**SEMIANNUAL REPORT FOR THE PERIOD
APRIL 1 TO SEPTEMBER 30, 1978 OF WORK ON:**

- 1) SUPERCONDUCTING POWER TRANSMISSION SYSTEM DEVELOPMENT**
- 2) CABLE INSULATION DEVELOPMENT**

November 7, 1978

**DIVISION OF ADVANCED TECHNOLOGY APPLICATIONS
ACCELERATOR DEPARTMENT**

**BROOKHAVEN NATIONAL LABORATORY
ASSOCIATED UNIVERSITIES, INC.**

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MASTER

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November 7, 1978

POWER TRANSMISSION PROJECT
TECHNICAL NOTE NO. 83

DIVISION OF ADVANCED TECHNOLOGY APPLICATIONS
ACCELERATOR DEPARTMENT

BROOKHAVEN NATIONAL LABORATORY
UPTON, NEW YORK 11973

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1. Administration

1.1. Budget

By the end of Fiscal Year 1978 the operating funds had been expended without significant changes in the average annual rate. The capital equipment funds were increased to cover the purchase of a CTI model 1400 refrigerator for use in the Bldg. 815 laboratory. The longer period for which experiments are kept cold and the increasing sophistication of thermal measurement has necessitated replacement of the old machines used up to the present.

1.2. Personnel

By the end of the fiscal year manpower ceilings had been established which will continue with possible minor modification for the future. The ceiling is based both on a head count and the full time equivalent (F.T.E.) man years. In Fiscal Year 1978 the personnel on superconducting power transmission development and cable insulation development amounted to 36 man years F.T.E.

Dr. A. Pearmain, who had a sabbatical appointment, left in September to rejoin the faculty at University College, Dublin.

2. Conductor Development

2.1. Extrusion of Bronze Tape Billets

Three billets were prepared in an attempt to determine if designs and procedures could be developed for extrusions in which a good bond between the bronze and Nb-1Zr is assured. A large extrusion prepared previously in the Airco superconductor tape development program was discarded because of poor bonding. The three billet variations are:

- (1) A Nb-1Zr rod in bronze.
- (2) As in (1) but a copper tube is inserted between the Nb-1Zr rod and the bronze.
- (3) A rectangular Nb-1Zr bar in bronze.

Billet (1) is the standard. In billet (2), copper is placed in the Nb-1Zr/bronze interface since past experience indicates that copper bonds well to both Nb-1Zr and bronze. Billet (3) was prepared to determine whether an extrusion could be made yielding a greater amount of tape, for the same diameter billet.

Bronze for these billets was purchased from Airco. The Cu-13%Sn bronze was cast into a billet 7 1/2" diameter 24" long and then extruded to nominally 4 1/2" diameter 48" long. After extrusion the billet was homogenized. Machining of the billets was as shown in the sketches in Figs. 1, 2 and 3. The 4.350" outside diameter was determined by the copper cans used by Nuclear Metals Inc. where the final extrusions were performed. The round holes in Billets 1 and 2 are gun drilled and honed. The rectangular hole in billet 3 made by sawing the billet in half, longitudinally milling a channel in each half, and then electron beam welding the two halves. A shim is used to make up the material removed by the saw cut. Some machining is done on the Nb-1Zr pieces and the OFHC copper tube so that the components just slide into the various holes. An attempt was made to clean the bronze

Figure 1. Bronze extrusion billet round w/Nb-1 Zr 1" dia.

Figure 2. Bronze extrusion billet round w/Nb-1Zr w/copper sleeve.

Figure 3. Bronze extrusion billet.

surfaces by etching with an NH_4OH + hydrogen peroxide solution but it was found that the etching of the holes could not be controlled. Therefore the only cleaning, which is performed just prior to final assembly, is a degreasing with MEK and ethyl alcohol. After cleaning, copper end plates are fitted to the billet and circumferentially electron beam welded to the bronze billet in order to seal the Nb-1Zr in vacuum.

At Nuclear Metals Inc. the billets were placed into copper cans, the space between the can evacuated, and the pump out tube then sealed. Extrusion is performed on a 1400 ton press. Die opening is rectangular 1/2" x 3" which gives a reduction ratio of almost 11:1. Temperature of the billet, liner, and die is 900°F. Extrusion speed is 15 inches per minute. Force values measured and extrusion constants calculated for the three billets are:

	Upset Force (lb)	Upset Ext. Constant (TSI)*	Running Force (lb)	Running Ext. Constants (TSI)
BNL Billet # 1	880	22.6	700	18.0
BNL Billet # 2	900	23.0	750	19.3
BNL Billet # 3	1200	30.8	850	21.8

*Tons per square inch.

All three billets extruded without any difficulty. Visual examination of the extrusion shows the Nb-1Zr has broken through the nose end of all three billets and there are several bubbles between the copper can and bronze at random locations in all three billets. The front ends of all three extrusions are shown in Fig. 4. A cross section of the extrusions is shown in Fig. 5. Extrusion # 1 is shown after having been reduced 40% by cold rolling. The "dog bone" of the Nb-1Zr in extrusion 3 can probably be minimized or eliminated by shaping the edges of the Nb-1Zr or making the Nb-1Zr slightly narrower.

Metallographic examination of a section through the extrusion showed the Nb-1Zr to be bonded to the bronze. To better evaluate the bond, a bend test was performed. This test consists of sawing through one layer of bronze slightly into the Nb-1Zr along the longitudinal axis on a section sample approximately 1/4 inch thick. The sample is then bent around a 1" radius. Results of the test are shown in Fig. 6. Significant benefit of copper at the Nb-1Zr to bronze interface is clearly shown. Although separation is observed for extrusion # 1 some marginal bonding exists as evidenced by a "snap" sound when the separation occurs. It is noted that the separation in extrusion # 1 occurs at a smaller included angle than is shown in the photograph. Extrusion # 3 appears to be well bonded but this might be due to the larger interface area which might affect this type of test.

These extrusions are now being cold rolled, with the final thickness to be about 3 mils. A single pass 40% reduction is being made between anneals. Extrusion # 1 has been rolled to 0.040" thickness thus far with no indications of separation.

Figure 4. Nose end of extrusions.

Figure 5. Cross section view of extrusions.

Figure 6. Results of bend tests to evaluate Nb-1Zr to bronze bond.

2.2. Evaluation of Commercial Tapes

2.2.1. Intermagnetics General Corporation

Evaluation has been initiated of a large shipment (~6000 ft) of 6 mm wide tape received from Intermagnetics General Corporation (IGC) which was fabricated as a requirement of the superconductor tape contract. This tape is similar to that evaluated previously, i.e., clad with type 302 stainless steel (0.001" thick) on one side and copper (0.002" thick) on the other.

2.2.1.2. General Description and Sampling

This lot of tape is 6 mm wide with the tapes having various lengths. While the target was to produce single lengths of 1000 ft, most pieces are less than that amount, however, pieces as long as 860 ft were obtained. Shortened lengths were the result of breakages which occurred in various stages of the tape manufacturing process.

Quantities and lengths of material as well as identification are shown in Fig. 7. Also shown are sample numbers which can be identified with results of the evaluation tests reported here.

2.2.1.3. Metallography

A typical metallographical cross section for these tapes is shown in Fig. 8 with the various components identified. The structure of the Nb_3Sn is typical of the IGC etched tapes.

Results of measurements made from the photomicrographs are shown in Table I. This series of tapes shows considerably more dimensional variation in tape components (other than the stainless steel and copper) than has been observed on IGC tapes previously evaluated. For the most part the Nb_3Sn layer varies between 4-5 μm . However, there are areas where it is as thin as 3 μm and as thick as 7 μm . Also there is considerable variation in the remaining Nb-Ir core thickness: from 5 μm to 9 μm . Perhaps the greatest variations take place in the solder. There is variation from side to side, which has been observed before, but in this lot of tapes thickness of up to 16 μm have been seen. These thick solder layers can affect the ac loss (increasing with increasing thickness) and affect the bend radius behavior.

2.2.1.4. AC Loss Measurements

AC loss measurements are made using the short sample apparatus. All measurements are made with the copper etched from the tape but with the stainless steel remaining on the tape since in the cable the tapes will most likely be wound with the stainless steel on the high field side. All measurements are made at 4.2 K. Tests results are shown in Table II.

Results from these tests show the losses in a significant number of locations to be over 20 $\mu\text{W}/\text{cm}^2$. This is to be compared with measurements made on earlier tapes where the losses were generally below 15 $\mu\text{W}/\text{cm}^2$. Measurements made at IGC on the unclad tapes show the losses to generally 10 $\mu\text{W}/\text{cm}^2$ or less. To resolve these discrepancies, measurements were made on individual components of the tapes

Etch Finish
Reel Inside

Etch Start
Reel Outside

Figure 7. Reel loading diagram and sample location.

Figure 8. Cross section of IGC tape #78123II M49.

Table I

THICKNESS MEASUREMENTS OF IGC
SUPERCONDUCTING TAPES #78120, #78121,
#78122, and #78123

Sample No.	Thickness of Tape Components (μm)				
	Nb ₃ Sn	Nb ₃ Sn	Solder/ Cu Side	Solder/ SS Side	Unreacted Nb-Zr
78120I M1	5-6	6	4	9	5
78120I M3	4-5	5-6	7-8	3	4
78120I M5	5-6	5-6	4-6	6-7	4-5
78120I M6	4-5	4-5	7-8	3	8-9
78120II M7	5-6	4-6	12-13	6-7	4-5
78120II M9	4-6	5-7	15-16	2-4	4-5
78120II M11	4-6	4-6	9-10	3-5	4-5
78120II M12	4-5	4-5	5	5	8
78121I M13	4-5	4-5	7-8	4-5	5-7
78121I M14	4-5	4-6	7	4-5	5-7
78121III M15	4	4-5	5-6	5-6	5
78121III M17	3-4	4-5	5-7	4-5	5-6
78121III M18	4-5	4-6	13-15	6-7	6-7
78122I M19	3-5	3-4	11-12	4-5	5
78121I M21	3-4	4-5	4-5	6-8	6-7
78121I M22	4-5	3-4	5-8	4-5	7
78122I M24	4-5	4-6	3-6	3-5	5
77122I M25	3-5	3-6	10-12	4-6	4-6
78122II M27	3-5	4-5	5-7	6-7	5-6
78122II M28	3-4	3-4	12-15	4-6	6-7
78122II M30	4-5	4-6	4-5	2-3	7
78122I M31	3-4	3-4	4-5	4-5	7-8
78122I M33	3-4	4-5	4-5	7	7-8
78122I M35	3-5	3-5	5-9	6-8	7-8
781123I M37	4-5	3-4	4	5-6	8-9
78123I M38	4-5	4-5	4	5-6	8
78123I M40	4-5	5-6	5-7	5-8	5-6

Table I (Continued)

Sample No.	Thickness of Tape Components (μm)				
	Nb_3Sn	Nb_3Sn	Solder/ Cu Side	Solder/ SS Side	Unreacted Nb-1Zr Core
78123II M41	4-5	4-5	3-4	3-4	8
78123II M43	4-5	3-5	4-6	5-6	8-9
78123II M45	4	4-5	3-6	6-7	7-8
78123II M47	4-5	4-5	4-6	4-6	7-8
78123II M48	4-5	4-5	3-5	5-7	7-8
78123II M49	4-5	6-7	4	5-6	7-8
78123II M50	5-6	4-6	3-4	4-6	6

Table II
SHORT SAMPLE ac LOSS
OF IGC SUPERCONDUCTING TAPES
MEASURED AT 4.2K

Sample Number	ac Loss ($\mu\text{W}/\text{cm}^2$)	
	at 500 A/cm	at 1,000 A/cm
Reel #78120I M1	22.0	204
M3	19.3	284
M6	12.9	210
Reel #78120II M7	21.0	287
M9	23.0	291
M11	20.4	275
M12	11.2	256
Reel #78121I M13	9.5	207
M14	17.1	271
Reel #78121II M15	9.4	224
M17	17.0	234
M18	19.0	254
Reel #78122I M19	16.3	230
M21	12.0	210
M24	14.1	237
Reel #78122II M25	20.0	230
M28	13.6	211
M30	13.3	227
Reel #78123I M31	14.0	239
M33	17.0	260
M35	15.5	233
M37	13.4	230
M38	15.9	244
M40	17.0	331
Reel #78123II M41	14.1	239
M43	13.0	239
M45	17.0	239
M47	17.0	257
M48	13.6	293
M49	14.4	240
M50	14.2	264

Note: All measurements made with copper etched from tape.

and it was found that the higher losses in some tapes are due to the higher losses of the type 302 stainless steel in those particular areas. Prior to using this batch of stainless steel ac losses were measured to qualify it for use in these tapes and it was found satisfactory. From these tests it is deduced that type 302 stainless steel is only marginally stable, not consistent along its length, and an alternate stainless steel which is more stable, such as 21-6-9 (Nitronic 40), should be used.

2.2.1.5. DC Critical Current

The dc critical current is determined on a mechanical test type specimen (0.125" wide x 2" gauge length) mounted on a 2 inch diameter holder. Voltage probes are mounted 2 inches apart on the reduced area test section and the critical current defined as that current producing a 1 μ V drop between the voltage probes. On those samples evaluated the critical current at 8.0 T and 4.2 K ranged between 228 A/6 mm and 288 A/6 mm. Using the IGC curve for Normalized Critical Current vs Transverse Magnetic Field these values extrapolate to 152 A/6 mm and 192 A/6 mm at 10 T, respectively, which meets the specified 150 A/6 mm at 10 T at 4.2 K.

2.2.1.6. Minimum Bend Radius

Minimum bend radius determinations are made by mounting the reduced tensile specimen on a holder of the desired radius and measuring the dc critical current while still on the holder at 8.0 T and 4.2 K. In earlier measurements, the determination of the critical current was difficult due to the sample quenching in the 4.0 T field. At 8.0 T the samples did not quench.

Results of the bend radius test are shown in Fig. 9. In agreement with previous measurements on the SS/Cu clad tapes the geometry favors the tape with the stainless steel on the o.d. Minimum bend radius, defined on the minimum radius to which the tape can be bent with no reduction in critical current, appears to be slightly more than 1/4 inch with the stainless in the o.d. and about 0.4 inch with the copper on the o.d. For the first time it is observed that the critical current is higher when measured with the copper in the o.d. This is probably due to the state of prestress of the tape that comes about during fabrication and which affects the critical current even when the tape is bent around a 1" radius. Some increase in critical current is observed to take place up to the minimum bend radius when the tapes are bent in either direction. These effects may have been present before but were not detectable when making the previous measurements at 4.0 T.

2.2.1.7. Room Temperature Tensile Test

The procedure used for the room temperature tensile test is to stress the reduced section tensile specimen held straight in the grips of the 10,000 lb Instron Universal Testing Machine at room temperature. Then the specimen is mounted on a 1 inch radius holder and the dc critical current measured at 4.2 K in a 8.0 T field.

Results of these tests are shown in Fig. 10. There appears to be no damage to these tapes until subjected to a tensile force, at room temperature, of about 200 lbs/in width. This is about the same value as measured previously on tapes of this configuration.

Figure 9. Minimum bend radius test IGC 77121 superconducting tape, 8.0 T - 4.2 K, test section 0.125" wide.

Figure 10. Room temperature tensile test IGC 77122 superconductor tape I_c measured at 8.0 T and 4.2 K specimen gauge section 0.125" wide x 2" long.

2.2.1.8. Cladding Bond Strength

This test consists of cycling the specimen five times between room temperature and 4.2 K, separating the two layers for a distance of about 1 in, and then applying a tensile force to the two components which tends to "peel" them apart. The minimum force measured after pulling a 2 in long soldered section is recorded. Table III shows the results of tests made on these tapes. The bond strength, for the 6 mm tape, is about 1.6 lbs between the copper and Nb_3Sn and 0.2 lb (min) between the stainless steel and the Nb_3Sn . The lower strength of the stainless steel- Nb_3Sn bond is probably due to the poor bond between the stainless steel and solder rather than the Nb_3Sn -solder bond. The section with a bond strength of 0.2 lb is below the 0.4 lb required in our specification.

2.2.1.9. Summary

From the samples evaluated it appears this batch of tapes meets our specification PTP No. 21, Rev. B in all but two requirements. These are both associated with the soldering process. First, and this was noted in earlier evaluated IGC tapes of this design, the bond strength is low between the stainless steel and Nb_3Sn . Although our requirement is not met there was no visual evidence of bond separation on these tapes when bent around a 1/8 inch radius mandrel. Also other tapes of this type which were evaluated previously and did not meet the solder strength requirement performed well in the 10 m test cable. More serious, relative to the soldering, is the thick solder layers detected in some of the tapes. Specification PTP 21, Rev. B sets the maximum thicknesses of the solder layers as 0.00025 in between the Nb_3Sn and copper and 0.0005 in between the Nb_3Sn and stainless. Measurements show that solder thickness between the Nb_3Sn and stainless steel to be within specification but the solder layers between the Nb_3Sn and copper in some tapes are considerably above specification; having solder thicknesses almost as high as 0.0006 in. These large thicknesses are extremely undesirable since they can significantly increase the ac losses in the cable.

2.2.2. Airco

Tapes are being prepared at Airco, Inc., Murray Hill NJ, using the bronze process. Recently six samples 6 mm wide x 12 in long were received for evaluation. These samples are from longer sections of tapes fabricated by the bronze process in which the bronze, 30 μm thick, is applied to a 0.001" thick Nb-1Zr strip by sputtering. After heat treating to form the Nb_3Sn layer the remaining bronze is etched from the tape, a thin 1 μm layer of copper sputtered to the tape to facilitate soldering, and then 0.001" thick type 302 stainless steel is soldered to one side of the tape and a 0.002" thick copper to the other. A Sn-3.5% Ag eutectic solder is used.

2.2.2.1. Metallography

A typical microstructure of these tapes is shown in Fig. 11 and the results of measurements made from the photomicrographs shown in Table IV. In general all the layers appear fairly smooth and uniform in thickness. For the most part the Nb_3Sn layers are 4-6 μm thick with an occasional area 3 μm thick. The solder layers are 3-4 μm thick but an area 8-9 μm thick was detected.

Table III

Cladding Bond Strength
IGC Superconducting Tapes
77126.00
77127.00

Cladding Bond Strength (lbs/6 mm width)

Tape No.	Cu/Nb ₃ Sn	Type 302 SS/Nb ₃ Sn
78121I M13	1.7	0.2
78123II M41	1.6	0.2

Solder: Sn-3.5 Ag eutectic

Figure 11. Cross section of Airco tape. Run 1 Reel 6.

Table IV

THICKNESS MEASUREMENTS OF AIRCO SUPERCONDUCTING TAPES

Sample No.	Thickness of Tape Components (μm)				
	Nb_3Sn	Nb_3Sn	Solder Cu Side	Solder SS Side	Nb-1Zr Core
Run 1 Reel 3	4	3-4	4	3-4	21-22
Run 1 Reel 6	5-6	4-6	4-6	4-5	18-20
Run 2 Reel 3	5-6	4-6	4-5	4-5	17-19
Run 2 Reel 4	4-5	4-5	4-5	$4-4\frac{1}{2}$	19-20
Run 3 Reel 2	4-5	4-5	4	4	20-21
Run 3 Reel 5	3-4	4-5	4-5	8-9	21-22

Some small void areas and inclusions were detected in the solder layers.

2.2.2.2. AC Losses

AC loss measurements are made using the short sample apparatus. Test results are shown in Table V. Losses of these tapes in the as-received condition is about $20 \mu\text{W}/\text{cm}^2$. With the stainless steel present the losses are between 5.4 and $9.4 \mu\text{W}/\text{cm}^2$. Measurements made on only the bare tape are within this same range and these unexpected high results for the bare Nb_3Sn tape are attributed to damage which may occur to the samples during the removal of the stainless steel. Measurement of a sample of the bare tape, i.e., prior to the soldering process, from a similarly processed material showed the losses to be $6.6 \mu\text{W}/\text{cm}^2$.

2.2.2.3. Summary

On this set of samples only the above tests were performed because the mechanical tests are more involved, and further, these were only preliminary samples of a similar large lot of material forthcoming. A complete evaluation will be made on this lot of material which will probably be used in a 10 m cable test.

From the results obtained on this set of samples it appears that material made by the bronze process has low ac losses as expected and at Airco the capability exists for making tape by the bronze process and to perform a reasonably good job of soldering it to other materials.

2.3. Contract Management

Development work on the contracts with Airco, Inc., Murray Hill, NJ and Intermagnetics General Corp., (IGC), Guilderland, NY to develop the capability to produce 1000 ft lengths of superconducting tape having the electrical and mechanical properties required for the BNL Power Transmission Line program is continuing. Contract development effort at these two companies is scheduled to end Dec. 30, 1978.

In view of the difficulty experienced by Airco in extruding and rolling a large extrusion billet, a support effort was undertaken at BNL to assemble three billets of medium size and have them extruded. Results of this effort are reported in Section 2.1. Airco has assumed the task of rolling the extrusions to a thickness of about 0.005" and processing the material into clad tapes. Currently the extrusions are being rolled. The sputtering line at Airco is fully operational and is routinely being used to apply thin layers of copper on the Nb_3Sn substrate and stainless steel. Solder joints made with materials having a sputtered layer of copper on them have exhibited very strong joints. Airco has also developed a capability of soldering various materials to Nb_3Sn . Results of measurements made on this material are reported in a previous section.

The procedures selected by IGC to produce 1000 ft lengths of tape is to prepare the Nb_3Sn superconductor substrate by the reaction of Nb-1%Zr sheet with liquid tin, remove the high ac loss surface material with an acid etch, re-tin the surface, and then solder the copper stabilizer and stainless strengthener to the tape. Using the procedures developed under the contract, single lengths up to 860 ft having reasonable low loss have been made and during this report period IGC delivered

Table V

SHORT SAMPLE ac LOSS OF AIRCO SUPERCONDUCTING
TAPE SAMPLES CLAD WITH TYPE 302SS AND COPPER

Sample No.	ac loss ($\mu\text{W}/\text{cm}^2$) at 500 A/cm	at 1,000 A/cm	Comment on Measurement
Run 1 Reel 3	5.4	191	Nb ₃ Sn + SS
Run 1 Reel 3	20.0	243	Nb ₃ Sn + SS + Cu
Run 1 Reel 3	6.7	185	Nb ₃ Sn + Solder
Run 2 Reel 6	5.5	208	Nb ₃ Sn + SS
Run 2 Reel 2	7.4	217	Nb ₃ Sn + SS
Run 2 Reel 4	9.4	239	Nb ₃ Sn + SS
Run 3 Reel 2	6.8	195	Nb ₃ Sn + SS
Run 3 Reel 5	8.3	208	Nb ₃ Sn + SS
Run 3 Reel 5	9.7	195	Nb ₃ Sn Only

5680 ft of 6 mm wide superconducting tape with stainless steel soldered to one side and copper soldered to the other. This tape is described in Section 2.2. These tapes meet our present specification except for portions of tape in which the solder layer between the Nb₃Sn and copper exceeded the specified maximum thickness and the strength of the solder bond between the Nb₃Sn and stainless steel was below the specified minimum.

Except for one remaining task all development work under the present contract at IGC is complete. The remaining task is to demonstrate the laminating and slitting process can yield three 6 mm wide tapes instead of two. Modification of the equipments is complete and test runs have shown that 3 strips can be produced. A final demonstration run will be made on tapes clad with type 21-6-9 stainless steel rather than the type 302 used in the past.

Progress reports for Airco and IGC are given in Appendices I and II.

2.4. Elastic Modulus of Polycrystalline Nb₃Sn Between 4.2 and 300 K

In the range of temperature between 4.2 and 300 K, most metals exhibit only small changes in Young's modulus. For example, the modulus of pure niobium is ~10% higher at 4.2 K than at room temperature. Superconducting compounds of A15 structure, however, behave quite differently and can display large decreases of modulus when cooled to low temperatures.¹ Ultrasonic measurements on Nb₃Sn single crystals indicate that the modulus actually vanishes in the [100] direction* at the martensitic transformation (~50 K)² and decreases to a very small value in non-transforming crystals.³

For large scale applications the behavior of polycrystalline Nb₃Sn is of interest. Measurements on polycrystals, however, are scarce and inconsistent with the single crystal data. Table VI lists room temperature values of Young's modulus obtained from ultrasonic velocity measurements on single crystals and polycrystals. For single crystals Young's modulus was calculated by taking the mean of Voigt and Reuss averages.⁴ For ultrasonic velocity measurements in polycrystals Young's modulus, E, was taken to be

$$E = \rho V_L^2 \quad (1)$$

where ρ is the density of Nb₃Sn [taken to be 8.83 g cm⁻³]³ and V_L is the longitudinal propagation velocity. Ultrasonic velocity measurements on polycrystals yield much larger values of E (up to 23.6 x 10¹¹ dynes/cm²) than polycrystalline averages of single crystal data (13.5 x 10¹¹ dynes/cm²) (Table VI). The high values deduced from Eq. (1) and polycrystalline data are probably associated with large attenuation in the polycrystals. The temperature dependence obtained from ultrasonic velocity measurements in polycrystals^{1,5} is also very different

¹L. R. Testardi, Physical Acoustics X, 193 (1973).

²W. Rehwald, Phys. Letts. A 27, 287 (1968).

³K. R. Keller and J. J. Hanak, Phys. Rev. 154, 268 (1967).

⁴For a discussion of Voigt and Reuss averages, see for instance H. B. Huntington, The Elastic Constants of Crystals in Solid State Physics 7, 213 (1958).

⁵L. R. Testardi, R. R. Soden, E. S. Greiner, J. H. Wernick and V. G. Chirba, Phys. Rev. 154, 399 (1967).

* i.e., along one side of the crystal.

Table VI
ROOM TEMPERATURE VALUES
OF YOUNG'S MODULUS FOR POLYCRYSTALLINE Nb₃Sn

Modulus (10 ¹¹ dyns/cm ²)	Remarks	Reference
13.5	Single crystal measurement. Mean of Voigt and Reuss averages. Non-transforming crystal.	Keller and Hanak
13.5	Same as above. Transform- ing crystal.	Rehwal
23.6	Ultrasonic velocity measure- ment on polycrystalline material.	Testardi et al.
17.9	Same as above; "critical angle technique".	C. C. Old ⁷ and J. P. Charlesworth

from that obtained by averaging single crystal data. In Fig. 12 the dashed line gives E versus temperature obtained from Eq. (1) using Testardi's data^{1,5} and the solid line is the mean of Voigt and Reuss averages calculated from data in Ref. 3. The dashed line was renormalized to agree with the single crystal data at 300 K.

In order to determine whether or not polycrystals show large variations of Young's modulus with temperature as shown by the solid line in Fig. 12 we have measured E in the range of 4.2 to 300 K. The technique is based on a bimetallic strip made of pure niobium and Nb_3Sn . Initially a Nb_3Sn/Nb tape is made by the solid-state diffusion method.⁶ Before reaction the tape consists of a niobium foil ($\sim 25 \mu m$ thick) sandwiched between two bronze layers (each $\sim 50 \mu m$ thick). The tape is then reacted at $725^\circ C$ for 50 h resulting in a Nb_3Sn layer thickness of $4.7 \mu m$ and $12 \mu m$ layer of unreacted niobium. The bronze layers are then etched away at room temperature in nitric acid. One of the two Nb_3Sn layers is then removed with an $HF/HNO_3/H_2O$ solution. Removal of one of the two Nb_3Sn layers causes the tape to curl with the niobium on the inside diameter, indicating that the Nb_3Sn was initially in compression. This compression is associated with permanent deformation of the unreacted niobium layer during cool down from $725^\circ C$ to room temperature in the presence of the bronze. The expansion coefficients of niobium and Nb_3Sn are well matched. This was verified by annealing tapes (with the two Nb_3Sn layers but without bronze) at $725^\circ C$ for 1/2 h and cooling them again to room temperature. The annealed tapes showed no residual stress at room temperature (i.e., no curling). Annealed tapes also showed no curling when cooled to liquid nitrogen temperature.

Because of the well matched expansion coefficients of Nb and Nb_3Sn , prestressed bimetallic strips made of these two materials can be used to determine the temperature dependence of the ratio E_{Nb}/E_{Nb_3Sn} as a function of temperature. The equation relating the ratio of the moduli to changes in radii of curvature is⁶:

$$\frac{\tilde{K}'}{\tilde{K}} = f \frac{R'}{R} \quad (1)$$

where

$$f = \frac{a}{2} \left\{ 1 + 1 - \frac{4L^2(R/R')^2}{\tilde{K}^2 a^2} \right\}^{1/2}$$

$$\text{with } a = \left\{ \frac{(1 + \tilde{K})(L^2 + \tilde{K}) + 3\tilde{K}(1 + L)^2 - (4 + 6L + 4L^2)\tilde{K}(R/R')}{\tilde{K}^2} \right\} \quad (2)$$

where the prime and unprimed values refer to measurements at low and room temperature, respectively. The symbols used in (2) are defined in Table VII.

⁶D. O. Welch, private communication.

⁷C. G. Old and J. P. Charlesworth, Report AERE-R7903 (Dec. 1974), Harwell Report.

Figure 12. Temperature dependence of Young's modulus for Nb_3Sn . Points are experimental values. The solid line is a polycrystalline average of single crystal data (Keller and Hanak). The dashed line was obtained from longitudinal velocity measurements on a polycrystal (Testardi, et al.). All values are normalized to give a room temperature value of 13.5×10^{11} dynes/cm².

Table VII

Definition of Symbols Used in Equation (2)

<u>Symbol</u>	<u>Definition</u>
ℓ	Thickness of Nb_3Sn
w	Thickness of Nb
ν	Poisson ratio
ρ	Radius of curvature
E	Young's modulus
$\tilde{K}^*$	$\tilde{K} = \left(\frac{E_0}{E_1}\right) \left(\frac{w}{\ell}\right) \left(\frac{1-\nu_1}{1-\nu_0}\right)$
L	$L = \ell/w$
R	$R = \rho/w$

*Subscripts 1 refers to Nb_3Sn , 0 refers to Nb.

Equation (2) neglects temperature variations of Poisson ratios.

• Results were obtained by observing the radius of curvature of the bimetallic strip between 4.2 and 300 K and calculating the modulus change from Eq. (2). For simplicity the Poisson ratios of Nb and Nb_3Sn were taken to be equal and temperature independent. Also the value of E_{Nb} was taken to be temperature independent and equal to 11.0×10^{11} dynes/cm². The value $E_{\text{Nb}_3\text{Sn}}$ (300 K) was taken to be 13.5×10^{11} dynes/cm². Experimental results shown in Fig. 12 agree well with the polycrystalline average indicating that large changes in Young's modulus indeed occur when Nb_3Sn is cooled to low temperatures. Note that the modulus softening appears to stop at or near the critical temperature of Nb_3Sn (~ 18 K).

2.5. 10 m Cable Test

In a recent review⁸ plans have been described for the completion of the 100 m outdoor test facility and for tests on 138 kV single and three phase cables. The ratings correspond, on a three phase basis, to a circuit capacity of 1000 MVA. In order to determine the final design of this cable, a number of shorter 10 m cables of similar rating are being made and tested. The first major test of one of these has been completed and is described here. It is concerned with the current carrying components of the cable.

The design of the cable has been discussed in several publications.^{9,10,11} The conductor configuration is illustrated in Fig. 13. Flexibility and the ability to take up longitudinal contraction are provided by helical tape construction, but double layers of opposite helicity are required in order to minimize the effects of axial magnetic flux. Layers of high purity aluminum back up the Nb_3Sn in order to share current during transient overloads or quenches. Starting with the commercially fabricated flexible bronze core, the conductors and dielectric were wound using a modified cable winding machine. The resulting 10.6 m long cable is installed in a horizontal dewar with its ends fixed to room temperature supports. The flow of supercritical helium refrigerant is through the core. The cable is short circuited at one end as it is intended for current tests only.

The test period lasted two months. During the first few weeks minor refrigerator problems necessitated cycling to room temperature three times. This produced no adverse effects. During the final run the cable was cooled continuously for a period of two weeks.

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⁸E. B. Forsyth, Superconducting Power Transmission - Progress at Brookhaven and the Prospects for this Technology, presented at ICEC 7, London, England, July 1978 (to be published).

⁹G. H. Morgan and E. B. Forsyth, Advances in Cryogenic Engineering 22, 434 (1976).

¹⁰M. Garber, J.F. Bussiere and G. H. Morgan, Magnetism and Magnetic Materials - 1976 (Joint MMM - Intermag Conf., Pittsburgh) AIP Conf. Proc. # 34, p. 84, 1976.

¹¹M. Garber, T. Barber and G. Morgan, IEEE Trans. on Magnetics MAG-13, 1977.

Figure 13. Schematic of cable construction. c, flexible bronze core; a, aluminum stabilizer layers; s, superconductor layers; d, plastic tape insulation; d', dielectric screen - not included in current test cable; k, compression layer - replaced by nylon lacing in this work.

2.5.1. Experimental Description

2.5.1.1. Materials

The superconducting tape was obtained from IGC.¹² The Nb₃Sn tape is clad with 25 μ m thick stainless steel on one surface and 50 μ m thick copper on the other. The stainless steel is a variety of type 302 verified to have low magnetic loss at helium temperatures. The copper has a residual resistance ratio, rrr, of 150. The cladding bond is a tin alloy solder which is non-superconducting above 5 K. The cable is wound with the stainless steel surfaces of the tapes facing the high field side. The tapes are 6 mm wide. As they were slit from a wider starting tape, the edges are stripped of Nb₃Sn. This leads to a type of ac loss which is discussed below.

The aluminum tapes are 200 μ m thick and 6 mm wide. The measured rrr (prior to winding) was 2500, so the tapes are approximately one skin depth thick. The tapes are coated with a thin flash of copper to facilitate end connections. Polypropylene tape, 90 μ m thick by 22 mm wide is used for the insulation. The expansion coefficient is such that the radial contraction of the wrapped insulation matches that of the metal layers. The bronze core is of double helix form in order to be mechanically stable. It is constructed of 1 mm thick by 11 mm wide tapes. The cable is held together radially by nylon lacing rather than by the compression layer shown in Fig. 13.

Figure 14 is a photograph of the cable during winding. The winding machine is at the far end. Earlier reports have shown closeups of this machine in action (see Ref. 11, for example). The cable winding and instrumentation were done in a basement room. In moving the cable from this location to the ground floor laboratory where the dewar and refrigerator were located it was necessary to bend the cable around corners. The cable was thus flexed through arcs roughly 30 ft in diameter. The superconducting iron-core transformer used to supply current to the cable is shown connected to the completed cable in Fig. 15. This photo shows the assembly prior to final insertion into the dewar. The transformer remains located in the cold box.

2.5.1.2. Dimensions

The principal dimensions are summarized in Table VIII. The lay angles of the superconductor and aluminum layers were chosen as close to $\pm 45^\circ$ as feasible, this angle leads to nearly complete cancellation of axial flux and, therefore, to small electromagnetic interaction with dewar walls or other external structures. The butt spaces between tapes are 0.3 mm wide. This would permit a 50 mm dia cable to be reeled on a 2 m drum without edgewise overlap of tapes.

2.5.1.3. Connections

A brief description of the manner in which terminations were made is given here. At the input end, 10 mm wide superconducting tapes are indium soldered to each superconducting layer of the cable. These links, lying parallel to the cable axis, are soldered to the leads

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¹²P. H. Brisbin, K. W. Pickard and P. S. Swartz, Low AC Loss Nb₃Sn Tape for Transmission Line Applications, presented at Applied Superconductivity Conf., Pittsburgh, Pa., Sept. 25 - 28, 1978.

Figure 14. Cable winding set-up. The winding machine has since been moved to the "cable factory" in Bldg. 820.

Figure 15. Cable with superconducting transformer being inserted into cryostat through far end cold box. The far end expansion engine is at top left.

Table VIII

10.6 m CABLE PARAMETERS

<u>CONDUCTORS: DOUBLE HELIX Nb₃Sn & Al</u>			
	DIAMETER	LAY ANGLE	NO. OF 6 mm TAPES IN EACH HELIX
INNER	24.5 mm	43°	9
OUTER	30.4 mm	44°	11

$$X_L(60 \text{ Hz}): 1.6 \times 10^{-5} \Omega/\text{m} \quad (L = 4.3 \times 10^{-8} \text{ H/m})$$

INSULATION: 3 mm, POLYPROPYLENE

CORE: BRONZE, DOUBLE HELIX

of the low temperature transformer. Copper strips are used to link the aluminum layers to the input transformer. At the short circuit end similar links are used for the superconducting layers. These are connected to a thin copper ring. The aluminum layers are extended and joined beyond the superconducting ones. The space between these separate joints was made in order to locate there a current monitor for the inner aluminum layers.

Photographs of the aluminum joint during its construction are shown in Fig. 16. The upper photograph shows the copper cylinder or ring which has been soldered to the underlying aluminum layers. To its right are the 1/2" wide Nb_3Sn tapes which are soldered to the superconducting helices and which form the superconducting short circuit. Returning to the copper ring at the left, one can see the toroidal current monitor through the slot provided to bring out leads. Also one can see the set screws used to fix the end of the conductor assembly to the bronze core, which extends to the left.

The lower photograph shows the copper links which join the copper cylinder to the aluminum helical layers (not seen, to the right).

2.5.1.4. Instrumentation

AC losses are measured by our electronic wattmeter. The total cable loss is determined by means of a voltage probe across the input. From this is subtracted the loss read by a probe across the short circuit joint. This correction is about 16% at 4000 A. In order to monitor the superconductive material loss, as opposed to the cable loss, voltage pick-ups running along the surface of individual tapes of the inner conductor are installed. Total cable current and inner aluminum layer current are measured by means of Rogowski coils. The voltage waveforms in Sect. 2.5.2.2. & 2.5.2.4. are compensated, i.e., the inductive component has been subtracted. The derivative signal from the Rogowski coil is used to do this. Care was taken to ensure that the phase of this signal is correct to better than 10^{-4} rad.

In order to study the response of the cable to thermal quenches a surface heater is wrapped over the inner conductor surface, under the dielectric wrap. It is made of aluminized mylar, low temperature resistivity 0.3 ohm/square ($\rho_{rr} = 3$). The heater length is 285 mm. It is located approximately 3 m from the shorted end. Usually, 70 W pulses were used to produce the local quenches.

Current to the cable is supplied through the transformer located in the cold box at one end of the dewar. This transformer has an iron core and Nb_3Sn tape windings. The step-down ratio is 82:1. CW excitation was available to 7000 A. Higher currents are obtained by resonant discharge of a 2000 μF capacitor bank. Q is about 50 up to 15 kA and falls thereafter due to transformer losses. A complete description of the transformer was given in PTP 69, 12-27, 1976, p. 35 et seq.

Figure 16. Short circuit assembly. Top-copper ring connecting inner and outer aluminum stabilizer helices. Current sensor is located inside this copper shell. Superconducting helix joints are to the right. Note set screws in copper shell which pin down the end of the cable to the core. Bottom - complete joint with copper strips to the aluminum helices.

Temperatures are measured by means of calibrated carbon resistors located every 2 m both in the core and outside the cable.

2.5.1.5. Refrigeration

The supercritical helium pressure was generally 12 atm. While the inside and outside of the cable are in pressure equilibrium, the flow of refrigerant is forced to take place through the core, entering at the short circuited end and leaving at the current input (transformer) end.

Figure 17 is a line diagram of the flow path of the helium. After leaving the cable the helium goes through the driver transformer and then to the far end expander. The cooled, low pressure helium then returns through the annulus between a 4" pipe and the 2" high pressure pipe inside which the cable is located. The available refrigeration was about 0.4 gm/sec corresponding to a flow velocity of about 1 cm/sec at 7.5 K. This was much lower than expected and provided an unplanned worst-case test, in effect.

2.5.2. Results

2.5.2.1. AC Losses

In Fig. 18 the sum of the inner and outer conductor losses per meter is shown as a function of temperature for $I = 3850$ A (500 A/cm) rms, $f = 60$ Hz. The peak near 9 K has been discussed previously.^{10,13} It is due to components of the current which circulate around the individual tapes and pass through the niobium substrate. As present plans call for line operation at approximately 6 to 8 K, the effect of this peak will be small.

A similar observation of this kind was found previously in a 1-m test - see PTP 69 (12-27/76)p. 75. However, in the present cable the loss at temperatures below the peak is greater. The loss at 7 K is approximately 0.2 W/m. This is about three times greater than expected from previous short cable experiments and is a matter of some concern. The observed current dependence up to 6000 A is $I^{2.2}$. Eddy current losses in normal metal parts of the cable and dewar appear to be ruled out by the frequency dependence, $f^{0.7}$, up to 110 Hz. In addition, the axial flux, measured by a coil wound around the cable, is too small to produce appreciable loss in surrounding pipes. Current flow in the aluminum layers is too small to account for the loss. The spiral surface probes show that losses in the Nb₃Sn tapes are about the same as for the as-received material. Thus the tapes were not degraded by the cabling process - at least not those monitored (5 m length of tape). The observed loss voltage waveform has a large sinusoidal component which together with the preceding facts indicates an ohmic loss mechanism. G. Morgan has suggested that current flow between the superconducting helices, passing through a layer of stainless steel may be the source of the observed dissipation. Preliminary computer modeling supports this possibility and indicates that replacing the stainless by copper would greatly reduce the loss.

Figure 19 shows the results of the computer calculation, in which it is assumed that the stainless steel layer acts as a cylindrical shell between the two superconducting helices of the inner conductor. The fraction of stainless

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¹³J. F. Bussiere, V. Kovachev, C. Klamut and M. Suenaga, Advances in Cryo. Eng., 24, 449 (1978).

Figure 17. Refrigerant flow diagram. The cable is located inside a 2" dia. pipe which contains the outer, static, supercritical helium. Flow is through the core. Return, low pressure helium is through the annulus between the 2" pipe and a surrounding 4" dia. pipe.

Figure 18. Ac loss, inner and outer conductors. $I = 3850$ A (500 A/cm) rms, $f = 60$ Hz. Horizontal bars represent temperature extremes along the cable.

Figure 19. Model calculations for loss in stainless steel layer located between the two superconducting helices. Numbers refer to fraction of surface making good contact with adjacent layers.

steel surface making good contact with the adjacent helical layer is taken as a variable, as indicated by the numbers on the different computer curves. In regions corresponding to frequency variation ~ 0.7 the losses are higher than the experimental ones of Fig. 18. However, it is not unreasonable to consider that this effect occurs only in parts of the cable. Previous 1 m model test results¹¹ using copper clad tapes gave much lower loss. This is consistent with the computer calculation results, and suggests that reversing the outer most superconducting layers should reduce the effect. A cable to test this is being made.

2.5.2.2. Fault Currents

At high currents the superconductive hysteretic loss waveform becomes much larger than that due to the previously discussed effect. The onset of the resistive transition can then be readily detected by deviations in the voltage waveforms. Figure 20 is an example of this behavior. Inspection of the spiral surface probes shows that not all parts of the cables are resistive at this point. It can be seen that for this single cycle of overload, the superconducting waveform is fully recovered after one cycle. Values of the resistive onset at $T = 5.3, 6.7,$ and 8.3 K were 22 kA (2850 A/cm) rms, 20.8 kA (2700 A/cm) and 19.3 kA (2500 A/cm) rms respectively. These values are in line with previous data for lab samples.¹⁴ As was expected, placing the copper cladding entirely on the low field side of the tapes provides adequate flux jump stability.

The cable was pulsed to greater currents than the above. The maximum 1/2 cycle current applied was 27.7 kA (3600 A/cm) rms. Recovery again occurred in one cycle, when the current had fallen to 8.4 kA.

2.5.2.3. Current in Stabilizer Layers

The normal state resistance of the Nb_3Sn tapes when quenched by a sufficiently large current is much larger than that of the aluminum - 40 times at 8 K. In this state current flow is mostly through the aluminum. This is observed in measurements at elevated temperatures (~ 25 K) and serves to show that the coil sensing the aluminum current works properly. In the superconducting region, current distribution among the various layers can be calculated by means of the model equations described in references 9 and 10. It is necessary to assume an effective ohmic resistance for the superconductive loss, and to use a computer to solve for the eight complex currents. The results of the calculation can be summarized as follows:

1. At low currents the fraction carried by the aluminum is small, about 1% of the total, and shifted by $\sim 180^\circ$ - i.e., there is a return flow.
2. As the superconductive loss increases, current transfers to the aluminum, amounting to approximately 25% of the total when the layer resistances are equal.

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¹⁴M. Garber, S. Shen, J. F. Bussiere and G. H. Morgan, IEEE Trans. on Magnetics MAG-11, 373 (1974).

Figure 20. Pulsed fault current and cable loss voltage. Rms values of the first three 1/2 cycles of current are 21.9 kA (2840 A/cm), 17.1 kA (2230 A/cm) and 10.0 kA (1300 A/cm). $T = 6.7K$.

3. The phase of the aluminum current shifts from 180° to 0° as it increases in magnitude.

Computer printouts illustrating these calculations are shown in Tables IX and X. In the first of these the Nb_3Sn layers are superconductive - a fictitious resistivity of 3×10^{-12} ohm-cm is assumed. The reference phase is that of the total current or "I-LINE." The current in the inner two aluminum helices is the vector sum of the first two current entries in the table. The phase of this total aluminum current is about 140° . The magnitude, is about 42 A rms - this is too small to account for the loss discussed previously. Table X shows the results when the superconductive layer resistance has increased to 10 x that of the aluminum. Figure 21 summarizes the calculations over the entire range of resistance. Magnetoresistance of the aluminum was not taken into account in this calculation.

These predictions are in reasonable accord with experiment, the main disagreement being that at low cable current the fraction carried by the aluminum is less than calculated by a factor of 2 to 3. Figure 22 shows cable and aluminum current waveforms for an initial current pulse of 21.2 kA rms. The first aluminum peak is 196 A rms. The phase angle behavior described above is apparent. A plot of the aluminum current magnitude and phase vs cable current is shown in Fig. 23. The high current points are derived from transient data such as Fig. 22, while the lower points are from cw measurements.

2.5.2.4. Recovery from Local Quenches

Cryogenic stability of the cable was touched on in early work.¹⁵ This refers to the property by which a local normal region of the cable does not grow in extent while current is being carried. A similar though not identical property is the response of a region which is thermally quenched while the cable is carrying current. A complete investigation of this phenomenon has not been carried out but rather we have demonstrated that the cable will perform adequately in this respect. The method is described by reference to Fig. 24. The upper trace shows the loss voltage from a 1 m long surface probe located in the heated region, while the lower trace shows the current detected in the aluminum monitor. Destruction of superconductivity and transfer of current to the underlying aluminum occurred 1.31 sec after the heater was turned on. With cable current left on, the heater was turned off at 1.61 sec and the normal zone collapsed within 0.07 sec. The maximum current transferred to the aluminum was 200 A rms, indicating that the normal region remained small in extent throughout. The effect is dependent on the length of time the 70 W heater remains on after quench begins. At 2300 A, recovery occurred for the longest such time tried, 2 sec, but the highest cable current tried, 3850 A (500 A/cm) rms, recovery occurred for heater on times up to about 0.14 sec. It should be noted that in view of the low helium flow velocity in these experiments, heat transfer between the inner conductor and the refrigerant was an order of magnitude less than it would be in a properly refrigerated system.

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¹⁵E. B. Forsyth, M. Garber, J. Jensen, G. Morgan, R. Britton, J. Powell, J. Blewett, D. Gurinsky and J. Hendrie, Proc. of the 1972 Applied Superconductivity Conf., (IEEE, New York, 1972), pub. No. 72 (H0682-5-TABSC), p. 202.

Table IX

Model Calculation for Nb₃Sn Layers Fully Superconducting

PROBLEM 1	SIGMA= 500.0	F= 60.0	PRINT= 1	COORD= 1	
D (CM)	PHI	PITCH(CM)	T (CM)	OHM-CM	R(OHM/M)
2.330E+00	41.300	8.332E+00	2.00E-02	1.10E-09	1.33E-06
2.370E+00	-41.600	-8.386E+00	2.00E-02	1.10E-09	1.32E-06
2.420E+00	42.600	8.268E+00	5.00E-03	3.00E-12	1.46E-08
2.440E+00	-42.800	-8.278E+00	5.00E-03	3.00E-12	1.45E-08
3.048E+00	-43.500	-1.009E+01	5.00E-03	3.00E-12	1.19E-08
3.066E+00	44.100	9.940E+00	5.00E-03	3.00E-12	1.21E-08
3.100E+00	-45.500	-9.570E+00	2.00E-02	1.10E-09	1.15E-06
3.155E+00	46.000	9.572E+00	2.00E-02	1.10E-09	1.15E-06
I-MAG.	I-PHASE	LOSS (W/M)	ZR (OHM/M)	ZX (OHM/M)	
4.011E+00	-49.01	2.142E-05	-1.24E-02	1.08E-02	
4.122E+01	-151.64	2.245E-03	-7.64E-04	-1.41E-03	
1.857E+03	.11	5.022E-02	1.02E-07	3.56E-05	
2.010E+03	.54	5.871E-02	3.43E-07	3.29E-05	
2.017E+03	-179.49	4.845E-02	-4.75E-09	-6.30E-09	
1.833E+03	-179.86	4.057E-02	-5.19E-09	-6.96E-09	
2.525E+01	43.96	7.330E-04	6.21E-07	1.04E-07	
4.997E+00	106.26	2.871E-05	1.93E-06	-2.54E-06	
I-LINE = 3.833E+03			V, INNER = 6.616E-02 /	89.946	
			V, OUTER = 1.591E-05 /	53.460	
			V, CABLE = 5.244E-05 /	.066	

Table X

Model Calculation for Nb₃Sn Layer Resistance 10 x Aluminum Resistance

PROBLEM 3	SIGMA= 500.0	F= 60.0	PRINT= 1	COORD= 1	
D (CM)	PHI	PITCH(CM)	T (CM)	OHM-CM	R (OHM/M)
2.330E+00	41.300	8.332E+00	2.00E-02	1.10E-09	1.33E-06
2.370E+00	-41.600	-8.386E+00	2.00E-02	1.10E-09	1.32E-06
2.420E+00	42.600	8.268E+00	5.00E-03	1.10E-08	5.34E-05
2.440E+00	-42.800	-8.278E+00	5.00E-03	1.10E-08	5.33E-05
3.048E+00	-43.500	-1.009E+01	5.00E-03	1.10E-08	4.37E-05
3.066E+00	44.100	9.940E+00	5.00E-03	1.10E-08	4.43E-05
3.100E+00	-45.500	-9.570E+00	2.00E-02	1.10E-09	1.15E-06
3.155E+00	46.000	9.572E+00	2.00E-02	1.10E-09	1.15E-06
I-MAG.	I-PHASE	LOSS (W/M)	ZR (OHM/M)	ZX (OHM/M)	
1.744E+03	-5.75	4.048E+00	-2.52E-06	4.66E-05	
1.981E+03	-4.51	5.186E+00	-1.33E-06	4.11E-05	
1.848E+02	70.80	1.825E+00	4.22E-04	1.25E-04	
1.677E+02	68.42	1.500E+00	4.59E-04	1.57E-04	
1.296E+02	-117.12	7.337E-01	6.84E-06	-1.50E-05	
1.760E+02	-110.27	1.371E+00	3.68E-06	-1.16E-05	
1.988E+03	176.62	4.541E+00	1.08E-06	1.36E-08	
1.736E+03	174.61	3.466E+00	1.23E-06	5.87E-08	
I-LINE = 3.833E+03					
V, INNER = 8.138E-02 / 87.341					
V, OUTER = 2.142E-03 / 177.337					
V, CABLE = 5.915E-03 / .081					

Figure 21. Model calculation of steady state current in aluminum layers as function of resistance of the Nb_3Sn layers.

Figure 22. Total cable current and aluminum stabilizer current. Current pulse (first to peak) is approximately same as Figure 20. Aluminum current values are given in text. Note phase shift to 180° at low currents. $T = 8.5K$.

Figure 23. Magnitude (0) and phase (1) of current in aluminum stabilizer layers vs total cable current. Vertical bars represent uncertainty in phase angle determination. $T = 8K$.

Figure 24. Quench initiation and recovery experiment. $I = 2.3 \text{ kA}$ (300 A/cm) rms. $T = 8K$. Upper curve: superconductor loss voltage for heated region; the cursor indicates the point at which this region of the cable goes normal. Lower curve: current in aluminum stabilizer. The sinusoidally varying signals in the normal region have a patterned appearance due to the choice of sampling rate and scale.

2.5.2.5. Summary

The tests described have been made on a realistically constructed flexible cable installed in a horizontal dewar with ends fixed to room temperature supports. Performance as regards fault current and quench recovery is very good. The cable remains entirely superconducting for single cycle pulses of current up to 19 kA (2500 A/cm) rms. It has been run continuously for periods up to an hour at 3850 A (500 A/cm), behaving quite stably. At this current, when it is thermally quenched it recovers to the superconducting state without disconnecting the current. The cable has been pulsed or quenched in the above ways more than 100 times without ill effect. The ac loss is less satisfactory, being two or three times larger than expected. The extra loss is resistive in nature and does not seem to be due to the superconductor, which appears to perform well. The effect was not observed in previous 1 m models and can, we believe, be eliminated.

The 10.6 m cable has revealed no obstacles for the design of the future 100 m prototype and is an encouraging precursor to it.

3. Cable Insulation Development

3.1. Materials

3.1.1. High Modulus Tape Development

A review of the "Ultrahigh Modulus Dielectric Tape Development Program" performed by Battelle Columbus Laboratory (BCL), was held June 28, 1978 at Brookhaven. This work was performed under BNL contract over a two-year period. Battelle personnel reported that they had successfully demonstrated the technical feasibility of fabricating a highly-oriented, ultrahigh modulus (UHM), single layer tape using a newly developed hydrostatic extrusion process.

Sample tapes were one inch wide by 0.005 inches thick and several feet long. Tensile moduli were greater than 1×10^6 psi, the BNL modulus requirement. However, additional engineering work remains in two areas: present tapes are uniaxially oriented and have a tendency to fibrillate along the tape machine direction. Also, present production rates of a few mm per min are too slow to be economically attractive.

Brookhaven National Laboratory's recommendations for the next phase of the work were that the major BCL engineering emphasis should be directed towards investigating those scale-up techniques necessary to appreciably increase the output rate of this extrusion system. Also, a biaxial component of orientation should be introduced to minimize the present tendency to fibrillate.

As a second task under this contract, BCL was asked to investigate possible methods that could be used to render the UHM tapes permeable to oil for use in the ambient temperature cable concept now under study at BNL. The three major methods suggested by this study are:

1. Embossing of the tape surfaces with an irregular pattern.
2. Etching of a soluble filler to produce sufficient pores to allow adequate oil permeation.
3. Producing a nonwoven tape from high modulus, highly oriented fibers.

3.1.2. Radial Pressure Measurements

In our previous semiannual report the first portion of a series of radial pressure measurements was reported. Strip pull-out forces were measured for five tape materials. Steel strips were inserted into "pads" of each material at five layer intervals. Strips then were covered by 5, 10, 15, 20, 25 and 30 layers of tape. The force required to just move each pull-out strip was measured on the Instron Tensile Testing Machine and reported.

During the present period the tape-to-steel-strip coefficient of friction was determined for each material and radial pressure values were calculated. The radial pressure versus number of tape layers above each strip are plotted in Fig. 25 for the five materials. It may be noted from the figure that, in general, the radial pressure exhibits an inverse relationship to "n", the measure of the anisotropy of the tape moduli. One set of measurements, however,

Figure 25. Radial pressure vs number of tape turns overlying radius of measurement for several materials.

do not follow this pattern. The radial pressures of kraft paper ($n = 1000$) are higher than those of embossed polycarbonate where $n = 607$. Both tapes are $178\text{ }\mu\text{m}$ thick. One possible explanation of this effect may be that the embossed polycarbonate (original thickness $100\text{ }\mu\text{m}$) behaves differently under compression than an unembossed tape with a smooth surface.

3.1.3. Effect of Tape Thickness on Radial Pressure

The dependency of radial pressure on tape thickness was reported several years ago by the Pirelli Co. of Milan, Italy.¹⁶ The simplified version of the equation for radial pressure, ϕ_r , that is applicable to tapes of high anisotropy, n , is usually given as

$$\phi_r = \frac{T \cdot \sin^2 \alpha}{S \cdot W} \cdot \frac{1}{\sqrt{n-1}} \quad (3)$$

where

T = taping tension
 α = taping angle
 S = tape thickness
 W = tape width
 $n = E_1/E_3$
 E_1 = tensile modulus in machine direction
 E_3 = compressive modulus normal to tape

A second dependency of ϕ_r on S was recently discovered that may effect future calculations of bending behavior of lapped plastic cables. The compressive moduli of three similar, high density polyethylene (HDPE) tapes, differing only in thickness, were measured by Dr. R. W. Penn, NBS, Gaithersburg, MD. The tape thicknesses were $100\text{ }\mu\text{m}$, $125\text{ }\mu\text{m}$, and $180\text{ }\mu\text{m}$, respectively. Compressive modulus measurements normally made at NBS are on a stack of 1-inch circular discs of each material. Sufficient discs are cut to make each stack approximately 0.3" high. Stress-strain measurements are then performed. The compressive moduli recorded for several values of applied load are given below in Table XII for the three materials.

Table XII

Compressive Moduli of High Density Polyethylene Films

<u>Applied Stress, psi</u>	<u>E_3, psi $\times 10^3$</u>		
	<u>HDPE 100 μm</u>	<u>HDPE 125 μm</u>	<u>HDPE 180 μm</u>
20	1.84	2.4	3.11
40	3.29	4.24	5.63
60	4.67	6.06	7.77
80	5.83	7.45	9.83
100	7.17	8.86	11.6

¹⁶Fundamentals of the Theory of Paper Lapping of a Single Core High Voltage Cable by P. G. Priaroggia, E. Occhini and N. Palmieri, Pirelli, SPA, Milan, 1961.

Inspection of the table shows that E_3 is almost directly proportional to S and that therefore n is approximately proportional to $1/S$. Evidently the interfacial component between tapes has a strong effect on the apparent compressive modulus of a stack of tapes. See Appendix III for a complete description of this measurement.

3.1.4. Tape Evaluation Studies for LASL

We were requested by Los Alamos Scientific Laboratory (LASL) to determine the moduli of kraft-paper tape under consideration as a dielectric material for use on their superconducting cable design. The paper, a 0.005" thick electrical insulation grade sold under the trade name "Copaco", is manufactured by the Cottrell Paper Company, Inc. of Fall River, MA. Measurements were made of E_1 , E_2 and E_3 where,

E_1 = tensile modulus in the machine direction

E_2 = tensile modulus in the cross-machine direction

E_3 = compressive modulus normal to the tape

The moduli are summarized in Table XIII below.

Table XIII

Moduli of 0.005" Copaco Kraft Paper at 293 K

A. Tensile Properties

Property	Machine Direction	Cross Machine Direction
Elastic Elongation, %	0.583	0.670
Total Elongation, %	10.3	15.9
Yield Strength, psi	3,260	1,990
Tensile Strength, psi	13,530	5,730
Tensile Modulus, psi	433,000	130,000

Table XIII (continued)

B. Compressive Moduli

Stress (psi)	E_3 (psi $\times 10^3$)	(Std. Dev.)	E_3 (N/m ² 10^7)	(Std. Dev.)
20	0.374	(0.031)	0.258	0.0214
40	1.52	(0.297)	1.05	0.0205
60	4.14	(0.476)	2.85	0.328
80	6.77	(0.79)	4.66	0.544
100	9.24	(0.907)	6.37	0.625

Calculation of the value of tape anisotropy "n" at a compressive stress of 20 psi is 1160 for this material. The high values of E_1 and n that accompany 0.005" thick Copaco should ensure excellent taping and bending characteristics.

3.1.5. Shielded Butt-Gap Experiment

Following two previous unsuccessful attempts, a striped tape with conducting edges was fabricated that appears to meet BNL specifications. As described in previous BNL quarterly progress reports this taping concept was designed to alter the stress distribution in lapped cables so that little or no stress would be present across the butt gaps. Polycarbonate tapes 3/4 inch wide by 5 mils thick were provided with 1/16 inch wide stripes on the edges and a 1/8 inch wide stripe in the center by the King Seeley Thermos Company. Stripes were applied to both sides of the tape and top and bottom edge stripes were connected with silver paint applied at BNL. Discharge inception tests are now being performed.

3.1.6. Compatibility of Polymers and Impregnant

Immersion tests at 100°C have continued with a variety of polymeric tapes in three representative oils. These are a polybutene synthetic cable oil, a naphthenic-aromatic cable oil, and a silicone fluid. At the present time the silicone fluid is not regarded as a serious contender due to cost. Among the polymers tested to date are low density polyethylene (LDPE), Tyvek and Valeron varieties of polyethylene, polysulfone, polycarbonate, and several varieties of polypropylene (PP). The results obtained to date may be summarized as follows:

1. LDPE and polysulfone disintegrate in the three oils and Valeron delaminates.
2. Tyvek swells and softens in these liquids and polycarbonate becomes hard and brittle.
3. Nonoriented PP loses weight via dissolution, but is dimensionally very very stable in polybutene and naphthenic oils. Both weight and dimension appear to be stable in silicone fluid.

4. The weight of biaxially oriented PP is stable in the polybutene and napthenic oils but shrinkage occurs in these liquids. This material disintegrated in silicone fluid.
5. Both the weight and dimensions of one polypropylene paper (Celanese K-101) are quite stable in polybutene oil, but this material disintegrates in silicone fluid and suffers dissolution in the napthenic oil.

Table XIV gives a detailed summary of the compatibility data for five varieties of PP after immersion in the three representative oils. At the time of writing this report the total residence times in polybutene, silicone fluid, and napthenic oil are 694, 1368 and 1338 hours, respectively. Data in the table are reported in percent change in weight, thickness, and lengths in machine and cross-machine directions.

3.2. Radiographic Cable Testing

3.2.1. Waltz Mill Test Facility

During May 1978, we were requested to assist in the examination and dissection of a 500-550 kV prototype paper-oil cable that was subjected to a wide range of endurance tests at the Westinghouse Test Facility at Waltz Mill Test Facility, Waltz Mill, PA. This cable first received a two year series of 60 Hz electrical tests followed by a period of thermal cycling in the Waltz Mill thermomechanical test bay. Severe mechanical distortion of the cable followed the thermomechanical testing. Some detail of the damage is illustrated in the accompanying radiographs. Figure 26 shows that the butt gaps were normal in size and distribution following electrical testing whereas Fig. 27 illustrates some of the severe damage that resulted from the thermomechanical bend tests (TMB). In addition to the presence of elongated butt gaps that usually follows TMB, a "slip plane" was found at a location approximately midway through the insulation thickness. Previous studies at BNL have shown that these slip planes usually occur at radii corresponding to lay reversals. One possible explanation for this correlation is that axial friction forces may be smaller at these locations than those present at intra-lay positions.

3.2.2. Thermomechanical Bending Studies

Requests to BNL have become more numerous to radiograph 345 and 550 kV paper-oil cable samples that have been subjected to TMB forces. An apparatus has been designed and built that will allow closed circuit, x-ray TV analysis of cable samples suspected of having soft spots, slip planes, or other defects incurred during construction, testing or operation of the cable. The equipment will provide rotational and translational examination of samples up to two feet in length. Conventional radiographs may be taken of irregularities found during TV analysis.

3.2.3. Summary

A summary of radiography performed for companies and institutions is given in Table XV.

TABLE XIV

Weight and Dimensional Changes of Five Polypropylene Films in Insulating Oil at 100°C

Polymer Type	Change, %*	Polybutene Oil (Amoco L-50)	Silicone Fluid (Dow Corning DC-200)	Napthenic Cable Oil (Sunoco Sun-XX)
		694 h	1368 h	1338 h
Polypropylene (Nonoriented)	Δ -WT	-3.9	-0.99	-4.1
	Δ -T	-1.1	<0.5	-2.9
	Δ -MD	-1.2	<0.5	-1.0
	Δ -CMD	-0.96	-0.68	+0.81
Polypropylene (Oriented-Single Layer)	Δ -WT	<0.5		-4.3
	Δ -T	+14.9	Disintegrated	
	Δ -MD	-6.1		-5.6
	Δ -CMD	-5.8		-5.0
Polypropylene (Oriented- Laminated)	Δ -WT		<0.5	
	Δ -T	Delaminated	----	Delaminated
	Δ -MD		-1.9	
	Δ -CMD		+0.80	
Polypropylene Paper (Celanese K-101)	Δ -WT	-2.8		-5.9
	Δ -T	+0.64	Cracked	-3.6
	Δ -MD	-3.3		-3.3
	Δ -CMD	<0.5		<0.5
Polypropylene Paper (Celanese-2500)	Δ -WT	-1.8		-3.2
	Δ -T	+1.6	Disintegrated	+7.6
	Δ -MD	-8.8		-9.9
	Δ -CMD	+0.91		+2.1

*WT : Weight

T : Thickness

MD : Machine Direction

CMD: Cross Machine Direction

Figure 26. Radiograph of 550 kV prototype paper
oil insulation prior to thermal cycling.

Figure 27. Radiograph of 550 kV prototype cable showing deep slip plane after thermomechanical bend tests.

Table XV

Cable Radiography Applications During FY 1978

1. Sands Point, N.Y.: Con. Ed. 345 kV Cable Pull Under L.I. Sound.	Found severe damage.
2. Okonite Cable Co.: Replacement Cable for Sands Point Installation.	-
3. LILCO: Investigate Molded Splice Joints of 69 kV Extruded Cable.	Found void.
4. General Cable Co.: Investigate Extrusion Symmetry of 138 kV Cables.	-
5. Waltz Mill: Check T.M.B. Damage in P.D. 500 kV Prototype Cable.	See Section 3.2.1.
6. PTI: Joint Integrity of Experimental Glass Insulated Cable.	-
7. PTI: Analysis of 345 kV T.M.B. Samples.	-

3.2.4. Room Temperature and Evaluated Temperature Loss Measurements

A high temperature cell for measuring the dielectric properties of polymer tapes between room temperature and 423 K has been constructed and is now in operation at the Polymer Division, NBS, Gaithersburg, MD. This cell, which has a capacity for four samples at a time, will be used to determine the dielectric properties of polymer tapes above room temperature as part of a program aimed at selecting tapes suitable for use as the electric insulation in oil filled high power transmission cables. Loss measurements made with this apparatus on single layers of polypropylene film revealed that the loss attains a minimum value of $75 - 80 \times 10^{-6}$ in the temperature range 323 - 363 K. The loss increases to approximately $110 - 120 \times 10^{-6}$ at 373 K.

A more detailed report of the loss tangent measurements performed by the Polymer Division, NBS is included in Appendix III to this report.

3.3. Small Sample Tests3.3.1. Accelerated Life Test

The predicted service life of electric equipment is often determined by the rate of deterioration of the insulation under operating conditions. The simulation of aging of electrical insulation poses a difficult problem to scientists and engineers in research and development. Superconducting power

cable designs must also undergo aging tests. Although the rate of thermal degradation and chemical changes might be expected to be greatly reduced by the low operating temperatures and inert gas environment, the presence of partial discharges can result in energy being repetitively deposited in a small region of the plastic which will have a low thermal conductivity at these temperatures. So far no experiments are known to us on aging phenomena in dielectrics at supercritical helium temperatures. We decided to accelerate aging by increasing the frequency of the applied voltage.*

The power supply is a series-resonant circuit, connected to a signal generator and 2 kW power amplifier via a step-up transformer. The series-inductor is 203 mH and the sample capacitance is about 2 nF. The resonant frequency is 7.8 - 7.9 kHz. The inductor, wound on a ferrite core, runs in circulating oil to prevent overheating.

The same sample construction used for the small-sample test is used. The concentric layers of the sample, from the mandrel outwards, are: 2 x 4 mil intercalated aluminized Valeron screen, 5 x 4 mil lapped polypropylene-polyurethane laminate, 1 x 1.5 mil Cryovac, 2 x 4 mil intercalated aluminized Valeron screen and aluminum/Kapton laminate outer electrode. The same stress cones are used that are normally used for the small-sample tests.

The sample has been run twice. Table XVI summarizes each run. Four months intervened between the runs. The resonant frequency changed slightly due to the rearrangement of the power supply. The test was terminated at the total 60 Hz equivalent time of 530 days without catastrophic electrical failure of the sample.

Table XVI		
<u>Operating Conditions for Accelerated Life Test</u>		
	<u>First Run</u>	<u>Second Run</u>
Tuned frequency (kHz)	7.81	7.93
Applied voltage (kV)	8.77	8.77
Total running time (min)	428	5,357
60 Hz equivalent time (min)	5.57×10^4	7.08×10^5
(day)	38.7	491.9
Liquid helium required (l)	500	1,800
Prescribed running condition of supercritical helium		
Temperature = 8 K		
Pressure = 206 psi (14 atm)		
Density = 170 kg/m ³		

* Semiannual Report for 10/1/77 - 3/31/78, p. 65-67.

The temperature distribution along the sample was checked by the three calibrated resistors attached at the top, middle and bottom of the sample. It was reasonably uniform during the test. Although the density should be kept constant, small fluctuations are unavoidable because of hysteresis of the regulating equipment and the thermal inertia of the four-inch cryostat. The supercritical helium was maintained at 8 K and 206 psia, corresponding to a helium density of 110 kg/m^3 . Consumption of liquid helium was larger than expected: 11 l/hr whereas the predicted usage was calculated to be only 5 l/hr. A high frequency voltage of 8.77 kV was applied to the sample during the test so that the electric field across the main insulation, PP-PU-PP tape was 10 MV/m, which is the design operating stress. This voltage is below the initial partial discharge inception voltage of 11.2 kV. As it is difficult to make partial discharge measurements at 7.9 kHz, 60 Hz measurements of discharge inception were made periodically during the test.

The room temperature SF_6 test preceded each run. The results of these tests are shown in Fig. 28. The SF_6 test after the second run was inconclusive because the SF_6 gas introduced in the four-inch cryostat before the sample had fully warmed and SF_6 solidified on the sample.

The partial discharge results in supercritical helium are shown in Fig. 29. The horizontal line at 20 MV/m cable stress indicates the design target partial discharge inception for a line operation stress of 10 MV/m. The three curves represent results obtained before the first run, at the end of first run and at the end of the second run. There are two curves for the end of the second run because two distinct types of discharges of different magnitude were observed. Partial discharges at the low inception stress were small ($\sim 5 \text{ pC}$) and random. At high stress, continuous partial discharges of large magnitude ($\sim 10 \text{ pC}$) were initiated at a distinct inception voltage. It has been our experience that so-called butt-gap discharge is less than a few pC. Therefore, the partial discharge at high inception stress may originate somewhere other than in the butt gaps. (Perhaps they are surface discharges along the stress cone.) If we restrict our attention only to the small discharges, the inception stress gradually decreased as the time of voltage application increased as seen in Fig. 30. It is very probable that during most of the period partial discharges were present since the partial discharge inception stress fell below the applied butt-gap stress. Since the initial inception stress was higher than the applied stress, originally undetectable small discharges ($< 1 \text{ pC}$) might have affected the sample and lowered the inception stress of detectable discharges.

The inspection of the sample after the test revealed the following:

1. The vapor deposited aluminum on the Valeron screen tapes of both inner and outer electrodes had eroded or flaked off, particularly at the upper part of the sample. These particles would then have caused a reduction in the partial discharge inception voltage.
2. Several small regions of the PP-PU-PP tape had delaminated. The tapes may have been structurally weak in these regions as a result of variation in the processing conditions at the time of manufacture, with the mechanical stresses created by thermally cycling of the material leading to actual separation of the layers. More rigorous inspection procedures may be necessary to ensure the tapes are free of such weak spots as received from the manufacturer. In any case, as the exposed polyurethane surface was quite irregular, this surface

¹⁷J. A. Kok, et al., Applied Science Res. B10, 257-268 (1968).

Figure 28. Partial discharge inception characteristics of small sample with SF₆.

Figure 29.

Figure 30.

probably did provide initiation sites for the partial discharges.

3. The electrical discharges did not produce any evident deterioration or tracking on the surfaces or edges of the insulating tapes. The finding that exposure to partial discharges for a year and a half (60 Hz equivalent time) resulted in no visible degradation is both significant and encouraging, and it supports the assumption that the principle penalty of the presence of partial discharges may be the increased losses rather than decreased service life of the cable. As the gradual drop in inception stress is not attributable to the steady destruction of the material itself, other reasons must be found to explain this observation.

Other researchers have found a reduction in the amount of damage resulting from butt-gap discharges at liquid nitrogen temperatures.¹⁸ They propose that this is a consequence of an oxygen-free environment, enhanced tensile strength at cryogenic temperatures, and the properties of discharges in cryogenic fluids. Similar reasoning can be applied to explain the results of experiments performed in supercritical helium. However, before definite conclusions can be drawn, it will be necessary to perform an impulse breakdown test of a sample subjected to partial discharges for a long period. The sample reported on here was not impulsed since this would have vitiated the possibility of observing the effects due only to partial discharges.

A number of mechanisms could be postulated to explain the erosion of the 400-A aluminum layer. It is known that if a metallic screen is not in good contact with the conductor surface, charging current will flow along the screening tape.¹⁹ If the resistivity of the tape is too high, the resulting voltage drop will produce a high stress over the short distance separating the screen and the conductor. At a helium density of 110 kg/m³ and a separation distance of 0.1 μ m, a voltage drop of only 160 volts is sufficient to initiate partial discharges in pure helium. The room temperature resistivity of the screening tape is about 70 Ω /m. If the screening tape is not in good contact with the metal electrode, the capacitive charging current will be forced to flow through a much reduced surface area where the contact is good. This could also contribute to deterioration of the aluminum screen. The charging current is calculated to be about 0.9 A.

Inspection of the aluminized layer indicated that it was most heavily damaged at the top of the cylindrical sample. Adhesion of the aluminum to polyethylene seems to be better than to polypropylene, but it is still possible that the slipping of the tape along the metal electrode as it contracted during thermal cooldown could have scraped off some of the aluminum coating. In any case, more attention will need to be given to the design of a suitable screening tape.

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¹⁸A. T. Bulinski and R. J. Juchniewicz, Intl. Conf. on High Voltage Electric Systems, Paris 15-02 (1974).

¹⁹G. S. Eager, G. Bahder, and D. A. Silver, IEEE Trans. on Power Apparatus and Systems PAS-88, 342 (1969).

3.4. Summary of Activity at NBS, Electrosystems Div., Gaithersburg, MD

The last six months brought to a close this phase of the project where partial discharges and dissipation factor had been studied on small samples. A complete report on this work, including a discussion of the special instrumentation for automatic partial discharge data accumulation, is forthcoming. The report will be issued as a separate NBS internal report.

A summary of the results obtained over the past six months is presented below.

1. The dissipation factor (D.F.) of the polypropylene laminate (3PP-2U(A)) is stress dependent.

2. Samples with a screening layer of two intercalated metallized Valeron tapes over the high voltage electrode always show a greater stress dependence than unscreened samples.

3. At the lowest stress, the D.F. of both screened and unscreened samples is usually about the same as the value obtained for a single sheet, $15 - 20 \times 10^{-6}$.

4. At 10 MV/m, the D.F. of the unscreened samples is greater than 24×10^{-6} .

5. Since the D.F. of the screened samples shows a greater stress dependence, the D.F. of the screened sample can be about 40% greater than that of the unscreened sample at a stress of 10 MW/m.

6. The partial discharge inception stress of the samples constructed has been relatively low, usually 5 to 7 MW/m for partial discharges of 0.05 to 0.3 pC.

7. The D.F. increases with time after the initial application of voltage, but soon reaches an equilibrium value and remains constant for periods up to four hours when the stress, pressure, and external sample temperature are held constant.

8. At the onset of partial discharges in a measurement of D.F. as a function of stress, the dissipation factor rises rapidly with increasing stress. The change in slope is referred to as "dissipation factor tip-up" and is most dramatic at low helium densities. A partial discharge detector sees the entire electrical circuit and cannot distinguish discharges occurring in the sample from those occurring in the external circuitry, stress cones, or other regions. The dissipation factor measurement bridge, however, only measures losses in the region of the active electrode, but, as it measures all sources of dissipation, other dissipative mechanisms such as interfacial polarization and surface conduction can also produce a "tip-up" even when partial discharges are absent.²⁰ Moreover, even when the increase in loss is due to partial discharges, the change in the dissipation factor may not accurately indicate the magnitude of this increased loss because of the dependence of the change on the amplitude and phase of the discharge pulses.²¹

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²⁰R. Goffaux, IEEE Trans. Electr. Insul. EI-13, 1 (1978).

²¹A. Kelen, IEEE Trans. Electr. Insul. EI-13, 14 (1978).

The dissipation factor of the polypropylene laminate is about 25% higher than the target of 20×10^{-6} , at least as measured in small samples at operating stress and temperature and without a screening layer. The screen invariably introduces increased losses and a greater stress dependence so that the dielectric loss tangent is about 75% greater than the target value at 10 MV/m. This increase may be less for realistically thick cables where the screen occupies a correspondingly smaller portion of the insulation volume.

3.5. Tests on General Cable (H.V. Test Cable # 2)

3.5.1. Partial Discharge Inception

The ambient temperature high voltage test apparatus has been completed and installed at the 5th Avenue Test Site. The apparatus is designed to permit testing of 20 foot lengths of cable with gas or liquid impregnant.

Figure 31 shows the apparatus connected to the 240 kV resonant test set for a 60 Hz test. A schematic illustration of the internal construction for the low pressure SF_6 test (to 100 psia) is shown in Fig. 32. A 20 foot length of the 4 mil polycarbonate section of the General Cable test cable has been tested in this apparatus and the partial discharge inception results are plotted in Fig. 33. The results for SF_6 pressure tested are better than the small sample results obtained previously for the same dielectric material. In order to avoid undue optimism, the calibration of the resonant test set voltmeter was checked with two standard capacitors. The calibration proved to be accurate. Future tests will include partial discharge measurement at high gas pressures, $\tan \delta$ measurements and impulse tests.

3.5.2. Thermal Resistance of Cable Insulation

An experiment was conducted at Brookhaven by Dr. Fukagawa (Visiting Researcher at RPI) to determine the thermal resistance of the 2.4 mil embossed polycarbonate insulation section of the General Cable H.V. Test Cable. These data will be useful in applying the results of the study by Dr. Fukagawa (see Appendix IV) to H.V. room temperature applications of plastic cables.

Figure 34 shows the experimental set up. The Chromalox heater wire has a nominal heat output of 8 watts per foot at 120 volts. The tests were conducted at atmospheric pressure.

Figure 34 through 36 graphically represent the data collected. In Table XVII the values of the thermal resistivity of the insulation tested are listed for each temperature recorded. The values of thermal resistivity (ρ) was calculated using the following relationship:

$$\rho = \frac{2 \pi R}{\ln\left(\frac{d_2}{d_1}\right)}$$

where

d_1 = center conductor diameter

d_2 = outer shield diameter

R = thermal resistance of electrical insulation per unit length

Figure 31. Ambient temperature high voltage cable test.

Figure 32. Assembly for SF_6 test of General Cable high voltage test cable.

Figure 33. SF_6 partial discharge inception levels for General Cable high voltage test cable compared with small sample results.

Figure 34. Location of thermocouples for thermal resistivity measurement.

Figure 35. Center thermocouple temperatures as a function of time and heater output.

Figure 36. Inner conductor temperature vs. position.

Table XVII		
Thermal Resistivities		
Conductor Temperature	R °C-cm/W	ρ °C-cm/W
50.6°C	57.9	774
66.7°C	54.6	729

R was measured experimentally using the relationship:

$$R = \frac{\Delta T}{W}$$

where

ΔT = temperature at inner conductor - temperature at outer shield

W = power input per unit length

Table XVII was calculated using the data from the thermocouples in the center of the test cable length. As can be seen from Fig. 35 these data points were subject to end effect errors which will give low values for R. The values for ρ listed in Table XVII compare reasonably well with published values (519,8°C-cm/W) when the effect of embossing and butt space voids are considered.

Future experiments will evaluate the effects of high pressure gas and liquid impregnants and will incorporate an improved experimental set up to reduce end effects.

4. Mechanical and Cryogenic Engineering

4.1. BNL Taping Line

A major goal was reached during this reporting period with the BNL cable taping line successfully completing initial operation. Using a simulated core and 32 taping pads, the entire line was operated at various speeds. Sixteen taping heads were rebuilt and installed on the winding line to supply the 32 tapes. Various screen and dielectric tapes were used for the first few runs, including multiple laminate tapes. Approximately 1.2 pounds of tape tension (mechanically set) was used for these runs while the major machine parameters were being adjusted. The winding line is shown in Fig. 37. Many taping line machinery components were needed for the first run. The installation of the hydraulic pump and motor set, used to synchronize the cable axial speed to the taping head rotation, is shown in Fig. 38 as completed. This combination provides an infinitely adjustable transmission for the purpose of dialing in the desired butt space between tapes. The positive displacement hydraulic units operate with extreme accuracy. All systems to operate and control the pay-off and take-up reels on the winding line wagons are shown in Fig. 39. The reels have been operated successfully in both manual mode and in automatic-follow mode. In automatic mode, which is used for the winding of cables, the pay-off and take-up reels supply and collect cable at the exact rate required by the winding machine. This is accomplished using a speed reference generator directly mounted on the caterpuller. The reels start, stop and follow with the linear speed of the puller. Catenary control mechanisms were designed and built at each end of the winding room. The puller, catenary controls, and take-up reel are shown in Fig. 40. These controls act to trim the speed of the automatic reel movement to maintain the proper cable angle entering and leaving the winding line.

A new puller machine was chosen and purchased to be modified and installed to provide adjustable core tension during actual future cable production. This machine is rated at 1300 lbs of pull and will be installed at the input of the winding line.

4.2. Cable Sample Tests

Several test runs of the 10 m cable were performed during this period. The electrical results are given in Section 2.5. Of concern in this section are the cryogenic aspects. An excellent cooldown, lasting 16 days, was made between May 25th and April 10th. The cable was cold and available to take current for 14 days. The next cooldown, in June, was aborted because of refrigerator freeze-up and similar problems. At this point it was determined that the efforts to operate the two old refrigerators for the laboratory tests did not justify the results and the system just simply would not carry the load of the high voltage tests. Steps to replace the old machines with a new larger machine were taken, with delivery expected in late November.

Following analysis of the previous tests, it was decided to verify the electrical losses by measuring the heat in-put to the helium coolant streams. An effort was made to improve the measurement of the temperature and mass flow rate of the helium before the next test. The test was started on August 1st and was plagued by all of the previous refrigerator problems. The temperature was never stabilized to a steady state condition, so it was impossible to determine the electrical losses on a quantitative basis. Plots of the temperature profiles of the flowing helium streams did show a change in slope with and without current on the cable. This change of slope did indicate more heat going into the helium when current was on the cable. Attempts to calculate heat balances at various operating conditions showed that the system was not in equilibrium and even more important, the temperature sensors were not sufficiently well calibrated to see 0.01 K difference from one to the other.

Additional tests are planned after installation of the new refrigerator and some changes in the temperature instrumentation.

4.3. Cable Terminations and Enclosures

4.3.1. Termination Components

Alto Development Company has completed casting three low temperature,

Figure 37. BNL taping machine.

Figure 38. Hydraulic pump and motor used to drive caterpiller of taping line.

Figure 39. Pay-off reel of BNL taping line.

Figure 40. Caterpuller and catenary control on
BNL taping line.

high voltage feedthrough bushings. Problems were encountered in drilling flange bolt holes which have delayed delivery, but shipment is expected during October.

The extruded aluminum sections have been wound into cylinders and brazed to form a coaxial pair of gas cooled 4 kA leads. This set of leads will be tested in conjunction with one of the sample cables during the next period.

4.3.2. Low-Temperature Voltage Test Apparatus

All fabricated components for the high voltage cable test enclosure have been received from the shops and component testing is underway. The two vacuum enclosures have been assembled and leak tested. The first two copper pressure vessels have been assembled pressurized, cooled to LN₂ temperature and the indium seals tested. These seals remained tight at low temperature after the bolted flanges had been repeatedly tightened with a torque wrench.

Considerable effort was expended in rearranging the equipment in the crowded laboratory. An additional doorway was cut in an interior wall to provide access around the long test cryostats. An overhead I beam and trolley system was installed at each end of the voltage test apparatus. The heavy vacuum enclosures and copper pressure vessels will all be suspended from the overhead rail to facilitate assembly and installation.

The Westinghouse High Voltage Bushing and SF₆ insulated elbow presently in use for room temperature cable tests will be moved into the high voltage area of the laboratory after the first test cable is in place.

4.3.3. Aspects of Heat Exchangers for Cryogenic Current Leads

A great many papers have been published on the subject of cryogenic current lead design; a review paper by Buyanov et al²² lists 64 references, covering uncooled leads, vapor-cooled leads and station-cooled leads. Generally, low-current leads for use at liquid-helium temperature are uncooled (except by conductivity at the bottom and top) and higher-current leads are vapor-cooled, perhaps with one point stationed at liquid-nitrogen temperature. One reason for the glut of papers is because the properties of materials are non-linear with temperature, making the heat-balance equations impossible to solve analytically without resort to approximations. Another reason is because leads may be used either in a closed (refrigerated) or open (liquid helium cooled) system, with different optimization requirements. Leads in a closed system have a gas flow fixed externally, and the lead pressure drop has little affect on it. In an open system, however, the heat input to the system by the leads provides part of the vapor so that the system pressure and hence the lead flow rate may be self-sustained. With such a superfluity of papers, one might think it impossible to have anything new to say on the subject, but this is not the case. For example, a recent paper by Jones et al²³ investigates the time-dependent behavior of current leads by numerical calculations. The assumptions made were fixed gas flow, perfect heat transfer and constant C_p and fixed bottom and top temperatures. The heat transfer to the heat sink at the low temperature end was not given.

In an earlier report,²⁴ the present author presented steady-state analytic solutions based on the assumptions of self-sustained gas flow, perfect heat transfer, constant C_p , Weideman-Franz Lorentz conductors (WFL) and fixed bottom and top temperatures.

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²²Yu. L. Buyanov, A. B. Fradkov, I. Yu. Shebalin, "A Review of Current Leads for Cryogenic Devices," Cryogenics 15, 193 (April 1975).

²³M. C. Jones, V. M. Yeroshenko, A. Starostin, L. A. Yashkin, "Transient Behavior of Helium-Cooled Leads for Superconducting Power Transmission," Cryogenics 18, 337 (June 1978).

²⁴G. H. Morgan, Optimization of Current Leads into a Cryogenic System, Accel. Dept. Int. Rpt. AADD-171 (Nov. 24, 1970).

Later, Goodzeit²⁵ used numerical techniques to lift the assumption of WFL conductors, but retained the other assumptions. He showed that very pure copper leads require somewhat less gas flow at optimum current than if made of WFL conductors. The present author showed²⁶ that when leads made of impure, low-conductivity WFL metals are used for off-optimum currents, they require less gas flow than if made of purer WFL metals and are capable of running at much higher over-currents in the self-sustained gas-flow mode.

It is customary in both self-sustained leads and fixed-flow leads used with superconducting devices to make a portion of the cold end of the lead superconducting. In the case of self-sustained leads which have the cold end cooled by liquid helium the superconducting section is partly in liquid, effectively preventing transfer of heat to the device. In order to achieve the same end in a fixed-flow supercritical helium-cooled lead, it is necessary to heat-sink the cold end with a superconducting heat exchanger. The boundary condition desired at the cold end of the heat exchanger is $\dot{Q} = kA \, dT/dx = 0$. This is not possible in principle with perfect heat transfer. The steady-state energy equations to be satisfied are

$$\frac{d}{dx} (kA \frac{dT}{dx}) - hu(T - \theta) + \rho I^2/A = 0 \quad (5)$$

and

$$hu(T - \theta) = C_p \dot{m} \frac{d\theta}{dx} \quad (6)$$

Here K , A , ρ , u and h are lead thermal conductivity, cross-sectional area, electrical resistivity, gas channel periphery and heat transfer coefficient. The gas and conductor temperatures are θ and T . With perfect heat transfer, $\theta = T$ and the system degenerates to a single, second-order equation

$$\frac{d}{dx} (kA \frac{dT}{dx}) - C_p \dot{m} \frac{dT}{dx} + \rho I^2/A = 0 \quad (7)$$

which permits specification of only two boundary values, the two end temperatures. With finite hu the slope at the cold end can be specified along with the inlet gas temperature θ_0 and the warm end conductor temperature T_1 . A non-mathematical way of demonstrating the possibility of such solutions is to consider the "gedanken-experimente" consisting of a pair of identical leads, one for current out, the other for current in, adiabatic except at the gas outlet ends and connected at the inlet ends by a superconducting link. Since the gas inlet ends have equal temperature, dT/dx is of necessity zero.

The present computer program sets $Y (= kA \, dT/dx)$ equal to zero at $x = 0$; it can also be set equal to a Newtonian condition or to $m\Delta H$, the self-sustained condition.

4.3.3.1. Method of Solution

The work reported here consists of the numerical solution of the energy equations with finite heat transfer and real material

²⁵C. L. Goodzeit, Optimization of Gas-Cooled Superconducting Magnet Leads, A Method for General Materials," Accel. Dept. Int. Rpt. PDD-72-1 (BNL 17233) (Sept. 1972).

²⁶G. H. Morgan, Design of AC Cryogenic Power Leads," Accel. Dept. PTP Tech. Note 48 (Nov. 10, 1975).

properties, except for the assumption of constant gas specific heat C_p in most of the computations. This latter is calculated from $(H_1 - H_0)/P$ ($\theta_1 - \theta_0$) and is slightly greater than the 300 K values, 5.245 vs 5.193 J/g-k. The method used for the numerical solution of the energy equations is a 2nd order finite difference approximation. Equation (5) is expressed as two, first order equations and the resulting tri-tridiagonal matrices are solved by an algorithm given by Von Rosenberg.²⁷ It should be noted that breaking the 2nd order equation into two first-order equations obviates supplying an expression for dk/dT . The temperature dependent materials properties are updated after each iteration using the old local value of temperature; 15 to 20 iterations are required and a single solution with a 40 step lattice takes about 2 seconds on the CDC 6600. The initial temperature profile assumed is exponential between the end temperatures, but not all solutions are obtainable with this assumption. It is sometimes necessary to obtain one or two intermediate solutions, saving the temperature profile obtained at each step as a first approximation for the next.

The electrical resistivity of a copper conductor is computed from the resistivity ratio following a method given by Jones et al²³ and the same subroutine gives the resistivity of an aluminum conductor by a similar process, modified to allow for the dependence of the ice-point resistivity on residual resistivity ratio $RRR = \rho_a(273)/\rho(4.2)$; a quite good correlation between RRR and $\rho(273)$ exists for all the aluminum alloys for which data was available. The thermal conductivity of either copper or aluminum is also calculated from RRR , following a procedure given by Powell et al²⁸ for copper.

The geometry of the lead was developed by K. Minati, it consists of extruded 1100 aluminum having the cross section shown in the inset of Fig. 41. It has three oval gas channels in parallel giving a cooling surface area per unit length about 5 times the base area. This extrusion is tightly coiled with the base on the outside then adjacent turns are brazed together. The cylinder thus formed is machined smooth on the outside and forms the inner conductor of a coaxial pair of leads. The outer conductor is similarly made but with the extrusion base on the inside. Figure 41 is a photograph of the as-brazed cylinders. Nb_3Sn ribbon is soldered to a portion of the cylinder at one end, forming the heat sink. DC measurements give a resistivity ratio of 12.3 for the brazed assembly. Based on a room temperature resistivity calculated as described above the effective radial thickness of the assembly is about 50% greater than the minimum thickness (1.5 mm) of the base of the extrusion. The lead will be used for 60 Hz, ac and the skin depth of the aluminum will vary from 3.2 mm at 4.2 K where $\rho = 2.396 \times 10^{-9}$ ohm-m to 11.8 mm at 300 K with $\rho = 3.277 \times 10^{-8}$.

The heat transfer coefficient is dependent on the local Prandtl and Reynolds numbers, so that the conductivity and the viscosity of helium must be calculated as a function of temperature. Although programs are available for the purpose, they are lengthy and relatively slow, so tabulated data²⁹ at a constant pressure of 12 atm was fitted to polynomials in $\log(\theta)$.

²⁷ D. U. Von Rosenberg, "Methods for the Numerical Solution of Partial Differential Equations," American Elsevier Publ. Co., N.Y. 1969.

²⁸ R. Powell, H. Roder and W. Hall, Phys. Rev. 115, 314 (1959).

²⁹ R. D. McCarty, "Thermophysical Properties of He-4," NBS Tech. Note. 631 (Nov. 1972).

Cross section of extrusion

Figure 41. Leads after brazing coils together.

With the coiled geometry described, both the heat transfer coefficient and the friction coefficient differ from that for a straight tube. The correlations by Mori and Nakayama³⁰ as given in the Handbook of Heat Transfer³¹ for flow in curved ducts is used, together with the degradation factor for heat transfer at large T given by Yaskin et al³² in the turbulent flow portion of the lead if any. Most of the flow is laminar. Mori and Yakahama also give expressions for the friction factor f, and the pressure drop is then calculated from

$$dP/dx = - 2f G^2/dD \quad (8)$$

where G is the mass flow per unit area, d is the density and D the hydraulic diameter. The density is assumed to vary as 1/T.

4.3.3.2. Results of the Computations

A lead length of 61 cm plus several different additional lengths of heat exchanger with superconductor was assumed. With a design current of 4000 A, and an inlet gas temperature of 6 K, the mass flow was varied to find the value such that the exit gas temperature was equal to the warm end fixed temperature, 300 K. This ensures the absence of a hot spot. The entire procedure was repeated with different values of cross-section in order to find the cross-section which required minimum gas flow. The whole process was then repeated varying the heat-sink length X_n , i.e., that portion of the lead consisting of heat exchanger plus superconductor. Values of X_n equal to 12.5, 25 and 37.5% of the non-superconducting length were studied. No solutions could be obtained with $X_n = 0$. Figure 42 shows the optimization curves for the three values of X_n . The ordinate m is the smallest gas flow at the cross-section given by the abscissa. The minima of the curves, i.e., the optimum cross-sections, decrease with increasing heat-sink length. A longer heat sink than 37.5% would not produce much improvement. The minima broaden with increasing heat sink length, that is, the cross-section becomes less critical. Table XVIII gives the area and mass flow of the optimized leads. Table XVIII includes a high-purity copper (RRR = 120) lead for comparison; the copper has the same resistivity ratio as Goodzeits 2nd material.²⁵ An interesting observation shown by Table XIX is that the temperature T_n at the warm end of the heat sink is in all cases less than the critical temperature $T_c = 16.5$ (actually a function of current) but the longer the heat sink, the more nearly the warm end temperature approaches T_c .

Table XVIII				
Cross-Section and Mass Flow of Optimized 4 kA Leads				
X_n , cm	7.62	15.24	22.86	30.48*
X_n , %	12.5	25	37.5	50
Area, Sq. cm.	2.94	4.1	4.83	0.38
Mass Flow, g/sec	0.222	0.190	0.181	0.234
* Copper Lead				

³⁰Y. Mori and W. Nakayama, Int. J. Heat & Mass Transfer 8, 67 (1965).

³¹W. M. Rohsenow, J. P. Hartnett ed "Handbook of Heat Transfer," 7-149 McGraw Hill ('73).

³²L. A. Yaskin, M. C. Jones, V. M. Yeroshenko, P. J. Giarratano and V. D. Arp, "A Correlation for Heat Transfer to Superphysical Helium in Turbulent Flow," Cryogenics 17, 549 (Oct. 1977).

Figure 42. Optimization curves for various X_n .

Table XIX			
Temperature T_n at Warm End of Heat Sink			
X_n , Cm	Current, kA		
	0	4	6
7.62	6.008	7.287	6.462
15.24	6.135	9.736	7.131
22.86	7.786	14.010	7.563
30.48*	6.067	8.093	6.860
* Copper Lead			

The copper lead is hypothetical; the cross-section is too small to construct a heat exchanger using the present design. In addition it is clear that a much longer heat sink would be desirable.

The behavior of the leads at currents other than optimum is relevant to the choice of heat sink length. Figure 43 shows the gas flow for a 300 K outlet temperature as a function of current for the four leads. It shows that at zero current, the aluminum lead with the shortest heat sink, which is also the one with the smallest cross-section, requires the least gas flow, while at 6 kA, the lead with the longest heat sink requires the least flow. The copper lead requires the most coolant at all currents. The results are also given in Table XX, expressed as percent of the smallest flow at 4 kA; 0.181 g/sec.

Table XX			
<u>Coolant Flow at Non-Optimum Currents</u>			
12.5	48	123	239
25	60	105	191
37.5	64	100	176
50*	69	129	252
* Copper			

Another item of interest in selecting a lead is the sensitivity to coolant flow fluctuations. A hypothetical situation might be a drop in gas flow while operating at optimum current; the minimum coolant flow for stable operation is thus of interest, or perhaps the coolant flow for which the temperature does not exceed a specified maximum anywhere in the lead. When solutions of this sort are attempted with the present computer program, a curious result may occur. At optimum current, even a slight decrease in flow from the 0.181 g/sec with the 37.5% long heat sink results in no solution. There are two ways in which the solution can fail: either the gas inlet temperature does not equal the desired value or the temperature profile may not converge after a reasonable number of iterations.

Figure 43. Gas flow as a function of lead current.

In the first case, $T(x)$, $\theta(x)$ and $Y(x)$ converge, but the value of $\theta(x)$ at $x = 0$ (the cold end), by linear extrapolation from the two adjacent computer values, does not equal the specified boundary value of 6 K. The maximum acceptable deviation is 0.5 mK. If the deviation is greater than this, then it is also found that the extrapolated value of Y deviates from the boundary value by more than 1 mW. This lengthy description of what might appear to be merely a result of the numerical process is justified because it may have a physical significance. Lack of convergence always occurs when the temperature profile of nearby solutions have an interior maximum. Thus it may be that lack of convergence indicates a runaway condition. Convergence to other than the specified boundary value, however, is taken as an indication that no solution exists. The two ways of failure to achieve a solution will be termed "no solution" and "no convergence." The curious result referred to is that while the lead with $X_n = 37.5\%$ has no solution for coolant flow less than 0.181 g/sec at 4 kA, the other two cases with shorter heat sinks do have solutions at 4 kA, with sub-optimum coolant flow, though none of the solutions are for a flow as low as 0.181 g/sec. At 6 kA, however, all four leads have solutions with sub-optimum coolant flow. All such solutions have an interior temperature maximum, as is shown in Fig. 44 for a lead with $X_n = 25\%$, and the exit gas temperature is greater than the fixed, warm-end conductor temperature. Figure 44 shows for comparison a solution with optimum gas flow, the flow such that the gas exit temperature equals the conductor temperature. Also shown is the temperature difference profile for the two cases. The temperature excursions due to reduced coolant flow at 6 kA are summarized in Table XXI.

Table XXI				
Temperature Maxima and Minimum Flow Rates for Four Leads				
Maximum Temp, °K				
% Change in Flow Rate	$X_n = 12.50\%$	$X_n = 25\%$	$X_n = 37.5\%$	$X_n = 50\%^*$
- 1	301.8	301.2	301.0	327.9
- 1.03				330.7
- 3	314.6	315.7	317.3	
- 3.7			332.5	
- 5	337.5	349.9		
- 5.51			367.7	
- 12.7	690.1			
* Copper Lead				

The outstanding result is that the lead with the shortest heat sink and smallest cross-section tolerates the greatest coolant deficiency. The flow of 0.378 g/sec for this case, however, is still 19% greater than the optimum flow of 0.318 g/sec at 6 kA for the lead with $X_n = 37.5\%$. The copper lead is least tolerant of reduced

Figure 44. Lead temperature profile.

coolant flow.

A final factor in designing the lead is the benefit of increasing the heat sink length beyond that for which the cross-section was optimized. It is found that performance does not change with increased X_n at or above optimum current, but that $\dot{m}$ is reduced at $I = 0$. If the lead with $X_n = 7.62$ cm (12.5%) has X_n increased to 37.5%, $\dot{m}$ at $I = 0$ decreases from 0.087 g/sec to 0.071 g/sec; the lead with $X_n = 37.5\%$ has $\dot{m}$ reduced from 0.116 to 0.106 g/sec by increasing X_n to 50%. The former result is to be expected, since $0.071/0.116 = 2.94/4.83$ i.e., for equal length, the coolant flow is proportional to the area at zero current.

4.3.3.3. Comparison with Transient Solutions

Jones et al²³ (hereafter termed JYSY) computed runaway or burn-out currents for leads of various resistivity ratio coppers. Their results are expressed in terms of the dimensionless parameters $M = \dot{m} C_p L / k_o A$ and $J = L \sqrt{L_o} / k_o A$ where L is length, prime denotes helium subscript o denotes properties at the warm end temperature and $L_o = k_o \rho_o / T_o$ is the WFL constant at the warm end. Table XXII gives MOP and JOP, the dimensionless parameters for the present leads at optimum current (4 kA) and mass flow, using $L = 61$ cm, the length excluding the heat sink.

Table XXII				
Runaway Current from Jones et al				
$X_u\%$	12.5	25	37.5	50.*
MOP	11.3	6.91	5.59	49.8
JOP	5.92	4.24	3.60	25.0
MOP/ $M_{bo}\%$	111.	115.	131.	106.
M_b (6 kA)	22.0	12.6	9.8	97.
$M_b/M_{bo}\%$	113	108	110	108
*Copper				

JYSY does not give M and J for minimum refrigeration; instead they concluded that optimum operation of a lead was with M about 14 and J equal to 85% of the burn-out value. Table XXII also gives the ratio of MOP to M_{bo} , the burn-out value of M for JOP, taken from Fig. 2 of JYSY. In view of this uncertainty of 10% or so in correlation, and considering the unknown behavior of the heat sink during a runaway situation, it would seem prudent to allow an additional 10% or more in gas flow if current transients 10 times or more current, as mentioned by JYSY are possible. Rates of rise given by JYSY would not apply to aluminum leads owing to the difference in heat capacity. In view of the various differences a transient analysis of the present leads would be worthwhile.

4.3.3.4. Heat Sinks Made of Different Materials

The temperatures T_n at the warm end of the heat sink, shown in Table XIX suggests that an optimized lead should have $T_n = T_c$.

A recent Soviet paper³³ appears to say the same thing for the case of WFL materials and perfect heat transfer. T_n can be increased by increasing the thermal resistance (a lower RRR), increased length or decreased cross-section. Increased length is generally undesirable and decreased cross-section may not be possible for mechanical reasons. Although thermal resistivity is not infinitely variable, a material can usually be selected with an RRR close to the desired value. One set of computations was made in which a lead made of the 120 RRR copper of Tables XVIII and XIX was fitted with a heat sink 30.5 cm long made of an RRR = 5 copper. The optimum area was found to be 1.1 cm² and the gas flow 0.179 g/sec at 4 kA. This combination lead thus seemed attractive but it was subsequently noted that with zero current, T_n rises to 34.9 K at the phenomenally low minimum gas flow of 0.048 g/sec. This suggests that the thermal impedance of the heat sink might be so great as to cause instability of some sort, especially under transient conditions, so this approach needs further study. It has promise, however, for standby leads.

4.3.3.5. Optimization Assuming $T_n = T_c$

If the transition section of the lead (that part of the lead without superconductor) is assumed to have a cold end temperature $T_n \approx T_c$, then a unique solution is obtainable. For the heat sink, equations (1) and (2) combine to give

$$dy/dx = \dot{m} C_p d\theta/dx \quad (9)$$

of which the integral is

$$y = \dot{m} (H - H_0) \quad (10)$$

where H is the enthalpy of the helium. If a gas inlet temperature of $\theta_i = T_c - \Delta T$ to the transition section is assumed (slightly less than T_c) than the new lower boundary condition is Eq. (10) where H is evaluated at θ_i . The cross-section thus obtained is then used to determine the heat sink length separately, for which boundary conditions $y = 0$, $\theta = 0$, and $T_i = T_c$ are assumed. Solutions obtained in this way are unique for the lead materials, have an absolute minimum of gas flows and are more quickly obtained than when using the procedure in which heat sink length X_s is allowed to vary. A few computations were made in this way, using different values of C_p for heat sink and transition section to more accurately represent ΔH in Eq. (10). It was noted that subsequent simultaneous optimization of the two parts gave substantially different results for heat sink lengths. Accordingly, the program was modified by the addition of temperature dependent C_p and H . Leads designed by the two step process are now substantially the same as leads designed with heat sink attached, although some mismatch occurs since ΔT must be guessed at for the two-step process. All computation, assume $\Delta T = 0.5$ K. The results of leads designed in this way are given in Table XXIII for aluminum and Table XXIV for copper.

Two of the leads of Table XXIII and XXIV were recomputed with attached heat sink. The aluminum lead with $RR = 12.3$ acquires a 28 cm heat sink and 0.1716 g/sec coolant flow. The copper lead with $RR = 20$ required a heat sink length of 52 cm and mass flow 0.1766 g/sec.

³³V. U. Maximov, A. I. Malykin, "Electric Power Losses of Current Input into superconducting Devices Cooled by Supercritical Helium," 1978 Applied Superconductivity Conference, Pittsburgh, Pa., Sept. 1978.

Table XXIII

Aluminum Leads Designed by Two-Step Process

RR	Area, cm^2	X_n, cm	$\dot{m}, \text{g/sec}$	$A \cdot \text{RR}/X_n, \text{cm}$
5	7.673	12.1	.1769	3.17
10.	5.700	21.	.1729	2.71
12.3	5.229	25.	.1715	2.57
20	4.253	34.	.1685	2.50
40	3.142	53.	.1658	2.37
60	2.623	68.	.1651	2.31
90	2.186	85.	.1647	2.32
120	1.920	100.	.1647	2.30

Table XXIV

Copper Leads Designed by Two-Step Process

RR	Area	X_n	$\dot{m}$	$A \cdot \text{RR}/X_n$
5	3.509	17.8	.1899	.986
10	2.804	30.0	.1813	.935
20	2.223	50.0	.1759	.889
30	1.926	65.5	.1740	.882
40	1.736	79.5	.1731	.873
60	1.496	104.	.1722	.863
90	1.292	135	.1718	.861
120	1.166	162	.1716	.864

The last column of Tables XXIII and XXIV is an empirical correlation. It was arrived at by noting that reducing the cross-section of the heat sink by a factor of two reduced X_n from 99.9 to 49.3 in the 120 RR heat sink. This says the heat sink length is proportional to the thermal conductance.

4.3.3.6. Effect of Reduced Heat Transfer

The heat exchanger of Fig. 41 was designed before the present computer program was available, and utilized heat flow numbers generated by the analytic solutions based on perfect heat transfer. The arbitrary assumption was made that a maximum $T - \theta$ of 2 K was necessary, and this led to a heat exchanger area/unit length equal to 10 times the periphery of a 5 cm dia lead. The present design gives a ratio of 5 in 6.5 cm dia lead, or about 2/3 desired. The present computer program gives a max. $\Delta T = T - \theta$ of 1.55 K at 174 K (in the aluminum lead with RR = 12.3), so it is reasonable to wonder if an area ratio of 5 is necessary.

The present program was modified to model a heat exchanger consisting of circular aluminum tubing of 0.8 cm o.d. and 0.09 cm wall helically wound around and brazed to an aluminum tube of 6.5 cm o.d. This gives an area ratio of 1.61, a factor of 3 less than the cross-section of Fig. 41 provides. In addition, the heat transfer coefficient is less by about 25% because of the increased hydraulic diameter (the flow cross-section is about the same).

A few comparisons are given in Table XXV and XXVI. Table XXV compares a few mechanical parameters and Fig. XXVI the gas flow at various currents.

Table XXV				
<u>Comparison of Two Lead Designs</u>				
<u>Tape</u>	<u>Area</u>	<u>X_n, 2-Step</u>	<u>X_n, Combined</u>	<u>Max $t-\theta$</u>
Fig. 41	: 5.229	25.	28.	1.55 (174 K)
Coiled tubing:	5.131	22.8	32.	6.2 (179 K)

Table XXVI						
<u>Coolant Flow at Various Currents for Two Geometries, g/sec</u>						
	Flow Rate, g/sec					
	I = 0	I = 2kA	I = 4kA	I = 6 kA	I = 8kA	I = 12kA
Coiled tubing	0.133	0.127	0.179	0.310	0.487	0.979
Fig. 41	0.115	0.129	0.172	0.297	0.458	0.908

Although a relatively large $t - \theta$ is observed, the performance of the coiled tubing heat exchanger is not much different from the more sophisticated heat exchanger of Fig. 41. Even at 12 kA, only 8% more coolant is required. The present program cannot analyze transient performance, but there is some indication that the simple heat exchanger is less stable near optimum flow at 4 kA.

4.4. Systems Engineering - Bonding of Cables

Three phase ac superconducting power cables have been designed up to the present with an outer conductor insulation layer rated at 10% of the cable voltage. The function of this insulating layer is to protect the cable against magnetically induced voltages between the outer conductor and the enclosing lead jacket.

Conventional single-conductor, metallicly sheathed cables in a three phase circuit have their sheaths either transposed or cross bonded to form a balanced trefoil circuit.³⁴ With a balanced load on the cable the voltages induced in each trefoil section of the sheath is zero. Fault currents containing a zero-sequence component do not balance and induce voltages and currents in each of the three sheaths. Current limiting techniques such as a sheath bonding transformer resistance or choke bonding are required to protect the sheath against excessive currents and dissipation.³⁵

Superconducting ac power transmission cables consists of three coaxial tubes symmetrically arranged in a cryogenic enclosure. Each coaxial tube is enclosed by a pressurized, cylindrical lead sheath and constitutes one of the phases of a three-phase systems. The coaxial tubes are superconducting and each coaxial phase pipe is self-contained, i.e., self shielded. The sheath is in a field-free region, with virtually zero induced voltage and zero sheath current. The effects of finite conductor Q , eccentricity of coaxial pipes, and asymmetry between phase pipes on cable performance have been studied and reported.³⁶ This investigation was limited to normal operating currents, i.e., current levels that are less than the critical current of the tapes.

Through-faults can result in line currents that will quench the superconducting tapes and destroy the zero field condition about the sheath. The induced sheath currents are calculated for two system configurations.

1. The sheath is periodically bonded to the outer conductor.
2. The sheath is insulated from the outer conductor.

³⁴ Underground Systems Reference Book, Edison Electric Institute, 1957, pages 3-36, thru 3-40.

³⁵ Halperin, H., and Miller, J. W., Reduction of Sheath Losses in Single-Conductor Cables, Journal A.I.E.E., Jan. 1929, pages 20-25.

³⁶ Meth, M. Calculation of Ground Currents in Superconducting Three-Phase AC Power Transmission Cables, PTP 63, Sept. 1976.

The magnitude of the bond-currents is calculated for the first configuration with different values of bonding resistors. The sheath to outer conductor (or neutral to ground) voltage is calculated for the second configuration; this voltage is termed the radial voltage. The fault condition is assumed to persist for $2\frac{1}{2}$ cycles and the temperature rise is calculated for the tapes and the sheath. Also the additional load on the refrigerator is determined. Fault and thermal calculations are performed for both aluminum and copper stabilized tapes.

4.4.1. Description of Cables

The cable studied conforms to the general characteristics of the 230 kV version designed for the Philadelphia Electric Company Study.³⁷ The general characteristics of the cable are given in Table XXVII.

Table XXVII	
<u>General Characteristics of Cable</u>	
Rated continuous power	5100 MVA
System voltage	230 kV
Rated continuous current	12.8 kA
Maximum fault current	125 kA
Inner conductor diameter	9.55 cm
Outer conductor diameter	12.62 cm
Sheath wall thickness	0.21 cm
Sheath inner diameter	12.92 cm
Tape thickness (2 superconducting and 2 stabilizer helices)	0.066 cm
Thickness of aluminum stabilizer	0.02 cm
Length of cable between refrigerator and expander	28 km
Temperature of helium refrigerant	6 K to 8.1 K

The cable is located in an enclosure with counterflow of the refrigerant as is shown in Fig. 45. The temperature of the conducting tapes approximate the profile of the inner flow; the lead sheath, to the temperature profile of the outer flow. Table XXVIII gives the temperature as a function of distance L from the refrigerator end of the cable. Values of all normal metal resistivities and skin-depths were calculated. The sheath is partially normal and partially superconducting as the transition temperature for lead corresponds to the temperature approximately half-way along a cooling loop, however, the actual transition also depends on the sheath current. All self and mutual inductances were calculated for the geometry of the PEC cable.

³⁷Forsyth, E. B. Preliminary Design for the PEC Transmission Study, PTP 57, April 1976.

Figure 45. Flow of refrigerant and temperature profiles.

Under fault conditions the transient behavior of a non-bonded cable system is summarized in Table XXIX. The performance of a bonded cable is summarized in Table XXX. From these calculations it appears that the outer conductor to sheath insulation may be eliminated. This will produce a useful reduction in the cost and diameter of the present cable design.

Table XXVIII		
<u>Temperature Profile of the Cable</u>		
<u>Distance from Refrigeration</u>	<u>Temperature of Tapes</u>	<u>Temperature of Sheath</u>
0	7 K	8 K
2	7.4	8.1
4	7.7	8.2
6	7.9	8.2
8	8.	8.1
10	8.1	8.
12	8.1	7.9
14	8.	7.7
16	7.9	7.5
18	7.8	7.3
20	7.7	7.0
22	7.6	6.7
24	7.4	6.5
26	7.2	6.3
28	6.9	6.

4.5. Fifth Avenue Facility

4.5.1. Refrigerator and Associated Equipment

Helium transfer lines and associated supports which connect the refrigerator and load, have been installed. Difficulties were experienced with the transfer lines due to leaks and poor workmanship. A visit to the manufacturer plant will certainly reduce the magnitude of these problems. It is expected that two PIP personnel will visit the MVE plant in November. Work continues on completing the system modifications to controls, storage tanks, valves, interlocks, piping, and supports to satisfy the design criteria established for the helium system operating with the superconducting cable enclosure connected. Two laser aerosol detectors are now installed in the system. They are located on the upstream and downstream sides of the absorber bed.

A minor problem still to be resolved is that a proper heat balance for the refrigerator has yet to be made. This is still under investigation. However the equipment runs well and performance is satisfactory for this stage of the work. The helium transfer lines were cold tested during the refrigerator runs. Exact performance has still to be determined.

Table XXIX
Transient Behavior of Non-Bonded Cable System for 125 kA
Fault and 2½ Cycle Duration

<u>Characteristics</u>	<u>Aluminum Stabilizer</u>	<u>Copper Stabilizer</u>
Peak sheath current	12 kA	33 kA
Peak Radial Voltage	20 volts	23 volts
Peak current in enclosure wall	74 A	226 A
Voltage drop along sheath, enclosure and ground	18.4 volts (0.65 mV/m)	57 volts (2 mv/m)
Temperature rise (over 8 K) in sheath	0.017 K	0.14 K
inner conductor	18.8 K	15 K
outer conductor	15 K	13 K
Heat energy generated in sheath	1.2 Joules/meter	10 Joules/meter
inner conductor	104 Joules/meter	310 Joules/meter
outer conductor	74 Joules/meter	207 Joules/meter
Additional heat load on refrigerator	179.2 Joules/meter	527 Joules/meter

The refrigerator system was operated three times during this reporting period. Data were taken to support our contention that the thermodynamic performance of one or more of the turbo-expanders was questionable. Table I shows the results of these tests. Turbine number XXXI is obviously a few percentage points better than the design and actually peaks at 73% at 80 K. Number 2 turbine has an efficiency of only 42.3% at the nominal operating temperature but a somewhat higher inlet pressure. This is almost 30 percentage points below the design value. The higher pressure would indicate that the mass flow rate through the turbine is not large enough to match the number one turbine. The number 2 turbine efficiency reaches a peak of 44.5% at 28 K which is also too high. Turbine number 3 is a joy to behold and the reason the refrigerator performs as well as it does. Exceeding the design efficiency by some 20 percentage points, it is helping make up for the number 2 turbines deficiency. It is not possible to determine the lower heat exchanger efficiencies with the present performance of the number 2 turbine so bad, but there are no indications that there is anything wrong in this area.

The above information has been transmitted to CTI Cryogenics and Sulzer for their analysis and comment. It is planned to meet the representatives in the near future to discuss the problem and arrive at a solution.

Table XXX
Summary of Bonding and Sheath Currents in Periodically Bonded Cable Under Fault Conditions

Stabilizer	Bonding Resistance (OHMS)	Bond Currents as Distance from Normal to Superconducting Transition (Peak values)							Sheath Currents (Peak values)	
		Superconducting Sheath			0	Normal Sheath			Super-conducting Sheath	Normal Sheath
		900 m.	600 m	300 m		300 m	600 m	900 m.		
Aluminum	10^{-6}	-	-	80A	8.5kA	25A	-	-	15.5kA	12kA
	10^{-5}	-	57A	640A	8.2kA	210A	7A	-	15.5kA	12kA
	10^{-4}	425A	780A	1.7kA	6.1kA	1.1kA	200A	7A	15.5kA	12kA
	10^{-3}	1kA	1.1kA	1.7kA	2.4kA	1.4kA	840A	570A	15.5kA	12kA
Copper	10^{-6}	-	-	105A	21.3kA	44A	-	-	42.5kA	32.5kA
	10^{-5}	-	64A	850A	21kA	43A	11A	-	42.5kA	32.5kA
	10^{-4}	500A	1kA	3.4kA	16.3kA	2.4kA	430A	85A	42.5kA	32.5kA
	10^{-3}	2.1kA	2.7kA	4.1kA	6.5kA	3.5kA	2kA	1.4kA	42.5kA	32.5kA

Table XXXI						
Turbine Efficiencies						
Turbine No.	Design			Actual		
	Inlet Temp. K	Inlet Press atm	Efficiency %	Inlet Temp K	Inlet Press atm	Efficiency %
1	90.0	15.1	67.0	94.5	14.9	70.1
2	22.2	8.4	70.2	22.7	9.4	42.3
3	8.0	14.0	50.0	7.3	14.9	70.2

4.5.2. 100 Meter Cable Enclosure

Considerable effort was directed at completing the welding, testing, insulating and pumping of the field joints on the MVA cable enclosure. Welding and testing the line went along without difficulty. Fixtures were made to apply the superinsulation. Some of the insulation at the ends of each enclosure section had to be removed because it was wet. Pumping the sections, after completing the closure, was a slow process because of the small pump-out port and the water vapor present in the insulating material. Average pumping time for each joint was 3 months. The criteria to establish adequate vacuum was 10 microns, with system isolated from pump, with a zero pressure increase in 24 hours. The vacuum pump-out seal off devices presented problems and they will be modified to include a combination pump-out relief system. Installation of these new pump-outs will occur during the next reporting period.

The six sections of cable enclosure fabricated by Minnesota Valley Engineering Co., have now been on site for over one year. During this period, the pressure in the factory sealed vacuum space has been monitored periodically in order to help predict the long term vacuum stability of the envelope.

Table XXXII summarizes the pressure changes that have occurred over the past year. Up to this time, the rate of rise does not appear excessive. The rate of rise is partially caused by surface outgassing, which is strongly temperature dependent. This can be verified by observing the pressure fluctuation caused by changes in ambient temperature. The pressure dropped significantly with the onset of cooler weather.

It should be noted that the "worst section" shown on Table XXXII is the spare section stored on the roof of the cable taping room in Building 820. The elevated temperature in this space could cause higher than normal pressure readings. By comparison, the "worst section" installed at the outdoor test site has a rate of rise of only 15 microns per year instead of 30.7 shown in Table XXXII.

Table XXXII

Pressure Changes in Vacuum Space of Six Cable Enclosures

	<u>Average of 6</u>	<u>Best Section</u>	<u>Worst Section</u>
Oct. 77 pressure reading μHg	7.5	8	8.3
Oct. 78 pressure reading μHg	20.4	11	39
Rate of rise $\mu\text{Hg}/\text{year}$	12.9	3	30.7
Apparent leak rate $10^{-7} \text{ atm cc/sec}$	8.3	1.9	20.0
Projected pressure after total of 5 years μHg^*	72	23	162
Projected pressure after total of 10 years μHg^*	137	38	315

Volume of vacuum space = 1537 liters/section

* At room temperature

4.6. Gas Analysis Program

The laser particle detectors have been used during running periods of the refrigerator to monitor effectiveness of the oil removal filters. Measurements continue to be very encouraging with levels of 5 ppb or less detected ahead of the charcoal/molecular sieve filter bed and about 20 ppb downstream of the bed. The larger downstream measurements indicate that dust from the bed is being picked up by the helium stream as it flows through the bed. One instrument was loaned to the Isabelle project for monitoring the inaugural run of the CVI refrigerator. Measurements of about 20 ppb were obtained during that run.

An additional on-going aspect of this project has been a life test of one of the laser particle detectors. The calibration/zero element of this unit was equipped with ball bearings lubricated with Bray Oil Company lubricant 3L38RP designed for outer space applications. These bearings have operated under varying conditions from vacuum to 250 psia helium for over 4000 hours to date and are still working satisfactorily.

4.7. Temperature Measurement

The various problems that have arisen in the past year with regard to temperature sensing sensors, and calibration have resulted in a study of the whole problem. This includes all facets of the problem with the hope that within

several months, all temperature sensors used in the 5-25 K range will be of a type reproducible (after temperature cycle) to a few millikelvin and calibrated in a consistent manner.

4.8. CVI Refrigerator

Installation of the refrigerator at the Isabelle magnet testing facility was completed to the point of making a test cooldown by the end of August. Some problems were encountered with the old Cosmotron 13.8 kV switchgear kicking out on compressor start up. On the 31st everything was operating and a successful cooldown was made with the machine operating in a closed loop. See Appendix V for Trip Report by Mr. John W. Bonn, CVI representative. Following this successful run it was agreed that the 13.8 kV switchgear had to be worked on to eliminate the erratic behavior.

On September 25th the machine was started again. This time it was connected to a large liquid helium storage dewar. By about noon it was producing liquid into the storage dewar. Later the liquefaction rate was determined to be 250 to 280 liters per hour and the liquid was being used to test magnets. At approximately 9:30 p.m. the second stage compressor shut down on several over temperature alarms. Immediate examination was made by the operator. He reported that the drive shaft of the 300 H.P. motor was cherry red in the region on the bearing. Later it was determined that some of the shaft had actually been hot enough to drip down the motor housing. The motor was sent to a motor rebuilding plant recommended by Allis Chalmers. Tests to date indicate no damage to the windings and a new shaft and bearing are being installed. These are various opinions on the cause of this failure and there are indications that something is wrong with the windings; however, normal tests have not turned up anything to account for the failure.

5. Visits, Meetings and Publications

5.1. Annual Review

The annual review was held at BNL on 11 August 1978. The schedule was as follows:

<u>Name</u>	<u>Place</u>	<u>Comments on Activity</u>
0900-1000	Conference Rm. Bldg. 480	Review of past year's highlights by E. B. Forsyth.
1000-1030	Conference Rm. Bldg. 480	Question period.
1030-1130	Bldg. 480	Visit to superconductor labs and dielectric labs.
1135-1230	Bldg. 820	Demonstration of winding machine.
1203-1340	Berkner, Room A	Lunch.
1340-1440	Laboratories Bldg. 815	Demonstration of 10 m test cable. Display of termination and pothead components.
1450-1550	Test Facility, Bldgs. 933 & 934	Tour of Fifth Avenue Site.
1600	Conference Rm. Bldg. 480	Review of review.

The following visitors attended the review:

<u>Name</u>	<u>Institution</u>
R. Bartlett	LASL
F. Edeskuty	LASL
T. Garrity	DOE
R. Hebner	NBS, Gaithersburg
S. Kalsi	GE
F. Khoury	NBS, Gaithersburg
F. Mopsik	NBS, Gaithersburg
M. Mulcahy	Boston Ed
M. Rabinowitz	EPRI
S. Walldorf	DOE

5.2. Visits to BNL

<u>Date</u>	<u>Visitor(s)</u>	<u>Affiliation</u>	<u>Purpose</u>
Apr. 14'78	Mr. E. D. Eich	Power Tech. Inc.	Consultation.
May 3'78	Dr. Nichols & 2 others	A. D. Little	Tour of Project.
May 19'78	Dr. M. Rabinowitz	EPRI	Discussion of polymer concrete bushings.
May 22'78	Environmental Science Group	Nassau Community College	B. Harrison gave a tour of Power Trans- mission Project.
June 6'78	Suffolk Department of Consumer Affairs		Toured Power Trans- mission Project.
June 16'78	Dr. J. Junchniewicz	NRC, Canada	Gave seminar on "Electrical Insulation for Cryogenic Cable"

<u>Date</u>	<u>Visitor(s)</u>	<u>Affiliation</u>	<u>Purpose</u>
June 23'78	N.Y. Chapter IEEE		Toured Power Transmission Project.
June 28'78	Dr. M. Epstein Dr. D. Bigg Dr. R. Ferrantino	Battelle Columbus Labs	Reviewed Battelle Tape Development Program and presented preliminary results on the study of a high modulus, oil permeable dielectric tape.
July 15'78	House of Subcommittee on Advanced Energy Technologies and Energy Conservation Res. Development and Demonstration		E. B. Forsyth discussed Power Transmission.
Aug. 11'78	Annual Review Meeting (List of Attendees attached, see Section 5.1.).		
Sept. 22'78	Dr. K. Tachikawa	NIM, Japan	Gave seminar on Superconducting Materials Research at the National Institute for Metals.
Sept. 22-24'78	Mr. Colin Walters	Rutherford Lab, U.K.	Gave seminar on Tokamak Coils Using Nb ₃ Sn Strip Superconductors. Discussed Nb ₃ Sn Tape Fabrication.

5.2.1. US-USSR Technical Exchange

Under the auspices of the US-USSR Technical Exchange Committee on Superconducting Power Transmission the following Soviet scientists visited the Power Transmission Project from 23 April through 6 May, 1978.

<u>Name</u>	<u>Institution</u>
E. Blinkov	Krzhizhanovsky Power Engineering Institute.
D. E. Rumyantsev	Krzhizhanovsky Power Engineering Institute.
V. M. Pan	Kiev Metallurgical Institute.

Their activities were primarily in the area of superconducting materials development. They also witnessed operation of the 10 m cable (Section 2.5).

5.3. Visits by BNL Personnel

<u>Date</u>	<u>Name</u>	<u>Place</u>	<u>Purpose</u>
Apr. 5'78	C. Klamut	Concord, MA	Discussed extrusion techniques used for fabricating superconductors.
May 2-4'78	A. McNerney A. Minardi J. Scrofani	Pittsburgh, Pa.	Visited Westinghouse Res. Labs. for consultation of bushing and travelled to Waltz Mill Test Site for cable analysis.
May 18'78	J. Jensen	Boston, Mass.	Visited CTI Cryogenics.
May 22-24'78	R. Gibbs	Westboro and Waltham, Mass.	Visited Helix Technology Inc.
June 11-14'78	M. Kosaki A. Pearmain R. Thomas	Pennsylvania	Attended 1978 IEEE Inter. Symposium on Elec. Insulation.
June 28'78	C. Klamut	Concord, MA	Witnessed extrusion of three BNL prepared billets.
July 2-14'78	E. Forsyth	London, U.K.	Presented invited paper at ICEC 7, discussed insulation development for cable and discussed superconducting tape development.
July 11'78	A. McNerney G. Morgan	Monmouth, N.J.	Visited Alto Development Corp.
July 13'78	J. Bunicci A. McNerney	Accident, MD	Visited American High Voltage Test Systems to witness acceptance test and consultation on capacitor bank design.
July 16-28'78	A. McNerney	Los Angeles, CA	Attended IEEE PES Summer Meeting, visited Lindsey Indus. Spatial Data Systems and & Lawrence Berkley Lab.
Aug. 23-25'78	E. Forsyth	San Diego, CA	Presented paper at 1978 Intersociety Energy Conversion Eng. Conf.
Aug. 28'78	C. Klamut	Reading, PA	Visited Kawecki Berylco to observe rolling of BNL prepared extrusions.

Sept. 24-28'78	M. Garber J. Bussiere G. Morgan R. Gibbs	Pittsburgh, PA	Presented paper and attended Applied Superconductivity Conference.
Sept. 25'78	C. Klamut	Reading, PA	Visited Kawecki Berylco to observe rolling of BNL prepared extrusions.

5.4. Publications

M. Garber, A 10 m Nb₃Sn Cable for 60 Hz Power Transmission, presented at the Applied Superconductivity Conference, Pittsburgh, Pa., September 1978.

J. Bussiere and J. Clem, Effect of Trapped Magnetic Flux on ac Losses of Nb₃Sn, presented at the Applied Superconductivity Conference, Pittsburgh, Pa., Sept. 1978.

E. B. Forsyth, Overview of Electric Transmission (Technology and Economics) presented at the 1978 13th Intersociety Energy Conversion Eng. Conf., Aug. 20-25, 1978, San Diego.

E. B. Forsyth, Superconducting Power Transmission - Progress at Brookhaven and the Prospects for this Technology, presented at the ICEC 7, London, England, July 4-7, 1978.

E. B. Forsyth, A. C. McNerney and A. C. Muller, Performance of Synthetic Materials in the Lapped Insulation of Cryogenic and Ambient Temperature Cables, to be presented at the 1979 IEEE/PES Conf. and Exposition on Overhead and Underground Transmission and Distribution, Atlanta, Georgia, April 1-6, 1979.

A. C. Muller, Properties of Plastic Tapes for Cryogenic Power Cable Insulation, presented at the 1978 ICMC Meeting, July 10-11, 1978, Munich, Germany.

F. I. Mopsik, Low Temperature Dielectric Loss Characteristics of Polypropylene, presented at the 1978 ICMC Meeting, July 10-11, 1978, Munich, Germany.

J. Bussiere and V. Kovachev, AC Losses of Bronze-Processed (Nb_{1-x}Zr_x)₃Sn Between 5 and 16 K, to be published in J. of Appl. Phys.

M. Suenaga, Degradation Mechanism of Nb₃Sn Composite Wires Under Tensile Strain at 4.2 K, presented at the Applied Superconductivity Conference, Pittsburgh, Pa., Sept. 1978.

INTERMAGNETICS GENERAL

CORPORATION

Appendix I

OPTIMIZATION OF Nb_3Sn TAPE MANUFACTURING PROCESSES FOR APPLICATION TO AC POWER TRANSMISSION BNL CONTRACT NO. 404101-S

Quarterly Report for the Period
1 July 1978 to 30 September 1978

Report Submitted by
INTERMAGNETICS GENERAL CORPORATION
Guilderland, New York 12084

IGC Report No. 1078-1
6 October 1978

P. O. BOX 566 • GUILDERLAND, NEW YORK 12084 • TEL. (518) 456-5456 • TWX 710-441-8238

1.0 GENERAL

Work during the Quarter ending 30 September 1978 was predominantly addressed to optimization of the cladding process. The investigative phase of this work was successfully completed. However, lamination of the tape, which constitutes a "deliverable item" for the task, was postponed in order to allow fabrication of the conductor with 21-6-9 stainless steel rather than with the 302 alloy as planned. The postponement was requested by BNL.

A paper reviewing recent results in this program was presented at the Applied Superconductivity Conference in September.

2.0 EXPERIMENTAL WORK

2.1 Cladding Process

This work was essentially concerned with optimizing the performance of IGC's tape laminating machine relative to the specific objectives of transmission line conductor. In addition to targets concerned with general product quality, e.g. bond strength and solder layer control, optimum inter-laminae edge registration was sought with the objective of maximizing the yield of slit product. For present operating standards this would entail three 6 mm product tapes from the 19 mm wide process strip, a particularly challenging objective.

An initial engineering review concluded that the existing machine was basically satisfactory for the accomplishment of these objectives without major modification. The conclusion was conditional, however, with respect to two factors of paramount importance:

- Precise edge guidance of the input foils through the soldering pass in the laminator
- Good control of input foil quality with respect to width, straightness and flatness specifications.

Accordingly, the laminator components, which regulate clearance relative to foil guidance, were remachined; the gap was set to a minimum clearance which would not cause binding. In addition, the machine was checked and adjusted comprehensively to insure conformance to operating specifications.

A series of experimental operations led to the production of sample laminates which exhibited inter-laminae registration within a few mils and which could be effectively slit to provide three 6 mm product tapes. Evaluation of the tapes was based on the customary inspection procedure and the examination of metallographic sections at intervals through the length of the samples. No incomplete laminae or other defects were found at the outside slits, and the solder layers were judged satisfactory relative to the requirements of Specification PTP 21A. In general, the experiments produced good clad tapes. All tape samples were fabricated with the standard combination of 25 μ m thick stainless steel and 50 μ m thick Cu; both "as heat treated" and "etched/tinned" superconductor were employed in various experiments. The work involved relatively short lengths of conductor; early runs were based on 25 - 50 ft lengths and a final laminate was 220 ft long.

Based on the favorable results obtained in the experimental trials, the decision was made to proceed with preparation of the 2000 ft lot of 6 mm tape. It should be recognized, however, that this product work constitutes a next-step proving experiment. Although the scope of work performed in the investigative phase is judged to be adequate based on the results observed, a substantial proof of process effectiveness requires a statistical basis, which can be practically obtained only in an actual product operation. Consequently, a comparatively generous quantity of "etched/tinned" superconductor has been made available in order to allow some flexibility in the lamination and slitting procedure. The superconductor,

which is now complete and ready for cladding, was produced in three nominal 500 ft runs. Operational results were entirely effective, and although quality assurance data are not fully complete, no problem is anticipated in satisfying ac loss and critical current criteria.

In the course of this task some work was devoted to the evaluation of 21-6-9 stainless steel, which had been coated with copper by high rate magnetron sputtering. The evaluation, which was requested by BNL, assessed the utility of the coated foil for tape fabrication by an initial examination of soldering behavior and other properties; subsequently, a short laminate was produced. Generally satisfactory behavior was observed and, in particular, the solder bond was found to be substantially stronger than the tin coated stainless employed thus far. A 6 mm x 18 ft conductor strip from the laminate was submitted to BNL for further evaluation.

Based on the overall satisfactory cladding behavior of the copper coated stainless in combination with the superior loss trait of the 21-6-9 alloy, BNL has requested that IGC utilize the material for preparation of the 2000 ft lot of tape associated with the cladding process study. Product fabrication work is, therefore, held in abeyance pending receipt of the copper coated 21-6-9 stainless from BNL.

2.2 Other Work

Difficulty with ac loss measurements was noted in the preceding progress report. This problem was eliminated as a result of helpful and timely assistance by BNL. The wattmeter was taken to BNL for a complete recheck; certain updated components were installed, and the instrument was recalibrated. Subsequently, losses were determined for the four tapes of the 5700 ft lot of conductor

delivered to BNL in June. Measurements were made on duplicate samples of both "start" and "finish" ends in the "etched/tinned" state. The average loss property for the conductor as described by these measurements was $9.9 \mu\text{W}/\text{cm}^2$ at a surface current density of 500 rms A/cm; the average deviation in these data was $\pm 1.2 \mu\text{W}/\text{cm}^2$.

The influence of input superconductor modifications was reviewed in the preceding Quarterly Report. That discussion concluded on the basis of somewhat indirect evidence that the ac losses of etched product depend upon the extent of Nb_3Sn formation in the input superconductor. That conclusion was verified by the results of an experiment performed early in the present report period. The new data demonstrated that product losses increase quite sensitively as the extent of Nb_3Sn characterizing the input superconductor is increased. These results will be discussed more fully in the final report for the contract.

3.0 NEXT QUARTER

The present contract is scheduled to terminate at the end of the next quarter. The remaining work comprises cladding the 2000 ft lot of conductor, which constitutes the balance of the "deliverable items" commitment, and preparing the final status report.

Appendix II

RE-78-155-CRE-99

CONTINUATION OF RESEARCH AND DEVELOPMENT
EFFORTS RELATIVE TO SUPERCONDUCTING MATERIALS

PHASE II

CONTRACT NO. EY-76-C-02-0016

BNL CONTRACT NO. 404100

Letter Report Covering the Period

July 1, 1978 to October 1, 1978

SUBMITTED BY

Airco, Inc. Central Research Laboratories

100 Mountain Avenue

Murray Hill, New Jersey 07974

October 1978

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1. INTRODUCTION

During this reporting period, the effort has been directed toward rolling the three bronze/niobium - 1% zirconium extrusions.

2. BRONZE/NIOBIUM EXTRUSIONS

Three configurations of bronze/niobium extrusion billets are used as the basis for this effort. The starting material consists of a large 305 mm bronze casting which is extruded to 115 mm diameter. The extrusion was homogenized at 700°C for 66 hours in an argon atmosphere. Analysis of the bronze indicates an average tin content of 12.4 weight percent. Three bronze sections of 110.5 mm diameter and 406.4 mm length are used to fabricate the composite billets.

Billet 1, shown in Figure 1a, consists of a single niobium - 1% zirconium rod in the center surrounded by an OFHC copper tube machined to fit between the bronze matrix and the core. The purpose of the copper tube is to prevent reaction of the bronze and niobium during the extrusion and annealing steps which could lead to poor bonding.

Billet 2, shown in Figure 1b, is similar to Billet 1 without the copper tube.

Billet 3, shown in Figure 1c, has a rectangular cross section niobium core. The bronze is first cut in half lengthwise and the appropriate opening milled into each half. The two halves and the niobium core are assembled and the bronze is electron beam welded along the length.

Copper discs are electron beam welded to both ends of each assembly to complete the billet. The thickness of the copper end plates is 6.3 mm. Each assembly is sealed in an evacuated copper can consisting of the 1.65 mm wall thickness copper tubing and 3.2 mm ends.

Extrusion conditions for each billet are listed in Table 1. All billets are extruded using a rectangular die of 12.7 mm x 76.2 mm dimensions. Billet 3 is oriented manually by the extrusion press operator to align the die and the rectangular core. Billet fabrication, assembly and extrusion were performed by BNL personnel.

3. INSPECTION OF EXTRUSIONS

The front ends of all three extrusions show fractures of the copper end plate to bronze joint. The photograph in Figure 2 illustrates this condition. The front of number 1 has fallen off while the curved fracture can be seen on numbers 2 and 3. The niobium cores are exposed behind the

fracture in the cases of extrusions 2 and 3. The core of number 1 could not be seen at this point.

Poor bonding of the copper can is seen along the lengths of the extrusions. Extrusion 3 is illustrated in Figure 3. This extrusion shows the worst case of delamination visible. The "bubbles" of delamination appear most frequently on one side and are thought to be only the copper can material.

The back ends of all extrusions appear to be sound. No evidence of end plate fracture is visible.

4: ROLLING

The rolling schedule is 40% reduction in area per roll pass with intermediate annealing between each reduction. Annealing consists of one hour at 455°C in an argon atmosphere. The initial passes are made using a four high rolling mill with 203.2 mm diameter work rolls. Later rolling (below 1.6 mm) is done on a Sendzimir mill.

Extrusion 2 is being rolled one step ahead of 1 and 3 to point out potential difficulties in the process. Cutting of extrusion ends is kept to a minimum to avoid contamination or oxidation. Ends are sealed by TIG welding in an argon glove box prior to annealing treatments.

Cross sections of extrusions 1 and 3 are shown after the first reduction to 7.6 mm in Figure 4. The cross section of extrusion 2 is very similar to 1. The billet design of 1 and 2 result in a very narrow strip of uniform niobium thickness. The core of extrusion 3 is much wider, although the ends of the core are thicker than the uniform portion. The alignment of the rectangular core was not exact as the core is not parallel to the surface.

Details of the rolling and annealing procedure to date are given in Table II. Although no cutting of the extrusions was planned, the front ends had to be cut and welded due to their failure to seal during extrusion. Initially a bronze filler was used for welding. The bronze is found to fracture during rolling and was replaced with a copper filler. The copper seems to hold up satisfactorily. Cutting of square ends is necessary at the end of stage 4 to accomodate the grips for front and back tensioning on the Sendzimir mill. All welding is done by TIG methods in an argon glove box.

The poor bonding of the copper can during extrusion has resulted in edge cracking of the copper during rolling. These jagged edges must be removed manually to allow guiding on the Sendzimir mill.

A tear at the back end of extrusion 3 at 1.65 mm thickness

requires cutting of a short length for gripping purposes. This cutting indicates some delamination at the back end. The niobium is much thicker relative to the bronze at this end than at the front. The extent of delamination is not known at this stage. Extrusions 1 and 2 appear sound.

5. FUTURE WORK

The rolling/annealing schedule will be completed to the final thickness of 0.13 mm as planned. Further work is expected to follow the steps outlined in the proposal.

PB:EM

Figure 1a. Billet 1: 13% Sn bronze, O.D. = 110.5mm; OFHC Cu tube, O.D. = 26.9mm; Nb-1% Zr rod, O.D. = 25.4mm. Length = 406.4mm.

Figure 1b. Billet 2: 13% Sn bronze, O.D. = 110.5mm; Nb-1% Zr rod, O.D. = 25.4mm. Length = 406.4mm.

Figure 1c. 13% Sn bronze, O.D. = 110.5mm; Nb-1% Zr plate, 25.4mm x 101.6mm. Length = 406.4mm.

Figure 2. Front ends of extrusions.

Figure 3. Delaminations of copper can - extrusion 3.

Figure 4. Cross sections of extrusions 1 (top)
and 3 (bottom).

TABLE I: EXTRUSION CONDITIONS

<u>Billet No.</u>	<u>Furnace Temp. °C</u>	<u>Die Temp. °C</u>	<u>Liner Temp. °C</u>	<u>Upset Pressure MPa</u>	<u>Running Pressure MPa</u>	<u>Speed mm/sec.</u>
1	650	480	480	317	266	6.3
2	650	480	480	312	248	6.3
3	650	480	480	425	301	6.3

TABLE II: ROLLING/ANNEALING SCHEDULE

<u>Stage</u>	<u>Thickness</u>	<u>Reduction (%)</u>	<u>Comments</u>			
			<u>Extrusion #1</u>	<u>Extrusion #2</u>	<u>Extrusion #3</u>	<u>All</u>
Initial	12.7 mm	--	Front end cut, welded with bronze	Not welded	Front end cut, welded with bronze	Anneal-455°C, one hour
1	7.6 mm	40	Reweld front	Mill slippage, weld front with bronze	Reweld front	Anneal
2	4.7 mm	38	Reweld front with copper filler	Reweld front with bronze	Reweld front with copper filler	Edge cracking of copper can. Anneal.
3	2.8 mm	40	--	Reweld front with copper filler	--	Anneal. Copper welds are not breaking.
4	1.65 mm	41	Back end cut for Sendzimir	Back end cut straight for Sendzimir	Both ends cut, and rewelded. Evidence of delamination at back end.	Anneal. Cracked edges of copper can must be removed for guiding on Sendzimir.
5	1.02 mm	38	Not rolled yet	Front tension only	Not rolled yet	First roll on Sendzimir mill. Anneal.

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Appendix III

Semi-Annual Report (April-October, 1978)

from

Polymer Science and Standards Division

National Bureau of Standards

on

The Structure, Dielectric Properties, and Compressibility of
Polymer Tape Insulation for High Power Transmission Cables

Synopsis

The dielectric loss of the PP-U-PP(C) and 3PP-2U(B) polypropylene-polyurethane laminates was measured at 4.2K to 323K at 100Hz and 1kHz (Section 1.1). The results are compared to data previously obtained on other polypropylene-polyurethane laminates. A detailed discussion of the origins of the differences in dielectric loss between the laminates in the range 4.2K to 10K is presented in which the relative contributions of the polypropylene and polyurethane layers to dielectric loss are examined. Evidence is described which indicates that the polypropylene layers undergo small decreases in dielectric loss at 4.2K to 10K as a result of the heat treatments they are subjected to in the lamination process. In addition, it has been found that high polyurethane contents in the laminates can cause a substantial increase in dielectric loss at 4.2K to 10K to levels substantially in excess of the criterion that $\tan \delta$ should preferably not exceed 20×10^{-6} in the insulation of superconducting cables.

Four other aspects of our investigations on polymer tape insulation are also reported. The occurrence of voids in the dyed (red) polyurethane layer of the 3PP-2U(C) polypropylene-polyurethane laminate is described (Section 1.2). A special cell for measuring the dielectric loss of films or tapes in the range 298K to 423K is now in operation, and preliminary results on the Polypropylene-A tape are reported which indicate that heating up to 383K causes irreversible changes in the dielectric loss of this material (Section 1.3). The dielectric loss of the Polyethylene-E film has been determined at 4.2K to 323K (Section 2). The compressibility characteristics of the 3PP-2U(C) laminate, three high density polyethylene films, and a sample of kraft paper have also been determined (Section 3).

1. Structural Aspects (F. Khoury, L. Bolz) and Dielectric Properties (F. I. Mopsik, S. J. Kryder) of Polypropylene and Laminated Polypropylene-Polyurethane Tapes.

1.1. Dielectric Properties of Polypropylene-Polyurethane Laminates

As part of the program for evaluating polymer tape insulation for superconducting high power transmission cables, the dielectric loss characteristics of the PP-U-PP(C) and the 3PP-2U(B) laminates were determined between 4.2K and 323K inclusive. The results are listed in Table I and plotted in Fig. 1. For the purpose of comparison, the corresponding $\tan \delta$ vs. temperature data for the three polypropylene-polyurethane laminates which we have previously studied [PP-U-PP(A) in Ref. 1; PP-U-PP(B) and 3PP-2U(A) in Ref. 2] are also plotted in Fig. 1. Data for the original Polypropylene-F film from which all the laminates were made are also included in Fig. 1.

The PP-U-PP(C) and the 3PP-2U(B) exhibit three features in common with the other laminates namely: (a) A sub 4.2K loss peak evidenced by an upswing in loss with decreasing temperature from 10K to 4.2K. (b) A broad, but prominent, loss peak centered at 150K. (c) A dramatic increase in loss above 225K. As indicated in a previous report (Ref. 3, Section 1.2.1) the latter two features are due to loss processes associated with the polyurethane binder layer(s). In contrast, the sub 4.2K loss peak is a characteristic of the polypropylene layers, and may be attributed to the presence of antioxidant in the original Polypropylene-F [Refs. 3,4]. As can be seen from Fig. 1, there are substantial differences in $\tan \delta$ among the laminates over the entire temperature range. Confining ourselves to temperatures below 200K, we have already pointed out, above, that the loss peak at 150K is characteristic of the polyurethane binder. On the basis of what is known about the relative proportions of polyurethane in the various laminates (there are uncertainties concerning the thickness(es) of the polyurethane layers in them), the magnitude of the 150K peak appears to increase with increasing polyurethane content. It is of interest to note in this connection that in comparison with the other laminates, this peak is strongest in the PP-U-PP(C) and in the 3PP-2U(B). We shall return to this point in due course in the ensuing examination and discussion of the dielectric loss characteristics of the PP-U-PP(C), the 3PP-2U(B), and the other laminates, in the temperature range of interest for superconducting cables, namely, 4.2K-10K.

The values of $\tan \delta$ for all the laminates at 4.2K to 10K are listed in Table II (columns 1 to 5). For reasons which will become apparent later, we also include in columns 1-5 the values of $\tan \delta$ corresponding to the peak at 150K. In anticipation of further discussion, the loss data at 4K-10K, and 150K for the original polypropylene-F film, and the Polypropylene-F after annealing for 2 hrs. in Argon at 408°C are also listed in Table II (columns 6 and 7, respectively, from Ref. 3).

The aspects of interest concerning the laminates are the relative contributions of the polypropylene and polyurethane layers to $\tan \delta$ in the region 4.2K-10K. It is important, in this connection, to bear in mind that the dielectric loss of polypropylene films in the range 4.2K-10K is apparently influenced by two distinguishable factors which we have discussed in some detail elsewhere [Refs. 3,4]. One factor has been mentioned above, namely, the presence of antioxidant which gives rise to a loss peak below 4.2K. The high temperature tail of that peak extends to 10K and accounts for the manifestation of an increase in loss with decrease in temperature between 10K and 4.2K. The second factor is the presence, in polypropylene films, of a broad loss peak of unknown origin centered at 30K. The low temperature tail of that peak extends to 10K and below. The overall dielectric loss of polypropylene films at temperatures in the 4.2K-10K range is thus determined by the superposition of the losses corresponding to the high temperature tail and low temperature tail of the peak below 4.2K and the peak at 30K respectively. Because

the Polypropylene-F film is subjected to heat treatments in the process of lamination with polyurethane, the effect of such heat treatments on the dielectric loss of the polypropylene is a factor which must be taken into account in considering what are the relative contributions of the polypropylene and polyurethane layers to $\tan \delta$.

As we have indicated in a previous report [Ref. 3] the effect of annealing the Polypropylene-F at 408K is to reduce or suppress the loss peak at 30K, with the result that the contribution of the low temperature tail of that peak to the dielectric loss in the 4.2K-10K range is lowered. This effect is illustrated by the data in columns 6 and 7 in Table II. It is also evident from the same data that annealing the Polypropylene-F at 408K did not eliminate the antioxidant related upswing in $\tan \delta$ with decrease in temperature in that temperature range. The relevance of these features of the effect of heat treatment on the dielectric loss of Polypropylene-F film is illustrated by comparing the losses at 4.2K to 10K of the PP-U-PP(A) and PP-U-PP(B) laminates (columns 1 and 2) with the corresponding losses exhibited by the original (unannealed) Polypropylene-F. As can be seen from the data given in Table II, these two laminates exhibit lower dielectric loss in that temperature range than the original Polypropylene-F film from which they were made. Interestingly, the losses of the PP-U-PP(A) and PP-U-PP(B) are in closer agreement with those exhibited by the annealed (at 408K) Polypropylene-F. Accordingly, it appears that the heat treatments involved in the lamination process had a similar effect (lowering $\tan \delta$) on the polypropylene layers in these laminates as annealing at 408K had on the Polypropylene-F, even though the nominal processing temperatures involved in lamination (366K-372K) were appreciably lower than 408K. It is also evident that any contribution of the polyurethane binder layers to the dielectric loss of these two laminates in the range 4.2K to 10K is negligible compared to the contribution of the polypropylene layers.

The PP-U-PP(A) and PP-U-PP(B) laminates exhibit the lowest values of $\tan \delta$ corresponding to the loss peak at 150K which is associated with the polyurethane binder layers. Examination of the loss data given in Table II for the other laminates shows that there is a progressive increase in the magnitude of the loss peak at 150K from column 1 to column 5. Correspondingly, and allowing for some deviations, the losses at 4.2K to 10K in the laminates listed in these columns tend to increase in the following order: PP-U-PP(B), PP-U-PP(A), 3PP-2U(A), PP-U-PP(C), 3PP-2U(B).

Assuming that the effect of the heat treatments involved in the production of all the laminates is to reduce the losses of the polypropylene at 4.2K-10K to the same extent caused by annealing the Polypropylene-F at 408K as was apparently the case for the PP-U-PP(A) and PP-U-PP(B), then it may be reasonably argued that the trend toward larger losses in the 3PP-2U(A), PP-U-PP(C) and 3PP-2U(B) at temperatures in the range 4.2K to 10K is due to an increasing polyurethane content in these latter three laminates. It is interesting in this connection that the losses in the PP-U-PP(C) and 3PP-2U(B) are, on the whole, comparable to those in the unannealed Polypropylene-F film. This feature suggests that, although the effect of the heat treatments involved in the lamination process is to reduce the dielectric loss in these layers at 4.2K to 10K to the levels exhibited by annealed (408K) Polypropylene-F, the decreases in the contribution of the polypropylene to dielectric loss at these temperatures are compensated by loss enhancing contributions from the polyurethane layers. Although the contribution of the polyurethane layer(s) to the dielectric loss of the PP-U-PP(C) and 3PP-2U(B) at these low temperatures is appreciably smaller than that of the polypropylene layers, the fact that the contribution of the former is certainly not totally negligible points to the importance of controlling the volume fraction of polyurethane in polypropylene-polyurethane laminates. This point is emphasized by the results of an experiment described below.

We have obtained direct evidence which illustrates that the contribution of the polyurethane binder to dielectric loss in the range 4.2K to 10K can be enhanced substantially by increasing the volume fraction of the polyurethane in polypropylene-polyurethane laminates. This effect was demonstrated by measuring the dielectric loss of samples consisting of 2 layers of polypropylene and 2 layers of polyurethane from the PP-U-PP(C) laminate. These samples were prepared as follows:- One of the polypropylene layers was stripped away from the PP-U-PP(C) tape. The remaining polypropylene layer with polyurethane binder was then folded back on itself, thus forming a laminate consisting of two outer layers of polypropylene with a layer of polyurethane between them which was twice as thick as the polyurethane layer in the original PP-U-PP(C). The values of $\tan \delta$ for the resulting laminate, 2PP-2U, at 4.2K to 323K are listed in Table I and plotted in Fig. 2. For convenience in comparing with the other laminates, the values of $\tan \delta$ at 4.2K to 10K, as well as at 150K, are also listed in Table III. It is evident from Table III that doubling the thickness of the polyurethane as compared to the PP-U-PP(C) has resulted in a very substantial increase in $\tan \delta$ at all temperatures in the range 4.2K to 10K to values well above the criterion that $\tan \delta$ for the polymer tape insulation in superconducting cables should preferably not exceed 20×10^{-6} . As expected from previous discussion, the 2PP-2U exhibits a much more intense loss peak at 150K than the other laminates listed in Table II.

Finally, we have also measured the dielectric loss at 4.2K to 323K of the constituent polypropylene layers in the PP-U-PP(C). A single layer of polypropylene was stripped off the laminate. The layer was then folded in two and the measurements were carried out on the double layer. The results are listed under 2PP-OU in Table I, and are plotted in Fig. 2. The data at 4.2K to 10K and at 150K are also given in Table III. It can be seen from Table II and Table III that the dielectric losses at temperatures in the range 4.2K-10K exhibited by the 2PP-OU are lower than in the original Polypropylene-F and are comparable to those of the annealed (408K) Polypropylene-F. This latter feature clearly provides further confirmation that the heat treatments involved in the lamination of the Polypropylene-F with polyurethane result in small decreases in the dielectric loss of the polypropylene layers in the range 4.2K to 10K.

In summary, the following features can be pointed out concerning the dielectric loss of polypropylene-polyurethane laminates in the range 4.2K to 10K: (i) All the laminates exhibit an upswing in $\tan \delta$ with decrease in temperature from 10K to 4.2K. This feature is a characteristic of the Polypropylene-F film which has been used in all the laminates and is associated with the antioxidant in the polypropylene. (ii) The heat treatments involved in the lamination process result in small decreases in the dielectric loss of the Polypropylene-F layers at temperatures in the range 4.2K to 10K. The decreases are comparable to those caused by annealing the original Polypropylene-F film at 408K, even though the heat treatments involved in the process of lamination are carried out at nominally lower temperatures (366K-372K). (iii) The contribution of the polyurethane layer(s) to the dielectric loss of the laminates (as received from BNL) is either negligible or relatively minor at 4.2K to 10K compared to that of the polypropylene layers. Increasing the polyurethane content of the laminates tends to increase the contribution of these layers to dielectric loss. Measurements based on the PP-U-PP(C) indicate that doubling the thickness of the polyurethane results in substantial increases in $\tan \delta$ at temperatures in the range 4.2K to 10K to levels well in excess of the criterion for the insulation in superconducting cables, namely, that $\tan \delta$ should preferably not exceed 20×10^{-6} in that temperature range.

1.2. Aspects of the Fine Structure of the 3PP-2U(C) Laminate

The 3PP-2U(C) laminate consists of two layers of polyurethane interspersed between three layers of Polypropylene-F. One of the polyurethane layers is dyed red and the other is undyed. The laminate exhibits a mottled appearance which is clearly visible to the naked eye and which is due to the uneven distribution of the dyed polyurethane layer. In addition to this mottled appearance, intensely colored circular regions $\sim 1\text{mm}$ in diameter are also present in the dyed polyurethane layer. As many as nine of these features have been seen in 4 sq. mm of the laminate. Several of these features have been found to exhibit one or a few voids at or near their center. Examples of such voids which vary in lateral dimensions and which are apparently as thick as the dyed polyurethane layer are shown in Fig. 3 where they are identified by the letter (v). The diametrical sizes of the voids shown in this Figure range from 30 μm to 380 μm .

1.3. The Dielectric Properties of Polymer Tapes at Elevated Temperatures

A high temperature cell for measuring the dielectric properties of polymer tapes between room temperature and 423K has been constructed and is now in operation. This cell, which has a capacity for four samples at a time, will be used to determine the dielectric properties of polymer tapes above room temperature as part of a program aimed at selecting tapes suitable for use as the electric insulation in oil filled high power transmission cables.

The dielectric loss $\tan \delta$ of the Polypropylene-A tape is currently being measured at 303K and above. The determination of $\tan \delta$ at elevated temperatures is being carried out in steps designed to determine whether exposure of the samples to high temperatures causes the dielectric properties of the samples to change irreversibly. Initial experiments indicated that, within experimental error, the Polypropylene-A did not undergo irreversible changes in $\tan \delta$ when heated up to 333K. Irreversible changes did occur, however, upon heating up to 383K and 393K, as can be seen from the three sets of measurements of $\tan \delta$ given in Table IV and plotted in Fig. 4. Following the series of measurements made up to 383K in Run 1, the samples were cooled to room temperature after which $\tan \delta$ was redetermined at 10K intervals up to 393K. The process was repeated in the third run in which measurements were made up to 403K. Temperature control in these experiments was better than 0.1K and the samples were allowed to dwell within 0.1K of the measurement temperature for at least half an hour in all cases.

The exact origins of the irreversible changes in $\tan \delta$ resulting from heating the Polypropylene-A remain to be determined. It should be emphasized that the samples were maintained in helium throughout the experiments so that oxidation is not the cause of the changes. Despite the differences in the measurements obtained in the three consecutive runs, the data obtained so far (see Fig. 4) are consistent with one another in that $\tan \delta$, in all three runs, first decreases and then increases with temperature between 303K and 380K-403K. Experiments are underway to determine the origin of the irreversible changes in the dielectric loss of the Polypropylene-A caused by heating, and to determine whether this tape can be stabilized against such irreversible changes by appropriate annealing treatments.

2. The Dielectric Loss Characteristics of the Polyethylene-E Film (F. I. Mopsik and S. J. Kryder)

The dielectric loss of an experimental polyethylene film, Polyethylene-E (which has a faint pink color and which contains an antistatic agent), was determined at 4.2K to 323K. The data are listed in Table V and are plotted in Fig. 5. As can be seen from the data, there is no upswing in $\tan \delta$ with decrease in temperature from 10K to 4.2K. In short, the antioxidant related loss peak below 4.2K which we have observed in other polyethylene films in earlier studies [Ref. 5], and which has been reported by others (e.g. Ref. 6), is apparently absent in the Polyethylene-E. In the range 4.2K to 10K $\tan \delta$ is 10×10^{-6} which is appreciably below the criterion of 20×10^{-6} for the insulation in superconducting cables. The film exhibits two pronounced loss peaks centered at 150K (γ -peak, $\tan \delta = 2.36 \times 10^{-4}$ at 100Hz) and 235K ($\tan \delta = 1.5 \times 10^{-3}$ at 100Hz). It should be pointed out that very erratic results were obtained with a specimen of the Polyethylene-E. The origins of the erratic data are not known. It may be noted, for further reference, that the Polyethylene-E films left a faintly discernible residue on the electrodes of the low temperature dielectric cell.

3. Compressibility of Polymer Films and Kraft Paper (R. W. Penn)

Compression tests were carried out on the following materials:

- The 3PP-2U(C) polypropylene-polyurethane laminate.
- Three different high density polyethylene (HDPE) films whose respective thicknesses were 100 μ m, 125 μ m, 180 μ m.
- Kraft paper (0.005" thickness).

The HDPE films and the kraft paper are those listed in a letter from A. C. Muller R. W. Penn dated 9/29/78.

Circular discs (1 inch diam.) were cut with a die from the sheets. Sufficient discs were cut from each material to make stacks approximately 0.3" high. The stacks were loaded at a rate of 2 pounds/sec. up to 100 pounds. The stress-strain curves for these materials are very non-linear and the zero load thickness is poorly defined. We, therefore, report tangent moduli, E_3 , at stress levels of 20, 40, 60, 80 and 100 psi. The results are listed in Tables VI to X. As in the case of previous compressibility measurements, we list, in Table XI the results of similar measurements on APS paper. Three separate stacks of films or sheets were tested for each material listed above. As can be seen from the data on the HDPE samples given in Tables VI to VIII, there is an apparent correlation between the compressibility of the stacks of these films and the number of layers of film in the stacks, which number was, of course, largest for the thinnest HDPE (100 μ).

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Table I.

Polypropylene-Polyurethane Laminates
Dielectric Loss ($\tan \delta$) at 100Hz and 1kHz

T(K)	PP-U-PP(C) $\tan \delta \times 10^6$ at 100Hz (1kHz)	2PP-2U* $\tan \delta \times 10^6$ at 100Hz (1kHz)	2PP-OU* $\tan \delta \times 10^6$ at 100Hz (1kHz)	3PP-2U(B) $\tan \delta \times 10^6$ at 100Hz (1kHz)
4.2	33(95)	41(88)	30(85)	39(105)
6	27(77)	37(75)	21(69)	28(83)
8	22(51)	36(65)	16(52)	21(63)
10	21(52)	34(54)	11(41)	17(53)
30	26(42)	58(60)	11(30)	18(39)
50	40(41)	86(65)	12(18)	34(39)
80	87(79)	136(105)	26(22)	92(83)
100	130(120)	198(156)	43(37)	130(123)
125	176(174)	270(222)	57(54)	181(173)
150	218(217)	345(300)	41(48)	232(225)
175	201(243)	328(345)	40(41)	217(258)
200	162(214)	279(342)	47(44)	179(228)
210	169(209)	-	-	-
225	187(206)	298(281)	45(48)	220(232)
235	316(292)	-	-	-
250	756(666)	1054(682)	54(58)	792(725)
273	1135(1163)	1511(1450)	109(91)	980(1040)
298	1125(1235)	1490(1476)	199(185)	1040(1036)
323	1650(1350)	1944(1632)	315(295)	3036(1500)

*One of the polypropylene layers was stripped from the PP-U-PP(C) laminate. The stripped layer of polypropylene was then folded on itself. The resulting double layer is identified as 2PP-OU. The remaining polypropylene film with the adhering polyurethane was folded on itself to form a sandwich consisting of two outer polypropylene layers and two inner polyurethane layers. The resulting sample is identified as 2PP-2U. See text.

Table II

Dielectric Loss, $\tan \delta \times 10^{-6}$ at 100Hz						
	1	2	3	4	5	6
	PP-U-PP(B)	PP-U-PP(A)	3PP-2U(A)	PP-U-PP(C)	3PP-2U(A)	Polypropylene-F Unannealed
						7 Polypropylene-F Annealed 408K
4.2K	28	32	35	33	39	32
6K	21	22	25	27	28	26 (est.)
8K	18	17	19	22	21	21
10K	15	15	15	21	17	23
150K	150	163	194	218	232	54

Table III

Dielectric Loss, $\tan \delta \times 10^{-6}$ at 100Hz			
	PP-U-PP(C)	2PP-2U*	2PP-OU*
4.2K	33	41	30
6K	27	37	21
8K	22	36	16
10K	21	34	11
150K	218	345	41

*See footnote in Table 1 and text for description of these samples.

Table IV

Dielectric Loss ($\tan \delta$) of Polypropylene A: Effects of Temperature Cycling

Temp (K)	Cycle 1	Cycle 2	Cycle 3
	$\tan \delta \times 10^{-6}$ at 100Hz (1kHz)	$\tan \delta \times 10^{-6}$ at 100Hz (1kHz)	$\tan \delta \times 10^{-6}$ at 100Hz (1kHz)
303	110 (90)	77 (97)	136 (163)
313	106 (79)	66 (84)	109 (139)
323	64 (62)	62 (69)	87 (115)
333	58 (54)	71 (62)	78 (91)
343	60 (50)	97 (82)	75 (75)
353	65 (60)	89 (83)	66 (76)
363	70 (63)	103 (98)	65 (87)
373	79 (68)	117 (104)	105 (72)
383	86 (65)	129 (119)	145 (94)
393		170 (118)	202 (120)
403			294 (133)

Table V

Dielectric Loss ($\tan \delta$) of the Polyethylene-E Film

Temp (K)	$\tan \delta \times 10^{-6}$ at
	100 Hz (1 kHz)
4	10 (15)*
6	10 (9)*
8	9 (11)*
10	10 (8)*
	7 (6)
30	18 (13)*
	17 (14)
50	26 (19)*
	27 (21)
80	58 (45)
100	106 (87)
125	183 (174)
150	236 (246)
175	204 (265)
200	189 (220)
205	286 (262)
210	552 (370)
215	1060 (603)
220	1323 (785)
225	1400 (1456)
235	1554 (1554)
250	1412 (2017)
273	754 (1207)
298	814 (642)
323	2520 (810)

*These measurements were obtained in a separate run carried out after the sample had been allowed to warm up to room temperature following an initial run in which $\tan \delta$ was determined from 10K to 323K.

Table VIHDPE 100 μm^*

Stress (psi)	E_3 (psi $\times 10^{-3}$)	(Std. Dev.)	E_3 (N/m ² $\times 10^{-7}$)	(Std. Dev.)
20	1.84	(0.0557)	1.27	(0.0384)
40	3.29	(0.0416)	2.27	(0.0287)
60	4.67	(0.0518)	3.22	(0.0357)
80	5.83	(0.0199)	4.02	(0.0137)
100	7.17	(0.162)	4.94	(0.112)

Table VIIHDPE 125 μm^*

Stress (psi)	E_3 (psi $\times 10^{-3}$)	(Std. Dev.)	E_3 (N/m ² $\times 10^{-7}$)	(Std. Dev.)
20	2.4	(0.0608)	1.65	(0.0419)
40	4.24	(0.108)	2.92	(0.0744)
60	6.06	(0.255)	4.18	(0.176)
80	7.45	(0.151)	5.13	(0.104)
100	8.86	(0.0807)	6.1	(0.0556)

Table VIIIHDPE 180 μm^*

Stress (psi)	E_3 (psi $\times 10^{-3}$)	(Std. Dev.)	E_3 (N/m ² $\times 10^{-7}$)	(Std. Dev.)
20	3.11	(0.0608)	2.14	(0.0419)
40	5.63	(0.0819)	3.88	(0.0564)
60	7.77	(0.165)	5.35	(0.114)
80	9.83	(0.0966)	6.77	(0.0666)
100	11.6	(0.252)	7.99	(0.174)

*Samples listed in letter from A. C. Müller to R. W. Penn, 8/29/78.

Table IX

3PP-2U(C) Polypropylene-Polyethylene Laminate

Stress	E_3		E_3	
(psi)	(psi x 10^{-3})	(Std. Dev.)	(N/m ² x 10^{-7})	(Std. Dev.)
20	2.74	(0.583)	1.89	(0.402)
40	9.1	(0.941)	6.27	(0.648)
60	18.2	(2.25)	12.5	(1.55)
80	27.2	(4.74)	18.7	(3.27)
100	34.2	(7.29)	23.6	(5.02)

Table X

Kraft Paper 0.005 inch*

Stress	E_3		E_3	
(psi)	(psi x 10^{-3})	(Std. Dev.)	(N/m ² x 10^{-7})	(Std. Dev.)
20	0.374	(0.031)	0.258	0.0214
40	1.52	(0.297)	1.05	0.0205
60	4.14	(0.476)	2.85	0.328
80	6.77	(0.79)	4.66	0.544
100	9.24	(0.907)	6.37	0.625

Table XI

APS Paper

Stress	E_3		E_3	
(psi)	(psi x 10^{-3})	(Std. Dev.)	(N/m ² x 10^{-7})	(Std. Dev.)
20	0.639	(0.0144)	0.44	(0.0099)
40	1.07	(0.0153)	0.737	(0.0105)
60	1.46	(0.0208)	1.01	(0.0143)
80	1.78	(0.0153)	1.23	(0.0105)
100	2.08	(0.0056)	1.43	(0.0039)

*Sample listed in letter from A. C. Muller to R. W. Penn, 8/29/78.

Figure 1.

Figure 2.

(a)

(b)

(c)

(d)

Figure 3. Light optical micrographs showing examples of voids situated at or near the center of the randomly distributed, intensely colored regions (~ 1 mm in diameter) in the red dyed polyurethane layer of the 3PP-2U(C) laminate. As can be seen, the voids which are identified by the letter (v) vary widely in size. A small island of dyed polyurethane (pu) is situated at or near the center of each of the larger voids shown in (a) and (b).

Figure 4.

Figure 5.

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APPENDIX IV

FINAL SUMMARY REPORT

ON

A STUDY OF THERMAL INSTABILITY IN SYNTHETIC PAPER
INSULATED UHV CABLES

BY

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§. 1 INTRODUCTION

The continually increasing demand for electric power suggests that underground transmission capacity in the 1990's will have to be much greater, perhaps 10 GVA for a single circuit. Consequently the feasibility of bulk power transmission systems such as UHV cables, compressed gas insulated (CGI) bus, cryogenic cables and superconducting cables are being studied both technically and economically.⁽¹⁾⁽²⁾ For UHV cables, 750-1100kV class will be adopted as the next step. Conventional kraft papers and some new synthetic papers are being considered for the insulating material of such UHV cables. The kraft papers have been employed extensively for cable insulation up to 550kV, therefore much experience has been gained in manufacturing and installing cables with the material. This great store of basic data seems to put kraft papers at an advantage for UHV cable applications.

However, kraft paper has a comparatively high intrinsic dielectric constant and a high dielectric power factor, thus electric breakdown related to the thermal instability could be a crucial problem.

Thermal instability occurs when the heat generated by the alternating electric field exceeds the heat that can be dissipated to the surrounding medium. Accordingly it depends upon the thermal dissipation characteristics of the system. Past efforts to study the thermal instability of insulation have been directed to problems associated with the mechanism of electric breakdown. That is, the primary concern has been with establishing the intrinsic electric properties of the insulating materials. Most of these investigations have followed the early work of O'Dwyer and Whitehead.⁽³⁾⁽⁴⁾

In cables the problem is complicated by both ohmic heating of the conductor by the transmission current and the thick insulation wall. Therefore little attention has been given to thermal instability in the cable. Only the classic paper of Brazier refers to it. He showed that

the electric breakdown of oil-impregnated paper cable is not due to tracking alone but due to thermal effects also, and that the breakdown voltage of the cables therefore depends upon the cable sheath temperature.⁽⁵⁾

This research has been almost forgotten with the advent of low loss, low density, deionized-water washed, oil-impregnated papers. Recently, with the recognition of an imperative need for UHV cables, attention has once more been directed to the thermal instability problem. An accurate analysis of this phenomenon has been made for oil-impregnated kraft paper insulated cables. It has shown that this insulation system cannot be employed for UHV cables if intensive forced-cooling and very high oil pressure are not also employed.⁽⁶⁾⁽⁷⁾

On the other hand, considerable development has occurred with some new types of synthetic papers such as polyethylene, polypropylene and polysulfone because of their excellent dielectric properties and their high breakdown strength. As a consequence the highly oriented, crystallized, polyethylene paper and the low loss kraft paper laminated polypropylene paper show much promise as candidates for UHV cable insulation, because of better compatibility with oil and greater resistance to swelling in oil.⁽⁸⁾⁽⁹⁾

Unfortunately the dependence of the dielectric properties upon temperature is nonlinear in these new materials. It is therefore necessary to study whether or not these cables will suffer from thermal instability.

This was the purpose of the investigation to be described in this report. The organization of the report is as follows. At first, a bibliography on thermal instability in the insulating materials is briefly presented (Section 2). Then the thermal properties of underground power transmission cables - especially pipe type oil-filled cables - is discussed in the case of no-forced cooling cables (Section 3) and forced cooling cables (Section 4). In Section 5 a tentative design for a UHV synthetic paper insulated cable is proposed and the thermal stability of this cable is analyzed. Finally Section 6 is devoted to a discussion on the critical dielectric power factor that UHV synthetic paper insulated cables must possess.

§. 2 AN HISTORICAL BIBLIOGRAPHY ON THERMAL INSTABILITY

§2.1 THERMAL INSTABILITY IN INSULATING MATERIALS

Past efforts to study thermal instability in insulation have been directed to the mechanism of electric breakdown called thermal breakdown, that is, the primary concern has lain with establishing electric properties of the insulating materials. This thermal breakdown theory in the insulation is briefly summarized in the following:

The first definitive expression of the thermal breakdown appeared in a contribution of Miles Walker to the discussion on a paper of Skinner.⁽¹⁰⁾ In Walker's opinion, "when a dielectric is under electric stress, dielectric losses occur in it and even these small losses can produce an appreciable temperature rise in the material because of a small thermal conductivity. Moreover these dielectric losses tend to increase with temperature. Accordingly the slight elevation of temperature due to a loss leads to an increase in the losses; this in turn raises the temperature still further. However the higher the temperature of the dielectric above its surroundings, the greater will be the amount of dissipation heat. Therefore the criterion for stability will be considered as follows: the rate of increase of a dielectric loss with temperature shall be less than the rate of increase of heat dissipation with temperature in the system where there is an equilibrium state for heat."

However Walker never analyzed this phenomenon quantitatively. The first attempt to formulate a definite mathematical theory of thermal breakdown was due to Wagner.⁽¹¹⁾ He assumed that the dielectric of the cable had many local defects which were cylindrical filaments of small diameter and high conductivity, and that the heat generated in the filament did not dissipate along the filament but was conducted away into the surrounding dielectric. He obtained the stability condition and the critical voltage, but his findings did not agree well with the test results by Rayner and others,⁽¹²⁾ who showed that the breakdown voltage of the material depends invariably on the thermal conductivity of the electrode.

A mathematical treatment of a steady-state thermal breakdown in the one-dimensional case was first given by Dreyfus in 1924 and a more detailed

one was given by Fock in 1927.⁽¹³⁾ The former solved the following equation which led to the critical voltage:

$$\lambda \frac{d^2 T}{dz^2} + p_0 \exp \gamma (T - T_0) E^2 = 0 \quad (2.1)$$

where λ denotes the thermal conductivity of the dielectric, T_0 its surface temperature and p_0 and γ are constants.

Fock included the effect of the thermal resistance of the electrodes and developed the complete solution not only for the resistivity law $\rho = \rho_0 \exp(-\gamma T)$ but also for $\rho = \rho_1 \exp(B/T)$. Moon followed Fock's original paper and corrected its defects:⁽¹⁴⁾

- a) The assumptions are not explicitly stated.
- b) The results of Fock's theory have been limited to a thin insulation wall and short range of temperature.
- c) His method to calculate the breakdown voltage is a tedious cut-and-try process using three tables.

He first explicitly stated the assumptions:

- 1. All heat flow is in the Z-direction. This will be approximately true in practice if the ratio of plate thickness to electrode size is very small.
- 2. The material is perfectly homogeneous with no weak spots.
- 3. Only the direct-current case is considered, with the voltage raised so slowly that thermal equilibrium obtains at all voltages below the breakdown value.
- 4. The dielectric is in a homogeneous electric field.
- 5. No electrolytic polarization is present. That is, except for the effect of temperature, the resistivity of the insulating plate is independent of Z.
- 6. The thermal conductivity is independent of temperature.

Then he showed that the equation for thermal equilibrium is

$$k_1 \frac{dT}{dZ} + \frac{V}{\rho} \frac{dV}{dZ} = 0 \quad (2.2)$$

whose solution is

$$V^2 = 2k_1 \int_T^{T_m} \rho(T) dT \quad (2.3)$$

where $\rho(T)$ is the resistivity of the dielectric and k_1 thermal conductivity.

Boundary conditions are introduced, resulting in two simultaneous equations:

$$k_1 \int_{T_1}^{T_m} \frac{dT}{\sqrt{2k_1 \int_T^{T_m} \rho(T) dT}} - \frac{\lambda d}{2V_1} (T_1 - T_o) = 0 \quad (2.4)$$

$$2k_1 \int_{T_1}^{T_m} \rho(T) dT - V_1^2 = 0 \quad (2.5)$$

where λ is watts transmitted through unit cross-sectional area of dielectric surface per unit temperature difference across the surface.

Eliminating the unknown T_1 in Equations (2.4) and (2.5), we obtain the relations between T_m and T_o , giving a curve like that of Figure 2.2.

Figure 2.2 shows that if a sample of dielectric be tested by applying a constant voltage and gradually raising the ambient temperature, the maximum temperature T_m and the temperature T_o will be practically the same through a wide range. At sufficiently high values of T_o , however, T_m becomes somewhat higher than T_o . Finally, at P, thermal instability results and the sample punctures. The critical condition for thermal instability is introduced by differentiating with respect to T_m the equation which was used to eliminate T_1 and equating to zero. Thus he derived the expressions for breakdown voltage and calculated it numerically.

This work clarified the thermal breakdown situation in cases where the conductivity ρ is dependent on temperature only, but did not address conditions appropriate to materials with field-dependent conductivity.

Klein and Gafni represented the conductivity σ of silicon oxide by the relation

$$\sigma = \sigma_o \exp\{a(T - T_o) + bE\} \quad (2.6)$$

where a , b , T_o and σ_o are constant, and derived the thermal breakdown voltage of the thin film capacitor. (15)

Chou and Brooks studied (16) thermal breakdown in sodium chloride by

using a relationship of the form

$$\sigma(T, E) = C \exp \left\{ - \frac{1}{kT} (\psi - \beta E) \right\} \quad (2.7)$$

which was measured by Hanscomb. In addition Sze discussed the maximum dielectric strength of Silicon Nitride films with the thermal breakdown theory.⁽¹⁷⁾ He approximated the conduction current density J of the nitride film for high temperatures and high fields by

$$J = C_1 E \exp \left\{ - \frac{q}{kT} (\phi_1 - \sqrt{\frac{qE}{\pi \epsilon_0 \epsilon_d}}) \right\} \quad (2.8)$$

where

E is the electric field, q the electronic charge, ϕ_1 the barrier height, ϵ_0 the permittivity of free space, ϵ_d the dynamic dielectric constant, k the Boltzmann constant.

O'Dwyer distinguished the thermal breakdown from the other purely electrical breakdown⁽³⁾ and showed in his book that this breakdown is governed by the following equations:

$$C_v \frac{dT}{dt} - \text{div}(\kappa \text{grad} T) = \sigma E^2 \quad (2.9)$$

$$\text{div}(\sigma \mathbf{E}) = 0 \quad (2.10)$$

where C_v is the specific heat per unit volume, σ and κ are the electrical and thermal conductivities respectively. (For an A. C. field the d. c. conductivity must be replaced by the appropriate loss factor.) Numerical solutions for the above equations for the one-dimensional d. c. case were derived by Copple et al. in 1938.⁽¹⁸⁾ They applied the differential analyser to the evaluation of the solution of Equation (2.9). Their results are shown in Figure 2.3. In the cases of $\beta = 1/2$ and $1/4$ the temperature increases without limit, while for lower field strength of $\beta = 1/8$ the temperature rises slowly to some upper limit that depends on the field strength. The former case causes the dielectric to break down thermally, the latter results in no breakdown.

The case of $\beta = 0.22$ in Figure 2.3 shows clearly the existence of a critical value of electric field intensity. This value can be obtained by solving the equation

$$- \operatorname{div}(\kappa \operatorname{grad} T) = \sigma E^2 \quad (2.11)$$

which ignores the time-dependent term of the above equations. O'Dwyer called this case steady state thermal breakdown.

He called the first case, exemplified by $\beta = 1/2$, impulse thermal breakdown, because this phenomenon may occur when the field is applied as a short pulse (of the order of seconds duration or less). Therefore it may be a satisfactory approximation to ignore the heat conduction term and obtain the equation

$$C \frac{dT}{v dt} = \sigma E^2 \quad (2.12)$$

Recently Beyer established, from the mathematical point of view, the theory of existence and uniqueness for the initial-value/boundary-value problem for the partial differential equation and proved analytically that there is a critical field intensity above which a thermal catastrophe occurs. (19)

§2.2 THERMAL INSTABILITY IN CABLES

We now discuss the thermal instability of cables. For the most part oil-impregnated paper has been used for electric insulation of cables. This dielectric material has a temperature-dependent dielectric loss, which increases exponentially as the temperature rises. Therefore, it is reasonable to suppose that thermal breakdown may also occur in oil-impregnated paper insulated cable. In 1935 Brazier demonstrated in his paper⁽⁵⁾ that in 66-130kV OF cables there were many breakdowns caused by thermal breakdown rather than tracking breakdown. Moreover he stated that cables have two kinds of thermal breakdown viz., "internal thermal breakdown" and "external thermal breakdown." These are shown in Figure 2.4. The former applies to cases in which the high heat generation in the cable leads to thermal breakdown (the voltage, in Figure 2.4, increases from 75kV to 200kV). The latter applies where thermal breakdown is caused by the higher ambient temperature of the cable.

This analysis has been forgotten with development of low loss dielectrics. Recently cable engineers have focused attention upon thermal breakdown because, even in OF cables insulated with the low-loss cellulosic paper, thermal breakdown may occur at normal voltage. It can therefore be a crucial factor in determining the transmission capacity of OF cables.

Fukagawa has evaluated in detail the heat generation and the temperature distribution in EHV OF cables using a computer and determined the upper limit of voltage for OF cables above which thermal breakdown will occur.⁽²⁰⁾ In addition, he turned his attention to whether or not EHV OF cables may be precipitated into thermal breakdown when carrying overload current.

He derived a new method⁽²¹⁾ to calculate the overload current of EHV OF cables, drawing on the work of Pierson who calculated the overload ratings in power capacitors in consideration of thermal breakdown.⁽²²⁾

Generally speaking the $\tan\delta$ of oil-impregnated paper tends to increase with time from the initial value because of long-term thermal degradation. Accordingly, it is considered that EHV OF cables which have

been in service for a long time may be prone to thermal breakdown. Fukagawa defined the life-time of EHV OF cables as the time until runaway thermal breakdown occurs. He derived the critical $\tan\delta$ from the thermal breakdown condition and estimated the lifetime of EHV cables, as shown in Figure 2.5.

Thermal instability can be considered to occur in DC OF cables as well as in AC OF cables. Fallou experimented with d.c. breakdown of oil-impregnated paper insulated cables with 6mm insulation thickness and obtained the results shown in Figure 2.6. In her opinion the decrease of the breakdown voltage at high temperature is due to the thermal instability.

When 500kV, 1000mm², OF DC cables were developed in Japan, Fukagawa et al. precisely calculated the heat generation of this cable, and compared it with that calculated from Fallou's approximate equation,⁽²⁴⁾ as shown in Figure 2.7.⁽²⁵⁾ The results indicated that Fallou's equation, tends to overestimate somewhat the heat generation in EHV DC cables.

"External thermal breakdown" as defined by Brazier has also been analyzed in EHV cables. In 1962 breakdowns were experienced in England with 138kV OF cables having two circuits. After studying this trouble in detail, it was determined that the problem was due to thermal runaway, specifically, thermal breakdown caused by the dryout of the local soil in the immediate vicinity of the cables. Fukagawa analyzed the thermal runaway phenomenon graphically, as shown in Figure 2.8, with some experimental moisture migration data of the soil.⁽⁶⁾

§. 3 CALCULATION OF THE TEMPERATURE RISE OF NATURALLY COOLED PIPE-TYPE CABLE UNDER THE STEADY STATE OPERATION

§3.1 THERMAL EQUIVALENT CIRCUIT OF POF CABLE

Dielectrics used as electric insulation have a maximum permissible temperature such as 85°C for oil-impregnated paper or 90°C for cross-linked polyethylene. On the other hand a cable itself generates heat when it is used as an electric transmission line. Therefore the permissible current rating of a cable is determined by the temperature rise above ambient.

The temperature rise of a cable can usually be calculated by using a thermal equivalent circuit. Figure 3.1 shows such a circuit for a naturally cooled, oil-filled pipe-type cable under the steady state operation. The term "steady state" means a continuous constant current just sufficient to produce the maximum conductor temperature. From Figure 3.1 the following equations can be derived:

$$T_c - T_s = W_c R_1 + W_d k R_1 \quad (3.1)$$

$$T_s - T_p = (W_c + W_d + W_s) R_2 \quad (3.2)$$

$$T_p - T_a = \{3(W_c + W_d + W_s) + W_p\} R_3 \quad (3.3)$$

$$T_a - T_g = \{3(W_c + W_d + W_s) + W_p\} R_4 \quad (3.4)$$

where

T_c : conductor temperature (°C)

T_s : shield tape temperature (°C)

T_p : pipe temperature (°C)

T_a : anti-corrosion layer temperature (°C)

T_g : ambient temperature (= 25°C usually)

W_c : conductor loss (W/cm)

W_d : dielectric loss (W/cm)

W_s : shielding layer and skidwire loss (W/cm)

W_p : pipe loss (W/cm)

- R_1 : thermal resistance per unit length of the insulation of each core ($^{\circ}\text{C cm/W}$)
- R_2 : thermal resistance per unit length between cores and pipe surface ($^{\circ}\text{C cm/W}$)
- R_3 : thermal resistance per unit length of the covering on the pipe ($^{\circ}\text{C cm/W}$)
- R_4 : thermal resistance per unit length external to the buried pipe outer surface ($^{\circ}\text{C cm/W}$)
- k : constant

The constant k is usually assumed to be equal to 0.5. This means that all of the dielectric losses are generated at the middle layer of the insulation wall. This assumption is satisfied only under the condition for which the dielectric constant and power factor of the electric insulation do not depend on temperature and voltage. Therefore this value of k is not effective when thermal instability is studied, as will be discussed later.

These parameters can easily be calculated from the equations which will be written in the following section, once cable construction and operating conditions have been prescribed. (26) (27)

§3.2 CALCULATION OF LOSSES

§3.2.1 Conductor loss

Conductor loss per unit length is expressed by

$$W_c = I^2 R_{ac} \quad (3.5)$$

where

I : current flowing in one conductor (kA)

R_{ac} : a. c. resistance of the conductor per unit length ($\mu\Omega/\text{cm}$)

The value of the a. c. electrical resistance of the conductor, R_{ac} , for pipe-type cable, is given by

$$R_{ac} = R_{dc} \cdot k_1 \cdot k_2 \quad (3.6)$$

where

R_{dc} : d. c. resistance of the conductor per unit length, at the temperature of T_o ($^{\circ}\text{C}$)

k_1 : the ratio of d. c. resistance at the conductor temperature to that at T_o

k_2 : ac/dc resistance ratio

The coefficient k_1 is expressed by

$$k_1 = 1 + \alpha (T_c - T_o) \quad (3.7)$$

where

α : the temperature coefficient of resistance ($= 0.00393^{\circ}\text{C}^{-1}$ for copper)

T_c : conductor temperature

The ac/dc ratio k_2 is

$$k_2 = 1 + \beta (\lambda_s + \lambda_p) \quad (3.8)$$

where

λ_s : the skin effect factor

λ_p : the proximity effect factor

β : constant = 1.7 for regular triangle configuration of three cable cores in the pipe
= 2.0 for cradle configuration of three cable cores in the pipe

The skin effect factor λ_s is a function of a parameter X_s defined by the following formula

$$X_s = \sqrt{\frac{8\pi f}{k_1 R_{dc}} \cdot K_s \times 10^{-3}} \quad (3.9)$$

where

f : the frequency in cycles per seconds

K_s : constant = 1.0 for non-segmented conductor
 = 0.435 for 4-segmented conductor
 = 0.39 for 6-segmented conductor

However, when $X_s < 2.8$, the skin effect factor λ_s is simplified to

$$\lambda_s = F(X_s) = \frac{X_s^4}{192 + 0.8X_s^4} \quad (3.10)$$

The proximity effect factor λ_p is expressed by

$$\lambda_p = \frac{\frac{3}{2}(\frac{d_1}{S})^2 G(X_s^1)}{1 - \frac{5}{24}(\frac{d_1}{S})^2 H(X_s^1)} \quad (3.11)$$

and is also simplified, when $X_s^1 < 2.8$, to

$$\lambda_p = F(X_s^1) \left(\frac{d_1}{S}\right)^2 [0.312 \left(\frac{d_1}{S}\right)^2 + \frac{1.18}{F(X_s^1) + 0.27}] \quad (3.12)$$

where

d_1 : conductor outer diameter (cm)

S : distance between conductor axes (cm)

$$X_s^1 = \sqrt{0.8} X_s$$

$$H(x) = F(x)/G(x)$$

$$G(x) = \frac{x}{4} \frac{\text{ber } x \text{ ber}'x + \text{bei } x \text{ bei}'x}{(\text{ber}'x)^2 + (\text{bei } x)^2}$$

§3.2.2 Dielectric loss

The dielectric loss of unit length cable in each phase, W_d (W/cm), is given by

$$W_d = \frac{2}{300} \pi f C V^2 \tan \sigma \quad (3.13)$$

where

C : capacitance of the cable per unit length ($\mu\text{F}/\text{cm}$)

V : voltage (line-to-line)(kV r.m.s.)

$\tan\sigma$: power factor of the dielectric at operating frequency (%)

The capacitance per phase per unit length for circular conductors can be calculated from

$$C = \frac{\epsilon}{181 \ln \frac{d_2}{d_1}} \times 10^{-5} \quad (3.14)$$

where

ϵ : dielectric constant of the insulation

d_1 : conductor diameter (mm)

d_2 : external diameter of the insulation (mm)

Formulae such as (3.13), (3.14) can be derived under the assumption that $\tan\sigma$ and ϵ do not depend on the operating temperature and the applied voltage. However, $\tan\sigma$ and ϵ of the insulating materials used for a cable do tend to depend on temperature and electric field strength. $\tan\sigma$, especially, strongly depends on temperature in the temperature range above 90°C . Therefore when thermal instability of UHV cable is studied, the dielectric loss and the capacitance should be considered functions of temperature.

§3.2.3 Shielding tape and skidwire loss

This loss W_s can be expressed by

$$W_s = \frac{2\gamma_s}{1 + \left(\frac{\gamma_s}{X_m}\right)^2} I^2 \quad (3.15)$$

where

γ_s : equivalent electrical resistance of shielding layer and skidwire per unit cable length ($\mu\Omega/\text{cm}$)

$$= \frac{\gamma_a \cdot \gamma_w}{\gamma_a + \gamma_w} \approx \gamma_w \quad (3.16)$$

γ_a : electrical resistance of shielding layers per unit cable length ($\mu\Omega/\text{cm}$)

γ_w : electrical resistance of skidwires per unit cable length ($\mu\Omega/\text{cm}$)

X_m : reactance of shielding layers and skidwires ($\mu\Omega/\text{cm}$)

$$= 4\pi f \ln \left(\frac{4d_4}{d_3 + d_4} \right) \times 10^{-3} \quad (3.17)$$

d_3 : cable core diameter excluding skid wires (cm)

d_4 : cable core diameter including skidwires (cm)

The shielding layer is usually a stainless steel tape, 0.2 mm thick, and the skidwire is a copper wire with a diameter of 2.5 mm. Therefore

$\gamma_a \gg \gamma_w$.

W_s is much smaller than the pipe loss, so W_s usually is approximated as 2% of conductor loss, i.e.,

$$W_s \approx 0.02 W_c \quad (3.18)$$

§3.2.4 Pipe loss

When a. c. currents are flowing in cable conductors, eddy currents are generated in the pipe. Pipe loss is almost all eddy current loss and changes with the cable core configuration in the pipe. Pipe loss per unit length can be calculated from the following semi-experimental approximate formula.

$$W_p = \begin{cases} 3 \times (1.15 d_a - 0.149 d_5) I^2 \times 10^{-3} & \text{triangular} \\ 3 \times (0.438 d_a + 0.226 d_5) I^2 \times 10^{-3} & \text{cradled} \end{cases} \quad (w/cm) \quad (3.19)$$

where

d_a : axial spacing of the cable cores (mm) = d_4

d_5 : inner diameter of pipe (mm)

§3.3 CALCULATION OF THERMAL RESISTANCES

§3.3.1 Thermal resistance of the insulation R_1

The thermal resistance of the electric insulation of unit length cable core, between the conductor and the screen is calculated from

$$R_1 = \frac{P_1}{2\pi} \ln (d_2/d_1) \quad (3.20)$$

where

P_1 : thermal resistivity of the insulation ($^{\circ}\text{C cm/W}$), 500-650 $^{\circ}\text{C cm/W}$ for oil-impregnated paper and 400-450 $^{\circ}\text{C cm/W}$ for polyethylene.

It is to be noted that this formulism is not applicable if thermal resistivity of the insulation depends on temperature.

§3.3.2 Thermal resistance of the oil between the surface of the cores and the pipe R_2

Strictly speaking, there is a thermal resistance of the shielding tape inside each cable core. However this value is sufficiently small to be neglected. A thermal resistance of the oil between the surface of the cores and the pipe, R_2 , is expressed by the following empirical formulism:

$$R_2 = \frac{25.7 \times 3}{1 + 0.00256 T_m d_e} \quad (3.21)$$

where

T_m : the mean temperature of the medium filling the space between cable and pipe

d_e : the equivalent diameter of the group of cable cores (cm) as follows:

$$= \begin{cases} 1.65 d_4 & \text{for two cores in pipe} \\ 2.15 d_4 & \text{for three cores in pipe} \\ 2.50 d_4 & \text{for four cores in pipe} \end{cases}$$

§3.3.3 Thermal resistance of the external serving on the pipe R_3

The thermal resistance of the external serving on the pipe, R_3 ($^{\circ}\text{C cm/W}$) is calculated from the following:

$$R_3 = \frac{P_2}{2\pi} \ln (d_7/d_6) \quad (3.22)$$

where

P_2 : the thermal resistivity of the material of the outer servings
and usually 600 °C cm/W for P. V. C

d_6 : outer diameter of pipe (cm)

d_7 : outer diameter of the external servings on the pipe (cm)

§3.3.4 External thermal resistance R_4

Thermal resistance of the surrounding of buried pipe-type cables is expressed with the following formula:

$$R_4 = \frac{g}{2\pi} \ln \left\{ \frac{d'_{p1}}{d_{p1}} \times \left(\frac{d'_{p2}}{d_{p2}} \right)^{q_2} \dots \left(\frac{d'_{pn}}{d_{pn}} \right)^{q_n} \right\} \quad (3.23)$$

where

g : thermal resistivity of the soil

$$d_{p1} = \frac{1}{2}d_7$$

$$d'_{p1} = L + \sqrt{L^2 - \left(\frac{d_7}{2}\right)^2}$$

L : depth of burial (distance from the surface of the ground to the center of the pipe)

d_{pi} : the distance from the center of cable #1 to the center of cable #i
($i = 2, 3, \dots, n$)

d'_{pi} : the distance from the center of the reflection of cable #1 to the center of cable #i, referred in Figure 3.2 ($i = 2, 3, \dots, n$)

q_i : the ratio of total dissipating loss per unit length of cable #i to the total dissipating loss per unit length of cable #1

§. 4 CALCULATION OF THE TEMPERATURE RISE OF FORCED-COOLED PIPE-TYPE CABLES UNDER THE STEADY STATE OPERATION

§4.1 THERMAL EQUIVALENT CIRCUIT OF FORCED-COOLED POF CABLES AND CALCULATION OF THERMAL RESISTANCE

To obtain substantial increases in current ratings of underground cable circuits, forced-cooling techniques have been developed. For pipe-type cables it is especially convenient because the pressure medium, oil or gas, can be circulated through the cable pipe without the addition of pipe installation for the forced-cooling fluid. Originally slow oil circulation was used to reduce longitudinal hot spots along the cable. Recently, with the demand of greater loading of power transmission circuits, artificial cooling has been used.

In this case the thermal equivalent circuit of radial heat flow as shown in Figure 4.1, is substantially the same as the case of a naturally cooled pipe-type cable, except for the heat sink of the oil. Also, compared with Figure 3.1, the thermal resistance R_2 between the cable core and the pipe surface is somewhat modified, viz., R_2 is divided into R_{21} , R_{22} and R_{23} in Figure 4.1. R_{21} , R_{22} and R_{23} are the film thermal resistance between the cable core and the oil, the thermal resistance of the oil and the film thermal resistance between the oil and the pipe, respectively. Usually $R_{22} \approx 0$.

§4.1.1 Thermal resistance of the insulation

According to some test results obtained from forced-cooling POF cables, it seems that cable cores are not cooled uniformly. It is believed that the reason for this is that the two triangular areas between the cable cores (see Figure 4.2) are almost independent channels with considerably greater resistance to flow than in the main channel and that in these narrow passages the coolant temperature is higher than in the main channel. As a result the radial heat flow is distorted in the insulation and the insulation thermal resistance apparently increases. Test results in Japan showed this apparent increase is less than 10% of the insulation thermal resistance which is given in the naturally cooled cables.

Generally speaking, the effects of non-uniform cooling depend on flow

rate of coolant, cable core configuration, thermal conductivity and thickness of the shielding tapes and skidwires and thermal conductivity of the oil. If coolant flow rate is very large and the flow is turbulent, or if the shielding tapes and skidwires have good properties for thermal conduction, the effect of non-uniform cooling will be very small.

Chato and Chern analyzed theoretically this heat distortion by using the finite difference method and showed that if the ratio Q_m/Q_i (Q_m : heat rate conducted circumferentially in the sheathing, Q_i : heat rate conducted radially in the insulation) is larger than 4, the nonuniformities may be ignored within the cable core, and that even though the thickness of the shielding layer is small, this layer will contribute to the uniform temperature.

At present there have not been more persuasive experimental data on this effect. Therefore in this paper it is assumed that insulation thermal resistance R_1 of the forced-cooling cable is equal to that of naturally cooling cable.

§4.1.2 Oil film thermal resistance, R_{21} , between cable cores and the oil

In the case of the naturally cooled POF cables the thermal resistances R_2 and R_3 are much smaller than the external thermal resistance R_4 and therefore the temperature difference between cable cores and the outer surface of the pipe is about 5°C at most. Therefore R_2 and R_3 are not considered to be the important factors for the calculation of temperature rise of the cable. On the other hand in the case of forced-cooling cable the thermal resistances between cable core and pipe are important parameters for determining the cable rating current because the external thermal resistance becomes very small.

Heat transfer between cable cores and the oil has been investigated by many researchers. One of the earliest studies was reported in 1952 by Buller, who developed a theoretical relationship for the calculation of thermal resistance R_{21} by using the empirical formula in McAdams' book. He described in Appendix of his paper: ⁽²⁹⁾

$$R_{21} = \begin{cases} \frac{50.92 u^{0.4} \left[\left(\frac{d_5}{2.155 d_4} \right)^2 - 1 \right]^{0.2}}{\left(\frac{d_4}{10} v \right)^{0.8}} & \text{(for turbulent flow)} \\ \frac{264.28}{d_4} & \text{(for viscous flow)} \end{cases} \quad (4.1)$$

where

v : oil velocity (cm/sec)

u : viscosity of oil (centipoises)

Generally speaking the convective heat transfer process is distinguished by considering two types of fluid motion: free convection and forced convection. Free convection is set up by differences in density of the oil due to temperature differences, and forced convection is set up by the pressure difference in the oil flow. These heat transfer results are correlated in terms of the Nusselt number, Nu . In the former case, Nu is expressed by

$$Nu = C_1 (G_r)^{n_1} (P_r)^{m_1} \quad (4.2)$$

and in the latter case

$$Nu = C_2 (R_e)^{n_2} (P_r)^{m_2} \quad (4.3)$$

where

$C_1, C_2, n_1, n_2, m_1, m_2$: constants

G_1 : Grashof number

P_r : Prandtl number

R_e : Reynolds number

$$R_{21} = 1/\pi \lambda Nu \quad (4.4)$$

where λ : oil thermal resistivity ($0.15 \text{ W/m}^0\text{C}$)

In the case of mixed convection the approach of adding the effects of two kinds of convective heat transfer has been followed:

$$Nu = C_3 [G_z + C_4 (G_r \cdot P_r)^{n_1}]^m \quad (4.5)$$

where

G_z : $f(R_e \cdot P_r \cdot D/X)$ Graetz Function

n, m, C_3, C_4 : constants

According to a paper by Notaro and Webster,⁽³⁰⁾ most of the studies in mixed convective heat transfer have been with vertical tubes and in horizontal channels, more characteristic of cable flow system, an empirical approach has been used, attempting to adopt the form of the correlation for horizontal tubes to vertical tubes. To evaluate the oil film thermal resistance, the individual resistances due to the two modes of heat transfer were considered as parallel resistances. Citing the data theoretically studied by Siegel et al. in heat transfer characteristics for laminar forced convection flow, Notaro et al. evaluated $R_{21} \approx 5 \text{ ft } ^\circ\text{C/W}$. This yields relatively high resistance to heat flow. In addition they derived the following relations with the calculated results of Figure 4.3.

$$R_{21} = \frac{1.37 C_1 [A_1 [R_f^x (W_c + W_s + W_d)]^{\frac{1}{3}}]^{-1}}{T_o + \frac{R_{fs}^* (W_c + W_s + W_d)}{2} + C_1 C_2} \quad (4.6)$$

in taking oil film mean temperature as

$$T_f = T_o + \frac{1}{2} R_{fs}^* (W_c + W_d + W_s) \quad (4.7)$$

and temperature dependence of oil viscosity as

$$\mu_f = \left[\frac{T_f}{C_1} + C_2 \right]^{-3} \quad (4.8)$$

where

A_1 : hydraulic area

C_1, C_2 : constants

In 1970 Zanona et al. reported⁽³¹⁾ the experimental investigation of the forced-cooling for a model POF cable system and suggested that the oil film thickness t_{oil} in the following equation of oil film thermal resistance should be about 1/32 inch:

$$R_{21} = \frac{360}{300} \frac{1}{2\pi\lambda} \cdot \ln\left(\frac{2t_{oil} + d_4}{d_4}\right) (^\circ\text{C ft/W}) \quad (4.9)$$

In 1971 in addition Williams et al. calculated oil film-thermal resistance $R_{21} = 4.97 (^\circ\text{C cm/W})$ by using the conventional equation for forced convection:⁽³²⁾⁽³³⁾

$$N_u = C(R_e)^{0.8} (P_r)^{\frac{1}{3}} \left(\frac{\mu_b}{\mu_w} \right)^{0.14} \quad (4.10)$$

where

$$C = \begin{cases} 0.008 & \text{at } R_e = 2100 \\ 0.014 & \text{at } R_e = 3000 \\ 0.023 & \text{at } R_e > 6000 \end{cases}$$

μ_b : oil viscosity at the bulk temperature

μ_w : oil viscosity at the surface temperature

This value is relatively lower than that by Notaro et al. In 1977 Williams, who conducted forced-cooling tests using 230kV and 345kV POF cables, argued against the theoretical study on thermal conduction by Glicksman (MIT) where he had stated that turbulent flow and even low laminar flow tended to "wash away" the stagnant oil layers between cables. (34) (35)

Abdulhadi and Chato reported through an exhaustive experimental study (36) that:

- a) the Nusselt number in the laminar flow regime is almost constant and has an average value of approximately six for all cable configurations;
- b) in the transition from laminar to turbulent flow ($R_e \approx 400$ for the bird-cage cable configuration, $R_e \approx 1400$ for triangular or cradle cable configuration), the Nusselt number was between 5 to 16 and gradually increased with increasing Reynolds number;
- c) in the higher turbulent flow regime, the difference between cable surface and oil temperatures was often less than 0.5°C , and the Nusselt number was between 30 to 60 and increased with increasing skidwire roughness ratio;
- d) the best correlation of heat transfer results is obtained if the Nusselt number based on the cable diameter is plotted against the quantity $R_e P_r^{0.4}$.
- e) the skidwires acted as turbulence promoters.

As mentioned above, we have not obtained definite formulae on oil film thermal resistance. For the purpose of this investigation (analysis of thermal instability in UHV synthetic paper-insulated cables) oil film thermal resistivity is considered to be insensitive because coolant is in the higher turbulent flow regime. Therefore it is assumed in this paper that R_{21} is equal to 2.0°C cm/W . This value corresponds to the heat flow of the Nusselt number 100.

§4.1.3 Oil film thermal resistance, R_{23} , between oil and pipe

The oil film thermal resistance between oil and pipe, R_{23} , is considered less important than that between cable cores and oil, R_{21} , because much of the heat generated in the cable cores is removed by the coolant. Also, as far as temperature rise due to heat leak is concerned, R_{23} has much less influence than $R_3 + R_4$. For these reasons only a few experiments have been conducted to investigate this matter.

Zanona et al. reported that R_{23} was about $6.33 (^{\circ}\text{C cm/W})$ for their model cable experiment and that the oil film thickness t'_{oil} defined in the following formula of oil film thermal resistance, should be about $1/4$ inch ($= 6\text{mm}$);⁽³¹⁾

$$R_{23} = \frac{1}{2\pi\lambda} \ln \left(\frac{d_5}{d_5 - 2t'_{\text{oil}}} \right) \quad (4.11)$$

where

λ : oil thermal conductivity (they used $1/\lambda = 610^{\circ}\text{C cm/W}$)

d_5 : inner pipe diameter

In addition Williams et al. obtained $R_{23} = 0.25^{\circ}\text{C ft/W}$ ($= 7.58^{\circ}\text{C cm/W}$) for return pipe with no cable core from forced-cooling tests on a 138kV POF cable.⁽³⁵⁾

Notaro et al. derived theoretical formulae but they are not sufficiently supported by the experimental data.

In this paper it is assumed that $R_{23} = 5^{\circ}\text{C cm/W}$ because the coolant flow is considered fully developed turbulent.

§4.2 CALCULATION OF TEMPERATURE RISE OF FORCED-COOLED POF CABLE WITH ONE CIRCUIT

In the case of the forced-cooled cable, the longitudinal temperature rise along the cable axis has also to be considered. In order to simplify the calculation of the temperature rise, it is assumed that POF cable line is a single circuit. Therefore thermal interference from the other cable lines need not be considered.

Figure 4.4 shows the location of point X in the pipe and the oil temperature along the pipe. Heat generated in the pipe in length dx is

$$3(W_c + W_d + W_s)dx \equiv 3Wdx \quad (4.12)$$

If Q , as shown in Figure 4.1, signifies the heat per unit length removed by the fluid, the oil temperature at the point x along the pipe, T_{ox} is given by

$$T_{ox} = T_g + R_{23}(3W - Q) + (W_p + 3W - Q)(R_3 + R_4) \quad (4.13)$$

Q is expressed by the following equation

$$Q\Delta x = mq(T_{ox+\Delta x} - T_{ox}) = mq\Delta T_{ox} \quad (4.14)$$

where

m : oil flow rate (g/sec)

q : specific heat of oil 2.02 (W sec/g°C)

Therefore if $\Delta x \rightarrow 0$, in Equation (4.14)

$$Q = mq \frac{dT_{ox}}{dx} \quad (4.15)$$

Substitution Equation (4.15) into Equation (4.13) gives the linear differential equation:

$$mqR \frac{dT_{ox}}{dx} + T_{ox} = T_g + 3RW + W_p(R_3 + R_4) \quad (4.16)$$

where

$$\begin{cases} R = R_{23} + R_3 + R_4 \\ W = W_c + W_d + W_s \end{cases} \quad (4.17)$$

From the well-known mathematical formula, the solution of this equation can be easily obtained:

$$T_{ox} = A \exp\left(-\frac{X}{mqR}\right) + T_g + 3RW + (R_3 + R_4)W_p \quad (4.18)$$

At $X = 0$, $T_{ox} = T_{oo}$

so that

$$A = T_{oo} - (T_g + 3RW + (R_3 + R_4)W_p) \quad (4.19)$$

$$\therefore T_{ox} = (3RW + (R_3 + R_4)W_p)(1 - \exp\left(-\frac{X}{mqR}\right)) + (T_{oo} - T_g)\exp\left(-\frac{X}{mqR}\right) + T_g \quad (4.20)$$

If T_{oo} , the oil temperature at the entrance, is lower than T_g , the ambient earth temperature, the coolant will not only cool the cable temperature, but will also cool the ambient earth. This is not efficient. Therefore T_{oo} , at lowest, should be equal to T_g . Equation (4.20), then simplifies to

$$T_{ox} = T_g + (3RW + (R_3 + R_4)W_p)(1 - \exp\left(-\frac{X}{mqR}\right)) \quad (4.21)$$

As expected, the oil temperature will be highest at the end terminal $X = L$. This temperature T_{oL} is given by

$$T_{oL} = T_g + (3RW + (R_3 + R_4)W_p)(1 - \exp\left(-\frac{L}{mqR}\right)) \quad (4.22)$$

Thus, the highest shield tape temperature and the conductor temperature are obtained as follows:

$$T_{sL} = T_{oL} + 3R_{22}W + WR_{21} \quad (4.23)$$

$$T_{cL} = T_{sL} + W_c R_1 + kW_d R_1 \quad (4.24)$$

§4.3 CALCULATION OF TEMPERATURE RISE OF FORCED COOLING POF CABLE WITH MANY CIRCUITS

If multiple forced-cooled POF cables are buried very close to each other, calculation of temperature rise will be more complicated, because heat leak from the other cables must be considered for cable temperature rise.

If the heat generation rate of cable i is expressed by W_i , the heat removal rate by oil of the cable i by Q_i and the heat leak of cable i to the surrounding soil by W'_i , the following equations must be satisfied:

$$W_i = 3(W_c + W_d + W_s)_i + W_{pi} \quad (4.25)$$

$$W'_i = W_i - Q_i \quad (4.26)$$

Therefore the shielding layer temperature rise T_{si} , the oil temperature rise T_{oi} , the pipe temperature rise T_{pi} , and the anticorrosion layer temperature rise T_{ai} of the cable i are expressed by:

$$T_{si} = T_{oi} + (R_{22} + \frac{R_{21}}{3})(W_i - W_{pi}) \quad (4.27)$$

$$T_{oi} = T_{pi} + R_{23}(W'_i - W_{pi}) \quad (4.28)$$

$$T_{pi} = T_{ai} + R_3 W'_i \quad (4.29)$$

$$T_{ai} = T_g + R_{gii} W'_i + \sum_{j \neq i} R_{gij} W'_j \quad (4.30)$$

where

$$R_{gii} = \frac{g}{2\pi} \ln \left\{ \frac{d'_{pii}}{d_{pii}} \right\} \quad (4.31)$$

$$R_{gij} = \frac{g}{2\pi} \ln \left\{ \frac{d'_{pji}}{d_{pji}} \right\} \quad (4.32)$$

d_{pji} , d'_{pji} , as illustrated in Figure 3.2 are the distances from the center of cable i to the center of cable j , or from the center of cable i to the image of cable j , respectively. d_{pii} is the radius of the outer external servings on the pipe of cable i , d'_{pii} is the distance from the center of cable i to the center of the image of cable i .

In addition, Q_i must satisfy the following equation:

$$Q_i = m_i q l_i \frac{dT_{oi}}{dx} \quad (4.33)$$

where

$$l_i = \begin{cases} 1 & \text{if the oil flow direction of cable } i \text{ is the same as the} \\ & \text{direction of } x\text{-axis} \\ -1 & \text{if the oil flow direction of cable } i \text{ is opposite to the} \\ & \text{direction of } x\text{-axis} \end{cases}$$

m_i : oil flow rate of cable i (g/sec)

Substitute Equation (4.33) into Equation (4.26) gives

$$W'_i = W_i - m_i q l_i \frac{dT_{oi}}{dx} \quad (4.34)$$

Eliminating T_{pi} , T_{ai} , W'_i from Equation (4.28) through Equation (4.30) by using Equation (4.34) gives the following differential equation expressed in matrix form:

$$(A) \left(\frac{dT_x}{dx} \right) + (T_x) = (B) \quad (4.35)$$

where

$$A = \begin{bmatrix} m_1 q l_1 R_{oi}^* & m_2 q l_2 R_{g12} & \dots & m_n q l_n R_{g1n} \\ m_1 q l_1 R_{g21} & m_2 q l_2 R_{02}^* & \dots & m_n q l_n R_{g2n} \\ \vdots & \vdots & \ddots & \vdots \\ m_1 q l_1 R_{gn1} & \dots & \dots & m_n q l_n R_{on}^* \end{bmatrix} \quad (4.36)$$

$$T_x = \begin{bmatrix} T_{o1} - T_g \\ T_{02} - T_g \\ \vdots \\ T_{on} - T_g \end{bmatrix} \quad (4.37)$$

$$B = \begin{bmatrix} R_{o1}^* & R_{g12} & R_{g13} & \dots & R_{g1n} \\ R_{g21} & R_{o2}^* & R_{g23} & & R_{g2n} \\ \vdots & & & & \vdots \\ R_{gn1} & R_{gn2} & \dots & \dots & R_{on}^* \end{bmatrix} \cdot \begin{bmatrix} W_1 \\ W_2 \\ \vdots \\ W_n \end{bmatrix} = -R_{23} \begin{bmatrix} W_{p1} \\ W_{p2} \\ \vdots \\ W_{pn} \end{bmatrix} \quad (4.38)$$

$$R_{oi}^* = R_{23} + R_3 + R_{gii} \quad (4.39)$$

It is to be noted that (A) is a symmetrical matrix. Therefore, as is well-known in matrix analysis, the solution of the ordinary differential equation of Equation (4.35) can be expressed by the following:

$$T_x = (I - e^{-A^{-1}x}) B + e^{-A^{-1}x} T_{xo} \quad (4.40)$$

where

I : Identity matrix

T_{xo} : the oil temperature of $x = 0$

$$e^B = I + B + \frac{1}{2!}B^2 + \dots \dots \dots (\text{for matrix } B.)$$

Therefore, if the eigen values and the matrix of eigen vectors of the matrix A^{-1} are expressed by λ_k , U_k , respectively,

$$A^{-1} = (U_k) \begin{pmatrix} \lambda_1 & 0 & \dots & 0 \\ 0 & \lambda_2 & \dots & 0 \\ \vdots & & & \\ 0 & \dots & \dots & \lambda_n \end{pmatrix} (U_k)^{-1} \quad (4.41)$$

$$\therefore e^{A^{-1}x} = (U_k) \begin{pmatrix} e^{\lambda_1 x} & 0 & \dots & 0 \\ 0 & e^{\lambda_2 x} & \dots & 0 \\ \vdots & & & \\ 0 & \dots & \dots & 0 e^{\lambda_n x} \end{pmatrix} (U_k)^{-1} \quad (4.42)$$

These matrices can be easily calculated by computer.

If the boundary condition is not given sufficiently by the temperature at $x = 0$ but given in part by the temperature at $x = L$ (terminal), the solution Equation (4.40) would be modified somewhat.

§. 5 ANALYSIS OF THERMAL INSTABILITY IN UHV SYNTHETIC PAPER INSULATED CABLES

§5.1 MATHEMATICAL EXPRESSIONS FOR THERMAL INSTABILITY AND DESIGNING OF UHV SYNTHETIC PAPER INSULATED CABLES

§5.1.1 Basic equations of thermal instability in cables

As stated in Section 2, thermal instability occurs when the rate of increase of a dielectric loss with temperature is greater than the rate of increase of heat dissipation with temperature in the cable system which is in a thermal equilibrium state. Therefore the mathematical expressions of thermal instability are as follows:

$$\left\{ \begin{array}{l} G(T) = W(T) \\ \frac{\partial G(T)}{\partial T} \geq \frac{\partial W(T)}{\partial T} \end{array} \right. \quad (5.1)$$

where

T : the insulation temperature ($^{\circ}\text{C}$)

$G(T)$: the dielectric loss per unit cable length in an alternating electric field (W/cm)

$W(T)$: the heat dissipation per unit cable length (W/cm)

In the case of UHV cables the insulation wall is very thick. Therefore T is distributed in a wide temperature range. However the hottest temperature, where thermal instability of the cable insulation is most readily precipitated, is in the insulation layer adjacent to the conductor. Therefore Equation (5.1) can be rewritten to

$$\frac{\partial G(T)}{\partial T_c} \geq \frac{\partial W(T)}{\partial T_c} \quad (5.2)$$

where

T_c : the conductor temperature

Luoni, et al. employed in their latest paper a new approximating criterion of thermal instability: ⁽³⁷⁾

$$\frac{\Delta T_a}{\Delta T_c} \leq 0 \quad (5.3)$$

where

T_a : the ambient temperature

This means that a decrease of the ambient temperature, ΔT_a , is required to dissipate all the heat generated by an increase of the conductor temperature, ΔT_c . Therefore less power can be dissipated when T_a is constant and as a consequence the system is unstable. On the other hand, if $\Delta T_a / \Delta T_c$ is positive, more power can be dissipated than the generated heat when T_a is constant, so that the system is stable.

In the case of DC cables this criterion may be more effective, as they stated in their paper, when heat dissipation, W , is not independently computed. However, as shown in the next section, heat dissipation and dielectric loss can be precisely estimated by solving nonlinear differential equations and therefore Equation (5.2) is still effective.

§5.1.2 Design of UHV synthetic paper insulated cables

It is predicted that bulk power - 10,000 MW - underground transmission systems will be required in the 1990's in order to meet the continually increasing demand for electric power. Consequently the feasibility of bulk power transmission systems such as UHV cables, CGIT, cryogenic cables and superconducting cables is being studied both technologically and economically. UHV synthetic paper insulated cable is one of the most promising transmission systems because of its low power factor, its low dielectric constant and its excellent AC breakdown strength.

However, some synthetic papers are not without their problems, e.g.,

- poor compatibility with oil
- poor resistance to swelling in oil
- poor mechanical properties for tape winding

They have been considered unsuitable for oil-filled cable insulation.

With the remarkable progress of recent research and development of synthetic paper technology, it has been shown that a few synthetic papers or synthetic film laminated papers have much better oil resistance and high dielectric breakdown strength. They are the highly oriented crystallized polyethylene paper and the paper-polypropylene laminated paper. Their

electric properties are shown in Table 5.1 in comparison with the properties of the kraft paper. (38)

It was recently announced that 550kV or 765kV cables, with synthetic paper as insulation had been manufactured. Long run stability under electric and thermal stress has not yet been tested, but if it proves successful, cables insulated with this kind of paper could be employed to transmit bulk power in the future.

To study the thermal stability of oil-filled, pipe-type cable with insulation of this kind Brookhaven National Laboratory proposed the following ratings: transmission power - 10,000 MW; system voltages - 500, 750, 1000kV; conductor cross-sectional area - 2500 mm^2 for each cable; allowable conductor temperature - 90°C .

The final report, (2) "Evaluation of the Economical and Technological Viability of Various Underground Transmission Systems for Long Feeds to Urban Load Areas," written for DOE by Philadelphia Electric Company, states that, as a result of comparing conductors with cross sections of 1013 mm^2 , 1266 mm^2 , 1520 mm^2 , a conductor larger than 1260 mm^2 did not appear to provide a significant increase in ampacity. However it seems reasonable to expect that bulk power transmission by a large conductor size will be more economical than the approach of increasing the number of circuits. Therefore, in this paper the largest conductor size manufactured by up-to-date techniques — 2500 mm^2 — has been selected. B.N.L. concurred with this choice.

Some synthetic papers have excellent thermal properties; the electric strength and dielectric constant change very little even when they are used at temperatures above 100°C . Thus a higher allowable conductor temperature than the conventional 85° can be selected. In this paper 90°C was chosen as the allowable conductor temperature because of concern for mechanical problems such as thermo-mechanical bending, which have not been fully investigated.

It can be generally assumed that breakdown is initiated in the oil-gaps and that the dielectric strength measured on cable samples is lower than that of a sheet sample. The ratio of the dielectric breakdown strength be-

tween the paper sample data and the cable data is 1.5 for A. C. voltage and 1.3 for impulse voltage in the case of 500kV class, kraft paper insulated cables. For synthetic paper insulated cables no data of this ratio have so far been obtained. For this reason and because of the need for a margin of safety the maximum operating stresses for A. C. and impulse voltages were selected as 20kV/mm and 100kV/mm, respectively.

For insulation design of cable, the Basic Insulation Level (BIL) is one of the most important factors. The BIL for UHV cables has never been determined. Therefore with an extrapolation of the known data for less than 500kV cables, the following values were selected: 1500kV, 2100kV, 2400kV for 500kV, 750kV and 1000kV cables respectively. As a result, the insulation thickness for 500kV was determined by A. C. withstand voltage, and the thickness for 750kV and 1000kV were determined by impulse voltage.

The assumed data for design of 500kV, 750kV and 1000kV synthetic paper insulated oil-filled pipe-type cables are included in Table 5.3. Figure 5.1 shows the cross-section.

The electric and thermal properties for these cables are presented in Table 5.4. These values can be calculated from equations stated in Chapter 3 or 4.

Table 5.1

Properties of Insulating Papers

	PPLP	PP	PE	Kraft Paper
Thickness (mm)	0.121	0.164	0.128	0.125
Density (g/cm ³)	0.90	0.74	0.69	0.65
Dielectric constant	2.7	2.2	2.2	3.3
Tan δ % 80°C	0.060	0.02	0.024	0.14
100°C	0.073	0.04	0.044	0.15
AC Strength kV/mm	—	—	104	74
Imp. Strength kV/mm	160	172	229	156

Table 5.2

Cable Ratings

System Voltage (kV)	500	750	1,000
Total transmitted power (MVA)	10,000	10,000	10,000
Number of circuit (cct)	6	4	3
Unit transmitted power (MVA/cct)	1,667	2,500	3,334
Rated current (kA)	1.925	1.925	1.925

Table 5.3

Cable Design

Voltage Class (kV)	500	750	1000
Conductor size (mm ²) material stranding diameter (mm)	2500 copper compact segmental 60		
Insulation thickness (mm) thermal resistivity ($^{\circ}\text{C cm}/\text{W}$) dielectric const. impregnant	20 450 (PE), 650 (PPLP) 2.2 (PE), 2.7 (PPLP) polybutene oil	32	50
Shield type thickness (mm) width (mm) number intercalation	tinned copper 0.13 38 one 0.06 mm metalized mylar tape		
Skidwire material type number	non-magnetic stainless steel 2.5 ^{mm} x 5 ^{mm} half-round two		
Overall diameter of core (without skidwire)	100	124	160
Pipe I. D. (mm) O. D. (mm)	305 314	356 364	406 414
Coating thickness (mm) O. D. (mm)	3.0 320	3.0 370	3.0 420

Table 5.4

Electric and Thermal Properties of Cables

Voltage class (kV)	500	750	1,000	Note
Capacitance ($\mu\text{F/Rm}$)	0.239	0.168	0.124	for PE
	0.293	0.207	0.153	for PPLP
Electric Resistance of				
Conductor d.c. ($\mu\Omega/\text{cm}$)	0.0732	0.0732	0.0732	at 20°C
" A.C. ($\mu\Omega/\text{cm}$)	0.1234	0.1168	0.1120	at 20°C
Conductor Current Loss				
W_c (W/cm)	0.771	0.433	0.415	at 20°C
Thermal Resistance ($^{\circ}\text{C cm/W}$)				
of Insulation R_1	36.59	51.99	70.25	for PE
	52.85	75.10	101.47	for PPLP
of Oil R_2 "	16.84	15.13	14.27	
	($T_m=65$)	($T_m=60$)	($T_m=50^{\circ}\text{C}$)	
of Coating R_3 "	1.81	1.56	1.37	
of Soil R_4 "	51.23	48.92	46.90	$g=100^{\circ}\text{C cm/W}$
				$L=200 \text{ cm}$
				lcct

§5.2 ACCURATE ESTIMATION OF THE DIELECTRIC LOSS

§5.2.1 Analysis of the dielectric loss

When the power factor of the cable is relatively insensitive to temperature, the following relations have been employed for estimation of the dielectric loss:

$$G = \frac{1}{3}\omega CV^2 \tan\sigma \quad (5.4)$$

$$T_c - T_s = (W_c + \frac{1}{2}G)R_1 \quad (5.5)$$

Equation (5.5) is identical to Equation (3.1) where the parameter k is 0.5.

For thermal breakdown to occur in the cable insulation, the dielectric loss must increase nonlinearly with the temperature rise of the cable, that is to say power factor has a strong dependence on temperature. On the other hand UHV cables have a thick insulation wall, so that the insulation temperature spans a wide range. The above-mentioned equations are therefore not valid for the analysis of thermal instability.

Using the symbols shown in Figure 5.2, the dielectric loss ΔG in an infinitesimal layer Δx is given by

$$\Delta G = \frac{2\pi\epsilon_0\epsilon}{\ln(\frac{x+\Delta x}{x})} \omega(E_x \cdot \Delta x)^2 \tan\sigma \quad (5.6)$$

where $\epsilon = \epsilon(T_x)$, $\tan\sigma = \tan\sigma(T_x)$

$$E_x = \frac{V}{\sqrt{3X \ln \frac{d_2}{d_1}}} \quad (5.7)$$

On the other hand, capacitance of the cable (when $\epsilon = 1$) C_o , can be expressed by

$$C_o = \frac{2\pi\epsilon_0}{\ln \frac{d_2}{d_1}} \quad (5.8)$$

Therefore, using the approximation of Equation (5.9), Equation (5.6) can be rewritten as Equation (5.10).

$$\ln(\frac{x+\Delta x}{x}) = \ln(1 + \frac{\Delta x}{x}) \approx \frac{\Delta x}{x} \quad (5.9)$$

$$\Delta G = \frac{1}{3} \omega C_o V^2 \epsilon \tan \sigma \frac{\Delta x}{x \ln \left(\frac{d_2}{d_1} \right)} \quad (5.10)$$

In the steady state the temperature difference ΔT_x between x and $x + \Delta x$ is

$$-\Delta T_x = \Delta R_i (W_c + \sum \Delta G) \quad (5.11)$$

where ΔR_i is the thermal resistance of the infinitesimal layer and can be expressed by the following relation:

$$\Delta R_i = \frac{P_i(T_x)}{2\pi} \frac{\Delta x}{x} \quad (5.12)$$

Dividing Equation (5.10) - (5.12) by Δx , and then allowing Δx to approach zero gives

$$\left\{ \begin{array}{l} \frac{dG}{dx} = \frac{1}{3} \omega C_o V^2 \frac{\epsilon(T_x) \tan \sigma(T_x)}{x \ln \left(\frac{d_2}{d_1} \right)} \end{array} \right. \quad (5.13)$$

$$\left\{ \begin{array}{l} -\frac{dT_x}{dx} = \frac{P_i(T_x)}{2\pi x} \cdot (W_c + G(T_x)) \end{array} \right. \quad (5.14)$$

These differential equations can be solved under the following boundary conditions,

$$\left\{ \begin{array}{l} T_x = T_c \\ G = 0 \end{array} \right. \text{ at } x = \frac{1}{2} d_1 \quad (5.15)$$

and the precise dielectric loss G and the accurate temperature distribution in the insulation T_x , can be obtained for the various conductor temperatures.

§5.2.2 Approximation of the basic parameters

When the above-mentioned nonlinear differential equations are solved, it is necessary that the temperature dependence of the basic parameters such as $\tan \sigma(T)$, $\epsilon(T)$, $p(T)$ be given.

Power factors

Generally speaking, the temperature characteristics of the power factor

of the insulation materials are V-shaped. At the commercial frequency of 50-60Hz, power factor tends to be high at the low temperature due to dipole moment, and also high at the high temperature (above 80°C) due to ionic conduction. It can therefore be written as follows:

$$\tan \sigma (T) = \alpha_1 \exp\left(\frac{E_1}{kT}\right) + \alpha_2 \exp\left(\frac{E_2}{kT}\right) \quad (5.16)$$

where

α_1, α_2 : constants (%)

E_1 : activation energy relating to dipole rotation (eV)

E_2 : activation energy relating to ionic conduction (eV)

k : Boltzmann's constant (eV/°K)

T : insulation temperature (°K)

For simplicity, the first term of the above equation can be approximated by a constant, α_0 , (because thermal instability occurs in a high temperature region) as follows:

$$\tan \sigma = \alpha_0 + \alpha_2 \exp\left(\frac{E_2}{kT}\right) \quad (5.17)$$

It is anticipated that synthetic paper for UHV cable application will have an extremely low dielectric loss. However, the dielectric losses of all the candidate polymer papers are significantly affected by intrinsic factors such as impurities, residual solvent and swelling.

Impurities can be due to impurities in the original chemicals or from residual catalyst in manufacturing process. Their effect on the dielectric loss arises from dipolar losses or conduction losses. Residual solvent may be a significant factor if a solvent is used in the paper-making process.

Regarding swelling, Reed has stated that the mobility of ions will increase and the low frequency losses will increase markedly when swelling occurs. If swelling reaches levels of 10-30% the power factor may be considerably affected. (39)

It is not known for certain how much the temperature-dependence of power factor is affected by these factors. No data for activation energy have been obtained.

Figure 5.3 shows typical temperature-dependence curves of power factor for synthetic papers. Curve (a) in Figure 5.3 is for PPLP.⁽⁴⁰⁾ This PPLP is a polypropylene film sandwiched between two layers of synthetic polypropylene paper. In this figure power factor of the synthetic paper itself is shown. At high temperatures the power factor of PPLP is almost equal to that of the paper.

Figure 5.4 shows the temperature-dependence of power factor in PP laminated paper insulated 765kV cables.⁽⁹⁾

This laminated paper is polypropylene film sandwiched between two layers of low loss cellulose paper and is impregnated with polybutene oil. The flat temperature-dependence of power factor would not cause thermal instability.

Polyethylene is one of the most promising synthetic materials for the insulation of UHV cables. PE has been recognized as having inferior compatibility with insulating oils at high temperature and has been excluded from synthetic insulation paper. However, if high density polyethylene is highly oriented and crystallized, its compatibility with oil is greatly improved without detriment to its excellent electrical properties.

Furukawa Electric Company has announced the successful development of a new modified polyethylene paper. The curve (b) in Figure 5.3 shows the relation between temperature and power factor for polyethylene.⁽⁸⁾ The power factor is very low but rises at higher temperature, which suggests thermal instability may occur.

To analyze thermal instability in UHV synthetic paper insulated cables, two kinds of temperature dependence were selected, namely curves (a) and (b) in Figure 5.3. From these figures the constants α_0 and α_2 , and activation energy E_2 were calculated. Results are shown in Table 5.5.

The activation energy of PPLP is very high; it is close to 0.9-0.96eV, the activation energy of oil-impregnated cellulose paper polluted with very small quantity of moisture. We conclude that this PPLP must have been polluted with impurities or residual solvent in the manufacturing process. However, as stated by the authors, such pollution will quite likely take place in a mass production. Therefore it is reasonable to assume that UHV

cables will have the same characteristics as far as temperature dependence is concerned as those shown in Figure 5.3 (a) and (b).

Electric stress is one of the factors which affect the dielectric loss of the insulation material. However the data on the dissipation factor behavior of PP laminated paper, Figure 5.5, shows that it is not significantly affected by electric stress.⁽⁴¹⁾ Similar data is presented for cellulose paper.

In light of this data, it was assumed that electric stress does not affect the power factor of UHV cables.

Table 5.5

Constants α_0 , α_2 and E_2

	α_0 (%)	α_2 (%)	E_2 (eV)
PPLP	0.015	$240e^{18}$	0.86
PE	0.017	$1.56e^{18}$	0.709
Kraft P	0.135	$0.072e^{18}$	0.58

Dielectric constant

Dielectric constant, ϵ , of the synthetic paper or synthetic film does not extensively depend on temperature. Table 5.6 shows the dielectric constant for typical synthetic papers. Fixed values 2.2 and 2.7 were adopted for the dielectric constants for PE and PPLP respectively in the thermal instability analysis.

Table 5.6

Dielectric Constant of the Typical Synthetic Papers and Others

PP	2.2
PP laminate paper	2.7
PE	2.2
Kraft P	3.3

Thermal resistivity

Few data on thermal resistivity of the synthetic paper have been measured and even less on the temperature dependence. At the 1977 ICC meeting in Milwaukee, two cable manufacturers stated that thermal resistivity of PPP is 547°C cm/W for PPP insulated cable manufactured by GCC or 653°C cm/W by Phelps Dodge Wire and Cable Company. However, no high temperature data have been obtained. Only recently, as discussed in Chapter 4, has the temperature dependence of thermal resistivity in oil-impregnated cellulose paper been pointed out. No public acceptance for this dependence has so far been gained and it has not been taken into consideration in cable design.

Hansen et al.⁽⁴²⁾ have made measurements and obtained some information on the temperature-dependence of thermal conductivity in the case of high density polyethylene. They obtained the results shown in Figure 5.6. As high density PE has a high degree of crystallinity, the thermal conductivity tends to decrease with the increase of temperature. PE paper which will be used for synthetic paper insulated cables will be of a high crystallinity and a high orientation. Therefore, for such materials one must assume that the thermal resistivity depends on temperature.

Much data for oil-impregnated cellulose paper suggests that the thermal resistivity of this material depends upon such physical properties as thermal conductivity and viscosity of impregnating oil, the composition of the paper (crystallinity, the length of the paper fibers, the fiber orientation and density), and such cable constant as taping tension and width and length of butt gap. Thermal conductivity of impregnating oil is particularly influential as far as the thermal resistivity of the oil impregnated paper is concerned.

What oil will be used for the impregnation of UHV synthetic paper insulated cables? In the recent paper Allam et al. stated that high molecular weight polybutene will produce the least swelling and is the most promising impregnant for polypropylene. On the other hand, Matsuura et al. employed hard dodecyl benzene (D.D.B.) for impregnation of polypropylene laminated paper. Moreover Iwata et al. have developed a new modified polyethylene paper which has much improved compatibility with D.D.B. oil.

It therefore seems likely that D.D.B. or P.B. oil will be used for UHV synthetic paper cables. The thermal resistivity of these oils is 450°C cm/W or 600°C cm/W respectively.

The authors of this report were led to expect that information on many of the material properties of the cable constituents, especially their temperature sensitivity, would be supplied by B.N.L. This did not occur. As a consequence, as far as the thermal resistivities of oils were concerned the constant values of 450°C cm/W for PE paper and 650°C cm/W for PPP were assumed in the analysis of thermal instability of UHV cables.

§5.2.3 Computing results

The nonlinear differential Equations (5.13) and (5.14) can be solved using appropriate parameter values for UHV synthetic insulated cables as designed in Section 5.1.2 and characterized in Section 5.2.2. The Runge-Kutta method for the computation is effective for simultaneous differential equations:

$$\left\{ \begin{array}{l} \frac{dy}{dx} = f_1(x, y, z) \\ \frac{dz}{dx} = f_2(x, y, z) \end{array} \right. \quad (5.18)$$

This method estimates the values of y and z at $x = x_0 + h$, y_1 and z_1 under the initial condition ($x = x_0$, $y = y_0$, $z = z_0$) as follows:

$$\begin{aligned} y &= y_1 = y_0 + k = y_0 + \frac{1}{6}(k_1 + 2k_2 + 2k_3 + k_4) \\ z &= z_1 = z_0 + l = z_0 + \frac{1}{6}(l_1 + 2l_2 + 2l_3 + l_4). \end{aligned} \quad (5.19)$$

where

$$\begin{aligned} k_1 &= hf_1(x_0, y_0, z_0), \quad l_1 = hf_2(x_0, y_0, z_0) \\ k_2 &= hf_1\left(t_0 + \frac{h}{2}, y_0 + \frac{k_1}{2}, z_0 + \frac{l_1}{2}\right), \quad l_2 = hf_2\left(x_0 + \frac{h}{2}, y_0 + \frac{k_1}{2}, z_0 + \frac{l_1}{2}\right) \\ k_3 &= hf_1\left(t_0 + \frac{h}{2}, y_0 + \frac{k_2}{2}, z_0 + \frac{l_2}{2}\right), \quad l_3 = hf_2\left(t_0 + \frac{h}{2}, y_0 + \frac{k_2}{2}, z_0 + \frac{l_2}{2}\right) \\ k_4 &= hf_1(x_0 + h, y_0 + k_2, z_0 + l_2), \quad l_4 = hf_2(t_0 + h, y_0 + k_3, z_0 + l_3) \end{aligned} \quad (5.20)$$

The computed results are shown in Figure 5.7; details of the computing program appear in the Appendix. The dielectric losses are quite negligible in the lower temperature range, but they increase exponentially as the temperature rises, reaching 0.1 W/cm, which corresponds to about 20% of the conductor ohmic loss, at a conductor temperature of 130°C.

§5.3 ESTIMATION OF THE HEAT DISSIPATION

§5.3.1 Estimation of the constant k

The temperature difference between the conductor and the shield layer has been defined as follows:

$$T_c - T_s = R_1(W_c + kW_d) \quad (5.21)$$

If the dielectric loss W_d is independent of temperature, the constant k as derived from Equations (5.10) to (5.12) will be 0.5. This is found by using the following relation:

$$\int \frac{1}{x} \log x dx = \frac{1}{2} (\log x)^2 \quad (5.22)$$

However when the dielectric loss, W_d , is very temperature dependent, k has a different value and will be a function of many factors. This is important in the analysis of thermal instability.

For simplicity, k is assumed to be approximately constant in this paper. Figures 5.8 and 5.9 show the relations between the conductor temperature and the shield temperature. In each figure, the solid lines show the accurate relations between them obtained by solving Equations (5.13) and (5.14) using Runge-Kutta method, and the dotted lines show the approximate relations obtained by calculating Equation (5.21) using an assumed value of k. As shown in these figures, Equation (5.21) coincides with the computer solution very well in the case of 500kV to 1000kV cables when the assumed values for k are 2/3 for PPLP insulation and 3/4 for PE insulation. Therefore, these values will be employed in the analysis. The consequential errors are estimated to be between 0.02% at 90°C and 0.10% at 140°C.

§5.3.2 Estimation of the dissipation

From Equations (3.1) to (3.4), the following equation can be derived:

$$T_c - T_g = W_d(kR_1 + R_2 + 3R_3 + 3R_4) + W_c \{R_1 + (1 + y_s)(R_2 + 3R_3 + 3R_4) + 3y_p(R_3 + R_4)\} \quad (5.23)$$

where

$$\left\{ \begin{array}{l} y_s = \frac{W_s}{W_c} \\ y_p = \frac{W_p}{3W_c} \end{array} \right. \quad (5.24)$$

Therefore, if the conductor loss, shielding layer loss and pipe loss when the continuous rating current, i_o , flows through the conductor are expressed by W_{co} , W_{so} and W_{po} respectively, the permissible maximum conductor temperature, T_{co} , can be satisfied by the following equation:

$$T_{co} - T_g = W_{do}(kR_1 + R_2 + 3(R_3 + R_4)) + W_{co} \{ (R_1 + (1 + y_s)(R_2 + 3R_3 + 3R_4) + 3y_p(R_3 + R_4)) \} \quad (5.25)$$

where

W_{do} : the dielectric loss at the conductor temperature of T_{co} ($^{\circ}C$)

Subtracting Equation (5.25) from Equation (5.24) gives

$$T_c - T_{co} = (W_d - W_{do})(kR_1 + R_2 + 3R_3 + 3R_4) + (W_c - W_{co}) \{ (R_1 + (1 + y_s)(R_2 + 3R_3 + 3R_4) + 3y_p(R_3 + R_4)) \} \quad (5.26)$$

In this paper we are analyzing the thermal stability or instability for the case in which the continuous rating current, i_o , flows through the conductor. Therefore, from Equations (3.5) to (3.9),

$$W_c - W_{co} = i_o^2 R_{dc} k_2 \partial(T_c - T_{co}) \quad (5.27)$$

and

$$W_d = W_{do} + \frac{T_c - T_{co}}{kR_1 + R_2 + 3R_3 + 3R_4} \{ 1 - \partial i_o^2 k_2 R_{dc} (R_1 + (1 + y_s)(R_2 + 3R_3 + 3R_4) + 3y_p(R_3 + R_4)) \} \quad (5.28)$$

In addition, the following equation can be derived from Equation (5.25):

$$3(R_3 + R_4) = \frac{T_{co} - T_g - R_1(kW_{do} + W_{co}) - R_2(W_{do} + W_{co}(1 + y_s))}{W_{do} + W_{co}(1 + y_s + y_p)} \quad (5.29)$$

The heat dissipated, W_d , can be calculated from Equations (5.28) and (5.29). In other words, knowledge of R_3 and R_4 is not required for the analysis if the continuous current rating is known.

The calculated value of $R_3 + R_4$ in Equation (5.29) is 7°C cm/W for 500kV PPLP cable. or 0.02°C cm/W for 1000kV PPLP cable. These values are unrealizable for none-forced-cooled cables.

If the cables are forced cooled, the following equations can be derived by a similar procedure from Equations (4.22) to (4.24):

$$W_d = W_{do} + \frac{T_{cL} - T_{cLo}}{kR_1 + 3(R_{21} + R_{22}) + 3R(1 - \exp(\frac{-L}{mqR}))} \times$$

$$[1 - \alpha i_o^2 k_2 R_{dc} \{R_1 + 3(1 + y_s)(R_{21} + R_{22}) +$$

$$3((1 + y_s)R + y_p(R_3 + R_4))(1 - \exp(\frac{L}{mqR}))\}] \quad (5.30)$$

$$3(R(W_{do} + W_{co}(1 + y_s)) + (R_3 + R_4)y_p W_{co})(1 - \exp(\frac{L}{mqR}))$$

$$= T_{cLo} - T_g - R_1(W_{co} + kW_{do}) - 3W_o(R_{22} + R_{21}) \quad (5.31)$$

However, the value of R_{23} is negligibly small compared with the value of $(R_3 + R_4)$ and therefore the above equation (5.31) is simplified.

$$3R(1 - \exp(\frac{L}{mqR})) = \frac{T_{cLo} - T_g - R_1(W_{co} + kW_{do}) - 3W_o(R_{21} + R_{22})}{W_{do} + W_{co}(1 + y_s + y_p)} \quad (5.32)$$

$$W_d = W_{do} + \frac{T_{cL} - T_{cLo}}{kR_1 + 3(R_{21} + R_{22}) + R(1 - \exp(\frac{L}{mqR}))} \times$$

$$[1 - \alpha i_o^2 k_2 R_{dc} \{R_1 + 3R_{21}(1 + y_s) + 3R(1 + y_s + y_p) \times$$

$$(1 - e^{\frac{-L}{mqR}})\}] \quad (5.33)$$

The calculated values for the oil flow rates, m , from Equation (5.32) are 34.5, 46.6, 72.6 kg/sec for 500, 750, 1000kV PE cables respectively, and 43.2, 76.8, 321.6 kg/sec for 500, 750, 1000kV PPLP cables respectively. These values correspond to oil flow velocities of 0.82, 0.87, 1.24 m/sec for 500, 750, 1000kV PE cables, respectively, and 1.03, 1.43, 5.48 m/sec for 500, 750, 1000kV PPLP cables, respectively. With the exception of 5.48 m/sec for 1000kV PPLP cables, these oil flow velocities will be realized readily in the commercial operation.

§5.4 DETERMINATION OF THERMAL INSTABILITY

As stated in Equation (5.2), thermal instability will occur when the rate of increase of a dielectric loss with conductor temperature exceeds the rate of increase of heat dissipation with conductor temperature for a cable which is in a state of thermal equilibrium. Therefore we can readily determine whether or not thermal instability will occur by examining the relationships between heat dissipation and the conductor temperature and between dielectric loss and the conductor temperature, and establishing the range over which the above criterion applies.

Figures 5.10 (a) and (b) show the results of such determinations for PPLP and PE cables. The curves in Figures 5.10 are dielectric losses and the straight lines in these figures are the heat dissipation results calculated from Equation (5.33). From these figures both PPLP cables and PE cables are known to be in the thermally stable state.

Figures 5.11 (a) and (b) show to what temperature these cables can be operated if thermal instability is to be avoided. For 1000kV PE cable thermal instability will occur when the conductor temperature is about 145°C . This cable possesses sufficient thermal margin. On the other hand, for 1000kV and 750kV PPLP cables thermal instability will occur at the conductor temperature of 130°C . Therefore if the applied voltage is higher than $1.0E_o$ (E_o being the rated voltage), these cables could become thermally unstable at a lower temperature than 130°C . Thus, the condition of the accelerated life test for these cables should be limited. This will be discussed in the next section.

The above result is due mainly to the power factor/temperature characteristics. As stated in Section 5, PPLP cables are assumed to have been impregnated by a little polluted oil and consequently have a higher power factor than PE cables.

PPLP cable, suitably manufactured, can be used at a higher temperature.

§. 6 DISCUSSION

§6.1 STUDY OF THERMAL INSTABILITY IN THE ACCELERATED TESTS

It has been shown that UHV cables, designed in accordance with Section 5.2, will be in thermally stable. However cable samples must ordinarily undergo accelerated life tests in the field testing station such as Waltz Mill, before being placed in commercial operation. In such tests cables are exposed to high temperatures and voltages, that is 90 to 120°C and 1.0 E_0 to 1.5 E_0 . Therefore thermal instability may occur during the tests. These conditions will be discussed in this section.

The 90 to 120°C of the accelerated life test will be obtained by increasing the external thermal resistance. This is most conveniently accomplished by reducing the cooling velocity. Heat dissipation can therefore be calculated in a similar way to that shown in Section 5.3.

Figure 6.1 indicates that 1000kV PE cable is still thermally stable even if it is exposed to the severest conditions (1.5 E_0 and 120°C). Thus, if it can maintain its characteristic without change during the accelerated life test, it will never experience thermal instability under the normal operation.

On the other hand PPLP cables are not thermally stable under the severe test conditions. As shown in Figure 6.2, the critical temperature for the onset of thermal instability in 1000kV PPLP cable, for both 1.3 E_0 and 1.5 E_0 , is around 120°C. Clearly, this cable should be tested at a lower temperature than 120°C.

Table 6.1 shows the critical condition for each PPLP cable. It will be noted that 750kV cable is in the same situation as 1000kV cable, while 500kV cable can be tested at 1.3 E_0 and 120°C.

Table 6.1

Critical Condition of PPLP Cable Accelerated Life Tests

Operation temperature	500kV	750kV	1000kV
110°C	<1.5 E_0	<1.5 E_0	<1.5 E_0
120°C	<1.3 E_0	<1.2 E_0	<1.2 E_0

§6.2 CRITICAL LIMIT OF INCREASE IN POWER FACTOR

In general, the power factor of a dielectric tends to increase gradually with time due to thermal deterioration when it is exposed to high temperature. Therefore, when a synthetic paper insulated cable is subjected to an accelerated life test, its power factor will increase. If this increase is high, thermal instability will occur. In this regard it is interesting to know the critical limit of increase in the power factor. This matter is addressed in the ensuing discussion.

Assume that the dielectric loss of synthetic paper insulated cable can be expressed approximately as follows:

$$G_m = \frac{1}{3}\omega CV^2 \tan\sigma_m \quad (6.1)$$

$$\tan\sigma_m = \alpha_o + \alpha_1 \exp\left(-\frac{E_2}{kT_m}\right) \quad (6.2)$$

$$T_m = T_c - p_c W_c R_l - p_d W_{dm} R_l \quad (6.3)$$

where T_m is the average temperature in the insulation wall, and p_c and p_d are constants. For 1000kV PPLP cable, values of $p_c = \frac{1}{3}$ and $p_d = \frac{1}{7}$ will give the good approximation for the dielectric loss.

The derivative of G_m with respect to conductor temperature T_c is given by

$$\begin{aligned} \frac{dG_m}{dT_c} &= a \frac{d\tan\sigma(T_m)}{dT_m} \frac{dT_m}{dT_c} \\ &= \frac{1}{p_d R_l + \frac{kT_m^2}{aE_2(\tan\sigma_m - \alpha_o)}} \end{aligned} \quad (6.4)$$

where

$$a = \frac{1}{3}\omega CV^2 \quad (6.5)$$

On the other hand the heat dissipation W_d is given by Equation (5.33) Consequently the derivative of W_d with respect to T_c is

$$\frac{dW_d}{dT_c} = \frac{A}{B} \quad (6.6)$$

where

$$A = 1 - \frac{3}{2} R_{dc}^2 k_2 (R_1 + 3R_{21}(1 + y_s) + 3R(1 + y_s + y_p)(1 - \exp(-\frac{L}{mqR}))$$

$$B = pR_1 + 3R_{21} + 3R(1 - \exp(-\frac{L}{mqR})) \quad (6.7)$$

Substituting Equations (6.4) and (6.6) into the criterion for thermal instability, Equation (5.2), gives the critical power factor $\tan \sigma^*$ as follows:

$$\tan \sigma^* = \alpha_o + \frac{k}{aE_2} \frac{T_m^2}{\frac{B}{A} - R_1 p_d} \quad (6.8)$$

when $\tan \sigma < \tan \sigma^*$, the cable is thermally stable.

From Equation (6.8), we observe the following about $\tan \sigma^*$:

- (1) The greater the activation energy E_2 , the lower is $\tan \sigma^*$. A low value for E_2 is therefore desirable.
- (2) This $\tan \sigma^*$ does not depend on α_1 . With thermal deterioration $\tan \sigma$ especially α_1 will increase. Both α_o and E_2 will have little variation. Therefore $\tan \sigma^*$ should be taken as the critical factor in the lifetime of the cable.
- (3) When the external thermal resistance is high, $\tan \sigma^*$ becomes low, so that where thermal dissipation is poor, thermal instability is more easily precipitated.

Figure 6.3 shows the critical value of $\tan \sigma$ and the thermally stable region for the 1000kV PPLP cable operated at $1.0 E_o$. This figure also includes the temperature characteristics of the power factor used in that calculation. From this figure the critical limit of increase on power factor is seen to be 0.05% if the cable is operated at 90°C .

There is little information on the long-term aging of different synthetic paper cable models. Buehler et al., who took aging data on polypropylene paper, 0.38 mm total thickness, impregnated with polybutene oil, reported that the power factor increased by approximately 0.02% during a 250 days period when operating at 110°C and 15.7 kV/mm. ⁽⁴³⁾ Edwards et al. showed that the power factor of PPL miniature cables at 100°C and 20 kV/mm was stable for three years. ⁽⁴⁴⁾

More data on the long-term aging of synthetic paper should be taken. No general conclusions can be drawn from such data as are now available.

However from the point of view of thermal deterioration the above-mentioned critical value of power factor is very low. Therefore it should be emphasized that this 1000kV PPLP cable has little margin with respect to power factor.

§.7 CONCLUSIONS

As rights of way for transmission circuits become difficult to obtain and environmental considerations become more stringent, greater emphasis will be placed on increasing the power capacity of 'single circuits'. One way of doing this is through UHV cable development. One might suppose that kraft paper could be used to advantage for UHV cable insulation because of the long experience in its manufacture and installation. However from the point of view of thermal instability this material is not well suited for this purpose. New insulation materials - synthetic papers - have therefore been aggressively developed, and some intrinsic problems of these materials such as poor compatibility with oil and poor resistance to swelling in oil have been overcome.

The purpose of the work described in this report was to study the feasibility of synthetic paper insulated UHV cables from the point of view of thermal instability. From many data which have so far been taken it would appear that PE and PPLP are promising. Therefore PE and PPLP insulated cables have been designed to transmit a bulk power (10GVA per a few circuits) using forced-cooling.

The thermal instability of cable depends strongly upon its power factor, especially at high temperature. On the other hand, power factor depends upon the conditions of cable manufacturing process. Therefore it is very difficult to draw general conclusions. Moreover, little data on power factor of synthetic paper insulated cables have thus far been measured. For this reason UHV synthetic paper insulated cables with a hypothetical but realizable power factor were postulated. Two examples were chosen. One corresponds to the case when the cables are manufactured under correct conditions and the other to a situation when they are impregnated with slightly polluted oil.

The following conclusions can be drawn:

1. The cables specified are thermally stable.
2. They may experience thermal instability when they undergo accelerated life tests. The test conditions should therefore be selected so as to avoid misleading results: the high temperature test should be carried out at low voltage and the high voltage test at low temperature.
3. The cables impregnated with the polluted oil will experience thermal breakdown. It is therefore necessary to

take strict precautions in cable manufacturing.

4. The power factor activation energy of UHV synthetic paper cables should be less than 0.7eV.

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APPENDIX

COMPUTER PROGRAM OF
SOLVING DIFFERENTIAL EQUATIONS

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1	C	MAIN PROGRAM OF THERMAL INSTABILITY BY FUKAGAWA	5.000
2		DIMENSION Y(3000),WD(3000),TAN(3000)	6.000
3		REAL LG1	7.000
4		READ(5,9) DH,XINI,XFIN	8.000
5	9	FORMAT(8E10.3)	9.000
6	300	READ(5,8,END=999) TC,V,RAC,AI	10.000
7	8	FORMAT(4F10.4)	11.000
8		Y(1)=TC	12.000
9		PAI=3.141596	13.000
10		T0=293.0	14.000
11		ALPH=0.004	15.000
12		FREQ=60.0	16.000
13		EPS=2.7	17.000
14		LG1=ALOG(XFIN/XINI)	18.000
15		C=EPS/18.0/LG1	19.000
16		V2=V**2/3.0	20.000
17		WCV2=2*PAI*FREQ*C*V2*0.1	21.000
18		B=WCV2/LG1	22.000
19		ALPH0=0.015	23.000
20		ALPH11=0.0372	24.000
21		ALPH12=26.77	25.000
22		EK=9985.774	26.000
23		ALPH1=ALPH11*EXP(ALPH12)	27.000
24		PI=650	28.000
25		A=PI/2.0/PAI	29.000
26		AI2=AI*AI	30.000
27		AI2RA=AI2*RAC	31.000
28		WC=AI2RA*(1.0+ALPH*(TC-T0))	32.000
29		Z0=0.0	33.000
30	C		34.000
31	C	N=(XFIN-XINI)/DH+1	35.000
32		X=XINI	36.000
33	C		37.000
34		DO 200 I=1,N	38.000
35		IF(I.GT.3000) GO TO 460	39.000
36		CALL FUNC(X,Y(I),Z0,DH,Y(I+1),ZNEXT,TANS,WDS,A,WC,B,ALPH0,ALPH1,	40.000
37	1	EK)	41.000
38		Z0=ZNEXT	42.000
39		TAN(I)=TANS	43.000
40		WD(I)=WDS	44.000
41		X=X+DH	45.000
42	200	CONTINUE	46.000
43	460	WRITE(6,6) TC,ALPH0,ALPH1,AI,V	47.000
44		X=XINI	48.000
45		NP=N+1	49.000
46		LINE=6	50.000
47		DO 400 I=1,NP	51.000
48		Y273=Y(I)-273	52.000
49		IF (I.EQ.NP) GO TO 30	53.000
50		WRITE(6,7) X,Y(I),Y273,WD(I),TAN(I)	54.000
51		LINE=LINE+1	55.000
52		IF(LINE.LT.57) GO TO 400	56.000
53			57.000

49		WRITE(6,5)	
50		LINE=2	58.000
51	400	X=X+DH	59.000
52	30	WRITE(6,7) X,Y(I),Y273	60.000
53	7	FORMAT(1H,10X,F10.2,4F20.5)	61.000
54	6	FORMAT(1H1,10X,'TC=',F7.2,5X,'ALPHA-0=',F7.4,5X,'ALPHA-1=',E13.6,	62.000
		15X,'I=',E6.3,5X,'E/V=',F7.2///1H,17X,'X',14X,'TX',	63.000
		215X,'TX-273',18X,'WD',12X,'TANDELTA'/)	64.000
55	5	FORMAT(1H1,17X,'X',14X,'TX',15X,'TX-273',18X,'WD',12X,'TANDELTA'/)	65.000
56		GO TO 300	66.000
57	999	STOP	67.000
58		END	68.000
59		SUBROUTINE FUNC(X,Y0,Z0,DH,YNEXT,ZNEXT,TANS,WDS,	69.000
	1	A,WC,B,ALPH0,ALPH1,EK)	70.000
60		F1(X,Z)=-A*(WC+B*Z)/X	71.000
61		F2(X,Y)=(ALPH0+ALPH1*EXP(-EK/Y))/X	72.000
62		XDH2=X+DH/2.0	73.000
63		XDH=X+DH	74.000
64		TANS=F2(X,Y0)*X	75.000
65		WDS=B*Z0	76.000
66		AK1=F1(X,Z0)*DH	77.000
67		AL1=F2(X,Y0)*DH	78.000
68		AK2=F1(XDH2,Z0+AL1/2.0)*DH	79.000
69		AL2=F2(XDH2,Y0+AK1/2.0)*DH	80.000
70		AK3=F1(XDH2,Z0+AL2/2.0)*DH	81.000
71		AL3=F2(XDH2,Y0+AK2/2.0)*DH	82.000
72		AK4=F1(XDH,Z0+AL3)*DH	83.000
73		AL4=F2(XDH,Y0+AK3)*DH	84.000
	C		85.000
74		YNEXT=(AK1+2.0*AK2+2.0*AK3+AK4)/6.0+Y0	86.000
75		ZNEXT=(AL1+2.0*AL2+2.0*AL3+AL4)/6.0+Z0	87.000
76		RETURN	88.000
77		END	89.000
			90.000

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TABLE OF FIGURES

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Figure 2.7. Heat rate generated by DC voltage in 1000mm² 500kV OF cables.

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Figure 4.2. Distortion of heat flow in cable core.

Figure 4.3. R_{21} vs. oil temperature T_f .

OIL TEMPERATURE:

Figure 4.4. Illustrating of oil flow through cable pipe.

DESIGN	A CM	B CM	C CM
500kV	32.0	30.5	10.0
750kV	37.0	35.6	12.4
1000kV	42.0	40.6	16.0

CONDUCTORS ARE 2500mm² copper

Figure 5.1. Internal cross sectional view.

Figure 5.2. Symbols used in this analysis.

Figure 5.3. $\tan \delta$ - temperature characteristics of insulation papers used.

Figure 5.4. Cable dissipation factor vs. conductor temperature at rated voltage (442 kV).

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Figure 5.10a. Discrimination of thermal instability for PPLP cables.

Figure 5.10b. Discrimination of thermal instability for PE cables.

Figure 5.11a. Discrimination of thermal instability, 1000kV PE cable, $1.0 E_0$.

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Figure 5.11c. Discrimination of thermal instability, 750kV PPLP cable, $1.0 E_0$.

Figure 5.11d. Discrimination of thermal instability, 500KV PPLP cable, $1.0 E_0$.

Figure 6.1a. Discrimination of thermal instability, 1000kV PE cable, $1.5 E_0$.

Figure 6.2a. Discrimination of thermal instability, 1000kV PPLP cable, applied voltage $1.5 E_0$.

Figure 6.2b. Discrimination of thermal instability, 1000kV PPLP cable, applied voltage $1.3 E_0$.

Figure 6.3. Critical power factor.

Appendix V

TRIP REPORT

John W. Bonn
Report of Visit to BNL
for
Start Up, Training and Checkout of the B500 Helium Refrigerator Liquefier
August 29 to September 1, 1978

TRIP REPORT

Name(s) John W. Bonn CVI Ref. No. B500

Place Visited Brookhaven National Laboratory

Date of Visit August 29 to Sept. 1 Date of Report September 6, 1978

Persons Contacted Joe Bamberger

Purpose of Visit and Comments: Start up, training and checkout of the B500 helium refrigerator liquefier

Copies to: Jack Jensen BNL
Joe Bamberger BNL
Bill Jaeger CVI
Jay Molvie CVI

The refrigerator/liquefier was completely installed, leak checked and subsystems checked out as much as possible prior to my arrival on August 29. The refrigerator was not connected to BNL's use point at this time.

Below is a description of some of the things that happened to the CVI supplied equipment during shipment, installation and operation of each component.

Compressor Skid

Gauge line broke at flared joint during shipment. Unit was under 20 PSI pressure and oil coated the interior of electrical enclosure. During installation the sump drain valve was broken off and approximately 80 gallons of oil drained from sump. On start-up oil level dropped and unit shutdown on low oil pressure. Approximately 80 gallons of processed oil was added to the compressor. Cooling water to oil cooler must run continually even when compressor is shutdown, otherwise cooling tower pumping system will not work properly. If water is shutoff at compressor skid, water pressure will increase to approximately 160 psig. Amp indicators were installed on the doors of motor starters. The first stage compressor current indicators appeared to indicate actual motor current while the second stage compressor current indicator seemed to indicate the current transformer secondary current.

Purifier Skid

A coalescer was installed downstream of charcoal adsorber and filters. This coalescer was from another piece of BNL equipment. This unit was installed dirty. Approximately 2 cups of oil were drained from the sump. Unit was cleaned with trichlorofluoroethelene (freon) and new filters were installed.

PCV-10 was removed and piping modified. A large buffer tank is connected directly to the compressor suction with a 6" line. This buffer volume is maintained at 1.2 psig by a Moore pressure regulator. A LH vacuum pump was added to the purifier skid to provide pumpdown of the compressor and purifier skids. Two "Lake Shore" pressure transducers were not working. These items were returned for repair.

Cold Box

Cold box was under vacuum and maintained a insulating pressure of 2×10^{-6} torr with all internal components at ambient temperature. Insulating vacuum with heat exchangers cold is less than 1×10^{-7} torr. (foreline pres. 35μ) The vacuum system provided with the cold box was modified. The connections between the vacuum system and piping system were eliminated. An additional vacuum pump was added for evacuation of the helium piping.

Turbine Expander

A leak was found in the helium supply valve to turbine #2. This leak was repaired and the valve is functioning properly.

The helium bulb thermometer located on the outlet of turbine #2 was leaking. This alarm point was eliminated until a new bulb can be provided. The bearing gas temperature alarm points have to be reset at 0°C .

A leak was discovered in a 1/4" "hoke" valve body, this valve to be replaced.

Optical fibers indicating turbine speed were reversed causing shutdown on over-speed of turbine #2. The optic fibers were switched and turbines operated properly.

Remote Console

Formica writing shelf was not on console. Writing shelf discovered outside exposed to weather. Shelf to be repaired or replaced by Brookhaven. Moore instrument indicating turbine brake pressure and speed not functioning properly. Electrical adjustment required. Control loop PIC-35 is not used. A Dwyer Photohelix operating a solenoid valve which bleeds the compressor suction into a dirty gas bag replaces this control loop.

V-J Piping

Some of the V-J piping was not installed at this time. All piping that was installed fit properly.

1/2" tube size bayonet connections provided at the cold box interface. These bayonets should have been 1/2 x 2 LHe pipe bayonets.

Brookhaven to notify CVI when these bayonets are to be changed on the piping.

Operations

Cooled refrigerator/liquefier to 4.2° K using bypass valve CV-93 and internal load heater set at 5° K. Load heater controlled very well in a closed loop mode. Additional refrigeration capacity was returned through CV-94 back to the lead stream circuit. Difficult to determine the lead stream flow because the gas was very cold back to the connections to the compressor suction. Lead stream flow meter design temperature is ambient.

Brookhaven's 13,800 volt power supply to the compressor motor was erratic on startup. The two first stage compressor motors would sometimes cause the C phase of the 13,800 volt power supply to drop out. Relays between "B" and "C" were switched to check relay setting.

Evidently the relays setting were alright because "C" phase still dropped out with "B" phase relay installed. Brookhaven electricians are going to try and correct this problem prior to restarting the unit again.

Training

Reviewed the operations of the helium refrigerator and subsystems with the technicians that were present at Brookhaven.

NOTES

Joe Bamberger will provide CVI with marked-up P & I Diagram of system modifications and additions.