use of a Large Force MB Unit would increase attainable densities for complex elements containing large masses of UO_2 .

TABLE A-VIII

ANNULAR TUBE COMPACTION DATA

Run No.	Density	Fuel Charge	Compacted Fuel Height	Compaction Lateral	Compaction Axial	Fuel Formulation Data											
						-3- $\frac{1}{2}$ +6	-6+8	-8+10	-10+12	-12+16	-16+20	-20+30	-30+60	-60+100	-100+200	-200	
1	85.5	15 kg	24.5	--	30 min Before	25	10	10	15	--	--	15	--	--	25	--	
						After	19.4	12.4	10.2	14.2	1.6	1.3	12.0	2.9	6.6	12.1	7.0
2	83.9	15 kg	24.9	15 min.	--	Before	--	25	10	10	15	--	--	15	--	--	25
						After	--	21.1	9.7	8.8	15.8	2.1	1.4	13.5	1.0	2.3	23.4
3a	84.1	28 kg	46.5	15 min.	--	Before	--	25	10	10	15	--	--	15	--	--	25
						After	--	20.9	11.8	12.5	12.6	1.1	0.7	14.4	0.5	16.4**	9.0
3b*	85.2	28 kg	45.9	15 min.	--	* Same as run 3a except that vibrator was moved from bottom of fuel element to											

** Believed due to incomplete sieving.

TABLE A-IX

OUTER CLAD OVALITY MEASUREMENTS

Location Inches from Bottom of Tube	Tube O.D.Measurement Before fuel Loading			Tube O.D.Measurement After Compaction		
	<u>0°</u>	<u>90°</u>	<u>Δ</u>	<u>0°</u>	<u>90°</u>	<u>Δ</u>
0	2.750	2.757	.007	2.769	2.775	.006
3	2.755	2.743	.012	--	--	--
4	--	--	--	2.766	2.756	.010
6	2.750	2.752	.002	--	--	--
8	--	--	--	2.751	2.755	.004
9	2.752	2.752	.000	--	--	--
12	2.751	2.751	.000	2.751	2.751	.000
15	2.752	2.754	.002	--	--	--
16	--	--	--	2.754	2.754	.000
18	2.752	2.749	.003	--	--	--
20	--	--	--	2.753	2.751	.002
21	2.754	2.751	.003	--	--	--
24	2.752	2.755	.003	2.752	2.750	.002
27	2.752	2.753	.001	--	--	--
28	--	--	--	2.752	2.751	.001

e. Swaging

1) Introduction

The objective of this program was to combine the low cost portion of the vibratory compaction process and small swaging reductions (less than 20% reduction in area) to produce powder packed fuel elements with densities in the range of 90-95% of theoretical. It was expected that the small swaging reduction would prevent the cladding defects which were often observed in the regular swaging process where large reductions (greater than 40%) are utilized. The objectives of this program did not include a detailed investigation of the mechanisms and variables involved in a swaging process, but were aimed at obtaining engineering data from which the most economical combination of the two processes could be chosen for any given fuel density requirement.

In earlier work on this program stainless steel tubes filled with fused depleted UO_2 were vibratory compacted to densities between 85 and 88% of theoretical and subjected to swaging reductions ranging from 6 to 12%. The data revealed gross densities after swaging from 88.7 to 91.3% of theoretical, depending on initial compaction density and reduction in area. Metallographic inspection of cladding sections revealed no detectable microcracks or other damage.

During the latter stages of this program, additional rods were compacted and swaged to complete the data required for evaluation of the vibswage process. A summary of this effort is presented below.

2) Program

Four series of Type 304 stainless steel rods filled with fused UO_2 were processed to extend the existing data. In the first series, rods of five different initial outside diameters (0.5205, 0.5265, 0.5330, 0.5390 and 0.5626 in.) and a wall thickness of 0.029 in. were compacted with fused UO_2

to an initial density of 88% of theoretical and reduced in cross-sectional area 8 to 15% by cold swaging. The other three series were identical to the first series except that they were predensified to 80, 82 and 84% T.D., respectively.

The method of fabrication was identical for all the rods. One end of each tube was capped with a hollow end plug which was torch-brazed into the tube using a low temperature silver braze alloy. Approximately 48 in. of fused UO_2 was placed in each of the 54 in. rods. All of the rods contained the three particle size powder distribution shown in Table A-X except the series which was initially compacted to 88%. This series had the seven particle distribution, shown in Table A-X in order to attain the higher range of density prior to swaging. However, it has since been found in vibratory compaction experiments that the three particle size formulations will yield densities greater than 88% of theoretical.

The rods with an initial density of 88% of theoretical were densified on the pneumatic vibratory compaction unit. All other tubes (80, 82 and 84% initial density) were predensified on a small portable vibrator in a matter of seconds.

After initial densification, the tubes were fitted with aluminum plugs which were held in place by crimping the stainless steel clad into the plug. The rods were swaged at room temperature on a four-die stationary swage. Dimensional surveys of the outside diameters indicated that rods initially compacted to densities of 85% T.D. or greater, and then reduced in area by swaging, resulted in a variation from rod to rod of ± 0.001 , whereas those initially compacted to densities of 80 to 84% of theoretical resulted in variations of ± 0.0005 .

TABLE A-X

PARTICLE SIZE DISTRIBUTIONS UTILIZED IN SWAGING EXPERIMENTS

	Particle Size*	wt%
Three Particles Size Distribution	-6 +16	60
	-30 +100	15
	-200	25
Seven Particles Size Distribution	-6 +8	26.1
	-8 +12	15.4
	-12 +20	18.4
	-20 +30	7.7
	-30 +60	8.9
	-60 +200	6.2
	-200	17.3

* U. S. Standard Sieve Size

Depending on the final length of the rods, six to ten 3 in. sections were removed from each rod at equal intervals. These sections were analyzed for local density variations by a water immersion technique. The gross density after swaging was determined by averaging the densities of the 3 in. segments. Tables A-XI and A-XII contain the complete density data for all of the vibratory compacted and swaged fuel rods utilized in this investigation. Figure A-12 is a plot of variation in UO_2 density with reductions in area for starting densities of 80, 82, 84, 85 and 88% of theoretical. The data indicate that rods initially compacted to 80 and 82% T.D. are leveling off in density at 15% reduction, and the 84% dense rods have begun to decrease in density after about 14% reduction. The rods loaded to 84% T.D. have attained a maximum density of 92-92.5% of theoretical. The rods with starting densities of 85 and 88% increased to a maximum and then fluctuated in the range of 91 to 93% of theoretical during further reductions in area. The fluctuating densities in the rods compacted to 84, 85 and 88% T.D. are probably caused by the breakdown of coarse fuel particles. The resulting de-densification is followed by a reorientation of the particles which results in a subsequent density increase. However, it should be noted that the fluctuations in the density curves are only about 0.5% which is approximately equal to the experimental error involved.

Results of the local density variation analysis are shown in Table A-XIII. The data indicate that variations are not appreciably different whether high or low initial densities are used. However, increased reduction in area does decrease the variation. These results indicate that fuel rods can be fabricated with local density variations in the range of $\pm 1\%$ from the gross average density.

TABLE A-XI

ANALYSIS OF VIBRATORY COMPACTED AND SWAGED FUEL RODS

Rod No.	Gross Average Density After Vibratory Compaction	Reduction In Area Of Fuel By Swaging	Elonga- tion Of Fuel	Gross Average Density After Swaging	Density of Swaged Rod Sections** % T.D.								
	% T.D.*	%	%	% T. D.	#1	#3	#5	#7	#9	#11	#13	#15	#16
B-15	80.1	9.6	1.5	87.8	88.6	89.1	88.2	87.3	87.5	87.9	87.5	87.0	87.4
B-16	80.1	9.5	1.6	87.0	85.0	86.1	87.0	86.1	88.0	88.0	87.2	89.0	87.0
C-15	80.5	11.3	2.4	88.7	87.5	88.1	88.8	89.7	89.3	88.3	89.4	89.3	87.9
C-16	80.3	11.4	2.5	88.6	87.0	87.7	88.5	88.9	88.3	88.7	89.3	90.3	88.5
D-17	80.1	13.0	4.1	89.8	88.8	89.5	89.4	89.6	90.3	89.9	90.1	91.0	89.7
D-18	80.1	13.4	3.3	89.6	88.4	88.5	88.7	88.7	90.0	89.9	90.3	91.1	90.6
E-15	80.3	16.1	5.2	90.6	89.5	90.9	90.8	90.7	91.3	91.3	91.0	91.0	88.7
E-16	80.1	16.0	4.7	90.3	88.6	88.5	90.5	90.6	91.1	91.2	90.9	91.3	90.0
B-13	81.9	8.7	1.9	88.1	85.2	87.4	87.4	88.1	86.8	89.3	89.1	90.4	89.2
B-14	82.1	8.6	1.9	88.6	89.4	88.3	87.1	89.0	88.7	89.1	88.7	88.1	89.0
C-13	82.1	11.3	2.6	89.7	91.2	89.4	89.7	89.8	89.5	89.1	89.9	90.2	88.9
D-15	82.1	13.0	4.2	91.0	90.7	91.9	91.4	91.4	91.5	91.0	90.8	90.6	90.0
E-14	82.0	14.9	5.8	91.5	91.2	91.1	91.8	91.8	91.7	91.8	91.3	91.7	91.0
A-10	83.9	7.2	1.4	90.0	----	----	----	----	----	----	----	----	----
B-11	84.0	9.2	2.0	90.4	----	----	----	----	----	----	----	----	----
C-12	83.9	11.6	3.4	91.9	----	----	----	----	----	----	----	----	----
D-14	84.0	12.8	5.6	92.4	----	----	----	----	----	----	----	----	----
E-10	84.0	14.6	7.7	92.3	----	----	----	----	----	----	----	----	----

* T.D. - Theoretical Density

** Three inch long sections selected from sixteen equal divisions of each rod

---- Not analyzed

TABLE A-XII

VIBRATORY COMPACTED AND SWAGED FUEL RODS

Rod No.	Gross Average Density After Vibratory Compaction (% T.D.)	Reduction In Area By Swaging (%)	Percent of Theoretical Density For Sections of Swaged Rods***											Gross** Average Density After Swaging (% T.D.)
			1	3	5	7	9	11	12	13	15	17	18	
A-1	85.0	6.5	87.9	91.6	90.6	89.4	86.9	86.6	87.8	----	----	----	----	88.7
A-2	85.0	6.9	90.3	91.5	86.5	88.7	88.0	88.3	89.4	----	----	----	----	89.0
B-2	85.2	7.5	89.1	91.1	89.3	91.4	89.6	91.1	88.6	----	----	----	----	90.0
B-3	85.0	7.9	90.9	91.1	91.0	90.5	90.5	91.0	90.6	----	----	----	----	90.8
C-1	85.0	10.6	91.9	92.1	91.5	92.4	89.4	91.3	90.0	----	----	----	----	91.2
C-3	85.1	9.8	91.8	92.2	91.1	91.9	91.3	90.9	89.7	----	----	----	----	91.3
D-1	85.1	13.0	88.9	91.7	89.6	90.8	90.6	90.8	90.0	----	----	----	----	90.3
D-3	85.0	12.6	91.6	92.3	91.4	91.5	90.7	90.9	88.2	----	----	----	----	90.9
E-1	85.0	15.2	92.4	92.5	92.0	92.0	92.3	92.2	----	92.3	92.1	92.2	90.7	92.1
E-3	85.0	15.2	91.9	92.6	92.3	92.3	92.6	92.4	----	92.7	92.7	92.6	90.4	92.3
E-2*	85.2	----	81.7	84.5	84.8	85.5	85.2	84.1	----	85.5	85.6	85.9	----	----
A-5	88.0	6.0	91.9	92.4	91.2	90.7	89.9	91.5	89.4	----	----	----	----	91.0
A-6	87.9	6.5	93.0	91.6	91.5	90.8	92.4	89.1	89.6	----	----	----	----	91.1
B-7	87.9	7.8	90.0	91.9	92.3	92.5	92.7	92.6	----	92.5	93.1	91.3	----	92.1
B-8	87.9	7.7	90.5	92.1	92.4	92.6	93.0	92.8	----	92.7	93.2	91.9	----	92.4
C-7	88.0	10.8	92.6	92.5	92.4	92.9	92.7	93.0	----	93.2	93.3	91.6	----	92.7
C-8	88.0	10.8	92.0	92.9	92.7	92.5	92.6	92.8	----	92.8	93.0	92.4	----	92.6
D-7	87.9	13.4	91.6	92.4	92.3	92.6	92.2	92.3	----	92.3	92.5	91.6	----	92.2
D-8	88.0	13.2	91.5	92.2	92.0	92.1	92.5	92.7	----	92.7	92.6	90.3	----	92.1
E-4	87.9	14.8	92.3	92.5	92.3	91.8	92.3	92.4	----	92.5	92.5	92.7	90.8	92.2
E-6	88.0	14.9	92.5	93.1	92.4	92.6	92.6	92.8	----	92.7	92.6	93.2	91.4	92.6
E-5*	87.8	----	82.4	87.8	88.2	86.0	87.6	87.2	----	87.7	88.7	87.3	----	----

* Not Swaged - Compacted Standard

** Based on Average of Sections

*** Three Inch Long Sections Selected from Equal Divisions of Each Rod

---- Not Analyzed

TABLE A-XIII

ANALYSIS OF LOCAL DENSITY VARIATIONS

Vibratory Compacted Density % T.D.*	Reduction In Area %	Gross Average Density % T.D.	Maximum Local Variance From Gross Average Density		
			Plus	% T.D.	Minus
80	9.6	87.8	1.3		1.8
	11.4	88.6	1.7		1.6
	13.4	89.6	1.0		1.2
	16.0	90.3	0.9		1.8
82	8.7	88.1	2.3		2.9 (1.3)**
	11.3	89.7	1.5		0.8
	13.0	91.0	0.9		1.0
	14.9	91.5	0.3		0.5
85	6.9	89.0	2.5		2.5
	10.6	91.2	1.2		1.8
	15.2	92.1	0.4		1.4 (0.1)**
88	6.5	91.1	1.9		1.4
	10.8	92.7	0.6		0.9
	14.9	92.6	0.6		1.2 (0.2)**

* T.D. - Theoretical Density

** Excludes End Section

Longitudinal sections representing the top, middle and bottom of the swaged cladding of rods E-10 (predensified to 84% T.D. and reduced in area 14.6%) and E-6 (predensified to 88% T.D. and reduced in area 14.9%) were mounted, polished and examined for clad integrity. Microcracks were not observed in any of the sections. Local depressions up to 0.0045 in. were noted on the inside wall of the swaged tube as a result of embedded UO_2 particles. There were no appreciable net changes in clad thickness.

Microhardnesses were also obtained from the cladding of rods E-6 and E-10. A series of readings was taken for each sample using a Tukon Microhardness tester. The average of twenty-five readings indicated that there was no difference in hardness from the outside to the inside of the tube wall. The average hardness of the tube wall for rod E-6 was 257 KHN (Knoop Hardness Number), rod E-10 was 254 KHN, and the as-received tubing was 173 KHN. The accuracy of these readings is ± 8 KHN.

In order to broaden the data already obtained, three separate investigations were performed using the vibratory compaction swaging process. All of the rods utilized were fabricated, compacted and swaged in an identical manner to those of the earlier runs.

In the first investigation, four rods (B-10, C-10, D-11 and E-7) were compacted on a high frequency portable vibrator to an initial density of 84% and then reduce in area 9 to 16% by swaging. These rods contained -12 +30 mesh particle size UO_2 in place of the -6 +16 indicated in Table A-X in order to determine the effect of a finer mesh size on final density and on the internal cladding surface. The initial compaction densities and swaging reductions were chosen for these investigations on the basis that previous experiments indicated that these combinations provided the most economical conditions. Table A-XIV summarizes the densities obtained and results of

TABLE A-XIV

VIBRATORY COMPACTED AND SWAGED FUEL RODS

Rod No.	Gross Average Density After Vibratory Compaction (% T.D.)	Reduction In Area After Swaging (%)	Percent of Theoretical Density for Sections of Swaged Rods*										Gross** Average Density After Swaging % T.D.	Net Change In Wall Thickness After Swaging (Inches)	Maximum*** Wall Depression After Swaging (Inches)	Elong. of Fuel %
			1	3	5	7	9	11	13	15	16					
B-10	84.3	9.8	90.6	88.0	89.2	89.9	90.2	90.4	90.8	91.1	90.8	90.1	+0.0005	0.0030	3.7	
C-10	84.0	12.0	90.2	90.3	90.2	91.2	90.7	90.2	90.8	90.3	90.1	90.4	+0.0006	0.0000	4.5	
D-11	84.2	13.9	89.6	90.3	89.7	89.6	90.0	90.3	90.3	90.2	89.9	90.0	+0.0003	0.0028	8.2	
E-7	84.0	16.3	90.7	90.5	89.7	90.1	90.1	90.3	90.3	90.5	90.2	90.3	+0.0005	0.0042	10.0	
F-1	82.1	22.4	90.3	90.4	90.6	90.5	90.7	90.7	90.4	90.4	89.4	90.4	-0.0002	0.0049	15.7	
F-2	84.1	22.0	90.1	90.2	90.1	90.1	90.8	90.8	90.5	90.6	89.9	90.3	-0.0008	0.0026	18.2	
F-3	85.2	21.8	89.1	90.0	90.4	90.3	90.3	90.4	90.5	90.3	89.4	90.1	-0.0010	0.0036	19.2	
G-2	80.4	21.9	88.1	87.2	87.5	87.7	87.6	88.0	88.8	88.3	88.0	87.9	+0.0002	0.0008	17.2	
G-3	79.8	22.2	88.1	89.1	89.1	88.6	88.0	88.0	88.1	87.9	87.2	88.2	+0.0007	0.0011	15.4	
G-4	80.0	21.9	88.1	88.3	88.1	88.2	88.5	88.4	88.1	88.5	87.4	88.2	+0.0003	0.0010	15.0	

* Three inch long sections selected from equal divisions of each rod.

** Based on average of sections.

*** Average wall thickness minus minimum wall.

+ Increase in wall thickness.

- Decrease in wall thickness.

measurements made on the cladding.

When compared to rods processed under similar conditions but using coarser UO_2 particles (rods B-11, C-12, D-14 and E-10 found in Table A-XI) the fuel in the present series elongated approximately 1 to 3% more with a resultant decrease in density of 0.3% to 2.4% of theoretical. It appears that the decrease in the coarse particle fraction is the factor causing a lower density. It is desirable to have a particle diameter ratio for the coarse, medium and fines in a ternary system of about 100:10:1.

As this ratio is decreased, the density tends to decrease. By decreasing the coarse particle fraction from -6 +16 mesh to -12 +30 mesh, the average diameter ratio was decreased from 30:5:1 to 15:5:1.

Longitudinal sections representing each rod were mounted and examined metallographically. Local depressions up to 0.0042 in. were observed on the inside wall of the swaged tube. This indicates that local depressions up to 0.0045 in. can be expected from rods containing either -6 +16 or -12 +30 particle distribution. No decrease was noted in the magnitude of local depressions by changing the coarse fraction from -6 +16 to -12 +30.

The small reduction in wall thickness caused by swaging (0.0006 in. maximum) is insignificant since it is within the error of the measuring apparatus. Microcracks were not observed on the inside wall of the swaged tube. However, two shallow intergranular cracks (0.004 and 0.006 in. in depth) were noted on sections of the outside diameter. The exact cause of this cracking cannot be determined at the present time, although it is probable that these defects were present in the as-received tubing. It does not appear that these cracks were caused by the process since many rods have been subjected to greater reductions by swaging without any trace of this phenomenon.

Three rods (F-1, F-2, and F-3) containing the three particle size UO_2 distribution, indicated in Table A-X, were compacted on a high frequency portable vibrator to initial densities of 82, 84 and 85%, respectively, and then reduced in area 22% by swaging. These data were utilized to evaluate the effect of the higher reductions on final density. Table A-XIV summarizes the data from this experiment. Rods processed under similar conditions, except that they were subjected to reductions in area by swaging of 15% instead of 22%, resulted in densities approximately 1 to 2% higher. If these data points were added to Figure A-12, a plot of density versus percent reduction in area for various initial compacted densities, it would indicate that the curves are undulating with a maximum density somewhere between 14 to 16% reduction in area. This undulation is attributed to the breakdown of coarse particles. As a result of this breakdown, no further densification occurs and the oxide acts as a solid mandrel causing the tube wall to thin and the element to lengthen.

Metallographic examination of sections from each of the rods gave no indications of microcracks. The maximum internal wall depression was 0.0049 in. In comparison with previous experiments, there seems to be a slight thinning of the clad although the amount is so small that it may be within the error of the measuring equipment.

In order to determine the density limitation imposed by the use of thinner cladding, a series of rods were fabricated from 0.010 in. thick stainless steel tubing. These rods were predensified to 80% of theoretical and then reduced in area 22% by swaging. Two different fused UO_2 particle size distributions, as shown in Table A-XV, were used. The coarse distribution was varied in order to determine the effect on tube integrity. The coarsest powder fraction used in previous experiments i.e., -6 +16 mesh was not

TABLE A-XV

PARTICLE SIZE DISTRIBUTIONS UTILIZED IN
THIN WALL SWAGING EXPERIMENT

<u>Rod Number</u>	<u>Particle Size*</u>	<u>wt%</u>
G-2	-12 +30	60
	-30 +100	15
	-200	25
G-3, G-4	-12 +20	60
	-30 +100	15
	-200	25

* U. S. Standard Sieve Size

utilized in this investigation as it was felt that this size range would be too coarse for thin wall tubes. Based on the results obtained from rods E-15 and E-16 (shown in Table A-XI) one would predict densities 90% of theoretical or greater for the thin wall fuel rods. However, the average density attained was 88%.

This agrees with the analysis of rods B-10, C-10, D-11 and E-7. Both particle size distributions produced the same final average density. A metallographic examination of the clad wall for integrity indicated no microcracks or other clad defects. The net change in the wall thickness after swaging was negligible.

3). Conclusions

The combined vibratory compaction and swaging process can be used to reach fuel densities of 92-93% of theoretical on a reproducible basis. The dimensional and local density variations are small and the process is economical. The data from this program indicated that rods initially compacted on a small portable vibrator to 84% T.D. and reduced in area 12-15% by swaging at room temperature provide the optimum economical combination of conditions. The elements can be vibratory compacted in less than one minute and swaged at the rate of 5-6 linear feet per minute. The gross average fuel density of 92-93% of theoretical is actually higher than the fuel density of a pelletized fuel rod containing pellets at 94% T.D. It appears that this process can become an economic competitor of pelletized UO_2 .

f. Rock-Riting

1) Introduction

The Rock-Rite process is used in the production of tubing from large tube rounds. The unique die design permits reductions of 50% or more in one pass. In addition, long continuous lengths can be handled with ease. Because of these features, it was anticipated that this process would be capable of producing powder loaded fuel elements.

An attempt was made to reduce a series of tubes containing fused UO_2 by this process. As a result of the development of an axial compressive load on the unreduced portion of the rods, buckling forced termination of these experiments. These experiments were performed on a Rock-Rite machine located at a vendor's facility aimed primarily at tubing production. Before this effort was initiated long negotiations occurred which were primarily hampered by considerations of uranium dioxide spills in the event of clad failure. It was for this reason, that the buckling, noted above, became alarming and resulted in a redirection of this effort. On this basis, ten rods, representing reductions in area of 20 to 39% and initial densities of 75 and 80% of theoretical were analyzed and all current conclusions concerning this process are based on this analysis.

2) Program

Twenty-nine, Type 304 stainless steel tubes with outside diameters of 0.563, 0.588, 0.625, 0.688 and 0.750 in. and a wall thickness of 0.028 in. were filled with approximately five feet of fused UO_2 . Sixteen of these tubes contained the three particle size UO_2 distribution, shown in Table A-XVI. Eight of the sixteen tubes were vibratory compacted to 75% T.D. and the remainder to 80% T.D. The remaining thirteen tubes were loaded with -12 mesh fused UO_2 powder and were vibratory compacted to 75% T.D. A sieve

TABLE A-XVI

PARTICLE SIZE DISTRIBUTIONS UTILIZED IN
ROCK RITE EXPERIMENT

	Particle Size*	wt%
CEND Three Particle Size Distribution	-6 +16	60
	-30 +100	15
	-200	25
Sieve Analysis of -12 Mesh UO_2	-12 +20	17.6
	-20 +40	9.3
	-40 +60	6.0
	-60 +100	27.7
	-100+200	18.7
	-200	20.7

* U. S. Standard Sieve Size

analysis of the -12 mesh powder is shown on Table A-XVI. All of the tubes were scheduled for reduction to a final outside diameter of 0.500 in., which would result in reductions in fuel cross sectional area ranging from 23 to 59%.

Figure A-13 illustrates an as-fabricated rod prior to Rock Riting. Foam rubber inserts were used as end plugs and the end sections of each tube were swaged to a diameter 0.020 in. less than the expected final outside diameter after Rock Riting to lock in the end caps during fabrication.

Many unexpected problems developed in the attempt to Rock Rite the specimens. Originally, the rods were to be joined by means of a connector rod (Figure A-14). This method was used effectively on previous experiments with MgO filled rods, but was unsuccessful at this time as evidenced by buckling and bending at the connector rod joint. This was the first indication that the rods were being compressed between the feed mechanism and the dies. The first six rods, representing reductions in area from 20 to 30%, were processed individually at a reduced feed rate. Attempts to achieve a 40% reduction in area on four additional rods were terminated because of tube buckling (Figure A-15) after only one-third of the length of each rod passed through the dies. Because of the possibility of a tube wall failure and a resultant spill of UO_2 into the machine, the remainder of the experiment was cancelled. Analysis of the four partially Rock Rited rods revealed that the tube diameter expanded 0.010-0.015 in. in the region ahead of the die. The expansion extended to the guide bushing as shown in Figure A-15.

The cause of the difficulties during this experiment is not clearly understood at this time, although a portion of the problem may be associated with improper control of forces during fabrication, causing an axial compression load on the unreduced portion of the rods which resulted in buckling.

The dies designed for this experiment were intended to maintain zero axial loading but very slight compressive loadings did develop and the fuel tube was not able to bear the columnar loading.

The six Rock Rited rods plus the sections of the four rods which were partially reduced were evaluated for dimensional tolerance, and the relationship between reductions in area and final densities. The dimensional survey of the rods revealed a variation from rod to rod of ± 0.0025 in. on the diameter compared with ± 0.001 in. for the MgO samples. Depending on the final length of the rods, two to six three-inch sections were removed from each rod at equal intervals. These sections were analyzed for local density variations by a water immersion technique. The gross density after Rock Riting was determined by averaging the densities of the three-inch segments. Table A-XVII indicates that rods initially densified to 75% T.D. and reduced in area 38 to 39% resulted in final densities between 90.2 and 91.6% of theoretical. Rods compacted to 80% and reduced in area 38% resulted in a final density of 90.2% of theoretical. Local density variations are shown in Table XVII. The general trends in gross and local densities are similar to those encountered with samples which were vibratory compacted and swaged to reductions in area less than 20%.

Rods R-2-3 and R-2-4 contained -12 mesh fused UO_2 powder. The remainder contained the three particle distribution (Table A-XVI). Since the experimental error involved in density measurements is about 0.5%, there is no apparent difference in density between rods processed under equivalent conditions and containing the above two particle size distributions.

Sections from the six rods were mounted and examined for clad integrity. Results are summarized in Table A-XVIII. Microcracks were not observed in any of the sections. Local depressions up to 0.0032 in. were noted on the

TABLE A-XVII

ANALYSIS OF ROCK RITED FUEL RODS

Rod No.	Gross Average Density Prior to Rock Riting (% T.D. ^b)	Reduction In Area Of Fuel After Rock Riting (%)	Elongation Of Fuel (%)	Gross Average Density After Rock Riting (% T.D.)	Density of Rock Rited Fuel Rod Sections ^c (% T.D.)								Maximum Local Variance From Gross Average Density	
					1	3	5	7	9	11	Plus	Minus		
R-2-4 ^a	75.0	22.5	11.0	86.8	85.9	87.2	86.6	86.2	87.4	87.4	0.6	0.6		
R-2-3 ^a	75.0	23.1	9.9	88.0	88.2	87.5	86.6	88.3	88.2	87.2	0.3	1.4		
R-2-1	75.1	24.1	8.6	88.5	86.9	89.5	89.9	84.8	90.3	89.8	1.8	3.7		
R-3-2	74.9	29.0	16.0	89.6	85.1	90.6	90.8	90.5	90.7	89.9	1.2	4.5		
R-3-1	74.9	30.3	17.2	89.5	----	89.2	89.8	90.1	89.6	88.8	0.6	0.7		
R-4-2	76.3	38.7	----	90.2	88.6	90.7	91.3	----	----	----	1.1	1.6		
R-4-1	74.9	39.3	----	91.6	91.1	92.2	----	----	----	----	0.6	0.5		
R-2-2	79.9	22.9	13.3	91.1	88.5	93.4	90.3	93.5	90.4	90.3	2.4	2.6		
R-3-3	80.2	29.0	----	92.0	93.7	91.5	90.7	----	----	----	1.7	1.3		
R-4-3	80.1	37.7	----	90.2	90.3	90.1	90.3	----	----	----	0.1	0.1		

a - Contains -12 Mesh Fused UO₂

b - T.D. - Theoretical Density

c - Three Inch Long Sections Selected From Equal Divisions Along the Length of Each Rod

---- Not Analyzed

TABLE A-XVIII

SUMMARY OF TUBE WALL EXAMINATION

Rod No.	Gross Average Density After Rock Riting (% T.D.) ^(a)	Reduction In Area Of Fuel After Rock Riting (%)	Gross Average Density Prior to Rock Riting (% T.D.)	Average Wall Thickness Prior to Rock Riting (Inches)	Average Wall Thickness After Rock Riting (Inches)	Minimum Wall Thickness After Rock Riting (Inches)	Net Change In Wall Thickness Due to Rock Riting ^(b) (Inches)	Average Minus Min. Wall Thickness After Rock Riting (Inches)
R-2-4	86.8	22.5	75.0	0.0285	0.0287	0.0255	+0.0002	0.0032
R-2-3	88.0	23.1	75.0	0.0285	0.0294	0.0270	+0.0009	0.0024
R-4-1	91.6	39.3	74.9	0.0284	0.0309	0.0285	+0.0025	0.0024
R-2-2	91.1	22.9	79.9	0.0281	0.0287	0.0255	+0.0006	0.0032
R-3-3	92.0	29.0	80.2	0.0283	0.0283	0.0255	0.0000	0.0028
R-4-3	90.2	37.7	80.1	0.0282	0.0271	0.0240	-0.0011	0.0031

a - T.D. - Theoretical Density

b - (+) Increase in Wall Thickness After Rock Riting

(-) Decrease in Wall Thickness After Rock Riting

inside wall. This is slightly better than the 0.0045 in. local depressions noted in rods which were vibratory compacted and swaged. However, higher densities were attained with the latter process. The lower density of rod R-4-3 which had a higher initial compacted density and greater reduction-in-area can be attributed to the change in wall thickness. It is apparent from the thinning of the clad that the UO_2 has reached its maximum density and is acting as a mandrel for the clad. In doing this, the particles have been broken into finer sizes causing a reorientation to take place with a resultant decrease in density.

3) Conclusions

Although earlier experiments using MgO established a feasible process, it is apparent from the present investigation that a considerable effort would be required to make this a practical process for thin-walled UO_2 filled fuel rods. Since greater success has been attained with alternate methods of fabricating powder packed fuel elements such as vibratory compaction and the combined vibratory compaction-swaging process, no further effort was expended on this task. This conclusion is particularly valid on the basis of the attainment of similar results at lower reductions with the combined vibratory compaction-swaging process. It is possible that the heavier tube walls used with Zircaloy and the trend toward increasing fuel rod lengths may dictate the reactivation of experimental effort in the future. However, before such reactivation, an evaluation of available processes should demonstrate a realistic incentive for proceeding with Rock Rite investigations.

FIG A-1 FUEL PROCESSING ROOM

FIG A-2 FLOW CHART OF CERAMIC FUEL PROCESSING OPERATION

FIG A-3 VIBRATORY COMPACTION APPARATUS SHOWING MOUNTED VIBRATORS, RAM, AND COMPACTION DIE

FIG A-4 SCHEMATIC OF SAMPLE COMPACTION OPERATION

FIG A-5 VIBRATORY COMPACTION APPARATUS SHOWING HIGH IMPACT UNIT
VIBRATORS (VIV) IN UPPER POSITION

FIG A-6 MULTIPLE PNEUMATIC COMPACTION APPARATUS WITH HIGH FREQUENCY VIBRATOR

FIG A-7 MULTIPLE PNEUMATIC COMPACTION APPARATUS MODIFIED TO ALLOW DIFFERENTIAL COMPACTION HEIGHTS

FIG A-8 GROSS DENSITY (% THEORETICAL DENSITY) AND LOCATION OF EACH ROD
IN A SIMULTANEOUSLY COMPACTED THIRTY-SEVEN ROD HEXAGONAL GROUP

FIG A-9 GROSS DENSITY (% THEORETICAL DENSITY) AND LOCATION OF EACH ROD IN A COMPACTED THIRTY-SEVEN HEXAGONAL PROJECTION WELDED CLUSTER

FIG A-10 MULTITUBE ANNULAR FUEL ELEMENT

FIG A-11 MULTITUBE ELEMENT CUTAWAY

FIG A-12 VARIATION IN UO_2 DENSITY WITH REDUCTION-IN-AREA

FIG A-13 ROCK RITE SPECIMEN

FIG A-14 ROCK RITE SPECIMEN WITH CONNECTOR ROD FOR CONTINUOUS ROCK RITING

FIG A-15 SCHEMATIC OF ROCK RITE SPECIMEN UNDER COMPRESSIVE LOAD

B. Thermal Performance Characteristics of UO₂

1. Introduction

The objectives of this program were to determine the out-of-pile thermal conductivity of UO₂ at high temperatures and to establish a correlation between the resulting microstructures and the thermal history of the element. Thermal performance testing attempts have been undertaken to simulate, out-of-pile, the thermal conditions that prevail in a fuel element during irradiation. Successful attainment of these objectives will provide more confidence in UO₂ thermal conductivity values and in the utilization of microstructural analysis as a means of deducing the thermal history of a fuel element.

2. Summary of Early Testing

To achieve the objectives of this program, test elements and associated testing equipment were designed. The basic requirements of the test equipment were to produce temperatures on the order of 2500°C at the center of the test element while maintaining element cladding temperatures at levels characteristic of reactor conditions. In order to achieve significant volumes of large grains, the cladding temperature must be high. On the other hand, to measure thermal conductivity of the UO₂ under large thermal gradients, the cladding temperature must be relatively low. The test equipment was designated to achieve both of these conditions. The design of the test equipment is described in detail in a previous Summary Report CEND 186³. The changes and modifications to the equipment and improvements in test procedures are summarized in this report. In addition, a detailed description of the most recent test methods and results is also presented.

The fuel containing test element was a 1.27 cm (0.5 in.) diameter tube, 20.3 cm (8 in.) long with a 0.038 cm (0.015 in.) thick cladding. Fine-wire thermocouples were spot welded to the cladding. On its axis a 0.190 cm (0.075 in.) diameter tungsten rod served as the

electrical heating element. When temperature measurements at various radial depths within the UO_2 were needed, holes were drilled through the cladding into the UO_2 to provide a sighting path for the optical pyrometer. The cooling jacket was a double walled cylinder, open at each end, which surrounded the test specimen. The annulus between walls contained flowing water for heat removal. A longitudinal slot in the cooling jacket provided the means for temperature measurements with an optical pyrometer.

When test conditions required a low cladding temperature, the space between the specimen and the inner wall of the cooling jacket was filled with a cast metal (Figure B-1). When high cladding temperature was desired, the space was occupied by inert gas (Figure B-2). In the high temperature case, the specimen was suspended from molybdenum wire loops. During the "grain growth" phase, the space between the specimen and the cooling jacket was filled with inert gas. The relatively low thermal conductance of the gas gap resulted in a cladding temperature much higher than if the gap were filled with metal. In addition, by varying the composition of the gas the thermal gradient within the specimen was varied, over a wide range, independently of the cladding temperature. This feature of "gradient control" provided additional freedom to establish a variety of thermal conditions within the specimen for purposes of achieving controlled grain growth.

The design considerations regarding "gradient control" were as follows: The total power loss from the specimen cladding was determined by a combination of two factors: first, the radiant heat loss, proportional to T^4 , is a function of cladding temperature, emissivities, and geometry; it is essentially independent of the nature of the blanket gas. The second factor, gas conduction, is approximately proportional to the difference between the cladding temperature and the cooling jacket temperature; it is quite sensitive to the blanket gas conductivity, particularly if one considers the wide difference in conductivity

between argon and helium. Therefore, for any given cladding temperature there is considerable ability to control the power input by adjusting the blanket gas conductivity, using argon, helium, or a mixture of the two. Initial test runs were made with elements of the "hot" cladding configuration. Both powder-packed and pelletized elements were tested with the intent of achieving conditions which would induce grain growth in the UO_2 . In all cases, the cladding temperature was the limiting factor controlling the power input. The stainless steel cladding temperature was limited to about 1120°C .

Cladding temperatures were measured with an optical pyrometer. Emissivity and transmission corrections were established by comparison with thermocouples attached to the cladding. The center temperatures (tungsten heater rods) were determined, in the case of the powder-packed elements, by measuring the overall electrical resistivity of the tungsten. This implicit measure of temperature was subject to several errors; the resistivity of the rods may not correspond to that of published data for pure tungsten due to impurities or processing methods; and most important, the axial temperature profile was not flat, thus contributing a significant unknown to the measurement of the overall resistance, and in turn to the determination of local temperature. The center temperature of the pelletized elements was determined by a sight hole drilled radially through the pellet, permitting optical pyrometry of the heater rod.

The initial effort using the hot cladding technique resulted in maximum heater temperatures in excess of 2200°C with the cladding temperatures approximately 1100°C . Post test examinations revealed no substantial microstructural changes.

The failure to produce substantial microstructural changes in the first series of tests could be attributed to three inter-related conditions, namely:

- a. the relatively low element center temperature coupled with
- b. the low radial thermal gradient and

c. very short test time.

As a result, hot cladding testing was discontinued.

Based on these results, the "cold cladding" tests (cladding temperature on the order of 100°C and center temperature in excess of 2200°C) were initiated in an effort to verify the thermal gradient influence on microstructural change and to develop a series of different microstructures for further study.

The system for "cold cladding" tests consisted of a tungsten heater on the axis of the UO_2 fuel rod, with a cast alloy heat transfer medium between the cladding and a surrounding water cooling jacket. The temperature of the heater rod was determined by optical pyrometry, sighting down "standpipes" and through a slot in the face of the UO_2 pellet, as shown in Figure B-1. Temperature readings were taken at 1.27 cm (0.5 in.) increments along the 20.3 cm (8 in.) length of the test sample. The temperature of the cladding was measured with thermocouples at corresponding axial positions.

Two inter-related effects limited the maximum heat flux in the test element to values lower than desired. The first effect which limited the experimental effort was hotspot development. This can be ascribed to the electrical resistivity versus temperature characteristics of the tungsten heater rod, particularly in the temperature range from 1000 to 2000°C. Appendix I of CEND-199⁴ explores this matter in detail. In brief, small local variations of conductance or geometry result in large changes in the center temperature of the element when using a tungsten heater. Because it is not possible to fabricate a unit completely free of such anomalies, hotspots develop. Therefore, use of a different heater material could alleviate this situation.

Tantalum (or tungsten-rhenium with a resistivity versus temperature relationship similar to tantalum) can be substituted for the tungsten as a heater element; to reduce hotspot formation. One experimental run was made to confirm the validity of this approach, and the results were promising.

The second effect limiting experimental effort was a high temperature drop through the cast alloy. Since the alloy had a melting point of 138°C , and the system was not designed to operate with molten metal, the maximum heat flux permissible was lower than that needed to achieve high temperatures at the center of the UO_2 . This excessive temperature drop represented a low value of total conductance from cladding to water jacket. The exact cause for this drop is not readily determined experimentally. However, it is probable that interface thermal resistance is the major contributing factor to low conductance in the coolant system.

The original choice of alloy was "Cerro Tru" a tin-bismuth eutectic consisting of 42% tin and 58% bismuth, having a thermal conductivity of about 0.2 watts/cm- $^{\circ}\text{C}$. It was chosen because of its desirable casting characteristics, notably that it undergoes a slight expansion on freezing from the melt. When systems were tested using this alloy, the temperature drop from fuel element cladding to cooling jacket was about twice that which had been calculated, considering the conductivity of the alloy, the geometry, and the heat flux. As a result, the heat flux had to be limited to avoid melting of the alloy.

An attempt was made to use a different alloy, "Cerro Cast" consisting of 60% tin and 40% bismuth, and having a thermal conductivity of 0.38 watts/cm- $^{\circ}\text{C}$. Although this alloy had a melting point which is the same as Cerro Tru, it did not cast as cleanly. When the system was run under power, the temperature drop clad to jacket was only slightly less than before. Thus, the heat flux limitation still existed.

Finally, a solid copper slug was machined and placed in the system. It was proportioned to almost fill the annulus between the clad and jacket, leaving only a 0.16 cm (0.062 in.) gap to be filled with casting alloys. It was hoped that the high conductivity of the copper, about 3.9 watts/cm- $^{\circ}\text{C}$, or approximately 10 times that of the casting alloy, would overcome the deficiencies of the

system and permit operation at higher heat fluxes. Major improvements were not observed.

It was concluded that interface resistance was the controlling factor in the coolant system and that variations of solid compositions would not result in significant improvement. Direct water cooling of the clad appeared to be the only feasible solution.

In order to obtain added data while the cold cladding equipment was being modified, additional hot cladding runs were made. The objectives were to determine if tantalum and/or tungsten-rhenium would be useful in this work, and further, to attempt to obtain significant microstructural changes in the UO_2 by operation with a large portion of the UO_2 at high temperature. Maximum heater temperatures of approximately 1800°C were reached with the cladding at about 1150°C . At power inputs above this condition, the ends of the elements rose in temperature much faster than the center portion. The cause of this behavior is explored in greater detail in CEND-204⁵. Thus, the attempts to determine if tantalum and/or tungsten-rhenium heaters were useful in this work and to obtain significant microstructural changes in the UO_2 by operation with large portions of the UO_2 at high temperature failed due to the inability of the system to attain high temperatures and still maintain a uniform axial temperature gradient. Lack of success with this phase of the testing led to final abandonment of the initial hot cladding test technique.

In order to provide direct water cooling of the cladding, it was necessary to devise an assembly wherein the water could be kept out of the UO_2 and at the same time permit optical observation of the heater at many points. A new cooling jacket was designed which provided for sealing the two ends of the fuel assembly with O-rings; the 13 individual sighting standpipes were also sealed with O-rings. This design permitted flooding virtually all of the element cladding with water, while operating the assembly in the atmosphere enclosure.

Test run 32 was aimed at confirming the value of both technique modifications: using a heater made of tantalum and direct water cooling of the cladding. The cladding was fitted with 13 standpipes, so that water cooling could be used, while optical sighting paths into the fuel were maintained. The test was a success in that both objectives were met. A reasonably flat axial temperature profile was obtained with deviations of less than 100°C at an average temperature of 1700°C . A power input in excess of 5000 watts was dissipated without difficulty. On the other hand, it was established that the choice of standpipe diameter was poor, in that an aperture effect was introduced in the optical path and made the determination of accurate temperatures impossible. For this reason, the test was terminated without attempting to take extensive data. The needed modification of equipment to accommodate larger standpipes was undertaken. Results of this test firmly established that solutions had been obtained to the two previous problems. The significant result was verification of the predicted influence of differences in heater resistivity-temperature relationships on thermal stability.

The primary objective of run #35 was to prove the adequacy of all the prior system modifications: direct water cooling on the clad, use of tungsten-rhenium heater material, and large sighting standpipes. Only seven standpipes were used; and all of these sighted directly on the heater. When the centerline temperature reached about 2350°C , there was rapid grain growth in the bottom of the sight holes, which caused blockage of the sight path within a few minutes. This phenomenon is attributed to vapor phase transport of UO_2 , and deposition in the cooler portions of the system. From this point in the test the center temperature had to be calculated from the electrical resistance of the heater, based on prior calibration and observations at lower temperature. This test run is worthy of particular mention because for the first time:

- a. The centerline temperature of the element was raised to about 2530°C

average, with a maximum temperature slightly in excess of 2600°C and held for an hour on three successive cycles. Although all the sight holes closed because of UO_2 transport at the highest temperature, sufficient data was taken during the first heating cycle to provide a correlation between heater resistance and temperature, this data was then used on following runs to determine center temperature.

b. Significant microstructural changes took place in the hottest portion of the system. As a result of this test run, we felt that the objectives of the task could be achieved with only minor modifications.

Having demonstrated the methods in run #35, test element #36 was prepared with 13 standpipes so that optical pyrometer readings could be taken at a number of radial positions. This would permit calculation of the thermal conductivity of the UO_2 fuel. The objectives of this test run were achieved without difficulty and reported in greater detail in CEND-211⁶. However, when preliminary thermal conductivity calculations from the measured data were made, we felt that an adjustment in position of the sight holes was necessary in order to achieve more reliable data.

Test element #37 was fabricated for the express purpose of modifications in the sight hole positions. By providing greater radial separation between the outermost two positions, the temperature difference across that interval would be greater thus giving a more reliable value of conductivity. This did not reach readable levels early enough in the run to be of much value. Data was collected however, and thermal conductivity calculations were again made, demonstrating the feasibility of the test technique. At this point in the test program, we felt that enough preliminary testing had been undertaken to instill confidence in the element fabrication techniques as well as the overall test system to conclude the preliminary test phase of the task and to proceed to design elements which would be capable of the type of performance

necessary in meeting the task objectives.

The final phase of the program consisted of tests made with elements # 38 to #41. Detailed descriptions of the test equipment and techniques as well as the procedures followed in obtaining thermal conductivity and microstructural changes are presented in the sections that follow.

3. Experimental Test System

The experimental test system was designed to meet the following requirements within the test elements; (a) a high central temperature, (b) a variable radial temperature gradient, (c) the desired gaseous atmosphere, (d) optical temperature measurements at selected radial locations with the fuel and (e) relative flexibility as to physical size and power requirements of various types of fuel element configurations.

A schematic of the entire test chamber is shown in Figure B-3. Because of the complexity of some of the components, they are described in detail.

a. Test Element Components

1) Test Element

Two types of test elements having different radial temperature gradients were designed and fabricated to permit the study of the influence of radial temperature gradient on the onset of various microstructural constituents and to increase the reliability of radial temperature measurements. In this report, they are referred to as the simple and compound test elements.

a) Simple Test Element

The simple test element consisted of a cylindrical column of UO_2 fuel pellets having an axial heater rod and clad in stainless steel. The dimensions of the components are listed in Table B-I and details of the simple test element are shown in Figures B-4a and B-5a.

The tungsten-rhenium alloy was selected for the heater material after experiments with pure tungsten rods resulted in a large variation in axial temperature; tantalum heaters deteriorated rapidly at temperatures in excess of 2000°C . The heater rods were initially centerless ground to the desired diameter and then cut to length before assembly.

Thirteen stainless steel standpipes were brazed externally to the cladding and provided optical paths through the cooling water to the fuel for temperature

TABLE B-I

DIMENSIONS OF SIMPLE TEST ELEMENT COMPONENTS

COMPONENT	MATERIAL	DIMENSIONS
Cladding	Type 304 SS	Outside Diameter 1.27 cm (0.500 in.)
		Inside Diameter 1.17 cm (0.460 in.)
		Length 21.6 cm (8.5 in.)
Stand Pipes	Type 304 SS	Outside Diameter 0.635 cm (0.250 in.)
		Inside Diameter 0.534 cm (0.210 in.)
		Length 2.54 cm (1.00 in.)
Fuel Pellet	UO ₂	Outside Diameter 1.17 cm (0.460 in.)
		Inside Diameter 0.190 cm (0.075 in.)
		Length 0.635 cm (0.250 in.)
Heater Rod	W-26 Re	Diameter 0.190 cm (0.075 in.)
		Length 24.2 cm (9.5 in.)

measurement. Slots cut in the fuel pellets to selected depths provided unobstructed paths for temperature measurements at the desired radial locations within the UO_2 or at the surface of the heater.

The fuel pellets were ground to O. D. and bore tolerances of less than ± 0.003 cm (± 0.001 in.), which provided approximately zero radial clearance between the pellets, the cladding, and the tungsten-rhenium heater at test temperatures. After assembly, each test element was radiographed to ensure that all pellets were properly positioned within the cladding.

b) Compound Test Element

The fabrication and assembly of the compound test element was identical to the simple test element with only three major changes in element configuration. Essentially, the cladding was increased to 1.905 cm (0.750 in.), the fuel column was reduced to 0.864 cm (0.340 in.) O. D. and ZrO_2 insulating sleeve was placed in the annulus between the fuel and the cladding. The purpose of this sleeve was to decrease the radial temperature gradient for any given heater temperature. Details of the compound test element configuration are shown in Figures B-4b and B-5b and dimensions are listed in Table B-II.

2) Cooling Jacket

The cooling jacket (Figure B-6) is a brass cylinder 6.35 cm (2.50 in.) in diameter flanged at both ends to permit easy insertion and removal of the test element. The 13 standpipes extend through the top of the jacket where they are sealed with O-rings. The element also is sealed to the end flanges of the cooling jacket with O-rings. The annulus between the test element and cooling jacket contains flowing deionized water for heat removal. Diffusers are provided to eliminate non-uniform cooling of the cladding due to impingement of the inlet water stream. The cooling water (Figure B-7) was recirculated and purified in an ion exchange column and cooled in a heat exchanger by tap water. The primary-secondary water system prevented precipitation of the mineral salts contained in tap water on the

TABLE B-II

DIMENSIONS OF COMPOUND TEST ELEMENT COMPONENTS

COMPONENT	MATERIAL	DIMENSIONS
Cladding	Type 304 SS	Outside Diameter 1.905 cm (0.750 in.)
		Inside Diameter 1.830 cm (0.720 in.)
		Length 21.6 cm (8.5 in.)
Stand Pipes	Type 304 SS	Outside Diameter 0.635 cm (0.250 in.)
		Inside Diameter 0.534 cm (0.210 in.)
		Length 2.54 cm (1.00 in.)
Insulating Sleeve	ZrO ₂	Outside Diameter 1.830 cm (0.720 in.)
		Inside Diameter 0.864 cm (0.340 in.)
		Length 1.27 cm (0.500 in.)
Fuel Pellet	UO ₂	Outside Diameter 0.864 cm (0.340 in.)
		Inside Diameter 0.190 cm (0.075 in.)
		Length 0.635 cm (0.250 in.)
Heater Rod	W-26 Re	Diameter 0.190 cm (0.075 in.)
		Length 24.2 cm (9.5 in.)

test element. (This modification was added after testing of element # 38).

3) Test Chamber

The cooling jacket-test element assembly is contained within a stainless steel chamber approximately 30.48 cm (12 in.) long, 15.24 cm (6 in.) wide, 12.70 cm (5 in.) deep. The chamber cover, containing the viewing window, is removed for insertion and removal of the test element. Liberal use of O-rings permits quick access into the chamber and provides adequate hermetic sealing. The leak rate of the chamber, blanked off from the pumps, is less than 0.5×10^{-3} Torr per minute. A vacuum level of about 10^{-5} Torr can be attained within 30 minutes from a cold start.

Power terminals, at both ends of the chamber, consist of water-cooled copper bellows with a split copper clamp to provide quick coupling to the tungsten-rhenium-heater. This design provides high current capacity while accommodating axial thermal expansion of the heater. The terminals were electrically insulated from the stainless steel chamber by plexiglass plates and hermetically sealed by O-rings.

The atmosphere control system (Figure B-8) includes a diffusion pump with its associated cold trap and mechanical pump; a second mechanical pump for preliminary pumpdown; gas purification furnaces containing zirconium chips; and a pressure relief system to prevent pressure buildup in the test chamber. Appropriate high vacuum valves provide good flexibility of operation.

Fuel temperature measurements are made with a laboratory type optical pyrometer equipped with a prism at the objective end to permit sighting in a vertical direction. Reproducibility of temperature measurement from operator to operator was found to be within 2°C . Corrections for specimen emissivity and optical path transmission were obtained by comparison of optical pyrometer readings with thermocouple readings. No evidence of sight glass degradation due to possible vapor deposition was observed.

b. Test Element Fabrication and Preparation

Pellet preparation, assembly, thermal cycling and examination procedures of the test elements are important functions in the overall test program. Caution was exercised in the grinding and assembly operations to maintain the close tolerances required to minimize temperature drops at the heater-pellet and pellet-cladding interfaces in simple test elements; and at the heater-pellet, pellet-insulator, and insulator-cladding interfaces in compound test elements.

The outside diameter of the pellets was finish ground to provide a cold clearance of less than 0.003 cm (0.001 in.) with the stainless steel cladding. The bore of the pellets was similarly ground to provide intimate pellet-heater rod contact during assembly. All pellets were end faced to a length of 0.635 cm (0.500 in.)

Thirteen out of the 32 pellets in each element were slotted axially for temperature measurements (Figures B-4 and B-5). Initially, slots 0.038 cm (0.015 in.) wide were cut axially into the pellets to the desired radial locations. After measurements were made to confirm the depth of the slots, a second axial cut widened the slot to 0.102 cm (0.040 in.) to a depth of 0.254 cm (0.100 in.) from the pellet I.D. This widened slot provided the optical angle necessary for proper viewing.

ZrO₂ sleeve pellets were drilled with a 0.318 cm (0.125 in.) diameter hole for the necessary optical path. In addition, three of the 13 optical measurements were made on the surface of the UO₂, thereby reducing the number of slotted UO₂ pellets to 10.

The fuel pellet column was assembled by individually inserting pellets over the heater rod, which was positioned in the center of the cladding. Each slotted pellet was positioned beneath its standpipe and then aligned by a shaped pin which was inserted through the standpipe and into the slot in the pellet. Retention of the alignment pins within the pellet slots during assembly prevented rotational displacement of the slotted pellets in relation to the standpipes. The

assembled test element was radiographed to establish proper assembly of the pellet column within the clad. Prior to test, the assembly was vacuum dried at 125°C for 8 to 10 hours. The test element was inserted into the cooling jacket and the assembly installed into the test chamber for evacuation to a pressure of about 10^{-5} Torr. The system was backfilled to atmospheric pressure with inert gas and the test element outgassed further by increasing the temperature of the heater to about 300°C. Several cycles of evacuation followed by backfilling with an inert gas were performed to ensure complete removal of moisture prior to the start of the high temperature tests.

4. Experimental Procedure

a. Thermal Conductivity

1) General Test Procedure

Power was applied to the heater rod at a rate of 100 watts/minute. When temperature measurements were made, a period of three minutes after a power change was allowed for reaching equilibrium. This length of time was determined to be adequate for reaching equilibrium by taking a number of temperature measurements at one minute intervals following a power change and determining the time necessary for the temperature readings to agree within 5°C.

Optical temperature measurements were made whenever the temperature of any radial position exceeded approximately 750°C, the lower limit of the optical pyrometer.

A number of heating and cooling cycles were usually made for comparison of data points between cycles and to determine any characteristic shifts of the temperature measurements as a result of thermal cycling. It was desirable to make the initial cycle one which would illustrate the general temperature behavior of the test element, i.e. (axial temperature variation) as well as the temperature at which the optical paths used to measure the heater temperature would begin to close by vaporization and deposition of the UO_2 . This was found

to occur when the heater temperature reached approximately 2350°C. The rate at which degradation of the optical paths proceeded increased with temperature to such an extent that the paths would completely close in a very short time at temperatures approaching 2400°C.

Following such an initial cycle, it was desirable to continue with a second heating cycle during which extensive temperature measurements were made. Similar measurements were made during a cooling cycle for comparison of the heating and cooling data.

The major portion of the temperature measurements for determining thermal conductivity were gathered from the procedure described above. Since relatively long time, high temperature operation was desirable for studying microstructural changes in the fuel, an additional cycle was run for this purpose. During this cycle a number of temperature measurements were made during heatup to serve as checkpoints and the test element was brought to a temperature of approximately 2400 to 2500°C and held for several hours or until the desired heater temperature could no longer be maintained (as determined by heater voltage drop measurement).

During all the thermal cycles, heater rod current and voltage were measured and the optical temperatures were recorded. The measured temperatures were corrected for emissivity and obstructions in the optical path before being used for thermal conductivity calculations.

2) Specific Tests

a) Element #38

This test element was designed so that a large number of temperature measurements could be made which would yield thermal conductivity values in the temperature range between 1000 and 1600°C. Details of the sight hole positions for element #38 are presented in Table B-III.

The first three temperature cycles (Figure B-9a) were made to determine the effectiveness of the sight hole positions. The next cycle was aimed at continuous

TABLE B-III

SIGHT HOLE POSITIONS

Sight Hole	Radial Distance Between Heater Rod Surface and Sight Hole Bottom					
	Element 38		Element 39		Element 41	
	cm	in.	cm	in.	cm	in.
1	0.000	0.000	0.000	0.000	0.000	0.000
2	0.041	0.016	0.333	0.131	0.333	0.131
3	0.104	0.041	0.201	0.079	0.203	0.080
4	0.000	0.000	0.000	0.000	0.000	0.000
5	0.170	0.067	0.071	0.028	0.076	0.030
6	0.099	0.039	0.333	0.131	0.333	0.131
7	0.000	0.000	0.000	0.000	0.000	0.000
8	0.043	0.017	0.076	0.030	0.076	0.030
9	0.165	0.065	0.201	0.079	0.203	0.080
10	0.000	0.000	0.000	0.000	0.000	0.000
11	0.041	0.016	0.333	0.131	0.333	0.131
12	0.102	0.040	0.074	0.029	0.076	0.030
13	0.000	0.000	0.000	0.000	0.000	0.000

operation for 24 hours with a centerline temperature of about 2500°C.

The desired test conditions were achieved without difficulty and a surface heat flux of 65 w/cm² was maintained for a few hours. After 13 hours at temperature, unstable electrical conditions were observed which increased with time and failure of the heater occurred after about 20 hours of operation. Even though the test ended in failure of the heater, sufficient temperature measurements were made from which a thermal conductivity curve could be evaluated.

The probable cause of failure was revealed on post-test examination of the element. A heavy mineral scale was found in the element in two regions; at the two ends where there was a short section of cladding not in contact with flowing coolant and in a band in the vicinity of sight hole #3. Since both areas were exposed to tap water, the scale build up was attributed to localized hot spots on the cladding. The scale was analyzed by X-ray diffraction analysis and found to be CaCO₃.

Due to scale formation on the element, ordinary tap water was considered unsatisfactory as the coolant; therefore, the primary-secondary cooling water system previously described was installed.

b) Element #39

This element was of the compound configuration previously described. Its purpose was to provide a radial temperature profile significantly different in shape from the radial temperature profile of the simple elements. Because the ZrO₂ has a lower thermal conductivity than that of UO₂, the radial temperature profile could be expected to be less steep. Therefore, the effect, if any, of a lower temperature gradient on the microstructure would be demonstrated, and the radial position of a given isotherm would be further away from the heater permitting more accurate thermal conductivity data. The sight hole positions for the test element #39 are presented in Table B-III.

A number of low temperature (1600 to 1800°C) cycles (Figure B-9 b) were

made to determine the behavior of the element and to check the adequacy of the sight hole positions prior to high temperature operation. Following these cycles, the element was heated to approximately 2400°C for 3 hours. During this cycle, a sufficient number of sight holes had closed and further thermal testing was therefore discontinued.

c) Element #40

This element was essentially identical to element #39. The tests of this element were considered unsatisfactory in that the axial temperature variation as compared to the axial temperature variation of the other elements tested was too large to be considered acceptable. A brief analysis of test-conditions failed to establish the cause of this anomalous behavior. Therefore, no further references are made to element #40 in this report.

d) Element #41

This element was again practically identical to element #39; the sight hole plan is presented in Table B-III. The first cycle of this element (Figure B-9c) consisted of rapid heatup, followed by rapid cooling; the purpose of this cycle was to determine the temperature at which closure of the optical paths would occur.

The element temperature was increased and temperature measurements were recorded until visible closure of the sight holes used for measuring the heater temperature began. The closure of the sight holes began when the heater temperature reached about 2400°C . As soon as this was observed, the element temperature was rapidly reduced suppressing the action and preserving the use of the sight holes for additional temperature measurements.

The second cycle consisted of small incremental increases in temperature (until the heater reached approximately 2300°C) followed by small incremental temperature decreases. This cycle established the relationship between heating and cooling data.

The third cycle was aimed at determining the effect of high temperatures and long times on the microstructural changes in the UO_2 . A centerline temperature of approximately 2400°C was maintained for five hours before the sight holes closed to such an extent that continued operation of the element served no purpose.

b. Misconstruction Evaluation

1) General Procedure

Following thermal testing, each test element was removed from the test chamber and impregnated with an anaerobic permafil to fix all segments of the element in place and permit sectioning. The resin was oven cured for approximately 2 hours at 200°F . The elements were sectioned in accordance with the plan presented in Figure B-10. All sections were prepared for metallographic examination and evaluation using standard preparation and etching techniques.

5. Results and Discussion

a. Thermal Conductivity

The individual temperature measurements for a given radial location (R_1 , R_2 , R_3 , and R_4) were averaged and are presented along with power data in Tables B-IV, B-V and B-VI for test elements #38, #39 and #41, respectively.

The thermal conductivity of the UO_2 was then calculated using the following assumptions:

1) The fuel pellets were in intimate contact with both the heater rod* and the cladding for ZrO_2 sleeve at test temperatures.

2) The heater rod-fuel pellet interface gap conductance was infinite,** therefore, no temperature drop at this interface was used in the temperature of

* Post test metallography indicated, in some sections, mechanical attachment of the heater rod surface to the pellet inside diameter. In addition, there was some evidence of a change in heater rod shape resulting from heater-fuel mechanical interaction. Both observations fortify the assumption that the heater-fuel conductance is high.

** In previous calculations a gap conductance of $2 \text{ watts/cm}^2\text{-}^\circ\text{C}$ was used. Discussions of the influence of this assumption on UO_2 conductivity is provided later in this section.

TABLE B-IV

THERMAL & ELECTRICAL DATA

Test Element 38 Cycle D

<u>Data Point</u>	<u>Current (amps)</u>	<u>Voltage (volts)</u>	<u>Average Temperature (°C)</u>			
			<u>R₁</u>	<u>R₂</u>	<u>R₃</u>	<u>R₄</u>
1	211	9.25	875	--	--	--
2	224	11.30	1159	828	--	--
3	233	12.90	1397	1012	--	--
4	241	14.50	1639	1192	886	--
5	248	16.20	1945	1417	1040	800
6	251	17.50	2189	1628	1184	853
7	253	18.80	2470	1874	1334	943
8	259	21.00	--	--	1566	1134
9	251	20.40	--	--	1552	1094
10	251	20.40	--	--	1560	1113
11	251	20.37	--	--	1588	1111
12	251	20.43	--	--	1586	1125
13	251	20.43	--	--	1598	1110
14	251	20.43	--	--	1625	1118

TABLE B-V

THERMAL & ELECTRICAL DATA
TEST ELEMENT 39 CYCLE F

DATA POINT	CURRENT (amps)	VOLTAGE (volts)	AVERAGE TEMPERATURES (°C)			
			R ₁	R ₂	R ₃	R ₄
1	162	8.98	1267	1008	824	---
2	166	9.89	1484	1172	930	784
3	170	11.1	1680	1330	1053	864
4	172	12.0	1873	1472	1167	940
5	175	12.9	2079	1670	1295	1036
6	178	13.8	2241	1804	1395	1110
7	178	14.2	2344	1846	1419	1118
8	178	14.2	2332	1886	1434	1123
9	178	14.3	----	1862	1435	1122
10	176	14.6	----	1886	1457	1136
11	176	14.5	----	1876	1448	1130
12	176	14.6	----	1858	1451	1124
13	176	14.6	----	1876	1460	1122
14	174	14.1	----	1776	1369	1064
15	172	13.7	----	1700	1303	1018
16	170	13.6	----	1610	1230	969
17	169	12.7	----	1510	1161	919
18	168	12.2	----	1420	1088	874

TABLE B-VI

THERMAL & ELECTRICAL DATA
TEST ELEMENT 41 CYCLES A, B, & C

Cycle	Data Point	Current (amps)	Voltage (volts)	Average Temperature (°C)			
				R ₁	R ₂	R ₃	R ₄
A	1	150	7.8	1082	887	--	--
	2	158	9.3	1405	1127	948	810
	3	161	10.75	1672	1339	1095	918
	4	166	11.82	1950	1563	1231	1034
	5	170	12.95	2223	1753	1374	1130
	6	172	13.60	2357	1868	1449	1186
B	7	157	8.65	1216	1011	826	--
	8	159	9.12	1322	1094	899	778
	9	160	9.70	1456	1201	976	845
	10	162	10.40	1584	1296	1039	891
	11	164	10.90	1700	1380	1097	936
	12	165	11.40	1828	1471	1167	988
	13	168	11.85	1948	1580	1227	1039
	14	169	12.25	2046	1654	1280	1072
	15	170	12.70	2142	1730	1328	1116
	16	171	13.20	2276	1827	1407	1155
	17	169	12.35	2065	1665	1293	1086
	18	168	12.00	1960	1592	1258	1056
	19	165	11.40	1806	1457	1175	990
	20	164	10.96	1678	1377	1107	949
	21	161	10.00	1435	1186	979	848
C	22	154	8.85	1294	1067	880	772
	23	161	10.50	1631	1314	1062	918
	24	166	11.80	1910	1566	1227	1036
	25	169	12.70	2143	1737	1344	1128
	26	171	13.60	2357	1899	1471	1209
	27	171	13.70	2391	1916	1471	1236
	28	171	13.75	-----	1931	1482	1224
	29	171	13.80	-----	1935	1495	1240
	30	169	13.70	-----	1968	1504	1276
	31	167	13.80	-----	1962	1538	1283
	32	165	13.80	-----	1967	1550	1301
	33	165	13.80	-----	1877	1572	1304
	34	165	13.84	-----	1962	1589	1306
	35	164	13.84	-----	1946	1583	1312
	36	164	13.90	-----	1934	1574	1320
	37	163	13.90	-----	1934	1583	1324

the inside surface of the pellet was equivalent to the heater surface temperature.

3) The contribution of cracks to the thermal resistance of the fuel was insignificant at operating temperatures.

4) The fuel pellets were homogeneous, isotropic bodies. Actually, the pellets were continuous single phase structures with uniformly distributed porosity.

5) Heat generation was uniform over the 19.7 cm (7.750 in.) effective length of the heater.

Values of thermal conductivity as a function of temperature were calculated by applying the integrated form of Fourier's law for heat transmission by conduction across a homogeneous material.

$$k = \frac{Q \ln \frac{R_2}{R_1}}{2\pi L (T_1 - T_2)} \frac{\text{watts}}{\text{cm} - ^\circ\text{C}} \quad (1)$$

where k = Thermal conductivity at a temperature $\frac{T_1 + T_2}{2}$

Q = Heat flux

R_1 and R_2 = Radial distances (cm)

L = Length of heat transfer surface (cm)

T_1 and T_2 = Temperatures at radial distances R_1 and R_2 ($^\circ\text{C}$)

The results of these calculations for test elements #38, #39 and #41 are presented in Tables B-VII, B-VIII and B-IX, and the data are plotted as a function of the average temperature of the radial segment of interest in Figures B-11a, 11b, and 11c. Since the accuracy of optical temperature measurements at low temperature is questionable, only those data above 1000°C are considered. These data are calculated values obtained from measurements on 93.5% of theoretical density UO_2 and no corrections for the effect of porosity were made. The resultant curve of the thermal conductivity as a function of temperature between 1000 and 2200°C is presented in Figure B-12 and values are reported Table B-X. The thermal conductivity decreases with increasing temperature beginning with a value of 0.033 watts/cm-°C at 1000°C, decreasing to a value of 0.024 watts/cm-°C at approximately 1700°C and remaining relatively constant at this value up to 2200°C.

These data are not in agreement with earlier results⁷ reported by CEND on UO_2 thermal conductivity. Previous results showed a minimum in the conductivity curve at 1400°C and a distinct increase in thermal conductivity was found for temperatures in excess of this temperature. The difference in the shape of the curve is attributed to the interface gap conductance that is assumed. In previous calculations, a constant gap conductance of 2 watts/cm²-°C was assumed which resulted in an upswing in the curve at higher temperatures. The present values of thermal conductivity resulted from the assumption that the conductance across the heater-fuel interface was infinite; we feel this is a more realistic analysis for the higher temperatures. The actual conditions can best be described by a variable gap conductance which increases with temperature and falls between the values used. We feel that in the temperature range of 1600 to 2200°C the assumption of an infinite interface gap conductance is more correct and leads to correct thermal conductivity values.

TABLE B-VII

THERMAL CONDUCTIVITY

Test Element 38 Cycle D

<u>Data Point</u>	<u>Power Watts</u>	<u>Avg. Temp. (R₁-R₂)(°C)</u>	<u>k(R₁-R₂) w/cm-°C</u>	<u>Avg. Temp. (R₂-R₃)(°C)</u>	<u>k(R₂-R₃) w/cm-°C</u>	<u>Avg. Temp. (R₃-R₄)(°C)</u>	<u>k(R₃-R₄) w/cm-°C</u>
1	1952	--	--	--	--	--	--
2	2531	994	0.022	--	--	--	--
3	3006	1204	0.023	--	--	--	--
4	3494	1416	0.023	1039	0.032	--	--
5	4018	1681	0.022	1228	0.030	920	0.040
6	4392	1908	0.023	1406	0.028	1018	0.032
7	4756	2172	0.023	1604	0.025	1138	0.029
8	5439	--	--	--	--	1350	0.030
9	5120	--	--	--	--	1323	0.027
10	5120	--	--	--	--	1336	0.028
11	5113	--	--	--	--	1350	0.026
12	5120	--	--	--	--	1356	0.027
13	5120	--	--	--	--	1354	0.025
14	5120	--	--	--	--	1372	0.020

TABLE B-VIII

THERMAL CONDUCTIVITY

Test Element 39 Cycle F

<u>Data Point</u>	<u>Power Watts</u>	<u>Avg. Temp. (R₁-R₂)(°C)</u>	<u>k(R₁-R₂) w/cm-°C</u>	<u>Avg. Temp. (R₂-R₃)(°C)</u>	<u>k(R₂-R₃) w/cm-°C</u>	<u>Avg. Temp. (R₃-R₄)(°C)</u>	<u>k(R₃-R₄) w/cm-°C</u>
1	1455	1138	0.026	916	0.036	--	--
2	1642	1328	0.024	1051	0.031	857	0.034
3	1887	1505	0.025	1192	0.031	958	0.030
4	2064	1672	0.024	1320	0.031	1054	0.027
5	2258	1874	0.026	1482	0.027	1166	0.026
6	2456	2022	0.026	1600	0.027	1252	0.026
7	2528	2095	0.024	1632	0.027	1268	0.025
8	2528	2109	0.026	1660	0.025	1274	0.024
9	2545	--	--	1648	0.027	1278	0.024
10	2570	--	--	1672	0.027	1297	0.024
11	2552	--	--	1662	0.027	1289	0.024
12	2570	--	--	1654	0.029	1288	0.023
13	2570	--	--	1668	0.028	1291	0.023
14	2453	--	--	1572	0.027	1216	0.024
15	2356	--	--	1502	0.027	1160	0.025
16	2312	--	--	1420	0.028	1100	0.026
17	2146	--	--	1336	0.028	1040	0.026
18	2050	--	--	1254	0.028	983	0.028

Table B-IX

THERMAL CONDUCTIVITY

Test Element 41 Cycles A, B & C

<u>Data Point</u>	<u>Power (watts)</u>	<u>Avg. Temp. (R₁-R₂)(°C)</u>	<u>k(R₁-R₂) (w/cm-°C)</u>	<u>Avg. Temp. (R₂-R₃)(°C)</u>	<u>k(R₂-R₃) (w/cm-°C)</u>	<u>Avg. Temp. (R₃-R₄)(°C)</u>	<u>k(R₃-R₄) (w/cm-°C)</u>
CYCLE A							
1	1170	984	0.028	--	--	--	--
2	1469	1266	0.025	1038	0.037	879	0.031
3	1731	1506	0.025	1217	0.032	1006	0.028
4	1962	1756	0.024	1397	0.026	1132	0.029
5	2202	1988	0.022	1564	0.026	1252	0.026
6	2339	2112	0.023	1658	0.025	1318	0.026
CYCLE B							
7	1358	1114	0.031	918	0.033	--	--
8	1450	1208	0.030	996	0.033	838	0.035
9	1552	1328	0.029	1088	0.031	910	0.034
10	1685	1440	0.028	1168	0.029	965	0.033
11	1778	1540	0.026	1238	0.028	1016	0.032
12	1881	1650	0.025	1319	0.028	1078	0.030
13	1991	1764	0.026	1404	0.025	1133	0.031
14	2070	1850	0.025	1467	0.025	1176	0.029
15	2158	1936	0.025	1529	0.024	1222	0.030
16	2257	2052	0.024	1617	0.024	1281	0.026
17	2087	1865	0.025	1479	0.025	1190	0.029
18	2016	1776	0.026	1425	0.027	1157	0.029
19	1881	1632	0.026	1316	0.030	1082	0.034
20	1797	1528	0.028	1242	0.030	1028	0.033
21	1610	1310	0.031	1082	0.035	914	0.036

TABLE B-IX (Cont'd.)

<u>Data Point</u>	<u>Power (watts)</u>	<u>Avg. Temp. (R₁-R₂)(°C)</u>	<u>k(R₁-R₂) (w/cm-°C)</u>	<u>Avg. Temp. (R₂-R₃)(°C)</u>	<u>k(R₂-R₃) (w/cm-°C)</u>	<u>Avg. Temp. (R₃-R₄)(°C)</u>	<u>k(R₃-R₄) (w/cm-°C)</u>
CYCLE C							
22	1363	1180	0.028	974	0.033	826	0.037
23	1690	1472	0.025	1188	0.030	990	0.034
24	1959	1738	0.027	1396	0.026	1132	0.030
25	2146	1940	0.025	1540	0.024	1236	0.029
26	2326	2128	0.024	1685	0.024	1340	0.026
27	2343	2154	0.023	1694	0.024	1354	0.029
28	2351	--	--	1706	0.023	1353	0.026
29	2360	--	--	1715	0.024	1368	0.027
30	2315	--	--	1736	0.022	1382	0.028
31	2305	--	--	1750	0.024	1410	0.026
32	2277	--	--	1758	0.024	1426	0.026
33	2277	--	--	1724	0.033	1438	0.025
34	2284	--	--	1776	0.027	1448	0.023
35	2270	--	--	1764	0.028	1448	0.024
36	2280	--	--	1754	0.028	1447	0.026
37	2266	--	--	1758	0.029	1454	0.025

TABLE B-X

THERMAL CONDUCTIVITY OF 93.5% T.D. UO_2

TEMPERATURE $^{\circ}\text{C}$	THERMAL CONDUCTIVITY watts/cm- $^{\circ}\text{C}$
1000	0.033
1100	0.031
1200	0.029
1300	0.028
1400	0.027
1500	0.026
1600	0.025
1700	0.024
1800	0.024
1900	0.024
2000	0.024
2100	0.024
2200	0.024

As a point of illustration, the thermal conductivity values which result from assuming a constant interface gap conductance are presented in Table B-XI. Comparison of these data with those resulting from assuming an infinite interface gap conductance show a definite upward trend of the thermal conductivity values in the range of 1600 to 2200°C.

A small portion of the data available in the literature on UO_2 thermal conductivity are presented in Figure B-13 for comparison with the CEND data. These data were obtained from measurements using UO_2 of 93-96% of theoretical density. In order to make the comparison meaningful, all the data were corrected to 100% of theoretical density by using the simplified Loeb⁸ equation. Lyons⁹ has compiled the UO_2 conductivity data published by a number of investigators and has established his "best estimate" curve which is meant to show a continuous curve of thermal conductivity from ambient temperature to its melting point.

There is excellent agreement among the investigators up to approximately 1200°C. The area of rather extensive controversy lies between this temperature and the UO_2 melting point. In this region, Lyons shows a continuation of the low temperature curve with no indication of distinct increases in conductivity with temperature. These data were derived from temperature distributions associated with "post-mortem" analyses of grain size changes produced by short time capsule irradiations at levels high enough to produce central melting. The data reported are in the temperature range of 1950-2450°C. Although Lyons' analyses of "post-mortem" microstructures and the derived temperature for the onset of these microstructures are probably correct, there is some controversy in the field with respect to the temperature and temperature range associated with these microstructural constituents. Utilization of different temperature limits for these microstructural constituents could provide better agreement between Lyons' data and

TABLE B-XI

UO₂ THERMAL CONDUCTIVITY
EFFECT OF INTERFACE GAP CONDUCTANCE

TEMPERATURE °C	THERMAL CONDUCTIVITY (watts/cm-°C)	
	INFINITE GAP CONDUCTANCE	CONSTANT GAP CONDUCTANCE
1000	0.033	0.034
1100	0.031	0.031
1200	0.029	0.029
1300	0.028	0.028
1400	0.027	0.027
1500	0.026	0.026
1600	0.025	0.026
1700	0.024	0.026
1800	0.024	0.027
1900	0.024	0.028
2000	0.024	0.031
2100	0.024	0.034
2200	0.024	0.039

those of the other investigators. In a similar manner, it was demonstrated that the shape of the CEND curve was dependent upon the assumption used with respect to the heater-fuel contact conductance. Although an upswing at elevated temperatures could be shown, we believe that the assumption required to provide the upswing is unrealistic. The assumption of near infinite heater-fuel gap conductance is substantiated by evidence discussed in the first footnote in Section 5-a. Thus, the CEND curve presented in Figure B-12 and B-13 represents our best estimate of the variation of thermal conductivity of UO_2 as a function of temperature.

Data published by Feith¹⁰ and Christensen¹¹ indicate a minimum followed by distinctly increasing conductivity values with temperature. These out-of-pile experiments were carried out using a radial heat flow technique under low thermal gradient conditions. The test specimens were also rather large, on the order of 5.08 cm (2.0 in.) OD.

Christensen believes the increase in conductivity above 1400°C reflects a high temperature electronic contribution to heat transfer. The specimen composition in his work changed from $\text{UO}_{2.01}$ to $\text{UO}_{1.99}$ during testing and the high temperature data points typify a substoichiometric oxide. Bates¹² indicates the presence of metallic uranium and the formation of an oxygen deficient phase in irradiated uranium dioxide at temperatures above 1600°C, which he believes results in increased thermal conductivity of the UO_2 . Feith also believes that there is a possibility that the upswing in his data is due to changes in stoichiometry of the UO_2 , but has not as yet confirmed these suspicions.

The CEND data were generated from production-run UO_2 pellets obtained from a commercial pellet vendor. The fabrication process was typical of that used in power reactors. The O/U ratio was 2.011 to 2.012. The test element size was also representative of power reactor elements. These test elements were operated under an extremely high temperature gradient (on the order of 4000°C/cm [10,000°C/in.] average).

The thermal conductivity data obtained from these tests essentially parallel the "best estimate" curve by Lyons, and do not show any indications of increasing conductivity with temperature. There appears to be good correlation between the out-of-pile measurements made under very high thermal gradients on elements which approximate reactor elements, and the conductivity derived from in-pile temperature correlations based on "post-mortem" grain size analysis.

A determination of the extent of stoichiometry changes in the thermally cycled fuel was made in the CEND test elements. A comparison between the lattice parameters of samples taken in the region of greatest microstructural change with fuel which remained in the as-sintered condition showed essentially no changes. We, therefore, assume that no significant changes in the O/U ratio resulted from the thermal testing. Direct measurement of O/U changes by oxidation to U_3O_8 was not possible because of the small volume of fuel in the region of interest.

The correlation of the out-of-pile techniques, properly designated and utilized, can be useful as a rapid and effective means of determining the in-pile conductivity trends. It certainly is a useful tool when compared with results from in-pile measurements based on short term irradiations. It is likely that fission fragments will affect the conductivity of irradiated UO_2 . However, it is unlikely that the short term in-pile conductivity measurements reported to date will have had sufficient build-up of fragments to provide meaningful changes. On this basis, there is an important role for out-of-pile measurements which have been properly designed. There is no out-of-pile substitute for thermal conductivity measurements on moderate burnup UO_2 fuel elements, neither are there in-pile measurements. Such in-pile data must be obtained if maximum utilization of the thermal potential of UO_2 is to be realized in efficient reactor designs. As far as screening data are concerned, we believe that the out-of-pile measurement techniques developed on this program will provide data which are as reliable as those obtained from short term in-pile measurements.

b. Microscopic Analysis

1) Microstructural Changes

Following thermal testing, the elements were sectioned and prepared for microstructural analysis. Photomacrographs were taken of the entire section to show the post-test macrostructural appearance of the test element. Photomicrographs were then taken at four positions around the heater rod, 90 degrees apart, as shown in Figure B-14 for elements #38 and #39. This procedure was followed in order to determine if any differences in microstructural changes occurred around the heater.

Analyses of the post-test sections of the three test elements revealed that significant microstructural changes occurred during thermal cycling under very high thermal gradient conditions. A minimum of three distinctive regions in each of the microstructures is quite evident. Photomicrographs of typical radial traverses made on sections of the three elements are presented in Figures B-15, B-16 and B-17. Figure B-15 is a complete traverse photomicrograph extending from the heater-pellet interface to the pellet-ZrO₂ sleeve interface. Figures B-16 and B-17 are partial traverses showing the regions of maximum microstructural change and the appearance of the structure adjacent to the pellet-ZrO₂ interface for Figure B-16 and adjacent to the pellet-cladding interface, for Figure B-17.

The outermost region of the fuel remained at temperatures well below the sintering temperature used in the pellet fabrication. Consequently, the grain size and the porosity distribution are essentially unchanged and the structure has remained in the initial, as-sintered condition. This region also shows no evidence of any interaction with the cladding or ZrO₂.

The central region of the pellet was exposed to higher temperatures and the anticipated structural changes are evident. In the as-sintered structure, the porosity is uniformly distributed throughout the structure. However, at higher

temperatures the migration of the pores to the grain boundaries together with the sweeping action of the boundaries during grain growth created a high density region adjacent to the grain boundary which was free of porosity. An extensive amount of equiaxed grain growth is evident at the high temperature side of this grain growth region. The porosity coalesced into rather large, discrete voids and was located either in elongated configurations in the grain boundaries or toward the central regions of the equiaxed grains. At the high temperature limit of this region, the grains exhibited a tendency to become elongated and oriented with their major axis parallel to the thermal gradient.

The innermost region of the fuel exhibited the greatest amount of microstructural change. In this region, the temperatures were high enough to promote an extensive amount of grain growth. In some sections, these grains were columnar while in others they tended to be equiaxed. The columnar grains seemed to be more prevalent in the compound elements (#39 and #41) than they were in the simple element (#38).

Both the large columnar and equiaxed grains contained a small percentage of randomly distributed voids throughout the center of the grains. Also, very little lenticular void formation was evidenced. This was probably due to the very high thermal gradient in the small test section which did not allow sufficient material to achieve the proper time and temperature conditions for extensive lenticular void formation. Therefore, the sweeping action of these voids and the resulting void free columnar grains were not observed. The major portion of the columnar grains in our studies were generated without the formation of lenticular voids and, therefore, did contain some porosity. The time and temperature dependence of void migration is clearly illustrated in these three photomicrographs. Element #38 (Figure B-17) was operated with a center temperature of 2500°C for approximately 20 hours and exhibited a rather wide band adjacent to the heater which was practically free of porosity. Elements #39 & #41 (Figures B-15 and B-16)

were operated with center temperatures of 2400°C for 4-5 hours and consequently exhibited almost no completely void-free band comparable to that of element #38. It is felt, therefore, that the element center temperature difference between 2400°C and 2500°C is more influential on the generation of void free grains than the time differences used in these experiments.

Hausner¹¹ recently published data on in-pile and out-of-pile microstructural changes in UO_2 , and the influence of various parameters on these changes. Data generated at CEND seem to be fully consistent with Hausner's data in that:

a) The occurrence of columnar grains in specimens operated with a center temperature of 2400°C was relatively low and lenticular void formation and generation of large void free columnar grains predominated in the samples operated with a center temperature of 2600°C .

b) The probability of occurrence of columnar grains was greater in the samples having lower radial thermal gradients than their occurrence in samples with high thermal gradients.

This investigation has shown that the occurrence of columnar grains was more prevalent in the elements having a lower radial temperature gradient and that the formation of large lenticular voids which generate pore free columnar grains was generally not observed. The lack of lenticular void formation can probably be attributed to operation of the elements at conditions which would not satisfy the necessary time and temperature conditions to impart sufficient mobility to the voids, allowing them to form large lenticular shapes which in turn generate large, void free columnar grains.

One of our objectives was to determine the isotherms at the outer limit of the blocky and/or columnar region and the grain growth region. The radial distance from the heater-pellet interface was measured to the limit of the blocky and/or columnar region with relative ease, as a sharp delineation occurred at this point. An average grain diameter of 25 microns was selected as the criterion

for the limit of the grain growth region. A linear intercept technique was used to determine the position of the occurrence of grains having an average diameter of 25 microns. The radial distance from the heater-pellet interface to this location was then measured. Results of these measurements are presented in Table B-XII.

With these measurements, together with the radial temperature for the simple and compound elements, the temperature for onset of particular microstructural changes was determined by the following technique.

2) Temperature for Onset of Microstructural Changes

a) Radial Temperature Distribution

In an attempt to determine the temperature for the onset of grain growth, a radial temperature distribution for the simple and compound elements was determined. Values for thermal conductivity as a function of temperature were used from Figure B-12 as well as lower temperature data from Figure B-13 and the same assumptions were made as for the thermal conductivity calculations.

Calculations of the temperature distribution within the UO_2 pellet were made by applying the integrated form of Fourier's law (Eqn. 1) for heat transmission by conductance across a homogeneous material.

$$Q = \frac{k2\pi L \Delta T}{\ln \frac{R_2}{R_1}} \quad (1)$$

Since the heat flow across any radial segment is constant and the temperature drop is proportional to the thermal resistance of that segment, this equation can be put in a form from which the temperature drop across an individual radial segment can be determined.

TABLE B-XII

TEMPERATURE FOR ONSET OF MICROSTRUCTURAL CHANGES

ELEMENT	RADIAL DIST. TO SIGHT HOLE (mm)	AVG. MEAS. TEMP. (°C)	DERIVED HEATER TEMP. (°C)	AVG. RADIAL DISTANCE		TEMP. FOR ONSET	
				BLOCKY/ COLUMNAR GRAINS (mm)	25 μ DIA. GRAINS (mm)	BLOCKY/ COLUMNAR GRAINS (°C)	25 μ DIA GRAINS (°C)
38	0.00	2470	2470	1.32	1.44	1880	1730
	0.42	1874	2480	1.30	1.42	1910	1760
	1.02	1334	2530	1.40	1.54	1820	1650
	1.68	943	2430	1.32	1.47	1850	1665
39	0.74	1886	2260	1.26	1.46	2035	1910
	2.01	1434	2320	1.32	1.50	2060	1960
	3.27	1123	2345	1.28	1.50	2120	2010
41	0.77	1916	2400	1.42	1.60	2070	1970
	2.04	1471	2400	1.51	1.72	2020	1915
	3.27	1236	2470	1.59	1.86	2030	1900

$$\text{Thus } \Delta T_{\text{total}} = \Delta T_1 + \Delta T_2 + \Delta T_3 \dots \text{etc.} \quad (2)$$

$$\text{and } \Delta T_1 = \frac{\ln \frac{R_2}{R_1}}{k_{m1}} \quad (3)$$

$$+ \frac{\ln \frac{R_3}{R_2}}{k_{m2}} + \frac{\ln \frac{R_4}{R_3}}{k_{m3}}$$

where k_{m1} = mean thermal conductivity for the average temperature of a segment having radii, R_1 and R_2 , as physical boundaries.

The radial temperature distribution curves were then determined from an assumed temperature profile within a UO_2 pellet. A series of iterations was performed until the assumed radial position for a given temperature drop was identical to the calculated radial position for the same temperature drop.

The results of these operations are shown in Figure B-18. It can be seen that the thermal gradient for the insulated pellets, (elements #39 and #41) at any point is lower than the gradient in the uninsulated pellets (element #38). The lower thermal gradient of elements #39 and #41 results in higher temperatures at any given radial location within the UO_2 pellet. This gradient also more closely approximates the in-pile thermal gradient for UO_2 and, as seen in Figures B-15, B-16 and B-17, the grain growth region in elements #39 and #41 (compound) is about twice as wide as the grain growth region in element #38 (simple). Thus, measurements of the locations of microstructural change were less difficult.

To check the validity of the radial temperature distribution determinations, the measured values obtained from sight holes were compared to the derived

temperatures for the location of the same sight holes. The results of this comparison are presented in Table B-XIII. The agreement for the data from the compound elements (#39 and #41) is better than that from the simple elements. However, the greatest variation found occurred in element #38 at low temperatures and at a large distance from the heater where the variation was on the order of 100°C. (An assumed value of conductivity was used to close the gap between the high temperature and low temperature values.) The data for elements #38 and #41 agreed within 30°C to 50°C, lending a relatively high degree of confidence in the thermal conductivity versus temperature relationship used, and the method of arriving at the radial temperature distribution.

b) Determination of Temperature for Onset of Grain Growth

Using the radial temperature distribution curves (Figure B-18), the measurements of distance from pellet edge to the microstructural change (Table B-XI), and the temperature prior to final cooldown for the section of interest, the temperature for onset of grain growth (grains with an approximate grain diameter of greater than 25 microns) and for onset of massive blocky and/or columnar grains was determined.

The following procedure was used in determining the range of temperatures shown in Table B-XII for onset of grain growth constituents. The optically measured temperatures (sightings to various radial positions within the UO₂) were used to enter the radial temperature distribution curves. Using the radial position of the sighting and the observed temperature for that radial position as input data (Figure B-19), the intersection of these two values fell on a radial temperature profile curve from which the temperature of the pellet ID surface was obtained by following the curve back to the ordinate. The temperature of onset of a particular microstructure was determined by moving along this temperature profile curve (Figure B-20) to the measured radial distance for that

TABLE B-XIII

COMPARISON OF MEASURED vs DERIVED FUEL TEMPERATURES

ELEMENT	PELLET I.D. SURFACE TEMP.	TEMP. @ 0.42 mm FROM HEATER		TEMP. @ 1.02 mm FROM HEATER		TEMP. @ 1.68 mm FROM HEATER	
		MEASURED	DERIVED	MEASURED	DERIVED	MEASURED	DERIVED
38	1639	1192	1200	886	840	---	---
	1945	1417	1425	1040	975	800	670
	2189	1628	1580	1184	1080	853	745
	2470	1874	1815	1334	1225	943	830

ELEMENT	PELLET I.D. SURFACE TEMP.	TEMP. @ 0.74 mm FROM HEATER		TEMP. @ 2.01 mm FROM HEATER	
		MEASURED	DERIVED	MEASURED	DERIVED
39	1873	1472	1515	1167	1195
	2079	1670	1685	1295	1305
	2241	1804	1800	1395	1395
	2344	1846	1880	1419	1450
	2332	1882	1870	1434	1440

ELEMENT	CYCLE	PELLET I.D. SURFACE TEMP.	TEMP. @ 0.77 mm FROM HEATER		TEMP. @ 2.04 mm FROM HEATER	
			MEASURED	DERIVED	MEASURED	DERIVED
41	A	1950	1563	1565	1231	1240
		2223	1753	1780	1374	1385
		2357	1868	1875	1449	1445
	B	1828	1471	1465	1167	1165
		1948	1580	1565	1227	1235
		2046	1654	1650	1280	1285
		2142	1730	1720	1328	1340
		2276	1827	1820	1407	1405
		2065	1665	1660	1293	1290
		1960	1592	1575	1258	1235
		1806	1457	1455	1175	1155
	C	1910	1566	1535	1227	1210
		2143	1737	1710	1344	1340
		2357	1899	1880	1471	1450
		2391	1916	1900	1471	1465

microstructure. The intersection of these two values established the temperature for onset of the microstructure of interest.

The results of these determinations are also presented in Table B-XII. These data indicated that the range for onset of the large grains (1820 to 1910°C) in the simple element occurred at a lower temperature than those for the compound elements (2020 to 2120°C). The onset of the 25 micron average* diameter grains also occurred at lower temperatures (1650 to 1730°C) in the simple element than in the compound elements (1900 to 2010°C).

These data are also self consistent with Hausner's¹³ data in that the onset of microstructural changes is shifted to higher temperatures when the specimens are held for a shorter time at a given temperature. The difference in thermal gradient between the simple and compound element design is probably not great enough to influence the time-temperature relationship for grain growth to any great extent.

Data published in CEND-217⁷ presented data for the onset of grain growth using an average grain diameter of 15 microns as the growth criterion. Although microstructural analysis indicates the possibility of establishing a definite demarcation between the as-sintered grains (10 micron average grain diameter) and grains having an average grain diameter of 15 microns, it is a far more difficult task than using an average grain diameter of 25 microns, and consequently, there is far more spread in the data. We still feel that it is an extremely difficult task to establish a clear cut and definitive zone limit for onset of grain growth. The changes in grain size in these regions are very gradual in contrast to the rather sharp demarcation on the low temperature side of the large grained region. Even here, the range for onset is about 100°C and could lead to variations in UO₂ conductivities determined from a "post-mortem"

* Obviously lower temperatures would be recorded as the onset of 15 micron diameter grains. Typical values are between 1400 - 1700°C for both the simple and compound elements.

evaluation of short term in-pile data which are dependent upon accurate delineations of boundaries as well as definitive temperature for the onset of these microstructural constituents. Therefore, the range of temperatures reported for the onset of grain growth will depend to a large extent on the ability to establish a definitive boundary between the region of grain growth and that of the as-sintered structure; these ranges will influence the determination of UO_2 conductivity values.

6. Conclusions

These out-of-pile investigations were designed to simulate the thermal conditions that prevail in a power reactor fuel element. However, as a result of the line heat generation source used in this program, an exact duplication of the temperature distribution encountered in in-reactor elements with a relatively uniform heat generation source was not possible. Other factors which could not be simulated include the influence (if any) of radiation on microstructural changes and the influence of fuel depletion and fission product buildup on conductivity. However, these factors must be of second order magnitude in importance as evidenced by the good correlation of conductivities determined as part of this ex-reactor investigation with those deduced from in-reactor testing. A comparison of results obtained as part of this program with those reported on the basis of "post-mortem" on irradiated fuels, leads to the following conclusions:

- a) Out-of-pile measurements of thermal conductivity made under high thermal gradient conditions can be used as an effective measure of in-reactor conductivities.
- b) For the out-of-pile measurement technique used, the selection of proper values of the fuel-to-heater interface conductance can strongly influence the shape of the thermal conductivity versus temperature curve. A variable gap conductance value that changes with temperature is needed for accurate calculations of UO_2 thermal conductivity. We believe that our assumption of near infinite gap conductance most closely simulates true conditions.

c) Our best estimate for the thermal conductivity of 93.5% of theoretical density stoichiometric UO_2 is a value of about 0.033 watts/cm- $^{\circ}\text{C}$ at 1000 $^{\circ}\text{C}$ decreasing to a value of 0.024 watts/cm- $^{\circ}\text{C}$ at about 1700 $^{\circ}\text{C}$, and then remaining essentially constant at a value of about 0.024 watts/cm- $^{\circ}\text{C}$ up to 2200 $^{\circ}\text{C}$.

d) Lenticular void formation was low and is attributed to the relatively low center temperature (2500 $^{\circ}\text{C}$) of the specimens.

e) Void migration was more apparent in the specimens operated at centerline temperatures of 2500 $^{\circ}\text{C}$ than in the specimens operated at centerline temperatures of 2400 $^{\circ}\text{C}$. Although void migration is time-temperature related, in the temperature range of interest in this experiment it appears that temperature is the influencing variable rather than time for void migration.

f) Columnar grain growth occurred more predominantly in specimens with a lower radial thermal gradient than in specimens with a high thermal gradient.

g) The temperature for onset of the 25 micron average diameter grains occurred at 1650 to 1730 $^{\circ}\text{C}$ for the single element and 1900 to 2010 $^{\circ}\text{C}$ for the compound element. For onset of large blocky grains, temperatures in the range of 1820 to 1910 $^{\circ}\text{C}$ were calculated at the higher radial thermal gradients and 2020 to 2120 $^{\circ}\text{C}$ for the lower radial gradients.

FIG B-1 ARRANGEMENT OF COMPONENTS - COLD CLAD CONFIGURATION

FIG B-2 ARRANGEMENT OF COMPONENTS - HOT CLAD CONFIGURATION

FIG B-3 TEST APPARATUS

FIG 4-b COMPOUND ELEMENT

FIG B-4a SIMPLE ELEMENT

FIG B-4 TEST ELEMENT

[illegible]

COMPOUND ELEMENT
FIG B-5b

FIG B-6 COOLING JACKET WITH TEST ELEMENT INSERTED

FIG B-7 COOLING WATER SYSTEMS

TCVG - THERMOCOUPLE VACUUM GAGE

IVG - IONIZATION VACUUM GAGE

DP - DIFFUSION PUMP

MP - MECHANICAL PUMP

CV - BALL CHECK VALVE

✕ - VALVE

⌚ - BOURDON TUBE PRESSURE GAGE

< - NEEDLE VALVE

FIG B-8 ATMOSPHERE CONTROL SYSTEM

(a)

(b)

(c)

FIG B-9 CENTERLINE TEMPERATURE HISTORY

ELEMENT #38

ELEMENT #39

ELEMENT #41

(T) = TRANSVERSE SECTIONS

(L) = LONGITUDINAL SECTIONS

* = THIS SURFACE PREPARED FOR METALLOGRAPHY

FIG. B-10 POST TEST METALLOGRAPHIC SECTIONS

FIG. B-II. THERMAL CONDUCTIVITY VS. TEMPERATURE - 93.5 % TD UO₂

FIG. B-12 THERMAL CONDUCTIVITY VS. TEMPERATURE
(93.5 % TD UO₂)

FIG B-13 THERMAL CONDUCTIVITY VS. TEMPERATURE

FIG B-14 POST TEST MICROSTRUCTURES AT FOUR LOCATIONS IN A SECTION

CENTER 2400°C ETCHED ELEMENT 41 SECTION 12 APPROX 5 HOURS AT TEMP 250 X EDGE 1250°C

FIG B-15 COMPLETE RADIAL TRAVERSE SHOWING EXTENT OF MICROSTRUCTURAL CHANGES TYPICAL OF ELEMENT 41

CENTER 2350°C ETCHED REGION ADJACENT TO HEATER APPROX 3 HOURS AT TEMP 250 X
ELEMENT 39 SECTION 9

EDGE 1125°C
REGION ADJACENT TO ZrO₂ SLEEVE

FIG. B-16 PARTIAL RADIAL TRAVERSE SHOWING REGIONS OF GREATEST MICROSTRUCTURAL CHANGE AND THE AS SINTERED STRUCTURE TYPICAL OF ELEMENT 39

CENTER 2500°C ETCHED REGION ADJACENT TO HEATER APPROX 20 HOURS AT TEMP 250 X
ELEMENT 38 SECTION 12

EDGE 200°C
REGION ADJACENT TO CLADDING

FIG. B-17. PARTIAL RADIAL TRAVERSE SHOWING REGIONS OF GREATEST MICROSTRUCTURAL CHANGE AND THE AS SINTERED STRUCTURE TYPICAL OF ELEMENT 38

FIG. B-18 RADIAL TEMPERATURE DISTRIBUTION - SIMPLE and COMPOUND ELEMENTS

FIG. B-19

FIG. B-19 TYPICAL PELLET I.D. SURFACE - TEMPERATURE DETERMINATION

FIG. B-20 TYPICAL DETERMINATION OF TEMPERATURE FOR ONSET OF GRAIN GROWTH

C. Collapse Criteria

1. Introduction

A program was conducted to develop accurate analytical methods for predicting the critical collapse pressure of free standing fuel cladding and to establish the limiting factors involved in the selection of thin walled fuel supported cladding. This program was initiated to develop methods for the design of rod type stainless steel clad fuel elements which would minimize the mass of metal required.

The first approach considered the use of higher strength, work-hardened cladding and permitted the use of a thinner-walled cladding to withstand the reactor coolant pressure. A series of collapse tests were performed on tubes with varying cold work, wall thickness and diameters to establish a relationship between these parameters and collapse pressure. Accepted classical criteria for predicting collapse were compared with experimental data and were found to be inaccurate in the collapse pressure range of interest. The tangent modulus criterion, which was evolved, provided a reasonably accurate criterion for predicting tube collapse of annealed as well as hardened tubing.

The second method which was evaluated involved the use of fuel supported stainless steel cladding. Rods containing simulated pellets and compacted powder of different densities were prepared for collapse testing and the influence of density, pellet diameters and pellet defects were evaluated. A summary of this effort is presented below.

2. Program

a. Free Standing Cladding

During this program, the following parameters were investigated:

- The effect of hardness, temperature, diameter, wall thickness and ovality on the external collapse pressure of Type 304 and 347 stainless steel tubing.
- Comparison of experimental results with calculated predictions of tubing collapse strength.
- Development of realistic criteria to predict tubing collapse in the transition zone where the classical elastic and inelastic criteria intersect.
- The directional stress-strain properties of Type 347 stainless steel.
- Low temperature annealing effects on the mechanical properties of work hardened Types 304 and 347 stainless steel.

1) Equipment and Procedure for Collapse Tests

The collapse test program was conducted primarily at room temperature with some testing at 550°F. The samples were 10-inch long tubing sealed at both ends. The L/D ratio was such that the end restraints could be ignored. All tests were performed in a 3-inch I.D. pressure vessel which was manually pressurized. A schematic drawing of the test fixture and a sketch of the test vessel are shown on Figures C-1 and C-2 respectively. The fixture consists of the pressure vessel, pressurizing pump, heating system, vent and rupture lines, and strain gage recording instrumentation. Water was used as the pressurizing medium for the initial cold tests and hydraulic fluid was used for the latter tests. Dow Corning Fluid No. 710 was used for the hot tests and those requiring use of strain gages.

Initial cold test samples were made up with one end vented as shown on Figure C-3. The sample was mounted in the test vessel as shown on Figure C-2,

and a manometer line was mounted to the head above it. The line and the sample were filled with fluid. In this way it was assumed that an accurate measurement of the collapse point could be obtained from the liquid level in the manometer. However, this was found to be unnecessary since collapse is accompanied by an audible snap and a sudden drop in pressure. Consequently, later samples were sealed at both ends. This technique made it possible to simultaneously test several samples providing there was a large spread in their collapse pressures.

In the hot collapse tests, the system was filled with Dow Corning 710 and the system was closed except for a vent line. Heating was accomplished by five (1 KW) strip heaters wrapped around the vessel and head. Temperature control was accomplished by means of thermocouples mounted on the inner and outer surface of the vessel. Venting of the system during heatup was provided because of the high thermal expansion rate of the fluid. Once temperature was reached, power to the strip heaters was turned off, the vent line closed and pressurizing initiated. The mass of the system and the insulation were sufficient to hold the temperature throughout the collapse testing.

2) Collapse Testing

The initial series of cold tests were performed on annealed, 1/8 hard and 3/8 hard, one-half-inch O.D., Type 347 stainless steel tubes. Tube wall thicknesses of 0.010, 0.015 and 0.025 inch were used. Figure C-4 shows a plot of the collapse pressures of this series as a function of t/R_o (thickness to outside radius). Also plotted on Figure C-4 are the predicted collapse curves based on two classical theories. The theories chosen were the "Elastic Collapse Theory" given by Timoshenko¹⁴ and the "Shear Energy Criterion" developed by Von Mises.¹⁵

Elastic Collapse Theory:

$$P_c = \frac{E}{4(1-\nu^2)} \left(\frac{t}{R_o} \right)^3$$

where P_c = Collapse Pressure (psi)
 E = Young's Modulus (psi)
 ν = Poisson's Modulus (psi)
 t = Tube Wall Thickness (inches)
 R_o = Outside Radius (inches)

Shear Energy Criterion or Inelastic Collapse Theory:

$$P_c = \frac{(1-B)\sigma_y \left(1 - \frac{R_i^2}{R_o^2}\right)}{\sqrt{3}}$$

where B = Percentage reduction in collapse strength due to out-of-roundness.
 σ_y = 0.2% offset yield strength of the material (psi)
 R_i = Inside Radius (inches)

Since the tubing generally is not perfectly round, an ovality factor, B , is introduced in the inelastic collapse theory. The factor for ovality was taken from the work of T. Jasper¹⁶ and put in the form shown on Figure C-5.

As can be seen on Figure C-4, the elastic collapse theory accurately predicts collapse in the thinner walled tubes ($t/R_o < 0.07$) and for thicker tubes where the predicted elastic collapse is well below the predicted inelastic collapse. Similarly, the inelastic collapse theory predicts collapse reasonably well when its predicted pressure is well below that of the elastic collapse theory. (See, for example, the annealed samples with a t/R of 0.10, shown in Figure C-4). However, in the transition region where the elastic and inelastic curves intersect, the test data has consistently shown that neither theory is valid. This region of intersection represents the primary area of interest in the design of free standing cladding due to the pressure range it encloses.

A series of hot collapse tests was conducted on tubing samples from the same tube lengths as used for the initial cold tests plus several additional samples to give a wider variation in t/R_o . The collapse test results are plotted on Figure C-6. The elastic and inelastic collapse theory curves are also plotted. These curves are based on the 550°F elastic and physical properties for this material. Here, as in the cold tests, the elastic theory, when applicable, accurately predicts collapse. The 1/8 hard sample with a $t/R_o = 0.10$ and the annealed sample with the $t/R_o = 0.086$ fell below the predicted curves, substantiating the previously established observation that neither criterion accurately correlates with the experimental data in the transition zone. The annealed tube, with the $t/R_o = 0.10$, collapsed reasonably close to the inelastic predicted value. The sample with the t/R_o of 0.15 was believed to be defective.

Based on the results of these first two series of tests, subsequent testing was aimed at determining the actual collapse curves for annealed and hardened tubing in this transition region between elastic and inelastic collapse. This region which extends from $t/R_o = 0.07$ to 0.12 is the area of greatest interest to the designer. Collapse pressures between 2000 psi and 9000 psi can be obtained by varying t/R_o within this range and varying the degree of cold work.

The next series of evaluations consisted of cold collapse tests on one-half inch diameter, annealed samples with t/R_o range between 0.08 and 0.15. The collapse data from these tests along with the annealed tube test data from the first series of tests were normalized to a common yield strength and zero ovality. Although collapse pressure is probably not a linear function of yield strength in this transition region, normalizing in this manner provided a better comparison between the test collapse points and theory than did the use of the raw data. A plot of the normalized data is shown on Figure C-7. The points which repre-

sent Types 304, 347 and 348 samples did not fall on a smooth curve but formed a band which originated on the elastic curve and approached the inelastic curve at $t/R_0 = 0.14$. The width of the band is not excessive, considering the different materials tested and the normalizing technique. These data provided additional evidence that the true collapse curve passes well below the intersection of the classical elastic and inelastic curves.

The next series of tests was conducted on 5/8-inch and 3/4-inch O.D. annealed tubes with a variation in t/R between 0.054 and 0.126. This series was conducted to further substantiate the results of the previous series of tests and to determine if collapse is simply a function of t/R_0 or if the radius is an independent variable. The results of this series are shown in Table C-1. It is interesting to note that all the data fall within, or reasonably close to, the previously determined collapse band.

The results of this and the previous series of tests indicated no significant difference in the collapse characteristics of Types 304, 347 or 348 stainless steels. The 20% variation in the yield strength of the sample makes the linear normalizing technique somewhat questionable; however, there is no doubt that the data show that collapse is a function of the t/R_0 and not radius alone.

The final series of specimens in this set of evaluations utilized work hardened tubing to determine if the same smooth transition occurs between the elastic and inelastic regions. The results of this final series, which were conducted on hardened Types 304 and 347 tubing, are presented on Table C-II. A plot of these data with the collapse pressures normalized to a common yield strength of 75,000 psi is shown on Figure C-8. Here again the points fell within a relatively narrow band that originated at the lower end of the elastic curve and approached the inelastic curve at a t/R_0 ratio well into the thick walled region. The data on this plot show that samples with a $t/R_0 = 0.10$ and a yield

TABLE C-I

RESULTS OF ROOM TEMPERATURE COLLAPSE TESTS ON ANNEALED
FREE STANDING STAINLESS STEEL TUBING CONDUCTED DURING THE
FOURTH QUARTER OF FY 1962

Sample Number	Material	Average Wall Thickness (Inches)	Average O.D. (Inches)	t/R _o	Ovality D _{max} -D _{min} .100 D _{min} (%)	Average 0.2% Offset Y.S.	Collapse Pressure (psi)	Collapse Pressure Corrected to 45,000 Y.S. and No Ovality
19-3/4-1	347	0.0195	0.750	0.052	0.05	-----	1070	*
20-3/4-A-1	304	0.0205	0.7488	0.0548	0.32	39,200	1025	*
20-3/4-A-2	304	0.0207	0.7499	0.0552	0.21	39,200	1020	*
28-3/4-A-1	304	0.0286	0.7502	0.0762	0.00	37,200	2475	3050
28-3/4-A-2	304	0.0286	0.750	0.0762	0.00	37,200	2400	2950
28-5/8-1	304	0.0278	0.6241	0.0891	0.06	41,100	2980	3420
28-5/8-2	304	0.0278	0.6240	0.0891	0.08	41,100	3000	3440
35-3/4-A-2	304	0.035	0.750	0.0933	0.00	39,400	3500	4080
35-3/4-A-3	304	0.035	0.750	0.0933	0.05	39,400	3500	4160
42-3/4-A-1	304	0.041	0.7504	0.1093	0.11	51,700	4900	4540
42-3/4-A-2	304	0.041	0.7501	0.1093	0.00	51,700	5100	4530
49-3/4-A-1	304	0.472	0.7498	0.1259	0.05	47,400	5810	5760
49-3/4-A-2	304	0.476	0.7496	0.1270	0.11	47,400	5950	5930

* Collapse is elastic, therefore, independent of Y.S. in this range
 ----- No data

strength of 75,000 psi collapsed at about 6000 psi. This agrees with the hot collapse pressures for similar samples with 550°F yield strengths of 76,000 psi as shown on Figure C-6.

The data from both cold and hot tests of annealed and hardened tubing generally showed the same trend. When collapse pressure was plotted versus t/R_o , the true collapse curve followed the elastic collapse curve until the elastic curve started to rise rapidly. The true curve continued with a relatively constant slope, passing below the intersection of the elastic and inelastic (Mises criterion) collapse curves and approached the inelastic curve.

The next objective in the program was to determine a method for accurately predicting collapse pressure for cladding in this area of greatest interest (t/R_o 0.07-0.12). Several approaches were considered including modifications to the Mises criterion and the elastic collapse theory. The most accurate method developed was the "Tangent Modulus Criterion". This criterion functions well in the elastic collapse zone and in the transition zone, but predicts low and thus, conservative values, as the actual curve approaches the inelastic curve. The "Tangent Modulus Criterion" is simply the elastic collapse formula, as presented in reference 14, with the two modifications shown below.

$$P_c = \frac{Et}{4(1 - \nu^2)} \left(\frac{t}{R_o} \right)^3$$

The first modification is the substitution of tangent modulus (E_t) for Young's modulus (E). By definition, Young's modulus is the slope of the stress-strain curve for a given material in the elastic region. Since Hook's law applies in this region the slope and, therefore, E are constant. The tangent modulus, however, is the slope at any point on the curve and, therefore, decreases with increasing values of stress above the proportional limit. Thus, when evaluating these thin walled tubes, we consider hoop stress in the tube at collapse, as shown below.

$$\sigma_c = \frac{PR_o}{t}$$

For samples which collapse in the elastic region, σ_c is below the proportional limit on the stress-strain curve for the material. The tangent modulus of elasticity at this point would be Young's Modulus (E) and, therefore, the collapse should, and, in fact, does agree with the value predicted by the elastic collapse formula. However, when samples collapse in the transition region, the equation for hoop stress in a thin walled tube indicates a stress level above the proportional limit. The tangent modulus of elasticity at this point is somewhat lower than E. Substitution of this actual value of E_t into the elastic collapse equation drops the predicted value below that predicted by elastic theory but not as low as the actual value. A further modification was suggested in the literature, namely, the increase in Poisson's ratio from 0.3 to 0.5 when the value of E_t falls below E. Thus the equation,

$$P_c = \frac{E_t}{4(1 - a^2)} \left(\frac{t}{R_o} \right)^3 ;$$

where "a", which varies between 0.3 and 0.5, is substituted for ν , was evolved. Poisson's ratio is greater for plastic deformation than for elastic strain. For steel, this ratio is 0.30 for elastic strain and 0.50 for plastic strain.

As in the elastic and inelastic collapse formulae, this criterion is based on the material being isotropic. In other words, it is assumed that the axial tensile properties of the tube as determined by test can be used in determining the circumferential collapse strength.

A formula now exists for calculating collapse with three variables, namely, E_t , "a", and t/R_o . E_t is a property of the material in question at the collapse stress (σ_c). The value of "a" is arbitrarily set as a function of E_t . The t/R_o ratio is the independent variable; however, the relationship,

$$\sigma_c = \frac{E_t}{4(1 - a^2)} \left(\frac{t}{R_0} \right)^2$$

exists since it is assumed $\sigma_c = \frac{PR}{t}$.

Because E_t varies with stress (σ_c) as specified by the stress-strain curve and also by the above formula, a simple direct calculation of P_c cannot be made using the tangent modulus formula. An iteration process must be used as follows. The designer will choose R_0 and will fix P_c at some level above reactor operating pressure. The material (annealed 304, 1/8 hard 347, etc.) is then selected and the wall thickness (t) chosen. Assuming a selection of 1/8 hard, Type 347 stainless steel, a tensile test must be performed on a sample of 347 stainless steel with the minimum 0.2% offset yield strength as allowed in the tubing specification. Using the assumed wall thickness (t), σ_c is calculated by the expression,

$$\sigma_c = \frac{PR_0}{t}$$

Using the stress - strain curve for the sample tested, the value of E_t is determined for the above calculated stress. This value is substituted into the original equation and P_c is calculated. If the resultant value agrees with the required P_c , the correct value of t had been assumed. If the values of P_c is low the calculation is repeated assuming a larger value of t . Several iterations may be necessary. It may also be found that an excessive wall thickness is necessary for the required pressure and clad radius. In this case, the use of stronger material should be considered and the iteration process repeated using the stress-strain curve for the higher strength material. The time required for the above iterations can be considerably reduced by use of the sets of curves for P_c versus t/R_0 and σ_c versus t/R_0 as plotted on Figures C-9 and C-10. A sample calculation is presented below.

Predict collapse for sample 25BW12 as shown on Table C-II.

$$0.2\% \text{ Offset } \sigma_y = 75,000 \text{ psi}$$

$$R_o = 0.5016$$

$$t = 0.0252$$

$$t/R_o = 0.1004$$

First Iteration:

Assume collapse pressure = 6000 psi,

From Figure C-9, $E_t = 20 \times 10^6$,

Then, from Figure C-10, $\sigma_c = 60,000$ psi,

From stress-strain curve, find the value of

$$E_t \text{ at } \sigma_c = 60,000 \text{ psi}$$

$$E_t = 17.5 \times 10^6$$

From Figure C-9, determine the corresponding P_c for

$$E_t = 17.5 \times 10^6$$

$$P_c = 5450 \text{ psi}$$

Second Iteration:

Assume collapse pressure = 5800 psi

From Figure C-9, $E_t = 19.25 \times 10^6$

From Figure C-10, $\sigma_c = 58,000$ psi

From stress-strain curve when $\sigma_c = 58,000$

$$E_t = 19.5 \times 10^6$$

From Figure C-9, $P_c = 5880$ psi

Predicted Collapse Pressure = 5840 psi

The collapse pressures found experimentally and by the new tangent modulus approach are appreciably below those predicted by the classical collapse equation as shown in Table C-II. Thus, use of the classical collapse criteria would permit overly optimistic design values even when engineering safety factors as high as 1.5 are offered.

TABLE C-II

RESULTS OF ROOM TEMPERATURE COLLAPSE TESTS ON HARDENED FREE STANDING

STAINLESS STEEL TUBING, CONDUCTED DURING FOURTH QUARTER OF FY 1962

Sample Number	Material	Hardness	Average Wall Thickness (Inches)	Average O.D. (Inches)	t/R _o	Ovality ^(a)	Average 0.2% Offset Y.S.	Actual Collapse Pressure (psi)	Predicted Collapse By Tangent Modulus	% Variation ^(b)	Predicted Inelastic Collapse ^(c)	% Variation ^(b)
25½C1	304L	1/8	0.025	0.5016	0.0999	0	60,800	4500	4950	-9.1	6668	-32.5
25½C2	304L	1/8	0.025	0.5015	0.0999	0	60,800	4440	4950	-10.3	6651	-33.2
25½B1	304L	1/4	0.0253	0.501	0.1005	0	77,100	5850	5600	4.5	8538	-31.5
133 25½B2	304L	1/4	0.0253	0.5011	0.1005	0	77,100	6045	5600	7.9	8536	-29.2
20BW1	347	1/4	0.020	0.5015	0.0797	0.36	77,300	3390	3530	-4.0	6837	-50.4
20BW2	347	1/4	0.020	0.5016	0.0797	0	77,300	3350	3530	-5.0	6835	-51.0
20BW3	347	1/4	0.020	0.5015	0.0797	0	77,300	3570	3530	1.1	6837	-47.8
25BW11	347	1/4	0.0252	0.5014	0.1004	0	75,000	6150	5840	5.3	8268	-25.6
25BW12	347	1/4	0.0252	0.5016	0.1004	0.12	75,000	6050	5840	3.5	8265	-26.8
30BW1	347	3/8	0.0302	0.5013	0.1203	0	87,650	9000	8500	5.9	11460	-21.5
30BW2	347	3/8	0.0302	0.5011	0.1202	0	87,650	8800	8500	3.5	11465	-23.2
35BW1	347	3/8	0.0351	0.501	0.1400	0	88,050	(d)	----	---	-----	-----
35BW2	347	3/8	0.0352	0.5011	0.1402	0	88,050	(d)	----	---	-----	-----

$$(a) \frac{D_{max} - D_{min}}{D_{min}} \cdot 100$$

$$(b) \frac{\text{Actual} - \text{Predicted}}{\text{Predicted}} \cdot 100$$

$$(c) P_{cr} = 0.548 \sigma_y \left(1 - \frac{R_i^2}{R_o^2} \right); \sigma_y = 0.2\% \text{ Offset Yield Strength}$$

(d) Tests Stopped at 11,000 psi With No Collapse.

3) Determination of Directional Properties of Cladding

As previously indicated, the predicted external collapse pressures in this program have been based on axial tensile properties of the samples. Actually, tube collapse is dependent upon circumferential compressive properties. Therefore, cold collapse tests were performed with strain gages to determine if the properties of hardened tubing, e.g., modulus of elasticity and 0.2% offset yield stress, were significantly anisotropic in the range of interest. If circumferential compressive properties were essentially the same as the axial tensile properties, the use of tensile data to predict tube collapse in accordance with the above criteria would be adequate.

SR-4 strain gages were mounted axially and circumferentially on two 1/8 hard and two 3/8 hard, Type 347 tube samples. The tube samples were externally pressurized in small increments of pressure. At various pressure intervals the pressure was reduced to zero to check initial strain gage readings. In two tests, the 1/8 hard tube samples were internally pressurized to obtain axial tensile properties as a check on strain gage readings. Values obtained for modulus of elasticity in the circumferential compressive and the axial tensile direction are as shown in Table C-III. Each of these values represent the average results of two test samples.

From the data, it is concluded that the modulus of elasticity in the circumferential compressive direction is essentially the same as in the axial tensile direction within an experimental error of 5%. These values are similar to values of modulus determined from tensile data on the same lots of tubing. Also, the predicted values for tube collapse, which are a function of circumferential yield stress, were computed from axial tensile data and have proved to be in reasonable agreement with the actual collapse pressures.

Therefore, it appears that these two properties of hardened stainless steel tubing are not significantly anisotropic in the region of interest. In addition,

TABLE C-III

MODULUS OFELASTICITY IN AXIAL AND CIRCUMFERENTIAL DIRECTIONS

<u>Hardness</u>	<u>Modulus of Elasticity x 10⁶ psi</u>	
	<u>Circumferential Compressive Direction</u>	<u>Axial Tensile Direction</u>
1/8	29.4	27.8
3/8	30.1	----

forward reflection X-ray diffraction photograms taken on selected samples did not indicate evidence of anisotropy.

4) Evaluation of Collapse Criteria for Large Diameter Tubes

The useful range of the collapse criteria, developed in earlier sections, was investigated for tubing with diameters up to 3.175 cm. Deviations from values predicted from the developed collapse criteria were assessed with the object of introducing modifications to the equation to provide collapse criteria for the entire range of tube diameters under investigation.

A series of Type 304 stainless steel tubing, 1.9 cm (0.750 in.) in outside diameter with wall thicknesses of 0.05 cm (0.020 in.) and 0.124 cm (0.049 in.) were used in initial tests to establish the tubing length to diameter ratio (L/D) required to represent infinite tubing. These samples represent wall thickness to outside radius (t/R) ratios of 0.05 and 0.13, respectively. Since this was the upper and lower t/R limit of interest, it was anticipated that tubing with outside diameters up to 3.175 cm in the same t/R range would have similar L/D limitations.

Three specimens were fabricated for each L/D ratio. The maximum percentage out-of-roundness was 0.20% although over 90% of the tubing were 0.13% or less. However, significant differences in collapse pressure were not observed as a result of this variation. The bottom curve of Figure C-11 shows a plot of collapse pressure versus L/D ratios for 1.9 cm (0.750 in.) outside diameter Type 304 seamless stainless tubing with a 0.05 cm (0.020 in.) wall. The data indicate that an L/D of nine or greater represents an infinite tubing length for a t/R of 0.05. The top dotted curve is for Type 304 seamless stainless tubing with an outside diameter of 1.9 and a 0.124 cm wall (0.750 and 0.049 in.). This curve indicates that an L/D of eight or greater represents an infinite tubing length for a t/R of 0.13, and that there is a greater spread in collapse pressures for heavier wall tubing.

Testing was performed on Type 347 weldrawn annealed tubing, 3.175 cm (1.250 in.) in outside diameter, with six different wall thicknesses in the range of 0.07 and 0.19 cm (0.028 and 0.075 in.). Stress-strain data were obtained from tensile tests of two samples from each wall thickness size. All of the 0.2% offset yield strength values were in the range of 2450 to 2730 kg/cm² (35,000 to 39,000 psi) with the exception of samples with 0.07 and 0.10 cm wall thicknesses. Values from these thinner wall tubing were in the range of 3290 to 3430 kg/cm² (47,000 to 49,000 psi). This increase was a result of the final sizing pass used at the mill.

Collapse tests were performed on duplicate specimens for each of the six different wall thicknesses (0.028 to 0.075 in.) of the 3.175 cm diameter tubing. The specimens were 63.5 cm in length which represented a length to diameter ratio of 20. The collapse data are shown in Table C-IV.

Figure C-12 is a plot of the actual collapse pressure versus the predicted collapse pressure for the Tangent Modulus, Elastic, and Inelastic Criteria. In this figure, the solid line indicates complete agreement between actual and predicted values; a range of $\pm 20\%$ from this line is also shown. In using the Tangent Modulus Criterion, the value of Poisson's ratio was set as a function of the tangent modulus (E_t), since Poisson's ratio is greater for plastic deformation than for elastic strain. As E_t decreased, Poisson's ratio increased from 0.3 to 0.5 as indicated in Table C-V.

The following conclusions were based upon data presented in Figure C-12.

a) With the exception of data points represented by the Tangent Modulus Criterion, all data fell below the indicated straight line relationship between actual and predicted collapse pressures. Thus, collapse pressures predicted by either the Elastic or Inelastic Collapse Criteria are greater than those actually observed.

b) For the range of t/R investigated, data points represented by the

TABLE C-IV -Collapse Data

Sample No.	Material	Avg. Wall Thickness (cm)	Avg. O.D. (cm)	$\frac{t}{R_o}$ (a)	Ovality (b)	Avg. 0.2% Offset Y.S. (kg/cm ²)	Actual Collapse Pressure (kg/cm ²)	Collapse by Tangent Modulus (c) (kg/cm ²)	Predicted Collapse by Elastic Criterion (d) (kg/cm ²)	Predicted Collapse by Inelastic Criterion (e) (kg/cm ²)	Actual Collapse Pressure Normalized to 2660 (kg/cm ²) (f)
CT-A-28-1	347	.073	3.177	.046	.10	3197	45.5	53.2	53.2	154.5	(f)
CT-A-28-2	347	.072	3.175	.045	.10	3197	44.8	53.2	53.2	154.5	(f)
CT-A-40-1	347	.101	3.172	.064	.04	3358	131.3	139.7	135.5	232.1	104.0
CT-A-40-2	347	.101	3.172	.064	.04	3358	127.8	139.7	135.5	232.1	101.2
CT-A-49-1	347	.123	3.177	.078	.20	2511	157.5	135.1	225.3	202.1	166.8
CT-A-49-2	347	.124	3.176	.078	.28	2511	155.8	135.1	225.3	200.7	165.0
CT-A-56-1	347	.144	3.178	.091	.04	2688	211.8	176.8	404.3	263.4	209.7
CT-A-56-2	347	.143	3.176	.090	.10	2688	199.5	175.0	391.0	256.3	197.5
CT-A-56-3	347	.143	3.176	.090	.02	2688	204.8	175.0	391.4	265.6	202.8
CT-A-65-1	347	.166	3.175	.105	.02	2657	266.0	213.5	619.2	303.1	266.0
CT-A-65-2	347	.166	3.176	.104	.06	2657	264.3	211.1	601.7	297.2	264.3
CT-A-75-1	347	.188	3.170	.119	.04	2717	329.0	274.8	904.4	347.8	322.1
CT-A-75-2	347	.188	3.171	.119	.06	2717	325.5	274.8	904.4	343.3	318.7

(a) t/R_o = Average wall thickness ÷ Radius

(b) $D_{max} - D_{min}/D_{min} \cdot 100\%$; where D = Outside diameter

(c) $P_c = \frac{E_t}{4(1 - a^2)} (t/R_o)^3$; E_t = Tangent Modulus, a = Poisson's Ratio

Reference: "The Development and Testing of UO₂ Fuel systems for Water Reactor Applications,"
CEND-157, June 1962

(d) $P_c = \frac{E}{4(1 - a^2)} (t/R_o)^3$; where $a = 0.284$, and E = Young's Modulus

(e) $P_c = \frac{(1 - B)\sigma_y (1 - R_i^2/R_o^2)}{\sqrt{3}}$; where B = Percentage reduction in collapse strength due to out-of-roundness
 $\sigma_y = 0.2\%$ offset yield strength of the material
 R_i = Inside radius.

(f) Collapse is elastic, therefore, independent of yield strength in this range.

TABLE C-V

Variation of Poisson's Ratio With Tangent Modulus

<u>Poisson's Ratio</u>	<u>Tangent Modulus (10^4 kg/cm^2)</u>
0.30	210
0.315	203
0.33	196
0.345	189
0.36	182
0.37	175
0.38	168
0.39	161
0.40	154
0.41	147
0.42	140
0.43	133
0.44	126
0.45	119
0.475	112
0.50	7 - 105

Tangent Modulus Criterion consistently fell within the band of $\pm 20\%$ surrounding the indicated straight line relationship. With one major exception at t/R of 0.046, all deviations from the solid line were in the direction of larger measured collapse pressures than those calculated. Thus, values obtained through application of the Tangent Modulus Criterion are considered to be conservative.

c) The Elastic Criterion provides a reasonably close relationship between actual and predicted collapse up to a t/R of 0.060. As anticipated (see Figure C-13) in the region beyond the transition between Elastic and Inelastic behavior, the Inelastic Criterion becomes more accurate as t/R is increased.

d) Except for the range of t/R from 0.060 to 0.115*, the combined use of the Elastic and Inelastic Criteria, in which the lowest calculated collapse pressure is employed, provides a reasonable accurate prediction of the actual collapse pressures. However, as noted above, all deviations are in the direction of predicting lower than actual collapse pressures.

e) For the entire range of t/R investigated, the Tangent Modulus Criterion provides the most conservative estimate of collapse pressures. In all cases, the difference between actual and predicted collapse, using this Criterion was less than 20%.

Figure C-13 shows a plot of the collapse pressures for the room temperature 3.175 cm outside diameter annealed tubing normalized to a yield strength of 2660 kg/cm² (38,000 psi) as a function of t/R_o (thickness to outside radius). Included for comparison are earlier data points for 1.270 and 1.905 cm (0.500 and 0.750 in.) outside diameter annealed tubing. Data points were not corrected for ovality since the influence of ovality on tubing collapse was not observed until the ovality exceeded 1% in 1.270 and 1.905 cm diameter tubing and 0.3% in 3.175 cm.

* By extrapolation from Figure C-13

tubing. (The greatest ovality observed in the 3.175 cm tubing was 0.3%.) Also plotted are the predicted collapse curves based on the three collapse criteria.

Data shown in Figure C-13 not only substantiate the conclusions made above for the 3.175 cm diameter tubing, but also indicate that these conclusions are generally applicable to the range of tube diameters investigated. The Tangent Modulus Criterion provides a close approximation of actual collapse for the 1.270 cm diameter tubing. As the tubing diameter increases, there is poorer agreement between actual collapse pressures and those predicted from the Tangent Modulus Criterion. In addition, actual collapse pressures approach those predicted by the Inelastic Model at lower t/R values. With only one exception ($t/R = 0.046$), all measured collapse pressures fell below those predicted by either the Elastic or Inelastic Collapse Criteria. On the other hand, the Tangent Modulus Criterion provided a reasonably accurate, but conservative, indication of collapse pressure; the agreement for the smaller diameter tubing (typical of boiling - and pressurized - water cladding) was particularly good.

5) Conclusions

a) For tubing with nearly isotropic mechanical properties, the axial tensile properties can be utilized in the determination of circumferential collapse strength.

b) The true collapse pressure curve for a given material is approximately a linear function when collapse pressure is plotted versus t/R_0 . The collapse pressure can be reasonably predicted for cladding in the region of interest by use of the tangent modulus criterion developed in this report. This criterion predicted collapse pressure is within 20% for both annealed and hardened samples of Types 304 and 347 stainless steels. The agreement was within 10% for 0.5 and 0.75 inch diameter tubing.

c) The tangent modulus criterion does not take into account tube ovality. However, ovality for the majority of the tubes tested in this program was below

0.2% with no special attempts made to obtain perfectly round tubes. The effect of ovality on tube collapse pressure was not observed until ovality exceeded 1%. Since tubing with less than 1% ovality is commercially available, it was not considered worthwhile to evaluate the effects of greater ovality.

b. Fuel Supported Cladding

The object of this phase of the program was to evaluate the use of stainless steel cladding of further reduced thickness such that it would collapse onto the fuel when subjected to reactor pressure.

The program was conducted to determine the extent of further limitations on the use of fuel supported cladding. Two basic fuel forms were considered. The first was pressed and sintered pellets, and the second was UO_2 powder which was compacted sufficiently to provide adequate support. Numerous samples of the two forms were prepared and the following variables were investigated:

- 1) Annular gap between pellets and clad
- 2) Variations in this gap due to unequal pellet diameters
- 3) Axial gap between pellets
- 4) Loss of support in regions around chipped pellets
- 5) Compacted powder density
- 6) Clad thickness
- 7) Clad hardness

Several methods of internally supporting the clad in the fission gas void region above the fuel column were also investigated.

The collapse tests were run by externally pressurizing 10-inch long, $\frac{1}{2}$ -inch diameter samples in the pressure vessel, previously described and shown in Figure C-2. Fuel pellets were simulated by the use of steel mandrels. In this way the various diameter variations, chip effects and axial pellet gaps could be more easily and accurately obtained. Fused, depleted UO_2 powder of typical particle size distributions was used for all the compacted powder samples.

Testing was conducted using 0.010 inch and 0.015 inch wall tubing. The method of testing consisted of externally pressurizing groups of test samples in increments of 500-1000 psi up to total pressures of 5000 psi. After each increment of pressure, the test samples were removed from the pressure vessel and inspected visually and dimensionally. The results are shown in Tables C-VI, C-VII and C-VIII. Figures C-14 through C-20 are photographs of representative samples showing nominal dimensional information and the external pressures to which the samples have been exposed.

1) Pellet Fuel Supported Clad Tests

The object of the tests involving pellet supported cladding was to collapse the clad uniformly onto the pellets along the entire specimen. This would provide the best heat transfer condition in an actual element, thus lowering the centerline temperature. It would also provide a smooth surface with no sharp bends which would offer the best resistance against fatigue failure resulting from strain cycling during core life.

Uniform collapse is primarily a function of initial clearance between the pellets and cladding.

Samples were tested with various clearance between clad and simulated pellets. Annealed and 1/8 hard, 0.500 inch O.D. tubes with 0.010 and 0.015 inch wall thicknesses were used. Mandrels with one continuous diameter (Type A) and mandrels with four different diameters (Type C) simulating pellet variations were used. These mandrels are shown on Figure C-3. Representative samples are shown on Figures C-14 and C-15.

The effect of gaps in the cladding support caused by chips in the pellets and by axial spaces between pellets was also evaluated. These were simulated by mandrels with grooves of various widths, flats and chamfers. Annealed tubes with 0.010 and 0.015 inch wall thicknesses were tested. Representative test samples are shown on Figure C-16. A listing of the simulated fuel supported

TABLE C-VI

RESULTS OF ROOM TEMPERATURE COLLAPSE TESTS ON SIMULATED PELLET FUEL-
SUPPORTED, THIN WALLED, TYPE 347 CLADDING

Sample No.	Hard- ness	Average Wall Thickness (Inches)	Mandrel* Type and Radial Gap (Inches)	Mandrel* Type and Radial Gap (Inches)	Observations
10AW12	Annealed	0.0098	A-0.0014	B-0.0022	Slight longitudinal ripple in the portion of tubing containing 0.0022 inch radial gap. After 5000 psi, ripple was 0.006 inch high and there was a slight concavity at 0.125 inch groove.
15AW12	Annealed	0.0148	A-0.0059	B-0.0029	After 3000 psi, longitudinal ripple over portion of tubing containing a 0.0059 inch radial gap. After 5000 psi, there is a slight amount of concavity over the 0.125 inch groove.
15AW13	Annealed	0.0148	A-0.0029	B-0.006	Very slight ripple and very slight concavity over 0.125 inch groove after 5000 psi.
10AW13	Annealed	0.0097	C-0.0024 0.0031 0.0040 0.0009	C-0.0016 0.0024 0.0029 0.0041	Ripple over 2/3 length of tube after 2000 psi. After 5000 psi, dimpling in center in addition to rippling along full length. Very slight diameter changes.
15AW14	Annealed	0.0148	C-0.0031 0.0040 0.0050 0.0056	C-0.0015 0.0021 0.0030 0.0036	After 3000 psi, longitudinal ripple formed only on half the length of tubing where gaps were the largest. Very slight diameter changes.
10AW14	Annealed	0.0096	A-0.0024	C-0.0014 0.0023 0.0029 0.0040	No ripple is evident. Clad deforms appreciably over 1/8 inch flat spot after 5000 psi pressure

* Each tube sample contains two mandrels 4 inches long

TABLE C-VI

(Continued)

Sample No.	Hard- ness	Average Wall Thickness (Inches)	Mandrel* Type and Radial Gap (Inches)	Mandrel* Type and Radial Gap (Inches)	Observations
15AW15	Annealed	0.0146	A-0.0031	C-0.0015 0.0022 0.0029 0.0040	After 3000 psi, longitudinal ripple over the portion of tubing containing the 0.0031 inch gap. Clad deforms slightly over chips.
10AW4	Annealed	0.0098	A-0.0023	A-0.0015	After 2000 psi pressure; slight longitudinal rippling in region of 0.0023 inch radial gap. After 5000 psi, ripple was 0.0015 inch high.
15AW10	Annealed	0.0147	A-0.0031	A-0.0014	Slight longitudinal ripple after 3000 psi in the portion of tubing containing 0.0031 inch gap. Ripple was 0.0045 inch high after 5000 psi. Region of 0.0014 gap did not ripple.
10AW5	Annealed	0.0099	B-0.0023	B-0.0016	Slight longitudinal ripple after 2000 psi in the region of the 0.0023 inch gap. After 4000 psi, deep concavity (0.010 in.) around 0.125 inch groove. Ripple 0.0075 inch high in region of 0.0023 inch gap.
15AW11	Annealed	0.0147	B-0.0031	B-0.0015	After 3000 psi, slight longitudinal ripple formed in the portion of the tubing containing the 0.003 inch gap. After 5000 psi, ripple was 0.008 inch high. Clad concavity 0.006 inch deep in 0.125 inch grooves.

* Each tube sample contains two mandrels 4 inches long

TABLE C-VI
(Continued)

Sample No.	Hard- ness	Average Wall Thickness (Inches)	Mandrel* Type and Radial Gap (Inches)	Mandrel* Type and Radial Gap (Inches)	Observations
10AW11	Annealed	0.0096	A-0.0025	B-0.0017	Slight longitudinal rippling after 2000 psi. Slight collapse at 0.125 in. groove after 3000 psi. After 5000 psi ripple was 0.0055 inch high; clad con- cavity was 0.015 inch deep at the 0.125 inch groove.
10AW15	Annealed	0.010	C2-0.005 0.002 0.0035 0.0025	C4-0.009 0.0065 0.008 0.007	1000 psi external pressure caused the tube to collapse on the C4 mandrel, forming a ripple. 2000 psi pressure caused failure of the tube at the end cap and slight (0.002 inch) deformation of tube over C2 mandrel. 3500 psi pressure caused pinching of the ripple over the C4 mandrel. Deformation of the tube onto the reduced diameters of mandrel is beginning. Pressurizing to 5000 psi caused further deformation to the mandrels.
10CW4	3/8	0.010	C2-0.005 0.002 0.0035 0.0025	C4-0.009 0.0065 0.008 0.007	1000 psi caused 0.002 inch deformation to oval shape at center of tube. 1500 psi caused 0.004 inch deformation to oval shape over the C4 mandrel. 2000 psi caused tube to collapse onto C4 mandrel forming a ripple. Other section deformed to ellipse with minor diameter on mandrel. 2500 psi caused tube collapse of end plugs and pinching of ripple. 3500 caused start of ripple over the C2 mandrel. No deformation onto reduced mandrel diameters is observed. Pressurization to 5000 psi caused pinching between mandrels and end plugs.

* Each tube sample contains two mandrels 4 inches long

TABLE C-VII

RESULTS AT ROOM TEMPERATURE OF COLLAPSE TESTS - THIN WALLEDTYPE 347 STAINLESS STEEL WITH COMPACTED POWDER FUEL SUPPORT

Sample No.	Hardness	Average Wall Thickness (Inches)	Internal Loading	Observations
10AW10	Annealed	0.010	Depleted UO_2 powder compacted to 80.8% T.D.	No measureable change in dimensions after 2000 psi external pressure. Extensive dimpling of the tube was observed after an external pressure of 3000 psi. Testing of this sample was discontinued.
15AW4	Annealed	0.015	Depleted UO_2 powder compacted to 82.9% T.D.	No measurable changes after 2000 psi external pressure. Slight dimpling at one end after 3000 psi. Dimpling over the entire surface was observed after external pressurization to 4000 psi, but deformation was to a lesser degree than on 10AW10.
10AW8	Annealed	0.010	Depleted UO_2 powder compacted to 89.4% T.D.	External pressurization to 5000 psi did not cause a measurable dimensional change or local deformation to the fuel supported section of the sample. Clad did collapse at end caps.
15AW5	Annealed	0.015	Depleted UO_2 powder compacted to 88.7% T.D.	External pressurization to 5000 psi caused no measurable dimensional change or local deformation of the fuel supported section.
10AW17	Annealed	0.010	Depleted UO_2 powder compacted to 83.7% T.D. A metal washer with 3/16 inch hole was placed on the top of the fuel and above it a one in. long, 0.040 in. wire diameter 0.480 in. diameter coil spring to hold the powder down and support the simulated fission gas gap.	No noticeable surface effects or dimensional changes after 1000 psi external pressure. Pressurizing to 1500 psi caused fuel supported tube section just below washer to buckle. Slight roughening of surface. Pressurizing to 2000 psi caused extension of buckled section; 0.002 in. uniform reduction in diameter in remaining fuel supported length and dimpling at end caps. Pressurizing to 2500 psi caused slight extension of buckled zone, but no other changes.

TABLE C-VII
(Continued)

Sample No.	Hardness	Average Wall Thickness (Inches)	Internal Loading	Observations
				Pressuring to 3500 psi caused collapse about the spring. Pressurizing to 5000 psi caused slight additional collapsing of clad between spring coils. Fuel supported section still intact; diameter 0.495 - 0.500.
10AW18	Annealed	0.010	Depleted UO_2 powder compacted to 83.3% T.D. A metal washer with a 3/16 inch hole was placed on top of the powder column. A one inch long, 0.480 inch diameter, 0.015 inch thick split sleeve was pushed into the tube to support the simulated fission gas gap.	Pressurizing to 1000 psi caused no noticeable surface effects or dimensional changes. Pressurizing to 1500 psi caused split sleeve supported section to collapse with ripple extending into fuel supported section. Pressurizing to 2000 psi caused slight extension ripple and 0.002 uniform reduction in diameter of remainder of fuel supported section. Pressurizing to 3500 psi caused slight ripple to form from sleeve end. Pressurizing to 5000 psi made no significant changes in fuel supported section. Diameter variation 0.492 - 0.507.
10AW31	Annealed	0.010	Depleted UO_2 powder vibratory compacted to 86.4% T.D.	Pressurizing this sample to 5000 psi did not collapse it except at the end caps. There is a slight waviness to the surface and a slight bow to the tube. However, these effects were caused by the loading process.
10BW5	1/8 Hard	0.010	Depleted UO_2 powder vibratory compacted to 85.3% T.D.	Pressurizing this sample to 2000 psi caused the formation of a ripple to one end plug. This is due to excessive radial clearance between the tube and end plug. This ripple propagated about one inch into fuel supported zone due to lower fuel density at the upper end of the fuel column. The remainder of the fuel supported section did deform more than 0.002 in diameter after 5000 psi pressure. However, there was slight local dimpling over the surface.

TABLE C-VII
(Continued)

Sample No.	Hardness	Average Wall Thickness (Inches)	Internal Loading	Observations
10AW35	Annealed	0.010	Depleted UO_2 powder compacted to 88.0% T.D.	External pressurization to 5000 psi caused no measurable dimensional change or local deformation to the fuel sup- ported section. As in the other two samples of this series, there was col- lapsing at the end cap due to undersize end caps.
10AW36	Annealed	0.010	Depleted UO_2 powder compacted to 85.0% T.D.	External pressurization to 5000 psi caused no measurable dimensional change or local deformation in the fuel supported section.
10AW37	Annealed	0.010	Depleted UO_2 powder compacted to 86.0% T.D.	External pressurization to 5000 psi caused no measurable dimensional change or local deformation.

TABLE C-VIII

RESULTS OF ROOM TEMPERATURE TESTS ON EXPERIMENTAL METHODS OF
SUPPORTED VOID SECTION OF THIN WALLED FUEL SUPPORTED CLADDING

Sample Number	Clad Material	Hardness	Average Wall Thickness (Inches)	Internal Loading	Observations
10AW32	347	Annealed	0.010	A 0.468" to 0.470" O.D. helical spring containing 40 coils of 0.046" diameter spring steel wire. Free length 8", compressed length in tube 3". The remaining length of the tube sample contains a steel mandrel to simulate fuel.	Initial diametrical clearance between spring and tube I.D. was 0.010" to 0.012". Thus the tube collapsed onto the spring with the formation of a ripple over the length of the spring at initial pressurization of 1000 psi. After 2000 psi pressure, slight deformation of the clad between the spring coils is observed. After 5000 psi pressure, failure of the sample due to column buckling is observed. The cladding deformed between the coils thus allowing the sample to shorten about one-half inch, compressing the spring. The spring did not collapse.
10AW35	347	Annealed	0.010	A 0.465" to 0.467" O.D. helical spring containing 30 coils of 0.032" dia. spring steel wire. Free length 8", compressed length 3". The remaining length of the sample contains steel mandrels to simulate fuel.	Initial diametrical clearance between spring and tube I.D. was 0.013" to 0.015". Thus the tube collapsed onto the spring with the formation of a ripple at initial pressurization of 1000 psi. Slight deformation of the clad between the coils was observed after pressurization to 1500 psi. Contour of spring on clad surface more pronounced after 2000 psi pressure. At 2800 psi the sample failed due to column buckling after considerable deformation of clad between coils. The spring was compressed approximately one-half inch but did not collapse.

TABLE C-VIII
(Continued)

Sample Number	Clad Material	Hardness	Average Wall Thickness (Inches)	Internal Loading	Observations
10BW10	347	1/8 Hard	0.010	A solid mandrel and a 5-1/32" long stainless steel sleeve, 0.478" O.D. by 0.015" wall thickness. 0.001" radial clearance between sample and sleeve.	No deformation occurred at 1000 psi external pressure. After 2000 psi slight deformation was observed at the end cap adjacent to the sleeve. At 2200 psi an audible snap was noticed. Removal of the sample from the test vessel at this time showed that it collapsed flat over the length supported by the sleeve.

samples and the observed results are shown on Table C-VI. The following general observations were made from these results:

a) For 0.010 inch wall, annealed, 0.500 inch diameter cladding, an initial radial clearance of 0.002 inch appears to be the maximum value to insure against any rippling up to 5000 psi.

b) For 0.015 inch wall, annealed, 0.500 inch diameter cladding, the maximum radial clearance which will not ripple increases to 0.003 inch. With slightly larger clearances, rippling appears at 3000 psi.

c) The limited data on 0.010 inch wall, 3/8 hard, 0.500 inch diameter cladding indicates no advantage for hardened over annealed clad. The annealed clad collapses in a more uniform manner with less tendency to ripple.

d) When there is a variation in pellet diameters, as simulated by the Type C mandrels, it is the clearance between the larger diameter pellets and the clad that determines the mode of collapse. If one small pellet is located between two large ones, no ripple will occur even if the radial clearance is excessive by the usual standards.

e) For 0.010 inch wall, annealed clad, the axial pellet separation of 1/16 inch, simulated by the Type B mandrels, did not result in a collapse into the groove at 5000 psi external pressure. A 1/8 inch axial space caused slight local collapse at an external pressure just over 3000 psi.

f) An external pressure exceeding 4000 psi was required to cause localized collapse of 0.010 inch wall, annealed clad onto flat spots and chips in simulated pellets.

2) Powder Fuel Supported Clad Tests

All samples which contained depleted fused UO_2 powder were compacted by vibratory compaction. Again, 0.010 and 0.015 inch wall, 0.500 inch diameter, annealed, Type 347 tubing was used primarily with a limited evaluation of 0.010 inch wall, 1/8 hard tubing. The purpose was to determine the minimum compacted

powder density required to adequately support the thin walled clad against collapse by external pressure. All testing was performed at room temperature. An arbitrary external pressure value of 5000 psi was conservatively chosen as the point where the samples would be evaluated. This provided for the slightly lower physical properties at elevated temperatures and a large safety factor over anticipated reactor pressures.

The testing of compacted powder supported samples was conducted in the same manner as that of simulated pellet supported samples, with the external pressure increased in increments followed by a visual inspection and dimensional check after each increment. Brief descriptions of the test samples and the observed test results are listed chronologically in Table C-VII.

The first series of tests was composed of the first four samples listed in Table C-VII. The observed results indicate that the minimum density required to support both 0.010 and 0.015 inch wall annealed, 0.500 inch diameter, Type 347 cladding between 82.9% and 89% T.D. The tests also indicated that even for sufficiently high densities, adequate support must be provided up to the end cap with no voids or low density region at the end of the fuel column. A photograph of this first series of samples is shown in Figure C-17.

The second series of tests consisted of two 0.010 inch wall thickness, annealed tube samples containing compacted powder at 83.7 and 83.3% T.D., respectively. These specimens and all subsequent powder loaded specimens were used to evaluate techniques for clad support in the fission gas void region at the end of the fuel. The results of these clad support tests are described in detail later in this section.

External pressures of 5000 psi caused slight dimpling of the surfaces but no significant dimensional changes in the region away from the experimentally supported void sections. However, in both samples the devices supporting the void sections failed permitting collapse and dedensification of the fuel into

the void section. The collapse extended into the dedensified fuel section. A photograph of these samples after testing is shown on Figure C-18.

The third series of tests consisted of a 0.010 inch wall thickness, annealed tube samples containing compacted powder at 83.7 and 83.3% T.D., respectively. These specimens and all subsequent powder loaded specimens were used to evaluate techniques for clad support in the fission gas void region at the end of the fuel. The results of these clad support tests are described in detail later in this section.

External pressures of 5000 psi caused slight dimpling of the surfaces but no significant dimensional changes in the region away from the experimentally supported void sections. However, in both samples the devices supporting the void sections failed permitting collapse and dedensification of the fuel into the void section. The collapse extended into the dedensified fuel section. A photograph of these samples after testing is shown on Figure C-18.

The third series of tests consisted of a 0.010 inch wall thickness, annealed tube sample containing 86.4% T.D. compacted powder, and a 0.010 inch wall thickness, 1/8 hard tube sample containing 85.3% T.D. compacted powder. The surface of the sample containing 86.4% T.D. powder was roughened slightly during fabrication but an external pressure of 5000 psi caused no dimensional changes or dimpling of the surface. The hardened tube sample, containing 85.3% T.D. powder, collapsed onto the upper end plug forming a ripple after 2000 psi pressure. The ripple extended about one inch into the fuel supported zone after 5000 psi external pressure, indicating possible lower density at the end of the fuel column. The 5000 psi pressure caused slight dimpling along the remainder of the tube but less than 0.002 inch dimensional change. A photograph of these samples, after testing, is shown on Figure C-18.

The fourth and final series of tests consisted of four 0.010 inch wall thickness, annealed tubes containing 85%, 86% and 88% T.D. compacted UO_2 powder. This

series was prepared and tested primarily to substantiate the results of the third series. As indicated on Table C-VII and the photograph on Figure C-19, results similar to the previous series were obtained. The 85% T.D. sample as well as the others were dimensionally stable under 5000 psi pressure.

3) Support of Fuel Element Gas Void Section

The final subject investigated as part of the fuel supported cladding program was the internal support of the fission gas void space above the fuel column. A gas void is required in elements containing pelletized fuel and will probably be required with high density powder-packed fuels also. The volume of the spacer allowed depends on the expected fission gas release and the allowable gas pressure buildup during core life. When considering compacted powder fuels, it is also necessary to prevent the powder from moving into this void space since the resulting dedensification at the top of the fuel column would destroy the fuel's ability to support the clad against external pressure.

The first concept evaluated consisted of inserting a split sleeve into the void of powder-packed sample 10AW18 and a small helical spring into sample 10AW17. These samples are described and the test results discussed in Table C-VII. The results were not successful but the use of springs showed some promise, providing the loading is radial only. Two samples were then prepared containing springs which provided an adequate axial force to hold the fuel in place and allowed for some variation in fuel column height without significant change in compressive load. These samples are described and the test results discussed in Table C-VIII. Sample 10AW32 containing the heavier spring resisted collapse until 5000 psi; however, the degree of clad deformation permitted before failure and the catastrophic type of failure as shown in Figure C-20, were ample reasons to end consideration of a spring for this purpose.

The last sample tested contained a 0.015 in. thick sleeve which was inserted in a 0.010 in. thick test sample forming 0.025 in. thick laminated wall.

A description of the sleeve and the test results are listed in Table C-VIII. The results indicate that the external pressure which could be supported by the two concentric cylinders was approximately equal to the sum of what each could individually support, and that it was well below that pressure which could be supported by a single tube whose wall thickness was equal to the sum of the two wall thicknesses.

4) Conclusions

a) The evaluation of fuel supported cladding indicated 0.010 in. wall annealed stainless steel can be used as a clad over 0.500 in. diameter pelletized UO_2 fuel. However, the diametrical clearance between the fuel and the clad must not exceed 0.004 in. Subsequent external pressurizing would then uniformly collapse the clad onto the fuel.

b) Vibratory compacted UO_2 powder of 86% T.D. or greater provides sufficient fuel support to prevent any deformation of 0.500 in. diameter, 0.010 in. wall tube samples at 5000 psi external pressure.

NOTE

- (1) ALL PRESSURE PIPING, VALVES AND FITTINGS TO WITHSTAND 10,000 PSI PRESSURE AT 600°F
- (2) CALROD HEATING UNIT AND INSULATION TO BE USED ON 550°F TESTS
- (3) PRESSURIZING FLUID TO BE DOW CORNING 710 FOR ALL HOT TESTS AND COLD STRAIN GAGE TESTS
- (4) PRESSURE VESSEL AND ALL PRESSURE PIPING WITH THE EXCEPTION OF THE PUMP and GAGE IS CONTAINED WITHIN A SAND BAGGED ENCLOSURE FOR ALL TESTS

FIG C-1 SCHEMATIC OF COLLAPSE TEST EQUIPMENT

FIG C-2 COLLAPSE TEST PRESSURE VESSEL

FIG C-3 COLLAPSE TEST CLAD SPECIMENS

FIG C-4 ELASTIC-INELASTIC COLLAPSE OF ANNEALED AND HARDENED TYPE 347 WELDED TUBING AT ROOM TEMPERATURE

FIG C-5 EFFECT OF OUT-OF-ROUNDNESS OF TUBES IN REDUCING MAXIMUM COMPRESSION STRESS AT COLLAPSE PRESSURE FOR VARIOUS $t/\bar{R}$ RATIOS

FIG C-6 ELASTIC AND INELASTIC COLLAPSE OF ANNEALED AND HARDENED TYPE 347 TUBING AT 550°F

FIG C-7 ELASTIC AND INELASTIC COLLAPSE OF ANNEALED STAINLESS STEEL TUBES AT ROOM TEMPERATURE

FIG C-8 COLLAPSE OF HARDENED STAINLESS STEEL TUBES AT ROOM TEMPERATURE

FIG C-9 COLLAPSE PRESSURE vs $\frac{T}{R_0}$ (TANGENT MODULUS CRITERIA)

FIG C-10 COLLAPSE STRESS vs $\frac{T}{R_0}$ (TANGENT MODULUS CRITERIA)

FIG C-II VARIATION IN COLLAPSE PRESSURE WITH CHANGES IN L/D

MATERIAL - TYPE 304 STAINLESS
O.D. - 1.9 CM
WALL THICKNESS - 0.124 CM
THICKNESS TO OUTSIDE
RADIUS RATIO - 0.13

MATERIAL - TYPE 304 STAINLESS
O.D. - 1.9 CM
WALL THICKNESS - 0.05 CM
THICKNESS TO OUTSIDE
RADIUS RATIO - 0.05

FIG C-12 VARIATION OF ACTUAL FROM PREDICTED ROOM TEMPERATURE COLLAPSE VALUES FOR 3.175 cm ANNEALED TUBING

FIG C-13 COLLAPSE OF ANNEALED STAINLESS STEEL TUBING AT ROOM TEMPERATURE

FIG C-14 COLLAPSE TESTED SAMPLES-THIN WALLED TUBING
WITH SIMULATED FUEL SUPPORT

MAXIMUM EXTERNAL PRESSURE

SAMPLE NO. 10AW-15

0.010" WALL THICKNESS

5000 psi

Contains C-2 and C-4 Mandrels

SAMPLE NO. 10CW-4

0.010" WALL THICKNESS

5000 psi

Contains C-2 and C-4 Mandrels

1/64 x 30°

1/8" FLAT - 1/64" DEEP

DIAMETRAL CLEARANCE:

.010"

.007"

.004"

.005"

.018"

.013"

.016"

.014"

1/16 x 45°

TYPE C MANDREL

TYPE C MANDREL

FIG C-15 COLLAPSE TESTED SAMPLES-THIN WALLED TUBING
WITH SIMULATED FUEL SUPPORT

FIG C-16 COLLAPSE TESTED SAMPLES-THIN WALLED TUBING
WITH SIMULATED FUEL SUPPORT

FIG C-17 COLLAPSE TESTED SAMPLES-THIN WALLED TUBING CONTAINING
COMPACTED UO_2 POWDER

FIG C-18 COLLAPSE TESTED SAMPLES-THIN WALLED TUBING
CONTAINING COMPACTED UO_2 POWDER

<u>SECOND SERIES</u>	<u>SAMPLE NO.</u>	<u>WALL THICKNESS</u>	<u>CONTENTS</u>	<u>MAXIMUM EXTERNAL PRESSURE</u>

	10AW17	0.010 inch	UO_2 Powder at 83.7% T.D. and 1" long helical spring 0.040" wire diameter	5000 psi

	10AW18	0.10 inch	UO_2 Powder at 83.3% T.D. and 1" long 0.015" wall thickness split sleeve	5000 psi
<u>THIRD SERIES</u>

	10AW31	0.010 inch	UO_2 Powder at 86.4% T.D.	5000 psi

	10BW5	0.010 inch	UO_2 Powder at 85.3% T.D.	5000 psi

DATE: 5-18-62

EXTERNAL PRESSURE 5000 PSI

ROOM TEMPERATURE

SAMPLE NO. 10-BW-10

WALL THICKNESS 0.010"

Contains 0.477" Dia. and Sleeve 5-1/32" Long, 0.478" Dia. x 0.015" Wall

Mandrel

Sleeve

SAMPLE NO. 10-AW-35

WALL THICKNESS 0.010"

Contains UO_2 Powder at 88.0% T.D.

SAMPLE NO. 10-AW-36

WALL THICKNESS 0.010"

Contains UO_2 Powder at 85.0% T.D.

SAMPLE NO. 10-AW-37

WALL THICKNESS 0.010"

Contains UO_2 Powder at 86.0% T.D.

FIG C-19 COLLAPSE TESTED SAMPLES-THIN WALLED TUBING
CONTAINING COMPACTED UO_2 POWDER

FIG C-20 RESULTS OF COLLAPSE TESTS ON A SPRING SUPPORTED THIN WALLED
STAINLESS STEEL TUBE

D. Projection Welding

1. Introduction

The current trend to larger reactors is continuously broadening the gap between fuel rod cluster requirements and the fabrication techniques available. As fuel clusters increase in length, the design of intermediate spacers for dimensional control becomes more difficult. In addition, the increased popularity of various forms of fuel management indicates the need for more rods per cluster in order to minimize the number of clusters to be moved, thus minimizing reactor down time.

The brazing process has been used with reasonable success in the past but as clusters increase in size, the fixturing problems and furnace requirements become severe and can lead to long cycle times in order to prevent serious fuel rod distortion. As an alternate to brazing, various mechanical wire wrapping methods have been devised; however, these fail to provide the same dimensional control and stiffness offered by spacers which are metallurgically bonded to the fuel rod cladding. In some cases, notably Zircaloy clad fuel rods, short clusters which substitute end plates for intermediate spacers are joined in series to attain the desired active core length. The practice is uneconomical from the fabrication standpoint and sometimes increases the core peaking factors.

2. Program Objectives

In order to develop a joining method which would be adequate and economical, the following basic objectives were established for a resistance welding method.

a) The method must be applicable for hexagonal and square arrays of tubes in a wide range of diameters and wall thicknesses, either empty or filled with pellets or compacted fuel powders.

b) The joint quality should be reproducible, independent of the number or length of fuel rods in an assembly and capable of easy inspection and repair.

Joint failures must result in weld nugget pull-out from the spacer material rather than the tube wall.

c) Weld penetration into the tube wall must be limited to not greater than 50% of the thickness of the wall.

d) The process must not cause distortion or indentation which will result in more than 0.001 inch closure of the tube ID. This restriction of the ID would prevent subsequent loading of fuel pellets.

e) The fuel rods must be capable of differential thermal expansion typical to that encountered in water reactors without causing weld failures.

f) The method must be applicable to the welding of work hardened cladding without losing the design advantage of high allowable strength values, i.e., annealing must be restricted to a very small region at the weld joint so that the ability of the assembled structure would not be impaired.

g) Coolant flow blockage by the spacers must be minimal and comparable to that for other type spacers currently in use in water reactors.

In order to meet the above requirements, a wide variety of welding techniques was considered. Projection welding of transverse wire spacers across rows of fuel rods was selected as the approach which would most likely fulfill the above objectives. The basic requirement of minimum heating of the assembly could be met without difficulty since projection welding localizes heat to the immediate joint regions. In addition, welding could be accomplished in very short welding times - on the order of one to five cycles, and adequate dimensional control of the cluster could be obtained by fixturing the fuel rods. Accurate control of welding could be obtained by fixturing the fuel rods. Accurate control of welding current and electrode force would maintain a uniform setdown of wire spacers onto the fuel rods.

The fabrication method developed on the program involved projection welding straight wire spacers transversely to single rows of fuel rods or tubes to form

subassemblies. The subassemblies were progressively assembled and projection welded to form the complete cluster.

3. Program

a. Summary of Early Efforts

Several wire spacer designs were evaluated on the basis of ease of assembly and economics. The designed shapes which included a saw tooth flat wire, a curved wire, wire fins and disk washers are shown schematically in Figure D-1. The saw tooth, fin and washer shapes were not considered practical and the designs were immediately abandoned. Attention was then centered on the sinusoidal wave-formed wire which provided a position seating location for each rod during welding, thus simplifying the required fixturing. However, it was found that the cost of manufacturing the spacers was economically unattractive.

The possibility of using straight wires as spacers had been considered early in the program, but calculation had indicated that the welding pressures necessary would cause severe deformation of the wires. A further analysis established that the high welding pressures were not inherent with the use of straight wires but were due to dimensional mismatch between mating components. It was shown experimentally that the use of precisely dimensioned spacers reduced the electrode force from several hundred pounds to less than 100 pounds and it was on this basis that the straight wire spacer concept was selected for the fabrication development of fuel rod clusters.

1) Cluster Fabrication Studies

The first fuel bundle fabricated was a 16-tube cluster filled with MgO to simulate the fuel. The cluster was fabricated on a triangular pitch by projection welding one tube at a time to evaluate the performance of electrode tooling and fixturing as well as quality of the welds, Figure D-2. The results of these tests were highly satisfactory and indicated a capability for fabricating larger clusters of rods.

A second larger cluster of 37 stainless steel tubes, one-foot in length, was constructed by projection welding stainless steel wire spacers across rows of tubes filled with MgO. The tubes were 0.500 in. OD by 0.035 in. wall and the wire spacers were 0.062 in. in diameter. A relatively high electrode force of 125 pounds required the use of steel shims between rows of tubes to prevent deformation of the assemblies. The appearance of this cluster is shown in Figure D-3.

Up to this time, welding had been performed on tubes filled with MgO powder to simulate the fuel. However, it was desirable to investigate projection welding of unfilled tubes in the event that it was more expeditious or convenient in the fabrication of clusters to fill the tubes following welding. Several 37-tube clusters, one foot in length of Type 304 stainless steel were fabricated using electrode forces ranging from 12 to 50 pounds. Evaluation of these clusters indicated that weld nugget penetration was always greater in the wire spacer than in the tube wall. Overall inconsistent weld quality was observed with the lower electrode forces; this was attributed to the design electrodes which did not operate efficiently with low electrode forces.

In view of the poor welding results and the physical design of the cluster, an indirect welding method was developed. In this method the electrodes were arranged in such a manner that the current flow was through the wire spacer and out of the tube on the same side where the wire was located. The tooling consisted of nine indirect welding electrode arrangements positioned side by side in a single integrated holder. The spring loaded, movable center electrode maintained pressure on the spacer wire while two blade-shaped outer conductors acted as contacts to limit current flow in the wall of the tube. The welding current flowed through the center electrode causing fusion at the tube-wire spacer interface and then divided between the two conductors located on either side of the electrode. This arrangement prevented overheating of the tube by

providing two current paths and thereby reducing the current density in the tube. This resulted in the location of the greater portion of both the weld nugget and heat-affect zone within the wire spacer rather than the tube wall. Because of the close spacings between tubes, the electrode sides were insulated to prevent current shunting through adjacent tubes. Since the welding current into and out of the joint was accomplished by the upper tooling, the lower tooling or fixtures was fabricated from insulating material profiled to accurately position a row of tubes for welding and to provide support for the accumulated electrode force.

One of the most important requirements for the satisfactory welding of tube-to-wire spacer joints was rapid follow-up of the electrode during the weld cycle. Very low electrode forces on the order of 5 to 15 pounds were necessary to produce high quality welds. These forces were sufficient to maintain proper electrical contact and light enough to prevent indentation of the tube wall at the weld joint.

The mass of the electrode was limited to 0.8 pounds to prove a set-down^{*} of 0.008 in. with a 5-pound electrode force. A low mass electrode also minimized friction of the moving parts of the center electrode during the follow-up portion of the weld cycle.

2) Optimization of Welding Techniques

The first welding experiment with the new fixturing and electrode configuration were performed on MgO-filled, Type 304 stainless steel tubes with a 0.035 in. wall thickness. Weld settings were developed for layers of four, five, six and seven rods each. Metallographic sections were taken from each layer welded to study the size and shape of the weld nuggets, the percentage wall penetration, and the reproducibility from tube to tube. After the settings were developed, a cluster assembly was fabricated to determine the adequacy of the toolings and

* Deformation of the wire at the weld joint due to expulsion of molten weld metal.

fixtures. An appreciable amount of current shunting in the fixtures was noted and resulted in non-reproducible assembly welds. All fixtures were re-machined to improve dimensional control and insulated to prevent current shunting during subsequent welding.

With the above fixturing improvements a cluster assembly was fabricated into a hexagon of 61-rods with a nine-rod central row (Figure D-4). This is the maximum size hexagonal cluster which can be assembled with the currently available tooling. No difficulties were encountered with the cluster of 61 MgO filled tubes, consequently the problem of welding empty tubes into hexagonal and square lattices was studied and techniques were developed for welding both 0.020 in. and 0.028 in. tube wall thicknesses into hexagonal and square clusters containing up to 61-rods.

In order to demonstrate that the projection welding process was insensitive to cluster length, two long clusters were fabricated. The first cluster was a 60 in. and the tubes were filled with MgO powder as shown in Figure D-5. A second cluster, 84 in. in length was assembled with unfilled stainless steel tubes with a wall thickness of 0.020 in. No difficulties were observed in fabricating these clusters.

During this period, a limited amount of work was performed on the fabrication of a one-foot long hexagonal cluster with 61 Zircaloy-2 tubes, 0.500 in. OD by 0.028 in. wall thickness, using 0.062 in. diameter wire spacers. The method used in fabricating the subassemblies and cluster was substantially the same as that for the stainless steel clusters. However, the duration of the welding current was limited to two cycles, which was believed to be of sufficiently short duration to eliminate the necessity for inert gas protection. Brittle and inconsistent quality welds were obtained, and were attributed chiefly to a heavy oxide coating on the as-received wire. The wire was subsequently cleaned; somewhat higher quality welds were obtained. Further effort on the projection welding of Zircaloy assemblies was continued during the later phase of

the program; these results are included in another section of the report.

b. Evaluation of Projection Welded Clusters

1) Introduction

This phase of the program was aimed at evaluating individual welds and the welded clusters with respect to mechanical strength, vibration characteristics, differential thermal elongation, corrosion, and pressure drop. The mechanical strength evaluations were based on the ability of the individual welds to withstand a variety of stress patterns.

In addition to stresses induced by externally applied loads, there are stresses which are introduced as a result of differential thermal elongation of different elements within a cluster during in-reactor operation. This problem becomes more critical as the length of the cluster increases. It was earlier proposed that projection welded clusters had, as one of their advantages, a greater ability to absorb differential thermal elongations without damage than other cluster designs which were available. Part of the testing program was devoted to an experimental demonstration of this performance.

The vibration testing phase of the program was conducted to determine the performance of the projection welded cluster under adverse vibration conditions. Along with vibration testing, an evaluation of the pressure drop across a typical bundle was obtained. The principal characteristic studied was the degree to which the spacer wires contribute to water flow restriction.

A corrosion test was performed on sections of a Zircaloy cluster which was projection welded in air to determine whether interstitial pickup had adversely affected the corrosion resistance properties of Zircaloy to high temperature water.

2) Mechanical Strength

This phase of the program involved destructive testing of the tube-to-wire spacer joints. Since the clusters were representative models and were not related to specific design, the individual weld strength data could be applied to

any configuration of tubes and spacers. With knowledge of the shear, tensile, and torsional resistance of a joint, a stress analysis of a specific cluster can be performed utilizing anticipated design loadings. It was found that the structural soundness of the projection welded clusters was not limited by the strength of the welds.

The initial shear testing apparatus used to evaluate the stainless steel weldments from the hexagonal cluster proved inadequate for the more flexible square clusters due to bending of the wires prior to failure of the welds. For this reason, a second fixture, shown in Figure D-6, was employed for testing the stainless steel square and the Zircaloy hexagonal samples. The load required to shear a single weld in each sample tested is included in Table D-I.

Tensile testing of the welds was first performed in a fixture where the tube was lifted directly off the wire by a holder fitted to the tube while the sample was held in position by a yoke. The rigidity of the stainless hexagonal clusters was sufficient to cause weld fracture without appreciable bending of the wires; the results are tabulated in Table D-II (a). Because of the greater center-to-center spacing in the square clusters the same technique could not be used, since flexibility of the wires caused the load to shift to a tensile stress on the wires themselves resulting in a straight tensile failure of the wire. The tensile values reported for the stainless square and Zircaloy hexagonal samples are thus indicative of minimum loads below which failure did not occur, but are lower than the ultimate strength of the weld.

The second method used for tensile testing the welds is shown in Figure D-7. In this method the fixture consisted of a pair of jaws to hold the wire while the weld was subjected to a tensile load. This resulted in transverse extrusion of the wire through the jaws prior to failure of the weld. No reliable technique for evaluating the ultimate tensile strength of the welds was developed. The listing in Table D-II (a) indicates the loads below which failure did not occur.

TABLE D-ISHEAR TEST

<u>Type Cluster</u>	<u>Sample No.</u>	<u>Load (lbs)</u>	<u>Ascending Order of Load</u>	<u>Area (in.) of Weld</u>	<u>Ascending Order of Area</u>
SS-hex	1	128	1	2.51×10^{-3}	1
SS-hex	2	189	4	3.12×10^{-3}	4
SS-hex	3	177	2	4.09×10^{-3}	6
SS-hex	4	194	5	3.52×10^{-3}	5
SS-hex	5	210	6	2.86×10^{-3}	2
SS-hex	6	180	3	2.98×10^{-3}	3
SS-square	1	202			
SS-square	2	206			
SS-square	3	205			
Zirc-hex	1	160			
Zirc-hex	2	114			
Zirc-hex	3	93			

TABLE D-II(a)TENSILE TEST

<u>Type Cluster</u>	<u>Sample No.</u>	<u>Load (lbs.)</u>
SS-hex	1	100
SS-hex	2	138
SS-hex	3	108
SS-square	1	116*
SS-square	2	79*
SS-square	3	126*
SS-square	4	127*

TABLE D-II(b)TORSION TEST

		<u>Torque (in.-lbs.)</u>
SS-hex	1	3.52
SS-hex	2	1.76
SS-hex	3	1.76
SS-square	1	6.88
SS-square	2	3.88
SS-square	3	8.80
Zirc-hex	1	2.01
Zirc-hex	2	1.76
Zirc-hex	3	3.52
Zirc-hex	4	4.94

* These values are reported as values under which the weld did not fail as the samples failed in the wire due to bending before the weld failed.

The third test in the program for determining the mechanical strength of the welds was a torsion test. A torque was applied to the tube with the wire fixed in the manner illustrated in Figure D-8. The highest torque applied before weld failure was recorded. The technique used to twist the welds was simple but quite reliable. The samples were cut so that each was a tube with a segment of the wire still welded in place as in the cluster; the wire was supported in a vertical position and the tube in a horizontal position. A series of calibrated weights was then suspended from the tube at a length of four inches. Knowing the moment arm and the weight required to cause failure the torque was calculated; these results are reported in Table D-II (b). During this test, a dial indicator was used to record the distance that the tube was depressed with each addition of weight. In every case, the weld exhibited sufficient ductility and the rotation before failure was greater than would occur under design conditions.

3) Loop Test

One of the criteria used in the program to evaluate the projection welded clusters was to determine the pressure drop associated with the wire spacers in the assembly. This characteristic is especially important when considering systems utilizing natural circulation of coolant. Pressure losses due to the wire spacers were measured for various flow conditions.

A second objective of the evaluation program was to determine if an analytical technique could be found which would predict the pressure losses for projection welded clusters of other geometries. Measurements were taken for both single-phase water and two-phase air-water conditions.

a) Single-Phase Flow

A flow loop was constructed which contained a vertical test section made of plexiglass into which the test cluster was mounted. A photograph of the test assembly is shown in Figure D-9. Pressure taps were located to measure the pressure difference between two points within the test section and to span three

banks of spacer wires. After measuring a pressure drop for the full range of attainable flow conditions, the assembly was removed from the loop and four additional banks of spacers were added to the cluster at the midpoint between the planes of spacers already present. The cluster was reinstalled in the loop and pressure drop readings were taken for the same tap locations and under the same flow parameters with the seven planes of spacers in place. Any increase in the readings was attributed directly to the spacers themselves. The friction losses and the elevation differences between taps were equal for both set-ups and independent of the number of spacer banks between the taps.

The cluster for the first phase of the test was seven feet long, contained 61 fuel tubes, and was of the hexagonal type. The tube diameter was 0.500 in., the wall thickness was 0.020 in., the spacer wire diameter was 0.062 in., and the center-to-center distance between tubes was 0.625 in. Two pressure taps were located in the tube wall and one was mounted in the plexiglass housing. The variation in pressure tap readings at each location was less than 7.5% in all cases.

Flow rates ranged from 200 gpm to 600 gpm and were varied in 100 gpm increments. The velocities associated with these flow rates were from 6.2 to 18.6 ft/sec, and the Reynold's numbers ranged from 2.58×10^4 to 1.25×10^5 using water temperatures of 85°F and 150°F. The velocities for this test are commensurate with current reactor designs, but temperature limitations prohibited the attainment of Reynold's numbers as high as the 4.0×10^5 upper limit encountered in reactor systems. The test results obtained did, however, cover a range sufficient for application to reactor conditions with little extrapolation error. The analytical data are reported in Appendix I.

Conclusions based on the experimental data presented in Appendix I are:

- (1) Loss coefficients calculated on an area restriction basis do not compare with measured values.

- (2) The results obtained from the present work indicate that the drag coefficient method can be used to predict pressure drops for other projection welded clusters of this type with different tube, wire and spacer geometries. Loss coefficients do not correlate well between clusters of different geometries.
- (3) The friction factors published by Moody¹⁷ are lower than the values obtained in the present tests.
- (4) Concerning steam-water conditions, the closeness in magnitude of the drag coefficients for single-phase water and air-water data indicates that single-phase drag coefficients can probably be used to predict pressure losses for steam-water, two-phase flow.

(b) Vibration

In order to evaluate the vibration characteristics of the fuel rods in flowing water, the projection welded cluster used for pressure drop tests was instrumented with accelerometers. Two accelerometers were placed inside a tube to measure the frequency and amplitude of the tube vibrations midway between planes of spacer wires. A second objective of this evaluation was to provide a means by which the natural frequency of a projection welded cluster could be predicted. A correlation of the experimentally determined natural frequency with predicted frequencies using two different assumptions was made.

The test cluster consisted of 61 fuel tubes, seven feet long, arranged in a hexagonal pattern. The tube diameter was 0.500 inch and the wall thickness was .020 inch. The wire spacers were in banks 20 inches apart and were of .062 inch diameter wire, yielding a center-to-center distance for the tubes of 0.625 inch. The tubes were filled with lead shot to simulate the mass characteristic of the fuel. Two Columbia Model 606 Piezo-electric Accelerometers were mounted in an

epoxy pellet to maintain a 90 degree orientation with each other. This arrangement is shown in Figure D-10. The pellet was placed in a position within the tube midway between two spacer banks to experience the greatest amplitude of vibration. The accelerometer leads were connected to two separate Columbia, Model 4000, low-noise, high-impedance, cathode followers and then to an oscilloscope for read-out.

Both solid-water and air-water vibration measurements were taken for conditions shown in Table D-III, which is a summary compilation of the data obtained. Photographs of the oscilloscope pattern were used to record the amplitude of the acceleration and the frequency of vibration. Figure D-11 shows a typical pattern obtained from single-phase water in one case and air-water mixtures in the other. The curves obtained from solid-water exhibited a primary vibration mode onto which a secondary frequency was superimposed. In the case of air-water mixtures, there were no low frequency modes evident, and the higher frequency mode was used in the calculations. Apparently, the magnitude of the secondary mode was high enough to obscure the appearance of the primary. A detailed discussion of the experimental procedure is included in Appendix II.

4) Differential Thermal Elongation

A representative stainless steel cluster of 37 tubes, seven feet long, and 0.500 inch diameter with a .020 inch wall thickness was used for differential thermal elongation tests. The tube arrangement was hexagonal with a spacing between centers of the tubes of 0.625 inch; the spacer wires were .062 inch diameter. Figure D-12 is a schematic diagram of the apparatus used to test the cluster. Figure D-13 is a photograph of the apparatus showing the vertical position of the cluster in test. The equipment circulated hot and cold water through a predetermined pattern of tubes. Solenoid valves automatically altered the flow pattern at fixed intervals such that tubes became alternately hot and cold in an effort to simulate repeated start-up and shut-down response in reactor. The

TABLE D-III

DATA FOR VIBRATION TEST

Velocity*** ft/sec.	Air Flow (CFM)	Void Fractions	Frequency CPS		Acceleration* (g's)	Deflection** y (in.)
			Primary	Secondary		
6.3	-	-	125	750	.13	<.0001
10.8	19.4	.42		1470	.29	
15.3	38.1	.59		1730	.45	
19.4	55.8	.68		1300	.45	
9.4	-	-	133	600	.13	<.0001
13.9	18.9	.32		1300	.41	
17.9	36.0	.53		1000	.51	
21.8	52.4	.57		800	.82	
12.6	-	-	133	700	.12	<.0001
16.6	17.7	.25		1500	.49	
15.4	-	-	125	1100	.13	<.0001
18.6	-	-	125	1200	.14	<.000086

* Accelerations in table above for solid water readings result from primary frequency. Accelerations for air-water readings result from secondary frequency.

** Deflection is for primary frequency; amplitudes from secondary frequency are negligible.

*** Based on a slip ratio of unity.

temperature difference for this test was 23°C (42°F); the test was run for 3,000 cycles. (The particular pattern chosen for the test provides for a 23°C (42°F) ΔT between each parallel row of tubes. This condition induced the maximum amount of stress in the welds.) The ΔT used in the test exceeded that expected in typical water reactor designs and was intended to develop stresses up to the failure point based on previous mechanical failure tests performed on these joints.

The support of the cluster was such that the tubes rested in the manifold at the bottom and were free to elongate in the upward direction. Measurement of the amount of actual tube elongation was accomplished by use of a dial indicator, as shown in Figure D-14, to monitor the vertical motion of the spacer wire on the outside of the cluster. Thermocouples were placed along the length of six tubes for temperature monitoring and control.

Fractures were observed after 2,000 temperature reversals at weld positions in the middle bank on the periphery of the cluster. From this point, to the end of the test, successive failures were observed at other peripheral positions and in other banks of the cluster. The test was stopped at 3,000 cycles, as planned, and disassembled for a detailed inspection. The 2,000 cycles required to caused failure is at least an order of magnitude greater than the anticipated number of cycles for one core lifetime in a typical power reactor.

During the test, the longitudinal motion of a spacer wire on the outside of the top bank was measured by a dial gage to be .025 in. Bowing was also observed, but was difficult to measure accurately. Feeler gages did indicate that the bowing caused a .006 to .009 in. difference in tube-to-tube spacing.

The stresses induced by the elastic resistance of the spacer wires to the differential elongation of the tubes during cycling were calculated. The three important stresses which were considered included bending stresses in the wire, shear stresses in the wire and shear stresses in the welds. A

number of calculations became necessary as a result of the different stress conditions at various welds in the cluster. The various cases considered are described below:

Case I - All inside tubes - supported by two clamped wires.

Case II - Tubes supported by two cantilever wires.

Case III - Tubes supported by one cantilever and one clamped wire.

Case IV - Tubes supported by one clamped wire.

The mathematical model used to define the system was based on a vertical series of elastic tubes held in place by horizontal banks of wires which were welded to the tubes. It was assumed that the manifolds at each end of the cluster did not restrict the tubes* and that the elongation occurred away from the tube centers. The first and second banks away from the center were treated independently; however, no differentiation was made with respect to location of a given bank above or below the center. The weld joints were considered as rigid supports that clamp the wire without angular deflection. A more complete description of the assumptions made is included in Appendix III along with the details of the calculations. The fracture patterns observed were consistent with that predicted by the stress analysis. The tendency for the peripheral tubes to fracture more readily than the inside tubes resulted from the different clamping conditions on the wires. Table D-IV contains a compilation of the stresses calculated for the system. The stresses in the second bank were higher than those in the first bank; the middle bank was assumed to have zero stress. As shown above, the incidence of fractures was greater in the second bank than the first bank and a few fractures on the peripheral tubes were observed in the middle bank. The fractures in the middle bank were attributed to binding of the tubes in the manifolds. Binding has been indicated on the basis of discrepancies between predicted and measured tube elongations.

* It will be shown later that binding did occur.

TABLE D-IV

COMPILATION OF STRESSES

Case	Bank	τ_{Wire} (psi)	τ_{Weld} (psi)	ρ_o (lbs.)	σ_f (psi)
I	1	12,600	23,900	76	24,400
	2	17,000	32,400	103	33,000
II	1	4,500	8,490	27	34,700
	2	7,670	14,500	46	59,200
III*	1	16,200	30,500	97	31,100
	2	24,500	46,200	147	47,200
III**	1	2,200	3,800	12	15,400
	2	2,860	5,780	18	23,100
IV	1	15,700	29,900	95	30,500
	2	24,500	46,500	148	47,500

* Applies to Clamped Wire

** Applies to Cantilever Wire

τ_{Wire} = Shear Stress on the Wire due to Axial Loading of the Tube

τ_{Weld} = Shear Stress on the Weld due to Axial Loading of the Tube

ρ_o = Shear Force on Weld due to Axial Load on the Tube

σ_f = Fiber Stress on Wire due to Bending.

Binding can affect the calculated stresses in the following two ways:

a) Binding at the top and bottom introduces a new model in which the tube is fixed at both ends. Resultant bowing on thermal expansion induces shear loads on weld joints.

b) A tube fixed at one end will exhibit growth away from that end rather than away from its center as assumed in the model.

It was concluded that weld fractures on peripheral tubes in the middle bank resulted from stresses induced by binding.

A comparison of the static strength of the welds was made for samples before and after thermal cycling. In the case of shear testing, the pre-test failure loads ranged from 128 to 210 lbs. and the post-test failure loads ranged from 117 to 144 lbs. This would indicate that gross changes in the static shear values did not occur as a result of the test. It is significant that failures did not occur in the first few cycles even though some of the calculated loads were in the range known to have caused fractures in laboratory tests. The occurrence of fractures after 2000 cycles can be attributed to fatigue. This failure contention is supported by the metallographic results which showed cracks starting at stress risers and propagating through the weld joint. Those fractures that were in the wire rather than in the weld were probably due to embrittlement of the wire in the region of the weld.

The thermal cycle test produced weld failures after 2000 temperature reversals at a tube-to-tube thermal gradient value three times that anticipated in a conventional power reactor design. The pattern of weld failures is consistent with that predicted by the analysis of stresses in the system during cycling. The number of cycles at which failures were first observed is approximately an order of magnitude greater than that anticipated during one core lifetime.

A second projection welded stainless steel cluster was tested for 3000 cycles with a tube-to-tube thermal gradient of 20°F which is 1.3 times

that anticipated in power reactor designs. These results are presented in another portion of the report.

5) Metallography

Two hundred and thirty-two of the 330 welds in the 37-tube thermal cycled cluster were examined visually and metallographically. Fractures were observed on 114 welds, whereas 118 welds were intact. Transverse fractures through the wire were observed in some failures, whereas other fractures were through the weld region in a direction parallel to the tube-wire interface. The latter fracture type was the most prevalent and occurred in two distinct modes. The first mode is shown in Figure D-15, which is a view of the wire showing the fracture which was started at a shrinkage void or "pipe" in the expelled metal portion of the weld. Figure D-16 shows a fracture which was started at a shrinkage void or pipe in the expelled metal portion of the weld. Figure D-17 shows one of these "pipes" in a section taken transverse to the wire. The cracks at the corners of the weld nugget were probably induced by localized stress risers. Cracks in the base metal of the wire may be associated with excessive arcing during welding.

Fractures through the weld wire are typical of those shown in Figure D-18. These fractures were of a brittle nature and originated at the boundary between the expelled metal and the base metal of the wire. Figure D-19 is another view of the condition shown in Figure D-18 and illustrates the propagation of the crack through the wire.

The distribution of fractures through the cluster fell into a reasonably well-defined pattern. This pattern is typical by the abundance of fractures on the end banks of wires. Of the 47 welds examined at the middle bank, only five failed. The top bank had 41 failures, whereas the bottom bank exhibited 37 failures. In addition, a greater abundance of failures was observed on peripheral welds than those in the middle of the cluster. These observations are

shown to be consistent with the stress analysis discussed in Appendix III.

6. Zircaloy Welding

Because of the rapid heating and cooling cycle experienced with the projection welding technique, it was considered possible that Zircaloy could be welded in air without excessive contamination of the weld zones. A technique such as this would result in impressive cost advantages over the conventional dry box welding technique. A cluster was welded in air to determine the extent of contamination and to appraise the basic feasibility of the process.

The cluster used for the test was fabricated from Zircaloy tubes .500 in. O.D. with a .028 in. wall thickness. The spacer wires were .062 in. in diameter and the lattice was hexagonal. Corrosion samples of both the weld area and adjacent tubing outside the heat affected zone were autoclaved. Both types of samples were cleaned and pickled in an acid containing $3\frac{1}{2}\%$ HF, 39% HNO₃, and 57% H₂O by volume. Three mils were removed from the surface. The conditions in the autoclave were 750°F steam at 1500 psi. Twelve samples containing welds and six without welds were suspended on stainless steel wire and hung on a stand. The autoclave conditions were maintained for 14 days at which time a visual inspection revealed the appearance of a small amount of white corrosion product surrounding the weld zone. Two welded samples and one plain sample were removed and the remainder left in the autoclave. The autoclave was operated at 750°F and 1500 psi for an additional 14 days. All of the samples were then removed for analysis.

The samples exhibited a shiny black oxide coating except for the heat-affected zone surrounding the welds. This annular shaped region was coated with non-adherent white ZrO₂. The outside diameter of this white area was measured as one eighth of an inch and the inside bordered the weld which is somewhat elliptical with typical major and minor axes of .080 in. and .055 in., respectively.

In order to observe corrosive penetration into the wall of the tube, two

samples were sectioned and mounted for metallographic inspection. The top picture in Figure D-20 reveals oxidation of the weld metal which could result in premature weld failure. The lower picture on the same page illustrates the "cratering" which took place adjacent to the weld. The plane of the section is beyond the weld and intersects the region of white corrosion product. As shown in the figure, the difference in wall thickness at the crater base on the tube wall away from the weld is .0022 in.

In addition to the visual and metallographic examinations, a weight gain analysis was run; Table D-V lists the data obtained. Samples four and five were those randomly removed after 14 days and sample eight was one which dropped to the bottom of the autoclave at some unknown time during the test. The values shown in Table D-V for the unwelded tubes are comparable to those predicted by published values.¹⁸ The values obtained for the welded samples are statistically lower than those for the unwelded tubes indicating loss of some of the white corrosion product.

Reviewing the observed results and the difference in weight gained per unit area, it is concluded that the corrosion results of the Zircaloy cluster welded in air are not acceptable. Thus, it appears that a minimum of an inert gas blanket is required.

c. Process Improvements and Dimensional Control

1) Introduction

The final phase of the program was conducted with the following objectives:

- (a) To refine the projection welding method by further improvements in electrode tooling, cluster fixturing and welding techniques. Control of all process variables would lead to optimum quality welds and close dimension control of tube-to-tube spacings.

TABLE D-V
WEIGHT GAIN DATA FOR
CORROSION TEST OF ZIRCALOY CLUSTER

<u>Sample Number</u>	<u>Weight Gain Per Unit Area (gm/dm²)</u>	<u>Days Tested</u>
<u>Welded Samples</u>		
1	.0305	28
2	.0304	28
3	.0323	28
4	.0267	14
5	.0285	14
6	.0312	28
7	.0351	28
8	----	*
9	.0243	28
10	.0342	28
11	.0327	28
12	.0344	28
<u>Plain Tube Sections</u>		
1	.0353	28
2	.0357	28
3	.0360	28
4	.0355	28
5	.0284	14
6	.0355	28

* Sample dropped from test stand at some unknown time during test

- (b) To test and evaluate full size clusters fabricated from stainless steel or Zircaloy for differential thermal expansion behavior.

The program included the following activities:

- (a) Fabrication of eight subassemblies of stainless steel for evaluations of relative ductility of two wire compositions, weld quality and dimensional control of tube-to-tube dimensions. Each subassembly consisted of two rows with 17 tubes, approximately 18 inches in length and joined with 4 wire spacers.
- (b) Fabrication of a 37 tube hexagonal cluster, 7 foot in length of stainless steel, by the improved method for thermal testing and destructive evaluations.
- (c) Fabrication of three subassemblies of Zircaloy-2 for evaluations of weld quality and dimensional control of tube-to-tube spacings, following by:
- (d) Fabrication of a 37 tube hexagonal cluster, 7 foot in length of Zircaloy-2 for thermal testing and destructive evaluations.

2) Program

Inadequate control of tube and wire spacer dimensions and surface cleanliness had been found as significant factors causing variable quality welds as well as poor control of tube spacing dimensions. The initial step on the program involved the establishment of the following requirements for tubes and wires:

- a) Type 347 stainless steel tubes, 1.270 cm OD by 0.051 cm wall, and spacer wire, 0.158 cm in diameter, were purchased in the bright annealed condition with a dimensional tolerance of ± 0.0013 cm (0.0005 in.) on the tube OD and on

the wire diameter. Closer control of dimensions would significantly reduce variations in the effective electrode force on the spacer wires leading to variable weld quality. As an example, if the tube OD is undersize, some of the electrode force is used in bending the wire into contact with the tube, resulting in a decrease in wire setdown and a reduction in spacing between adjacent tubes. The use of bright annealed tubes and spacer wires would prevent the formation of inclusions and gas voids in the weld nuggets due to surface contamination (oxides, drawing compounds, mill dirt).

b) As a target for this program, it was desired to maintain the 1.587 cm (0.625 in.) distance between tube centers to ± 0.008 cm (0.003 in.) while maintaining an over-all cluster envelope of ± 0.102 cm (0.040 in.) In order to meet these requirements, the cluster support fixture was reworked to permit control of tube-to-tube spacing to the desired ± 0.008 cm. Additional tie bars were included in the support fixture to provide a more rigid restraint of assembled tubes during welding. Accurately positioned spacer rods were inserted into both ends of the row of tubes to maintain the tube-to-tube distance.

c) Copper mandrels about 6.35 cm (2.5 in.) in length were inserted into the tubes to prevent indentation of the tube wall during welding. Indentation was limited to 0.003 cm (0.001 in.) maximum in order to allow subsequent loading of the tubes with closely dimensioned fuel pellets.

d) The previous differential thermal expansion test on a stainless steel cluster revealed that some failures had occurred after 2,000 cycles through an embrittled region of the spacer wire at the weld joint. Accordingly, subassemblies would be fabricated under identical conditions to determine whether embrittlement could be attributed to the wire composition or to the welding.

procedure. Wires of Type 347 stainless welding composition,* used previously, and of nominal compositions* would be used in these evaluations. A controlled wave welder using a four cycle weld schedule was employed to reduce the time at welding temperatures and thereby reduce the heat effect on the wire.

A group of four stainless steel subassemblies (Figure D-21) were fabricated to evaluate the relative ductility of the two wire compositions as well as weld quality and dimensional control. The welder settings for the 8-tube and 9-tube rows are included in Table D-VI.

Visual examination at a magnification of 10X revealed severe electrode indentations of the wire spacers for approximately 10 percent of the weld joints. The indentations were due to slight misalignment of the electrodes which exerted an eccentric force on the wires. These weld joints exhibited excessive weld metal expulsion on the indented side of the wire spacer (Figure D-22). Metallographic evaluation revealed non-symmetrical welds with gross localized melting of the indented portion of the wire. Other weld joints which did not show wire spacer indentation contained symmetrical weld nuggets which were centrally located on the tube-wire interface. Weld penetration into the tube wall varied from 25 to 50% of the wall thickness. All welds examined were essentially free of injurious defects including voids, inclusions and fissures. There was no evidence of cracks in the tube wall or in the wire spacers adjacent to the welds.

* Composition of Type 347 Stainless Steel Spacer Wires

	<u>Nominal</u>	<u>Welding Type</u>
C	0.07%	0.07%
Mn	0.57	1.50
Si	0.42	0.30
Cr	19.14	20.00
Ni	8.58	10.00
Cb + Ta	0.83	0.80

TABLE D-VI

WELDER* SETTINGS FOR
TWO ROW SUBASSEMBLIES

8 - TUBE ROW
10 Pound Force on Each of 8 Electrodes

48% Initial Heat

1	Cycle	heat No. 1 - 32%	phase shift - 15%	slope rate
2	Cycle	heat No. 2 - 7%	phase shift - 10%	slope rate
1	Cycle	heat No. 3 - 5%	phase shift - 10%	slope rate

9 - TUBE ROW
10 Pound Force on Each of 9 Electrodes

53% Initial Heat

1	Cycle	heat No. 1 - 31%	phase shift - 15%	slope rate
2	Cycle	heat No. 2 - 7%	phase shift - 10%	slope rate
1	Cycle	heat No. 3 - 5%	phase shift - 10%	slope rate

* Sciaky Resistance Welder	Machine No. 8324
Model PMCO4STKX-200-36-30	
120,000 ampere maximum short circuit secondary current	

The wire spacers of the two compositions showed no loss in ductility after bending the weld heat-affected portions to an angle of 90°. This improved ductility is attributed to the use of a steeply peaked weld cycle in lieu of the previously uniform 5-cycle weld schedule. The coarse dendritic structure along with the shrinkage cavity or pipe within the expelled metal had been significantly reduced by the present welding technique.

Dimensional inspections at both ends of the two assemblies showed tube center-to-center spacings of $1.587^{+0.030}_{-0.025}$ cm (0.620 to $0.625^{+0.012}_{-0.010}$ in.), for tubes within a row. Due to skewing of the two rows, the diagonal spacing between centers of adjacent tubes varied from 1.575 to 1.618 cm (0.620 to 0.637 in.). Similar lateral misalignment or skewing of the tube rows was observed for all four subassemblies.

The fifth subassembly was fabricated following further modifications in tooling and fixturing to improve electrode-spacer wire alignment and to control tube center-to-center spacing to 1.587 ± 0.008 cm (0.625 ± 0.003 in.). Visual inspection showed that electrode indentation of the wire spacers had been essentially eliminated with a corresponding balance of expelled weld metal on both sides of the joint. Shrinkage voids or pipes, noted previously, were absent in the reduced volumes of expelled metal (Figure D-23). Dimensional measurements of center-to-center spacings for tubes lying in rows and between rows are shown below:

Tubes Within Row

First End	8-tube row	$1.587^{+0.008}_{-0.008}$ cm	$(0.625^{+0.003}_{-0.003}$ in.)
	9-tube row	$1.587^{+0.003}_{-0.008}$ cm	$(0.625^{+0.001}_{-0.003}$ in.)
Opposite End	8-tube row	$1.587^{+0.003}_{-0.008}$ cm	$(0.625^{+0.001}_{-0.003}$ in.)

9-tube row $1.587 \begin{smallmatrix} +0.008 \\ -0.008 \end{smallmatrix}$ cm ($0.625 \begin{smallmatrix} + 0.001 \\ - 0.001 \end{smallmatrix}$ in.)

Tubes Between Rows

First End $1.587 \begin{smallmatrix} +0.020 \\ -0.005 \end{smallmatrix}$ cm ($0.625 \begin{smallmatrix} +0.008 \\ -0.002 \end{smallmatrix}$ in.)

Opposite End $1.587 \begin{smallmatrix} +0.033 \\ -0.003 \end{smallmatrix}$ cm ($0.625 \begin{smallmatrix} +0.008 \\ -0.001 \end{smallmatrix}$ in.)

The data indicate that the dimensional requirement of 1.587 ± 0.008 cm (0.625 ± 0.003 in.) has been maintained for the center-to-center spacing of tubes lying within rows. However, lateral displacement between tube rows had caused the triangular pitch of tubes (center spacings) between rows to vary from -0.003 to -0.005 cm (-0.001 to -0.002 in.) to $+0.020$ to -0.033 cm ($+0.008$ to -0.013 in.) of the nominal value of 1.587 cm. Additional refinements were made in the cluster support fixtures to maintain the tube center triangular pitch dimension to 1.587 ± 0.008 cm.

Metallographic examination of longitudinal and transverse sections of randomly selected weld joints showed welds of suitable quality in relation to soundness, symmetry and penetration into the tube wall and spacer wires. Improvement in the surface cleanliness of tubes and wire spacer has resulted in welds which are essentially free from gas voids and inclusions. Shrinkage cracks of the type shown in Figure D-24, although undesirable, may have no significant effect¹⁹ on the strength properties of the weld joint on the basis of their orientation and location within the welds. Welds of this type have survived 2,000 thermal cycles.⁴ Figure D-25 illustrates uniformity of weld metal expulsion and depth of the fused nugget into the tube wall.

The results of dimensional measurements of tube center to tube center distances for the sixth subassembly are included in Table D-VII. The inspection data show that distances between adjacent tubes in both rows of the two subassemblies were maintained within the required range of 1.587 ± 0.008 cm (0.625 ± 0.003 in.). Spacings for the end tubes on both sides of the second subassembly were out of specification and it is probable that the unrestrained side tubes may have rolled during shipment.

A major emphasis was placed on additional reworking of the fixtures in order to eliminate lateral displacement or skewing of adjacent rows of tubes. Dimensional inspection of a subsequently fabricated 2 row-17 tube stainless steel subassembly showed that skewing had been eliminated. However, the corrective displacement of the rows of tubes had resulted in an isosceles triangular pitch where the diagonal tube to tube had resulted in an isosceles triangular pitch with the diagonal tube to tube spacings exceeding the maximum value of 1.595 cm (0.628 in.) by 0.008 to 0.013 cm (0.003 to 0.005 in.). Two approaches were considered for decreasing the spacing between rows of tubes.

- a) Increase wire spacer set down from 0.015 cm (0.006 in.) to 0.025 cm (0.010 in.) by increasing the electrode force on the weld joint.
- b) Reduce wire spacer diameter from 0.157 to 0.147 cm (0.062 to 0.058 in.)

The latter approach was used on the basis that the welding parameters would remain unchanged. The wire was cold drawn to 0.147 cm (0.058 in.) and bright annealed. In addition, the electrode faces were regrooved to accommodate the smaller diameter wire. The final stainless steel subassembly met the tube to tube spacing requirement of 1.587 ± 0.008 cm (0.625 ± 0.003 in.); all welds

TABLE D-VII

Dimensional Inspection of Tube-to-Tube Spacings

<u>TUBES WITHIN ROWS</u>		<u>First Subassembly</u>			<u>Second Subassembly</u>						
One End	8 tube row	1.587	$\begin{smallmatrix} +0.008 \\ -0.008 \end{smallmatrix}$	cm (0.625	$\begin{smallmatrix} +0.003 \\ -0.003 \end{smallmatrix}$	inch)	1.587 $\begin{smallmatrix} +0.000 \\ -0.008 \end{smallmatrix}$	cm (0.625	$\begin{smallmatrix} +0.000 \\ -0.003 \end{smallmatrix}$	inch)	
	9 tube row	1.587	$\begin{smallmatrix} +0.003 \\ -0.008 \end{smallmatrix}$	cm (0.625	$\begin{smallmatrix} +0.001 \\ -0.003 \end{smallmatrix}$	inch)	1.587 $\begin{smallmatrix} +0.003 \\ -0.008 \end{smallmatrix}$	cm (0.625	$\begin{smallmatrix} +0.001 \\ -0.003 \end{smallmatrix}$	inch)	
Opposite End	8 tube row	1.587	$\begin{smallmatrix} +0.003 \\ -0.008 \end{smallmatrix}$	cm (0.625	$\begin{smallmatrix} +0.001 \\ -0.003 \end{smallmatrix}$	inch)	1.587 $\begin{smallmatrix} +0.000 \\ -0.008 \end{smallmatrix}$	cm (0.625	$\begin{smallmatrix} +0.000 \\ -0.003 \end{smallmatrix}$	inch)	
	9 tube row	1.587	$\begin{smallmatrix} +0.003 \\ -0.003 \end{smallmatrix}$	cm (0.625	$\begin{smallmatrix} +0.001 \\ -0.001 \end{smallmatrix}$	inch)	1.587 $\begin{smallmatrix} +0.008 \\ -0.008 \end{smallmatrix}$	cm (0.625	$\begin{smallmatrix} +0.003 \\ -0.003 \end{smallmatrix}$	inch)	
 <u>TUBES BETWEEN ROWS (Diagonal Spacings)</u>											
One end		1.587	$\begin{smallmatrix} +0.020 \\ -0.005 \end{smallmatrix}$	cm (0.625	$\begin{smallmatrix} +0.008 \\ -0.002 \end{smallmatrix}$	inch)	1.587	$\begin{smallmatrix} +0.020 \\ -0.000 \end{smallmatrix}$	cm (0.625	$\begin{smallmatrix} +0.008 \\ -0.000 \end{smallmatrix}$	inch)
Opposite end		1.587	$\begin{smallmatrix} +0.033 \\ -0.003 \end{smallmatrix}$	cm (0.625	$\begin{smallmatrix} +0.013 \\ -0.001 \end{smallmatrix}$	inch)	1.587	$\begin{smallmatrix} +0.015 \\ -0.000 \end{smallmatrix}$	cm (0.625	$\begin{smallmatrix} +0.006 \\ -0.000 \end{smallmatrix}$	inch)

evaluated were of acceptable quality. The wire spacers at the weld joints exhibited satisfactory ductility as demonstrated by bending them to an angle of 90°. It was concluded that all important process variables involving welding and fixturing were controlled sufficiently for the fabrication of 2.13 meter (7 foot) long 37 tube hexagonal cluster of stainless steel tubes.

a) Thirty-Seven Rod Stainless Steel Cluster

A 37-tube cluster was fabricated using the modified fixturing and electrode tooling. The major emphasis was placed on dimensional control of tube-to-tube spacings within the rows as well as positioning of tube rows to avoid skewing. The welder settings are included in Table D-VIII.

The cluster was clamped in the support fixture during shipment to Combustion Engineering to prevent damage to the weldments. Results of evaluations are reported below:

(1) Non-Destructive Evaluation

Visual inspections of the 37 tube cluster showed electrode indentations in the wire spacers at several weld joints adjacent to the sides of the cluster. However, there was no visual evidence of cracked or incompletely welded joints. A detailed dimensional inspection was made of tube-to-tube spacings at both ends of a cluster. Measurements at both ends of the cluster are shown in the layout drawing in Figure D-26. An inspection of the dimensional data indicated that approximately 50% of the spacings did not meet the requirements of ± 0.008 cm. of the nominal value of 1.587 cm. These measurements are also plotted on a distribution chart, included in Figure D-27, to evaluate dimensional variations with respect to position in the cluster. The following observations are presented to explain the out of tolerance spacer dimensions:

- (a) Below Minimum Dimensions (1.549 to 1.579 cm or
0.610 to 0.621 in.)

These close spacings were observed for tubes located on either side of a

TABLE D-VIII

WELDER SETTINGS FOR 37 TUBE CLUSTER

<u>No. Tubes in Row</u>	<u>Phase Shift - %</u>			<u>Cycles</u>		<u>Electrode Force-lbs.</u>
	<u>Initial Heat</u>	<u>Heat 1</u>	<u>Heat 2</u>	<u>Heat 1</u>	<u>Heat 2</u>	
4	38	25	14	2	2	10
5	43	26	14	2	2	10
6	48	28	14	2	2	10
7	53	30	14	2	2	10

row of tubes and may be due to "rolling" or rotation of the tubes on the weld joint during handling and assembly into a cluster. A second reason may involve excessive manual torquing of the mandrels inside the tube causing rotation or "rolling" of the tubes. A third factor included skewing between tube rows.

(b) Above Maximum Dimensions (1.598 to 1.615 cm or
0.629 to 0.636 inch)

In all cases, excessive spacings were observed only in the diagonal directions as a result of skewing between rows of tubes.

A careful evaluation of the above data indicated that these variations in spacer dimensions would not seriously impair the performance of the cluster during differential thermal elongation tests. It is noted that the spacer dimensions are "built-in" during fabrication rather than developed during subsequent operations. Also, a major number of the out-of-dimension spacings are located in the outer peripheral region of the cluster where the thermally induced stresses would be developed to a lower level than in the interior of the cluster. Finally, a reasonably high structural integrity was expected of the cluster on the basis of the satisfactory appearance of the weld joints.

(2) Thermal Test

The earlier differential thermal elongation test was terminated after 3000 temperature reversals with a tube-to-tube temperature differential of 23°C (42°F). The temperature differential was greater than twice that anticipated in a water-cooled power reactor and the number of cycles achieved prior to the first visual indication of weld failures was almost ten times that expected for one core lifetime. The causes of weld failures were attributed to cyclic stresses induced by thermal elongation, poor weld quality and binding of the ends of tubes in the manifolds which altered the type of restraint on the system.

A second test was conducted on another 37 tube cluster under thermal conditions which were more representative of actual reactor operating

conditions.* This cluster was the last one fabricated in the program and incorporated all of the developed features noted above. The test apparatus was redesigned to eliminate binding of the end fittings by use of plastic tubing between the manifolds and ends of tubes of the cluster. The apparatus contained hot and cold water lines which channeled the water alternately through rows of tubes in the cluster. The temperature differential was maintained at 8°C (15°F) for 3000 cycles. Visual inspections of the weld joints during the test revealed no evidence of weld failures. The test was terminated without incidence following 3000 temperature reversals of 3 minutes duration each.

Dimensional inspections at random locations of tube-to-tube spacings at the end of the cluster showed no significant deviations from the values determined for the same locations prior to the thermal test. The cluster was sectioned transversely at three locations immediately adjacent to the wire spacers for visual and metallographic evaluations. Tube-to-tube spacing measurements were not made on these sections due to distortion of the tube ends as a result of the cutting operation. The weld joints appeared to be of a consistently high quality, as typified by controlled weld metal expulsion and freedom from electrode indentation on the wire spacers. No failures were found in the weld joints for the three section of the cluster. Metallographic evaluations of several randomly selected weld joints showed acceptable quality welds relative to symmetry, soundness and penetration into the tube wall. There was no evidence of cracks in the weld regions of the tube wall as a result of the thermal test.

It is concluded from these evaluations that the cluster of fuel tubes, fabricated by the projection welding methods, has adequately performed under thermal cyclic conditions typical for a water cooled reactor. The weld quality was

* The stainless steel tube and wire dimensions of this cluster were 1.270 cm (0.500 inch) with 0.051 cm (0.020 inch) thick wall and 0.158 cm (0.062 inch) diameter, respectively.

satisfactory. However, additional developmental work is required on the process in order to ensure tube-to-tube spacing dimensions within ± 0.008 cm of the nominal value.

b) Zircaloy Cluster

Program activities have included several modifications to the tooling and the fabrication of small Zircaloy tube-wire subassemblies for weld evaluation and autoclave corrosion testing. Initially, the mandrels were machined to a smaller diameter to fit the Zircaloy tubes. The wall thickness of the 1.27 cm (0.500 in.) diameter tubes is 0.064 cm (0.025 in.) as compared to 0.051 cm (0.020 in.) for the stainless steel tubes used on the program. Finally, the electrode faces were regrooved to accommodate the 0.157 cm (0.062 in.) diameter Zircaloy wire. A small inert gas welding chamber was fabricated from plexiglass to contain the test assemblies as well as the outer portion of the electrodes. Air inside the chamber was purged by flowing argon and welds were made in the oxygen-free atmosphere. Several single row assemblies consisting of three lengths of Zircaloy tubes were projection welded to single spacer wires. Welder settings were identical to values reported in Table D-VI for stainless steel subassemblies. Visual inspection revealed electrode indentation at all weld joints. The symmetrical appearance of expelled weld metal on both sides of the weld joints indicated that the electrodes were accurately positioned on the spacer wires. Indentation of the tube wall was controlled to less than 0.003 cm (0.001 in.) by use of expandable copper mandrels. Tube-to-tube dimensions were not measured on the single wire assemblies. Metallographic examinations showed acceptable quality welds for one group of assemblies. In addition, one assembly contained ring-type and shallow penetration welds. These may be due to either a low electrode force or slight variations in dimensions of the tube OD.

Autoclave corrosion tests were conducted on the weld joint sections of the Zircaloy assemblies in 360°C degassed static water for 5 days. The regions of

the weld and heat affected zone of all specimens were uniformly black oxide coated indicating that a suitable welding atmosphere had been utilized.

The work performed indicated the adequacy of the projection welding process for Zircaloy material. Welds of adequate quality and strength were obtained with limited changes in both the electrode tooling and the fixturing.

Conclusions

1. A method for fabricating fuel rod clusters by projection welding spacer wires between rows of fuel rods was developed as an alternate to brazing and mechanical joining techniques. The joining method is applicable to filled or empty fuel rods arranged in square or hexagonal arrays.
2. The method involved the progressive assembly and projection welding of spacer wires to individual rows of fuel rods. Inspection and repair welding of all fuel rod-to-spacer wire joints can be performed at all stages of fabrication. A multiple electrode technique for the simultaneous welding of up to nine joints was developed and utilized in the fabrication of clusters containing up to 61 stainless steel tubes.
3. Electrode tooling and cluster fixturing were developed for the production of high quality welds. Individual welds were evaluated for mechanical strength, corrosion resistance to high temperature water and for quality of joint by metallography. Two clusters consisting of 37 empty fuel rods of Type 347 stainless steel, seven feet in length, were tested for differential thermal elongation, pressure drop and vibration behavior under conditions similar to those for a water reactor. These tests demonstrated the ability of the fabricated assembly to withstand these conditions without failures to the welds.

4. A 37 tube cluster 7 feet in length, of Type 347 stainless steel showed high structural integrity following 3000 temperature reversals with a tube-to-tube temperature difference of 8°C (15°F). However, additional developmental work will be required on the process to obtain a consistent tube-to-tube spacing dimension of ± 0.008 cm (± 0.003 inch).
5. Suitable quality welds were obtained for Zircaloy tube to Zircaloy spacer wire joints in an inert gas filled welding chamber. Weld quality by metallography and corrosion resistance to high temperature water were satisfactory.

STRAP
(STRAIGHT WAVE)

STRAP
(CURVED WAVE)

FINS

WASHERS

FIG D-I SHAPES OF WIRE SPACERS

FIG D-2 PROJECTION WELDED TRIANGULAR LATTICE

FIG D-3 HEXAGONAL CLUSTER OF 37 TUBES FILLED WITH MgO POWDER

FIG D-4 SIXTY-ONE TUBE CLUSTER- 0.500" DIA $\times$ 0.035" WALL,
TYPE 304 STAINLESS STEEL FILLED WITH MgO

FIG D-5 FIVE FOOT LONG MgO FILLED CLUSTER

FIG D-6 FIXTURE USED FOR SHEAR TEST

FIG D-7 FIXTURE FOR TENSILE TEST USING JAWS TO HOLD WIRE

FIG. D-8 APPARATUS FOR TORSION TEST

FIG D-9 TEST ASSEMBLY SHOWING FLOW LOOP AND ASSOCIATED MANOMETERS

FIG D-10 PELLETIZED MOUNTING OF ACCELEROMETERS FOR MEASURING "g's" ACCELERATION TO FUEL TUBE IN PROJECTION WELDED ASSEMBLY

FIG D-11 OSCILLOSCOPE READOUT FOR SOLID WATER
AND AIR-WATER MIXTURE

FIG. D-12 THERMAL CYCLE APPARATUS

FIG D-13 APPARATUS USED FOR DIFFERENTIAL THERMAL ELONGATION EXPERIMENT

FIG D-14 VIEW SHOWING POSITION OF DIAL INDICATOR

FIG D-15 FRACTURE FACE THROUGH WELD NUGGET

FIG D-16 FRACTURE INITIATED AT SHRINKAGE VOID
IN EXPELLED METAL

FIG D-17 SHRINKAGE VOID IN SECTION TAKEN TRANSVERSE TO WIRE

FIG D-18 FRACTURE THROUGH THE WIRE INITIATED AT NUGGET-WIRE BOUNDARY

FIG D-19 VIEW SHOWING FRACTURE FACE OF WIRE

FIG D-20 OXIDATION OF SECTION TAKEN ADJACENT TO WELD SHOWING
DEEPEST CRATERING DUE TO CORROSION

FIG D-21 VIEW OF EVALUATION SUBASSEMBLY

FIG D-22 WELD JOINT SHOWING MASSIVE EXPULSION OF WELD METAL ON INDENTED SIDE OF WIRE SPACER

FIG D-23 APPEARANCE OF WELD JOINT SHOWING EXPELLED METAL

FIG D-24 LONGITUDINAL SECTION OF WIRE SPACER SHOWING WELD

FIG D-25 TRANSVERSE SECTION OF WIRE SPACER SHOWING WELD

FIRST
END

OPPOSITE
END

FIG D-26 TUBE-TO-TUBE SPACING MEASUREMENTS FOR 37 TUBE
TUBE STAINLESS STEEL CLUSTER

FIG D-27 FREQUENCY DISTRIBUTION CHART OF TUBE-TO-TUBE SPACER DIMENSIONS FOR 37 TUBE STAINLESS STEEL CLUSTER

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APPENDIX I

Coolant Flow Pressure Drop

Two analytical equations were used to reduce the data. The first equation utilized a loss coefficient resulting from successive contraction and expansions as the coolant passed through the spacers. Test measurements were used to calculate the spacer loss coefficient and are compared with the predicted analytical value resulting from the area restriction. The theoretical loss coefficient was determined by considering the restriction of the spacer as an abrupt contraction and expansion or as a sharp edged orifice. In either case the theoretical loss coefficient was a function of the geometry only and did not vary with Reynold's number. The second expression is called a "drag coefficient," and was used to assign a pressure loss to restrictions such as the wire spacers in the projection welded clusters. Correlations of predicted and experimental values using both these methods are presented.

The loss coefficient was derived from the measured data using the pressure difference method as follows:

$$(1) \Delta P_s = \Delta P_2 - \Delta P_1$$

where ΔP_{t_1} = the pressure drop between two points along the cluster
with three spacer banks

ΔP_{t_2} = the pressure drop between these same two points with seven
spacers

and ΔP_s = the difference in pressure drop due to the additional four
spacer banks

$$(2) \Delta P_s = n K \rho v^2 / 2g$$

where n = number of additional spacer banks (4)

K = loss coefficient associated with one bank of spacers

ρ = water density (lb/ft³)

v = average unrestricted water velocity (away from spacers)
(ft/sec)

and g = acceleration due to gravity (ft/sec²)

Solving equation 2 for "K".

$$(3) \quad K = \frac{\Delta P_s}{n \rho v^2 / 2g}$$

The loss coefficient "K" thus obtained is plotted versus Reynold's number on Figure I-1. Loss coefficient "K" can also be determined from equation 4 below when values for "f" are taken from the appropriate Moody curve. The "K" values so obtained are plotted on Figure I-2 which compares the loss coefficient from each method.

$$(4) \quad \Delta P_t = n K \frac{\rho v^2}{2g} + f \frac{L}{D} \frac{\rho v^2}{2g}$$

where ΔP_t = measured pressure drop between taps (elevation subtracted out) (lb/ft²)

f = friction factor

and L = length between taps.

The discrepancy between the loss coefficients from each technique can be explained by scrutinizing the values of "f" which were used. To demonstrate the variation in the friction factor, a cluster of tubes without any spacer wires was placed in the test loop and the pressure losses measured. Since the spacers were absent, equation 4 yields a direct measurement of "f" which can be plotted against Reynold's number as in Figure I-2. This curve is significantly higher than the Moody values shown and compares very favorably with the values for "f" which would be required to make the "K" values of the two methods agree. This comparison leads one to conclude that the applicable friction factors should be higher than those published as Moody curves and more nearly those measured empirically.

The second analytical expression used to reduce the pressure drop data is based on a drag coefficient " C_s ". DeStordeur²⁰ includes a detailed explanation of the use of drag coefficients and suggests that this technique be used to correlate pressure loss data for fuel assemblies of the type tested in this program.

The pressure loss due to the spacers can be written as follows:

$$(5) \Delta P_s = \frac{n C_s \rho v_s^2 A_s}{2g A}$$

where C_s = spacer drag coefficient

ρ = water density (lb/ft³)

v_s = water velocity in the spacer region (ft/sec)

A_s = flow area in the spacer region (ft²)

A = unrestricted flow area (away from spacers), (ft²)

g = acceleration due to gravity (ft/sec²)

and n = number of spacer banks

Solving equation 5 for " C_s ".

$$(6) C_s = \frac{2gA\Delta P_s}{n \rho v_s^2 A_s}$$

Using equation 6, measured values of " ΔP_s " permit the calculation of the drag coefficients which are plotted against the spacer Reynold's number in Figure I-3. The spacer Reynold's number applicable to this data is defined by

$$Re_s = \frac{\rho v_s D_s}{\mu}$$

where Re_s = spacer Reynold's number (dimensionless)

D_s = spacer wire diameter

and μ = dynamic viscosity of the water (lb/ft-sec)

Figure I-3 contains a plot of the drag coefficient predicted by reference 20 for the cluster tested in the present program. The apparent discrepancy in the predicted and measured values is probably due to the difference between the spacer geometries. The tests reported in reference 20 were run with wire rows which were only two inches apart, yielding an effect of one bank on its downstream neighbor. Furthermore, the wire grids were oriented 90° with respect to each other as opposed to the parallel relationship of the present tests.

Having experimentally determined the drag coefficients from one projection welded cluster, it was felt that a prediction of the pressure losses for another cluster of a different geometry could be made. To evaluate the correlation, a second cluster of 25 rods, 0.438 in. diameter, 6 feet long, using a wire diameter of 0.062 and a square pitch of 0.665 in. was tested. The drag coefficients so derived agree with the values shown in Figure I-3 very closely.

The agreement found in the drag coefficients between clusters leads to the conclusion that the drag coefficient method for correlating the pressure losses of these clusters is valid. In addition, the loss coefficients resulting from calculation based on an orifice model are much lower than those found experimentally. These predictions indicate that the value of "K" should be independent of Reynold's number. The values generated from the experimental data show a decrease in "K" as "Re" increases and, therefore, contradicts the analysis based on the loss coefficients.

Two-Phase Flow

In addition to solid water tests, measurements of the pressure drop were taken on the original hexagonal cluster for two-phase air-water mixtures. The pressure difference method was used and the resulting drag coefficients plotted in Figure I-5; loss coefficients are plotted in Figure I-4.

The analytical definition of the loss coefficient for two-phase flow is as follows:

$$(7) \Delta P_s = \frac{nK \bar{\rho} V^2}{2g}$$

where ΔP_s = spacer pressure loss (lb/ft²)

V = two-phase water velocity (ft/sec)

$\bar{\rho}$ = average density (lb/ft³)

and n, K, g = number of spacers, loss coefficient, and acceleration due to gravity, respectively, as in the single-phase expression.

The average density is:

$$(8) \bar{\rho} = \rho (1 - \alpha) + \rho_a \alpha$$

where ρ = water density (lb/ft³)

ρ_a = air density (lb/ft³)

and α^* = void fraction

Since $\rho_a \alpha$ is negligible,

$$\bar{\rho} \approx \rho (1 - \alpha)$$

The water velocity for air-water mixtures is defined as:

$$(9) V = \frac{V_w}{1 - \alpha}$$

where $V_w = Q_f/A$

Q_f = water flow rate (ft³/sec)

and A = flow area away from spacer region

Substituting 8 and 9 into 7 and solving for "K".

$$(10) K = \frac{\Delta P_s (1 - \alpha) 2g}{n \bar{\rho} V_w^2}$$

* Slip ratio determined from Marchiterre and Hoglund correlation. Equation for void fraction from known flow rates and slip ratio as follows:

$$\alpha = \frac{Q_g/B}{Q_f + Q_g/B}$$

where Q_g = volumetric flow rate of air (ft³/sec)

Q_f = volumetric flow rate of water (ft³/sec)

B = slip ratio (ratio of air velocity to water velocity)

Equation 10 can be used to compute the loss coefficient having measured ΔP_s by the pressure difference method. Figure I-4 is the resulting plot of loss coefficients for the data obtained by the present test.

To express the pressure loss in terms of the drag coefficient for air-water mixtures, equation 5 for single-phase can be modified as follows:

$$\Delta P_s = \frac{n C_s \rho (1 - \alpha) V_s^2 A_s}{2g A}$$

where the definition of n , C_s , A_s , A , g , and ΔP_s is the same as in equation 5.

ρ = water density (lb/ft³)

α = void fraction

and " V_s " can be written as

$$V_s = \frac{Q_f}{A_s (1 - \alpha)}$$

The drag coefficients computed in the above manner from measured pressure drops using the pressure difference method are plotted in Figure I-5.

The general agreement between the drag coefficients and loss coefficients for single-phase and two-phase flow indicate that single-phase coefficients can probably be used to predict pressure losses for steam-water flow. These comparisons are made on Figures I-4 and I-5.

FIG I-1 PLOT OF LOSS COEFFICIENT vs REYNOLD'S NUMBER FROM THE PRESSURE DIFFERENCE METHOD

FIG I-2 COMPARISON OF LOSS COEFFICIENTS DERIVED FROM PRESSURE DIFFERENCE METHOD TO THOSE DERIVED BY CALCULATING FRICTION AND ELEVATION

FIG I-3 PLOT OF DRAG COEFFICIENT vs REYNOLD'S NUMBER

FIG I-4 COMPARISON OF TWO PHASE AND ONE PHASE LOSS COEFFICIENTS

FIG I-5. COMPARISON OF TWO PHASE AND ONE PHASE DRAG COEFFICIENTS

APPENDIX II

Vibration Testing

In order to predict the natural frequency of the system, certain assumptions must be made. These are defined below along with the mathematics. It is felt that the fuel rods of the projection welded fuel assembly will behave as a semi-fixed beam. For this reason, the simple supported and fixed end beam cases will be considered. The Rayleigh method of equating the potential and kinetic energies will be used to determine the fundamental mode of vibration.

Natural Frequency - Free Ends

Assume that the deflection can be expressed by equation 1)

$$1) \quad y = y_0 \left(\sin \frac{\pi x}{l} \right) \sin \omega t$$

where, y = deflection at any point along the beam at time "t"

y_0 = maximum deflection occurring at the center of the beam

ω = angular velocity

t = time

x = distance along the beam

l = length of the beam

Equating the potential and kinetic energies, the resulting natural frequency from Thompson is:

$$2) \quad f = \frac{\pi}{2} \sqrt{\frac{EI}{Wl^4}}$$

where, f = natural frequency of a single tube (cps)

E = modulus of elasticity, lb/in²

I = moment of inertia, (in⁴)

W = weight per unit length, (lb/in)

ℓ = length between supports, (in.)

g = acceleration due to gravity (in./sec²)

Natural Frequency - Fixed Ends

Assume the deflection curve is

$$3) y = \frac{1}{2} Y_0 (1 - \cos \frac{2\pi x}{\ell}) \sin \omega t$$

The natural frequency for this case is

$$4) f = \frac{\pi}{2\ell^2} \sqrt{\frac{16 E I g}{3W}}$$

where, W = weight per unit length

In calculating the weight per unit length " W ", the lead shot, tube wall and surrounding water of a unit cell should be considered. Only the tube wall should be considered in computing the moment of inertia since it is assumed that the lead shot and water does not add stiffness to the fuel rod. On this basis, the natural frequency for the free end case with 20 in. between spacers was 51.3 (cps). For the fixed end case, the natural frequency was 119.2 (cps). As shown in Table II-1, the primary frequency was measured at 125 and 133 cycles per second. Using the equation for simple harmonic motion, the following mathematical progression leads to the equation for the maximum deflection of the tube as a function of acceleration and frequency.

$$y = A \sin \omega t$$

where y = deflection (in.)

A = amplitude of deflection

ω = angular velocity (rad/sec)

t = time (sec.)

Converting angular velocity to frequency $\omega = 2\pi f$ where f = frequency (cps), and differentiating twice to get acceleration, a , from deflection.

$$\frac{d^2 y}{dt^2} = a = A 4\pi^2 f^2 \sin 2\pi f t$$

The maximum value for $\sin 2\pi f t$ is "1" which reduces the equation to

$$a = A 4\pi^2 f^2 \quad (1)$$

TABLE II-1

DATA FOR VIBRATION TEST

Velocity*** ft/sec.	Air Flow (CFM)	Void Fractions α	Frequency CPS		Acceleration* (g's)	Deflection** y (in)
			Primary	Secondary		
6.3	--	--	125	750	.13	<.0001
10.8	19.4	.42	--	1470	.29	--
15.3	38.1	.59	--	1730	.45	--
19.4	55.8	.68	--	1300	.45	--
9.4	--	--	133	600	.13	<.0001
13.9	18.9	.32	--	1300	.41	--
17.9	36.0	.53	--	1000	.51	--
21.8	52.4	.57	--	800	.82	--
12.6	--	--	133	700	.12	<.0001
16.6	17.7	.25	--	1500	.49	--
15.4	--	--	125	1100	.13	<.0001
18.6	--	--	125	1200	.14	<.000086

* Accelerations in table above for solid water readings result from primary frequency. Accelerations for air-water readings result from secondary frequency.

** Deflection is for primary frequency; amplitudes from secondary frequency are negligible.

*** Based on a slip ratio of unity.

Since the amplitude "A" is equal to the maximum value for the deflection "y" and it is the maximum value of deflection which is to be calculated, we can make the substitution, $y = A$, and transpose to write

$$(1) y = \frac{a}{4\pi^2 f^2}$$

substituting $a = 386 g$

where $g =$ the multiple accelerations due to gravity

and $386 = (32.2 \text{ ft/sec}^2) (12 \text{ in/ft})$

$$= 386 \text{ in/sec}^2/g$$

from (1) and (2)

$$y = \frac{386 g}{4\pi^2 f^2}$$

combining constants

$$(3) y = \frac{9.79 g}{f^2}$$

Using this equation to calculate the deflections, it was found that the largest value obtained was for single-phase water at the highest flow rate velocity of 18.6 ft/sec. This is only .000086 in. deflection and can be considered negligible as can all others. The frequency used for the calculation was the primary frequency, in the case where both were present, and the secondary, where no primary was evident. Since these secondary frequencies are so high and the deflection varies inversely as the square of the frequency, it can be seen that the deflections, due to the secondary modes are insignificant.

Due to an instrumentation failure, one of the accelerometers could not be used when recording the data for the test. Values obtained while setting-up the test and calibrating were sufficient to conclude that both accelerometers were yielding the same values. Since the directions of the acceleration pick-ups were at ninety degrees, the worst vector addition would yield a resultant value equal to the square root of two times the values reported for one pick-up.

This is still a negligible value. Thus, it can be concluded that for this spacer interval, flow induced vibration in the tubes is not of sufficient magnitude to present any problems. The primary frequency of vibration was measured at 125 and 133 cps which most nearly agrees with that calculated using the fixed-end model. This agreement indicates that predictions of natural frequency for systems such as the one tested here should be made on the basis of the fixed-end model. If the designer can avoid a natural frequency in the tubes, which is close to that of the forcing vibration of the pump, vibration of an assembly of this type should be no hazard.

APPENDIX III

ANALYSIS OF THERMAL STRESS CYCLING OF PROJECTION WELDED TUBE CLUSTER

A. Test Conditions

The following test conditions were assumed to prevail:

1. The test was performed with the tube cluster in the vertical position. Hence, stresses due to the weight of the cluster and water were negligible.
2. At constant temperature, for all tubes, the base end of each tube rested on the bottom base plate of the water manifold.
3. The water manifold at the top of the cluster had a 3/16 in. clearance for axial expansion of the water tubes.
4. During thermal cycling, the hot and cold tube banks were parallel to the rows of the projected welded wires.
5. The water tubes were 84 in. long and were held together by the banks of welded wire spacers, 20 in. apart, with the end banks 2 in. from the ends of the tubes.
6. Figure III-1 shows an end view of a quadrant of the fuel tube cluster. For identification purposes, the rows and positions in the rows of the individual tubes are identified.
7. Figure III-1 shows the five banks of welded wire spacers labeled for identification purposes.

B. Model

The following mathematical model was selected to represent the cluster during test.

1. The cluster wires are regarded as beams of circular cross-section with horizontally clamped ends at the weld joints.
2. The water tubes at the weld joints act as supporting walls for the wires. These water tubes are considered as being mounted on elastic vertical and horizontal foundations.
3. Loads on wires, due to thermal expansion of the water tubes, are regarded as concentrated loads at the midpoint between supports.
4. The friction loads between the tube walls and the manifold heads are small.

5. The water tubes are regarded as beams of cylindrical cross-section which are free at the ends.

C. Definitions

The following definitions are used in developing the mathematical model.

α = thermal expansion coefficient = $9.5 \times 10^{-6}/^{\circ}\text{F}$ for stainless steel.

ΔT = temperature difference = 42°F .

$y(x)$ = vertical deflection of wire from neutral position at a distance "x" from one supporting end.

$P(x)$ = load on wire at a distance "x" from one supporting end.

ℓ = distance between spacer banks (see Figure III-1).

d = wire diameter = 0.062 in.

d_o = outside diameter of tube = 0.500 in.

d_i = inside diameter of tube = 0.460 in.

R = radius of curvature of wire.

e = strain in wire due to bending.

σ_f = fiber stress in wire due to bending.

E = Young's modulus of elasticity = 28.0×10^6 psi for Type 347 SS.

I = moment of inertia of the cross-section.

I_w = moment of inertia for wire.

I_t = moment of inertia for tube.

$Lw(x)$ = Laplace transform of the function $w(p)$

$w(x)$ = load per unit length along a beam.

K = elastic stiffness of foundation.

b = constant, defined by $(\frac{k}{4EI_{\text{wire}}})^{\frac{1}{4}}$

ΔS = change in length of fuel tube due to temperature change.

$\Delta \ell$ = change in length of fuel tube due to elastic compression or tension.

A = cross-sectional area of $\frac{1}{2}$ in. diameter tube with a .020 in. wall = .0314 in.².

$$\mu = x/a$$

a = distance along wire between tube-to-wire weld points = 0.312 in.

h = beam span

S = length of the spacer wire between weld points in the stressed condition.

ϵ = tensile strain in wire due to change in length.

σ_t = tensile stress in wire due to change in length.

τ_{weld} = shear stress on the weld due to axial loading of the tube.

τ_{wire} = shear stress on the wire due to axial loading of the tube.

D. Analysis

A beam subject to transverse forces will deflect according to the relation

$$\frac{EI d^4 y}{dx^4} = w(x) \quad (1)$$

If the beam rests on an elastic foundation, we have

$$\frac{EI d^4 y}{dx^4} + Ky = w(x) \quad (2)$$

For a beam on an elastic foundation, clamped horizontally at both ends under the influence of a concentrated load, the Laplace transform is

$$Lw(x) = w(p) = P \int_0^\infty e^{-px} dx$$

Therefore,

$$y(x) = Y_2 \phi_2(x) + Y_3 \phi_1(x) \quad 0 < x < a \quad (3)$$

where

$$\phi_1(x) = \frac{1}{4b^3} (\sin bx \cosh bx - \cos bx \sinh bx) \quad (4)$$

$$\phi_2(x) = \frac{1}{2b^2} (\sin bx \sinh bx) \quad (5)$$

$$\phi_3(x) = \frac{1}{2b} (\sin bx \cosh bx + \cos bx \sinh bx) \quad (6)$$

$$\phi_4(x) = \cos bx \cosh bx \quad (7)$$

and

$$Y_2 = \frac{Po}{EI} \frac{\phi_2(a)\phi_2(l-a) - \phi_3(l)\phi_2(l-a)}{\phi_2^2(l) - \phi_3(l)\phi_1(l)} \quad (8)$$

$$Y_3 = \frac{Po}{EI} \frac{\phi_2(l)\phi_2(l-a) - \phi_3(l)\phi_1(l-a)}{\phi_1(l)\phi_2(l) - \phi_2^2(l)} \quad (9)$$

Since data evaluation from the above equations is tedious, equation (1) is solved for case where the horizontal supports are rigid. The resulting reaction force is equated to the axial force in the water tube, and the wire deflection for equilibrium, $y(x)$, is calculated.

For a beam on rigid horizontal supports, clamped on both ends, the Laplace transform is

$$Lw(x) = W(p) = P \int_0^a P e^{-ax} dx \quad (10)$$

and the solution to equation (1) is

$$Y(x) = \frac{Poa^3}{8EI} \left[\left(\frac{x}{a}\right)^2 - \frac{2}{3} \left(\frac{x}{a}\right)^3 \right] = \frac{K}{8} \left[\mu^2 - \frac{2}{3} \mu^3 \right] \quad (11)$$

$$\text{where } K = \frac{Poa^3}{EI} \quad \text{and } \mu = \frac{x}{a} \quad 0 < x < a \quad (12)$$

For a beam on rigid horizontal supports, clamped on one end and free at the other end, the solution is

$$Y(x) = \frac{Poa^3}{2EI} \left[\left(\frac{x}{a}\right)^2 - \frac{1}{3} \left(\frac{x}{a}\right)^3 \right] = \frac{K}{2} \left(\mu^2 - \frac{1}{3} \mu^3 \right) \quad 0 < x < a \quad (13)$$

where

$$K = \frac{Poa^3}{EI \text{ wire}} \quad \text{and } \mu = \frac{x}{a}$$

$$\text{The moment of inertia of a circular cross-section is } I = \frac{\pi d^4}{64} \quad (14)$$

$$I_w = (0.73) (10^{-6}) \text{ in.}^4 \text{ for .062 in. wire diameter} \quad (15)$$

The banks of spacer wires are at 20 in. intervals along the tubes. The elongation of a tube in each 20 in. interval, due to a 42°F temperature rise is

$$\Delta S = \alpha \Delta T = 20 (9.5) 10^{-6} (42) = .008 \text{ in.} \quad (16)$$

Assuming friction forces at the manifold small enough to allow the cold tubes to center themselves, the wires in the central bank will not deflect. The central bank shall be used as the datum level, and the first and second banks on either side will be treated equally.

The deflection that a given wire spacer undergoes is the difference between the thermal expansion and the elastic compression for the length of tube being restrained by the wire spacer.

$$Y(x) = \Delta S - \Delta L \quad (17)$$

E. Calculation of Stresses

A number of different stress states, existing within the cluster, are treated independently below as Sections I, II and III.

Section I - Vertical Tube Loads and Shear Stresses

Case I

Tube supported by two wire spacers clamped at both ends. (Includes all non peripheral tubes). The wires support a concentrated load at the midpoint of the span; hence, the deflection of the wire is given by equation (11)

$$Y(x) = \frac{P_o a^3}{8 EI} \left(\mu^2 - \frac{2}{3} \mu^3 \right) \quad (18)$$

Solving for P_o

$$P_o = Y(x) \left(\frac{EI}{a^3} \right) 8 \frac{1}{\mu^2 - \frac{2}{3} \mu^3} \quad (19)$$

This load will cause some elastic compression of the water tube which can be calculated from the relation

$$P_o = \frac{AE \Delta l}{2 l} \quad (20)$$

The factor "2" is necessary since two wires, one on each side of the tube, are each exerting a compressive load, " P_o ", on the tube. Equating (19) and (20) obtains

$$Y(x) = \frac{A a^3}{16 I} \left(\mu^2 - \frac{2}{3} \mu^3 \right) \frac{\Delta l}{l} = 27 \frac{\Delta l}{l} \quad (21)$$

Hence, for the first and second banks of wire, we have;

$$\text{Bank \#1 } l = 20 \quad Y(x) = 1.35 \frac{\Delta l}{l} \quad \Delta l = .42 \Delta S \quad Y(x) = .58 \Delta S$$

$$\text{Bank \#2 } l = 40 \quad Y(x) = .67 \frac{\Delta l}{l} \quad \Delta l = .60 \Delta S \quad Y(x) = .40 \Delta S$$

For the first bank $\Delta S = .008$ in. and for the second bank $\Delta S = .016$ in.

Hence, for the first bank

$$P_o = Y(x) \left(\frac{EI}{a^3} \right) 8 \left(\mu^2 - \frac{2}{3} \mu^3 \right) = (.58) (.008) \left[\frac{(28)(10^{-6})(.73)(10^{-6})}{(.312)^3} \right] (8)(3) \\ = 76 \text{ lbs} \quad (22)$$

$$\text{and the shear stress in the wire is } \tau_{\text{wire}} = \frac{76}{2\pi(.031)^2} = 12,590 \text{ psi}$$

$$\text{The shear stress in the weld is } \tau_{\text{weld}} = \frac{76}{.00318} = 23,900 \text{ psi}$$

For the second bank:

$$P_o = (.40) (.016) (16.3) 10^3 = 103 \text{ lbs}$$

$$\text{and the shear stress in the wire is } \tau_{\text{wire}} = \frac{103}{2\pi(.031)^2} = 17,000 \text{ psi}$$

$$\text{The shear stress in the weld is } \tau_{\text{weld}} = \frac{103}{.00318} = 32,400 \text{ psi}$$

Case II

Peripheral tubes with wire spacers clamped at one end, free at the other (cantilever beams).

For a cantilever beam

$$Y(x) = \frac{Poa^3}{2 EI} (\mu^2 - \frac{1}{3} \mu^3) \quad (23)$$

Combining equations (20) and (28) obtains

$$Y(x) = \frac{A a^3}{4 I} (\mu^2 - \frac{1}{3} \mu^3) \frac{\Delta l}{l} = 215 \frac{\Delta l}{l} \quad (24)$$

For the first and second banks respectively:

$$\text{Bank \#1} \quad l = 20 \quad Y(x) = 10.76 \Delta l \quad \Delta l = .157 \Delta S \quad Y(x) = 1.68 \Delta S$$

$$\text{Bank \#2} \quad l = 40 \quad Y(x) = 5.4 \Delta l \quad \Delta l = .271 \Delta S \quad Y(x) = 1.46 \Delta S$$

Solving equation (28) for $\mu = 1$

$$Po = 3 Y(x) \frac{EI}{a^3} = 2010 Y(x) \quad (25)$$

For the first bank

$$Po = (1.68) (.008) (2010) = 27 \text{ lbs} \quad (26)$$

The shear stresses are

$$\tau_{\text{wire}} = \frac{27}{2\pi(.031)^2} = 4500 \text{ psi} \quad (27)$$

$$\tau_{\text{weld}} = \frac{27}{.00318} = 8,490 \text{ psi} \quad (28)$$

For the second bank

$$Po = (1.46) (.016) (2010) = 46 \text{ lbs.} \quad (29)$$

and the shear stresses are

$$\tau_{\text{wire}} = \frac{46}{2(.00300)} = 7,670 \text{ psi} \quad (30)$$

$$\tau_{\text{weld}} = \frac{46}{.00318} = 14,470 \text{ psi} \quad (31)$$

Case III

Peripheral tubes (two wire spacers, one clamped at both ends and one free at one end).

For equilibrium, we have

$$Po \text{ clamped} + Po \text{ cantilever} = Po \text{ elastic compression} \quad (32)$$

For a specified deflection, the cantilever beam will carry 1/8 of the load that the clamped beam will carry. From (32) we have,

$$9/8 P_o \text{ clamped} = P_o \text{ elastic compression} \quad (33)$$

Combining equations (19), (20), and (33) obtains

$$9/8 Y(x) \left(\frac{EI}{a^3} \right) 8 \left(\mu^2 - \frac{2}{3} \mu^3 \right) = AE \frac{\Delta l}{l} = P_o \text{ elastic compression} \quad (34)$$

Hence,

$$Y(x) = (8/9) \frac{(Aa^3)}{8 I} \left(\mu^2 - \frac{2}{3} \mu^3 \right) \frac{\Delta l}{l} = 47.7 \frac{\Delta l}{l} \quad (35)$$

For Banks 1 and 2, we have

$$\text{Bank \#1 } l = 20 \quad Y(x) = 2.37 \Delta l \quad \Delta l = .316 \Delta S \quad Y(x) = .75 \Delta S$$

$$\text{Bank \#2 } l = 40 \quad Y(x) = 1.18 \Delta l \quad \Delta l = .48 \Delta S \quad Y(x) = .57 \Delta S$$

For the clamped wire (36)

$$P_o = Y(x) \left(\frac{EI}{a^3} \right) 8 (3) = Y(x) \frac{(28) (10^6) (.73) (10^{-6}) (24)}{(.0302)} = 16,240 Y(x)$$

For the first bank

$$P_o = (.75)(.008)(16,240) = 97 \text{ lbs} \quad (37)$$

and the shear stresses are

$$\tau_{\text{wire}} = \frac{97}{2(.0030)} = 16,160 \text{ psi} \quad (38)$$

$$\tau_{\text{weld}} = \frac{97}{.00318} = 30,500 \text{ psi} \quad (39)$$

For the second bank of wires, we have

$$P_o = (.57)(.016)(16,240) = 147 \text{ lbs} \quad (40)$$

and the shear stresses are

$$\tau_{\text{wire}} = \frac{147}{2(.003)} = 24,500 \text{ lb/in}^2 \quad (41)$$

$$\tau_{\text{weld}} = \frac{147}{(.00318)} = 46,200 \text{ lb/in}^2 \quad (42)$$

The cantilever beam will carry 1/8 of the load that the clamped beam carries. The stresses for the cantilever beam will be:

For the first bank

$$\tau_{\text{wire}} = 2200 \text{ lb/in}^2 \quad (43)$$

$$\tau_{\text{weld}} = 3800 \text{ lb/in}^2 \quad (44)$$

and for the second bank

$$\tau_{\text{wire}} = 2860 \text{ lb/in}^2 \quad (45)$$

$$\tau_{\text{weld}} = 5780 \text{ lb/in}^2 \quad (46)$$

Case IV

Peripheral tubes (one wire clamped at both ends). These tubes have only one clamped wire supporting them. The expression for the deflection of this wire remains as

$$P_o = Y(x) \left(\frac{EI}{a^3} \right) 8 \frac{1}{\mu^2 - \frac{2}{3} \mu^3} \quad (47)$$

but the elastic compression of the tube, " Δl ", is half the value for those having two wires.

$$P_o = AE \frac{\Delta l}{l} \quad (48)$$

Therefore, combining (47) and (48)

$$Y(x) = \frac{Aa^3}{8I} \left(\mu^2 - \frac{2}{3} \mu^3 \right) \frac{\Delta l}{l} = 53.8 \frac{\Delta l}{l} \quad (49)$$

For the first and second banks respectively

$$\text{Bank \#1} \quad l = 20 \quad Y(x) = 2.69 \Delta l \quad \Delta l = .271 \Delta S \quad Y(x) = .73 \Delta S$$

$$\text{Bank \#2} \quad l = 40 \quad Y(x) = 1.35 \Delta l \quad \Delta l = .425 \Delta S \quad Y(x) = .57 \Delta S$$

From (47), we have for $\mu = 1$

$$P_o = Y(x) \left(\frac{EI}{a^3} \right) (24) = 16,300 Y(x) \quad (50)$$

For the first bank

$$P_o = (.73)(.008)(16,300) = 95 \text{ lbs} \quad (51)$$

Hence, the stresses are

$$\tau_{\text{wire}} = \frac{95}{2\pi(.031)^2} = 15,730 \text{ lb/in}^2 \quad (52)$$

$$\tau_{\text{weld}} = \frac{95}{.00318} = 29,900 \text{ lb/in}^2 \quad (53)$$

For the second bank:

$$P_o = (.57)(.016)(16,300) = 148 \text{ lbs} \quad (54)$$

and the stresses are

$$\tau_{\text{wire}} = \frac{148}{2\pi(.031)^2} = 24,500 \text{ psi} \quad (55)$$

$$\tau_{\text{weld}} = \frac{148}{.00318} = 46,500 \text{ psi} \quad (56)$$

Section II - Horizontal Tension Loads in the Wires

The lateral deflections in the tubes and the associated tensile stresses in the spacer wires vary with the Case and the Bank. The analysis is shown for Case I, banks 1 and 2. The results of the calculations for the other cases are included in Table III-I.

Bank 1

If the water tubes are regarded as beams, the cross-sectional moment of inertia is

$$I_t = \frac{\pi (d_o^4 - d_i^4)}{64} = \frac{\pi}{64} (.5^4 - .46^4) = (8.4)10^{-4} \text{ in}^4 \quad (57)$$

Assuming the tube ends are rigidly supported in the lateral direction by the manifolds and that the spacer wires act as support columns, the lateral deflection of the tube is given by

$$x(y) = \frac{P_o l^3}{8 EI} \left[\left(\frac{y}{d} \right)^2 - \frac{2}{3} \left(\frac{y}{d} \right)^3 \right] \quad (58)$$

where

At the first bank of spacer wires $y = l = 20 \text{ in.}$

$$\text{Hence, } x(y) = \frac{P_o (20)^3}{8(28)10^6(8.4)10^{-4}(3)} = .0142 P_o \quad (59)$$

It is obvious that the tube is very soft for lateral deflections in the region between the middle bank and the first bank of spacer wires. Therefore, any tension loads in the spacer wires will be relaxed by lateral motion of the tubes to closer spacing.

Bank 2

At the second bank of spacer wires, the deflection curve is

$$x(y) = \frac{P_o y^2}{6 EI} l^3 (l - q)^2 \left[36l - (l - q)y \right] \quad (60)$$

where $l = 40 \text{ in.}$ $y = q = 2 \text{ in.}$

Hence,

$$x(y) = \frac{P_o (2)^2 (38)^2}{6(28)10^6(8.4)10^{-4}(40)^3} \left[3(2)(40) - 42(2) \right] = 10^{-4} P_o \quad (61)$$

Thus, a lateral force of 10 pounds is required to produce a .001 in. lateral deflection..

The length of the spacer wire along its deflection path is given by

$$S = \int_0^a \left[1 + \left(\frac{dy}{dx} \right)^2 \right]^{\frac{1}{2}} dy \quad (62)$$

TABLE III-I

AXIAL STRESSES IN THE WIRES

Case	a^2 (in) ²	$[y(x)]^2$ (in)	S (in)	Total Strain ϵ	Stress σ_t (psi)
I - Bank 1	(.312) ²	$[(.58)(.008)]^2$	.3120344	.0001103	3,090
- Bank 2	"	$[(.40)(.016)]^2$	.3120006	.00000192	538
II - Bank 1	"	$[(1.68)(.008)]^2$	.3122893	.000927	26,000
- Bank 2	"	$[(1.46)(.016)]^2$	.3128	.00256	71,600
III - Bank 1	"	$[(.75)(.008)]^2$	.3120576	.0001846	5,170
- Bank 2	"	$[(.57)(.016)]^2$	.31213326	.00042712	11,960
IV - Bank 1	"	$[(.73)(.008)]^2$	.31205465	.00017516	4,920
- Bank 2	"	$[(.47)(.016)]^2$	.3123326	.001066	11,960

Differentiation of equation (11) and substitution into equation (62) yields

$$S = a \int_0^L \left[1 + \frac{k^2}{16a^2} (\mu^2 - 2\mu^3 + \mu^4) \right]^{\frac{1}{2}} d\mu \quad (63)$$

Since the above expression does not integrate simply, an approximation is made, which assumes that the wire spacers are stretched in a straight line between weld joints.

$$S = \left[a^2 + Y(x)^2 \right]^{\frac{1}{2}}$$

The total strain in the wire is $\epsilon = \frac{S - a}{a}$

and the associated stress is $\sigma_t = E \epsilon$

The analysis for the other banks is similar, and the results are tabulated below. The bending stresses associated with these deflections are presented in Section III.

The stresses have been tabulated above, and the lateral deflections of the water tubes, resulting from the wire strains, are calculated below.

Equations (59) and (61) give the

$$X(y) = (1.4) 10^{-2} P_0 \quad (64)$$

$$X(y) = 10^{-4} P_0 \quad (65)$$

It is evident, by substituting the values of (s-a) from the table for y(x) in equations (59) or (60), that the resulting values of P_0 are all below 25 pounds. Hence, the tensile stresses shown in the above table will become insignificant due to the lateral deflection of the tubes.

Section III - Bending Stress

To calculate the maximum fiber stress in the wire spacers, the radius of curvature, as calculated below, was used

$$R = \left[\frac{1 + \left(\frac{dy}{dx} \right)^2}{\frac{d^2y}{dx^2}} \right]^{\frac{1}{2}} \quad (66)$$

for the clamped wire

$$\frac{dy}{dx} = \frac{k}{4} (\mu - \mu^2)$$

$$\frac{d^2y}{dx^2} = \frac{k}{4} (1 - 2\mu)$$

for the cantilever wire

$$\frac{dy}{dx} = k \left(\mu - \frac{\mu^2}{2} \right) \quad (67)$$

$$\frac{d^2y}{dx^2} = k (1 - \mu) \quad (68)$$

Hence,

$$R_{\text{clamped}} = \left[\frac{1 + \frac{k^2}{16} (\mu - \mu^2)^2}{k/4 (1 - 2\mu)} \right]^{\frac{1}{2}} \quad (69)$$

and

$$R_{\text{cantilever}} = \left[\frac{1 + k^2 (\mu - \mu/2)^2}{k (1 - \mu)} \right]^{\frac{1}{2}} \quad (70)$$

The maximum fiber stress will occur at the point where the values of curvature are at a minimum. For the clamped wire, the minimum radius will occur at $\mu = 0$ and $\mu = 1$

$$R_{\text{min clamped}} = 4/K = \frac{4}{\frac{Po a^3}{EI}} = \frac{4 EI}{Po a^3} \quad (71)$$

and for the cantilever wire, the minimum value occurs at

$$R_{\text{min cantilever}} = 1/K = \frac{EI}{Po a^3} \quad (72)$$

The maximum fiber strain can be calculated from the relationship

$$e = \frac{r}{R} \quad (73)$$

where r = the radius of the wire cross-section and the maximum fiber stress will be

$$\sigma_f = E e \quad (74)$$

Substituting from equations (71) and (72) into (74) yields

$$\sigma_{f \text{ max clamped}} = \frac{a^3 r Po}{4 I} = 321 Po \quad (75)$$

$$\sigma_{f \text{ max cantilever}} = \frac{a^3 r Po}{I} = 1286 Po \quad (76)$$

Values of σ_f are tabulated for the various cases in Table III-II.

TABLE III-II

BENDING STRESSES

<u>Case</u>	<u>Restraint</u>	<u>Po</u>	<u>σ_f (psi)</u>
I - Bank 1	clamped	76	24,400
- Bank 2	clamped	103	33,000
II - Bank 1	cantilever	27	34,700
- Bank 2	cantilever	46	59,200
III - Bank 1	clamped	97	31,100
	cantilever	12	15,930
- Bank 2	clamped	147	47,200
	cantilever	18	23,100
IV - Bank 1	clamped	95	30,500
- Bank 2	clamped	148	47,500

FIG III-1 DESIGNATION OF TUBE, WELD AND WIRE LOCATIONS WITHIN THE CLUSTER