EXPERIMENTAL RESULTS OF THERMALLY CONTROLLED SUPERCONDUCTING SWITCHES FOR HIGH FREQUENCY OPERATION *

G.B.J. Mulder, D. ter Avest, H.H.J. ten Kate,
H.J.G. Krooshoop and L.J.M. van de Klundert.
University of Twente, Dept. of Applied Physics,
Low Temperature Laboratory, Enschede, The Netherlands.

Abstract

The aim of our study is to develop thermally controlled switches which are to be used in superconducting rectifiers operating at a few hertz and 1 kA. Usually, the operating frequency of thermally controlled rectifiers is limited to about 0.1 Hz due to the thermal recovery times of the switches. The thermal switches have to satisfy two conditions which are specific for the application in a superconducting rectifier: a) they have to operate in the repetitive mode so beside short activation times, fast recovery times of the switches are equally important, b) the power required to effect and maintain the normal state of the switches should be low since it will determine the rectifier efficiency. To what extent these obviously conflicting demands can be satisfied depends on the material and geometry of the switch.

This paper presents a theoretical model of the thermal behaviour of a switch. The calculations are compared with experimental results of several switches having recovery times between 40 and 200 ms. Also, the feasibility of such switches for application in superconducting rectifiers operating at a few hertz with an acceptable efficiency is demonstrated.

Introduction

A thermally controlled switch basically consists of a composite of superconductor and heating element which is thermally insulated from the helium bath by means of an insulation layer. The switches considered here use the cylindrical geometry shown in Fig. 1 and have thermal insulation on both the inner and the outer side of the conductor. The switch conductor itself has either a round or a rectangular cross-section.

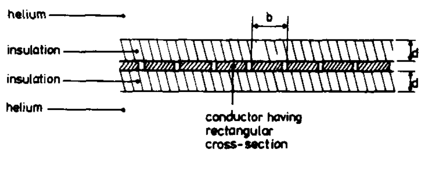

Fig. 1. Cross-sectional view of the thermal switch.

It is possible to discriminate three stages during the operation of a switch, each of which has some typical aspects that must be considered when designing the switch. As illustrated in Fig. 2 we have:

(I) Thermal activation. The switch is opened by means of the heater. After supplying the heater with a pulse P, the temperature T_{sc} of the superconductor rises, depending on the thermal conductivity of the material between the heater and superconductor. The pulse energy E_{act} will be determined by the necessary increase of enthalpy ΔH of the switch to raise the temperature of the conductor above T_c.

(II) Stationary situation. In the stationary situation the resistance of the switch, R_{sw}, is constant, which implies a constant length of normally conducting superconductor. The electrical dissipation P_{stat} in this length keeps the temperature above T_c so it is not necessary to use the heater. In fact, to keep the temperature above T_c, it is more efficient to dissipate in the conductor than in the heater. It is essential to note that R_{sw} is not necessarily the maximum switch resistance. By operating the switch in a stationary situation in which the minimum propagation current appears, R_{sw} becomes linearly dependent on the applied voltage for a specific voltage range, see Fig. 3. All normal regions then have the same maximum temperature, providing the switch has a constant insulation thickness. At the minimum propagation current the dissipated power is equal to the conducted heat and the normal-superconducting boundaries do not move.

(III) Recovery of the switch. The switch is closed by making the voltage across the switch and thus the dissipation equal to zero. During recovery, heat will be conducted from the superconductor to the helium bath, the temperature will drop and the maximum current will increase with time as indicated in Fig 4. The recovery time τ_{rec} is determined mainly by ΔH and the thermal conductivity of the insulation.

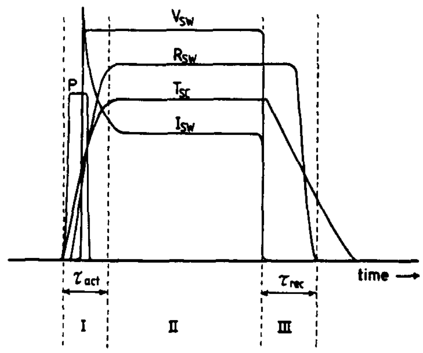

Fig. 2. Diagrams explaining the switch operation.

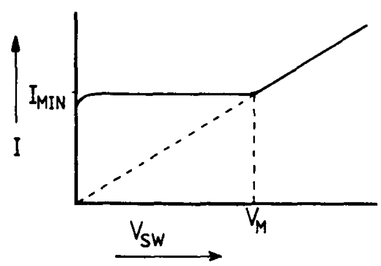

Fig.3. The current through the switch as a function of the applied voltage in the stationary state. For fast recovery the voltage should not exceed V_m which is equal to $I_{min} \cdot R_{max}$.

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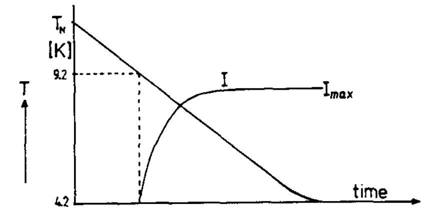

Fig.4. The temperature and maximum current of the switch versus time during the recovery stage.

Theory

When designing a thermally controlled switch, its properties such as R_{sw}, τ_{rec} and P_{stat} have to be adapted to the specifications of the rectifier, i.e. the required load current and power. For instance, the switch resistance and the current-carrying capacity, i.e. the required load current of the rectifier, will put a constraint on the volume of the switch and are therefore a measure for E_{act} and the stationary dissipation P_{stat}. The insulation material will be a compromise between a low P_{stat} and a short τ_{rec}. It is advantageous to use a thin insulation layer with a low thermal conductivity so that E_{act} can be minimized.

In order to obtain the shortest possible recovery time of a particular switch, we must ensure that the switch will not develop its full off-resistance. If the resistance would exceed R_{max} the maximum temperature in a normal region T_N would increase with V_{sw}. If on the other hand the switch is only partially open so that a situation of coexisting normal and superconducting regions occurs, T_N and τ_{rec} are minimized and independent of V_{sw}.

It is interesting to note that ΔH is approximately proportional to T_N^4 whereas the dependence of P_{stat} on temperature lies between T_N^2 and T_N^3. T_N is therefore an important parameter. In the case of a partially open switch, T_N and I_{min} will be calculated as a function of the insulation material and thickness. With I_{min} and T_N it is possible to determine P_{stat} and to give an estimate for ΔH and τ_{rec}. For the calculation we shall consider the geometry of Fig. 1 with a conductor of rectangular shape. If the insulation has a poor thermal conductivity compared to the conductor we may assume the cross-section of the conductor to have a uniform temperature. To describe the temperature variation as a function of the length coordinate of the conductor in the normal-superconducting transition region, we must solve:

$$\frac{\partial}{\partial x}\left[\lambda_w(T)\frac{\partial T}{\partial x}\right] + P_{prod} - P_{con} = 0, \qquad (1)$$

where x is the length coordinate, $\lambda_w(T)$ the average longitudinal thermal conductivity of the composite, P_{prod} (W/m^3) the electrical dissipation per unit of volume of the conductor and P_{con} (W/m^3) the heat conducted by the insulation per unit volume of the conductor. If we assume $\lambda(T)=\lambda_o T^{\gamma}$ for the insulation we find:

$$P_{con} = \frac{2\lambda_o b}{(\gamma+1)dA}\left[T^{(\gamma+1)} - T_b^{(\gamma+1)}\right], \qquad (2)$$

with A the cross-section of the composite, d the insulation thickness, b the width of the composite and T_b the helium bath temperature (4.2 K). In the normal conducting region of the conductor P_{prod} satisfies $P_{prod}=\rho_w(I/A)^2$, where I is the current through the composite and ρ_w the average longitudinal resistivity of the composite. In the superconducting region $P_{prod}=0$ and in the current-sharing region we can write $P_{prod}=\rho_w(I/A)^2(1-I_c(T)/I_c(4.2))$, where $I_c(T)$ is the superconducting part of the transport current. The maximum temperature T_N the normal zone follows from the thermal equilibrium $P_{prod}=P_{con}$:

$$T_N = \left[T_b^{(\gamma+1)} + \frac{\rho_w I^2(\gamma+1)d}{2\lambda_o bA}\right]^{1/(\gamma+1)}. \qquad (3)$$

By substitution of P_{prod} and P_{con} in (1) we obtain 3 equations for the three regions coupled at $T=T_c$ and $T=T_{cs}$ (T_{cs}=the current-sharing temperature). Using the transformation $s=\lambda_w\partial T/\partial x$ and the boundary conditions s=0 at $T=T_b$ and $T=T_N$, eq. (1) can be solved uniquely. This calculation was performed for two different insulation materials, namely STYCAST 2850FT epoxy ($\lambda=0.0051\cdot T^{1.8}$) and KAPTON ($\lambda=0.0016\cdot T^{1.1}$). The data used for the conductor were $A=6.2\ 10^{-7}$ m^2, b=7 mm and $\rho_w=5.2\ 10^{-7}$ Ωm, which corresponds to the conductors of switches 5, 6 and 7 discussed further on. Table 1 gives the results for various insulation thicknesses. The length of the normal-superconducting transition region Δl is much smaller than the length of the normal zone itself and therefore the assumption that the first term in eq. (1) is zero at the hottest part of the normal zone is justified. The length of the current-sharing region is in turn much smaller than Δl (less than 1 %). Therefore, current-sharing is of little significance to this problem and in fact the results hardly depend on whether a current-sharing region is taken into account or not [1].

Table 1 I_{min}, T_N and the length of the normal-superconducting transition region Δl.

insulation material	insulation thickness	I_{min} [A]	T_N [K]	Δl [mm]
STYCAST	0.1 mm	15.5	11.7	1.0
	0.2 mm	11.0	11.7	1.4
	0.5 mm	7.0	11.8	2.3
	1.0 mm	4.9	11.7	3.2
	2.0 mm	3.5	11.7	4.5
KAPTON	20 μm	9.6	12.2	1.7
	50 μm	6.1	12.1	2.6
	100 μm	4.3	12.2	3.7
	130 μm	3.8	12.2	4.6
	200 μm	3.1	12.2	5.7
	500 μm	1.9	12.2	9.5

From Table 1 it becomes clear that T_N is independent of the insulation thickness for a given insulation material. In fact, it was found [1] that also the geometry of the switch has no observable influence on T_N and that T_N is determined mainly by the power of T in the thermal conductivity, i.e. γ.

Experimental switches

A summary of data and results of several experimental switches that have been tested is given in table 2. The purpose of the first four of them was to systematically investigate the influence of the insulation layer thickness d on the thermal behaviour. Except for d, these switches are almost identical. The recovery time τ_{rec} is observed to be approximately proportional to d, whereas I_{min} is proportional to $1/\sqrt{d}$. Theoretically, the linear relation between τ_{rec} and d is expected only if the enthalpy increase of the insulation layers is a negligible fraction of the total enthalpy ΔH of the switch. The variation of I_{min} with $1/\sqrt{d}$ is predicted by eq. (3) and is in agreement with the calculations. Switches 5 and 6 illustrate the effect of different insulation materials, namely STYCAST and KAPTON. The superiority of KAPTON is due to its extremely low thermal conductivity. Compared to STYCAST, the insulation thickness d can be made approximately 10 times smaller and consequently the heat capacity of this layer is also less.

The influence of V_{sw} during the stationary off-state on R_{sw}, P_{stat}, and τ_{rec} respectively is illustrated in Fig. 5. In the low voltage range ($V_{sw}<0.8 \cdot I_{min}R_{max}$) we do indeed observe the appearance of a constant minimum propagation current. As expected the recovery time τ_{rec} is in this range independent of V_{sw}. The figure furthermore demonstrates that optimum performance of the switch cannot be achieved if V_{sw} exceeds the value $0.8 \cdot I_{min}R_{max}$. In that case T_N becomes substantially higher than 12 K and as a result τ_{rec} is deteriorated. In some of the switches T_N has been measured by means of thermocouples. At the minimum propagation current these measurements yield 11.6 K for STYCAST and 12.0 K for KAPTON, which is in good agreement with Table 1.

Implications for superconducting rectifiers

Subsequently, the influence of the switches on the performance of superconducting rectifiers will be explained. The operating principles of such rectifiers have been treated elsewhere [2] as well as their feasibility for powering superconducting coils [3]. Figure 6 shows the circuit of a full-wave rectifier consisting basically of a superconducting transformer, two switches and a load coil. The average rectifier power $\bar{P}_L$ is proportional to the operating frequency. The following relation holds:

$$\bar{P}_L = \alpha\, f\, k^2(\tfrac{1}{2}L_p\hat{I}_p^{\,2}) = \alpha\, f\, k^2\, W_p \,, \qquad (4)$$

where α = a constant typical for the control signals,
f = operating frequency,
k = coupling constant of the transformer,
L_p = primary inductance of the transformer,
$\hat{I}_p$ = amplitude of the primary current.

There are two obvious methods to scale up the average power of the rectifier. Firstly, by increasing the frequency, i.e. improvement of the control times of the switches. Secondly, by increasing the primary energy W_p. Both methods have their own drawbacks and in practice a compromise should be made.

1) A consequence of the first method is that a thin insulation layer is required in order to achieve fast recovery times of the switches. As a result, I_{min} increases and P_{stat} becomes relatively high.
2) The disadvantages of the second method, are threefold:
   - The protection of the primary coil becomes more and more problematic as W_p grows.
   - The production costs of the transformer, the most expensive rectifier component, will increase.
   - The losses in the transformer become relatively important.

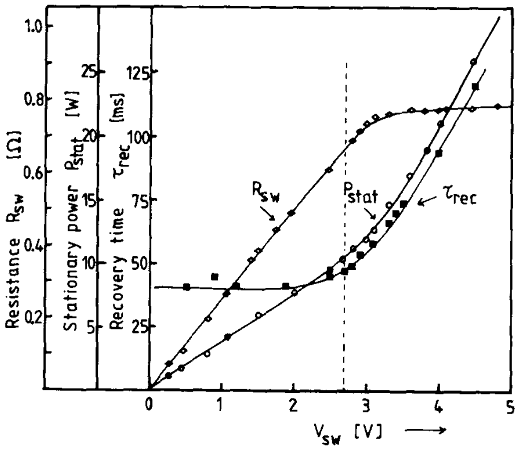

Fig. 5. The properties of switch 6 versus the voltage that is applied during the stationary state.

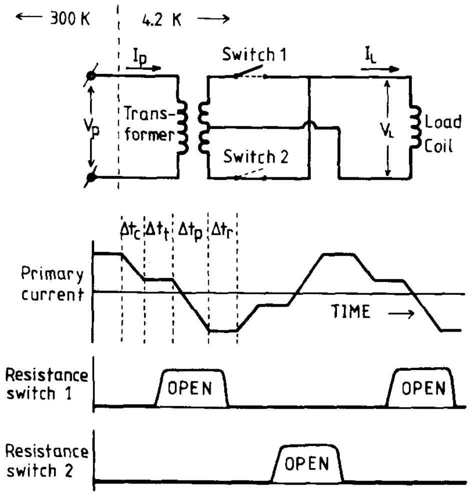

Fig. 6. The electric circuit of a full-wave superconducting rectifier. The diagrams above clarify the operating cycle of the rectifier.

Table 2 Experimental switches.

no	Type of conductor	insulation: material	thickness	R_{sw} [Ω]	I_{max} [A]	τ_{rec} [ms]	I_{min} [A]	E_{act} [J]
1	MCA7#	STYCAST	1.75 mm	113	≃180	215	0.13	-
2	MCA7#	STYCAST	1.3 mm	116	≃180	190	0.14	-
3	MCA7#	STYCAST	0.9 mm	135	≃180	100	0.20	-
4	MCA7#	STYCAST	0.6 mm	140	≃180	64	0.23	-
5	MCA3+	STYCAST	0.5 mm	0.84	1250*	80	5.3	0.5
6	MCA3+	KAPTON	130 μm	0.84	1800*	40	4.0	0.3
7	MCA3+	KAPTON	130 μm	0.84	1400*	40	4.0	0.25

*Measured at dI/dt = 100 kA/s.
#Twisted cable consisting of three strands.
+Braided cable of 144 strands having the matrix removed by etching.

Table 3 Conductor specifications.

Type of conductor	MCA3	MCA7
wire diameter [μm]	114	127
superconductor	NbTi	NbTi
matrix material	Cu	Cu30Ni
number of filaments	1	114
fil. diameter [μm]	74	7.8
matrix : sc. ratio	1.4	1.35
ρ_N [10^{-8} Ωm]	52	36

*The matrix is finally etched away.

An aspect of raising f or W_p is that the load voltage V_L will increase accordingly. During the pump step of a full-wave rectifier, twice V_L appears across one of the switches. In order to remain in the minimum propagation range, the following condition has to be satisfied: $V_L < 0.4 \cdot R_{max} I_{min}$. The implication is that R_{max}, and thus the size of the switch, is proportional to the average power that can be delivered by the rectifier.

Let us now determine the effect of the control times of the switches on the maximum achievable rectifier frequency. Consider the four intervals of the operating cycle of a rectifier (Fig. 6). Every half-cycle we have to consecutively open a switch, perform a pump step, close a switch and commutate the secondary current from one secondary branch to the other. The time required to open and close a switch is $\Delta t_t + \Delta t_r$, which depends entirely on the control times of the switch. The lower limit of the pumping time is generally related to the maximum voltage (V_p) or peak power ($\hat{P}$) of the available power supply for the primary coil, $\Delta t_p > L_p 2\hat{I}_p / V_p = 4W_p/\hat{P}$. In a well-designed rectifier the pump time Δt_p is comparable to the control times Δt_t and Δt_r or preferably even somewhat larger. The commutation time Δt_c can usually be made small compared to the total period time because the effective primary inductance during the commutation is only $(1-k^2)L_p$. The maximum frequency is

$$f = \frac{2}{\Delta t_t + \Delta t_p + \Delta t_r + \Delta t_c} \geq \frac{2}{\tau_{act} + \tau_{rec} + 4W_p/\hat{P}} \quad (5)$$

Efficiency

Next, we determine how the reverse current through the switches during the pump steps contributes to the inefficiency of the rectifier system. The instantaneous power loss in the switch is $2V_L I_{min}$, because the reverse current equals I_{min}. The delivered power to the load is also proportional to V_L namely $V_L I_L$. It can easily be checked that when the coil is charged from zero current to I_L, the integrated energy loss equals

$$W_{ohm} = 2\ L_L\ I_L\ I_{min} = 4\ \frac{I_{min}}{I_L}\ W_L\ , \quad (6)$$

where W_L is the stored energy in the load coil. The equation shows that W_{ohm}/W_L is independent of the operating frequency or the choice of the control signals. This is a consequence of the fact that reverse current during the pump intervals is constant (I_{min}). Apparently, the ratio of minimum propagating current and maximum current-carrying capacity of the switch is an important parameter, since it is a measure for the ultimate efficiency that can be achieved.

A second source of dissipation in the switches is the activation energy E_{act}, which is lost to the helium bath each time the switch recovers. This occurs twice a cycle, so

$$W_{act} = 2\ f\ E_{act}\ W_L/\bar{P}_L\ . \quad (7)$$

Clearly, the activation loss is proportional to f and E_{act}. The required activation energy E_{act} is in turn linearly dependent on the volume (and thus the heat capacity) of the materials that constitute the switch, such as conductor, epoxy, heater and insulation layer. Note that the factors W_{ohm}/W_L and W_{act}/W_L are both independent of the primary energy content W_p.

A 1 kA thermally switched rectifier

The switches 6 and 7 mentioned in table 2 have been applied in the 1 kA thermally controlled rectifier described previously [4]. The rectifier was tested succesfully up to 2 Hz at an average power of 100 W. Above 2 Hz, the load voltage is such that the switches develop their full off-resistance and as a result τ_{rec} becomes too large to ensure a reliable operation. The integral losses of this rectifier have been determined calorimetrically for several values of I_L. For example, at I_L = 800 A, f = 2 Hz and $\bar{P}_L$ = 100 W, the measured efficiency amounts to 95 %. The calculated efficiency for this particular example, is 96 %. The loss contributions can be specified as follows.

- 2.0 % of the energy is lost as reverse current loss.
- 1.1 % is used to activate the switches.
- 0.9 % are eddy current and hysteresis losses in the transformer

It should be mentioned that the design value for the primary current $\hat{I}_p$ is 30 A whereas the currently available power supply generates only 20 A. If the primary current is raised to 30 A, the average power increases by a factor 2.25 and the calculated efficiency becomes well over 97 %, which is an excellent figure for most applications.

Conclusions

The thermal behaviour of thermally controlled switches has been investigated from both a theoretical and an experimental point of view. A very important parameter of the switch is the maximum temperature T_N that occurs in the normal regions when the switch is in the resistive state. Once T_N is known it is possible to predict the recovery time τ_{rec}, the activation energy E_{act}, the stationary dissipation P_{stat} and the minimum propagation current I_{min}. The calculated and measured results, which are in good agreement, show that T_N is approximately 12 K and hardly depends on the choice of the thickness or material of the insulation layer. Another result is that in order to combine a low value of P_{stat} with a short thermal recovery time, which are in fact contradictory demands, the insulation material should have a very low thermal conductivity. One material having an extremely poor conductivity at cryogenic temperatures is KAPTON. Two "KAPTON-switches" have been applied with succes in a 1 kA superconducting rectifier with an efficiency of at least 95 %. Some problems which have so far prevented this rectifier from being used at its design specifications are related to the room temperature equipment which drives the rectifier. Full testing of the rectifier is expected at the end of 1987.

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