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Radial Thermal Conductivity of All Aluminum Alloy Conductors

Jose Rodriguez and Christian M. Franck,

Abstract—An accurate thermal rating of an overhead transmission line must take into account the higher temperatures present at the interior of the conductor. In this article we present our experimental study of the radial temperature profile of an All Aluminum Alloy Conductor (AAAC). We have monitored the temperature of every conductor layer as a function of electrical current, line tension and time. We have analyzed the data using a continuous model and a layer model of the conductor. Both models reproduce closely the experimental data in the steady and transient state. Also, we discuss the effect of the higher core temperature in the thermal rating of a transmission line.

Index Terms—AAAC, overhead transmission lines, radial thermal conductivity

I. INTRODUCTION

The electrical energy consumption of many countries will keep increasing in the future [1]. This poses a problem not only for the production of electrical energy but also for its transmission, since the higher currents on the transmission grid can get closer to the maximum allowed values. One of the alternatives to address this problem is to implement a Dynamic Line Rating Scheme (DLRS) for the management of the transmission grid. In this scheme the maximum current capacity of the transmission grid is not fixed, but changes continuously as a function of the weather conditions [2], [3], [4], [5], [6]. Depending on the grid details, a DLRS is expected to increase the average maximum current capacity by $\approx 15\%$ and to increase the safety of the line since the risk of overload is minimized [4], [7]. One of the most important issues for the implementation of the DLRS is to keep the stability of the transmission grid under the higher operating currents. In particular, the maximum allowed temperature of the transmission conductor has to be respected to ensure that the limits on sag and annealing effects on the aluminum material are not exceeded. For this, it is necessary to have a detailed thermodynamic picture of the conductor and, in particular, the higher temperatures at the interior of the conductor have to be accurately predicted and controlled. For this reason, the standard methods used for calculating the maximum current capacity of a conductor [8], [9], [10] need to be modified in a DLRS to account for the higher core temperature of the conductor [11], [12].

In this article we present our studies of the radial temperature profile of an All Aluminum Alloy Conductor (AAAC) which is typically used in the Swiss transmission grid. Our study builds on previous works in several ways. Most studies on the radial temperature profile of transmission lines have been focused on steel-core conductors, and less on other conductors such as AAACs, All Aluminum Conductors (AAC)

and copper conductors [13], [14], [11], [15], [16], [17], [18]. Notably, there are only three published values of the radial thermal conductivity of AAACs (reported in the comment section of Ref. [14]). In this article we contribute to increase the available data by reporting the thermal conductivity of our AAAC as a function of the tension in the line. To our knowledge, the effects of tension on the radial thermal conductivity has been systematically studied only once [17] for an AAC; but this study was done using a short segment of conductor and a clamping system different from that used in real applications. In our experimental setup we have used a long conductor to avoid heat sink effects from the edges, as well as a realistic clamping system for the line (this is important since different clamping mechanisms have a different influence on the inner conductor structure). Also, in all previous studies the thermocouples have been implanted through holes drilled in the conductors. This method has the drawback that it is not possible to know where the thermocouples are exactly located. In our experimental setup we have implanted the thermocouples directly in the different conductor layers eliminating the positioning problem. Finally, we want to mention that previous studies have focused on the core to surface temperature of the conductor to obtain effective thermal conductivities. In this article we use the whole temperature profile to validate the continuous model of radial heat conduction, and we derive the parameters required by a layer model of the conductor. This layer model of the conductor provides further insight in the “microscopic” mechanism of heat conduction studied in Refs. [17] and [19].

We have structured this article as follows. In Section II we introduce our experimental setup and measurement procedure. After this, in Section III we present the steady state radial temperature profiles of the conductor under different currents and tensions. From this data we calculate the effective radial thermal conductivity of the conductor, and compare the experimentally measured temperature profile with the prediction of the “continuous model” of the line. Also, we propose a “layer model” which reflects more closely the layered nature of the conductor. In Section IV we analyze the time evolution of the temperature profile after a step-up and a step-down in electrical current. We present the time constants for the two processes, and compare the experimental results with numerical solutions of the two models mentioned above. Here we also analyze the effect of the higher core temperature on the warming time of the line. In section V we discuss the effects of the higher internal temperature on the maximum current capacity of the line; and in section VI we discuss different aspects of

our methods and results. Finally, in Section VII we present a summary of our results.

II. EXPERIMENTAL SETUP AND METHODOLOGY

Our test facility is located indoors, and this allows us to perform our measurements under zero wind speed. The facility consists of a 9 meter test conductor mounted on a steel frame, a high current transformer and a servo motor to adjust tension. The test conductor is a 550 mm² AAAC with a radius of 15.26 mm, and 60 strands arranged in four layers around a core wire (61/3.39mm). The conductor is made of Aldrey (Al-0.5%Mg-0.5%Si) and has an estimated tensile strength of 160 kN¹. The conductor is held by wedge-type clamps which are attached to the steel frame by tension clamps. This combination of wedge and tension clamps provides a realistic tensioning setup for the conductor. The tension is adjusted using a DC servo motor with a worm gear box, and measured by an HBM tension transducer connected to the analogue input of a National Instruments DAQ module. Although the tensioning system is designed to withstand 50 kN, in this study we limited the maximum tension to 30 kN² (equal to an average tensile stress of 54.5 N/mm² for our conductor). The electric current is generated by a power supply (3-phase 400VAC/63A) connected to a variable transformer which is further connected to a high-current transformer. This equipment allows us to adjust the current between zero and 2100 A. In order to prevent degradation of the test conductor, we kept the current below ≈ 900 A (this keeps the conductor temperature below $\approx 80^\circ\text{C}$). The current stability is typically ± 2 A at 900 A and this value is smaller for lower currents.

The temperature at different points of the conductor is measured using T-type thermocouples. These thermocouples are introduced into 1 mm deep holes drilled in single strands and are held in place by epoxy resin. To prevent an electric contact between the thermocouples and the test conductor, the hot junctions are coated with a thin layer of insulating paint before installation. All thermocouples are thermally anchored to the conductor and the cold junctions are directly connected to a national instruments NI-9214 module. The temperature signal is optically transmitted from the DAQ module to a computer running LabVIEW.

The thermocouples which monitor the temperature inside the conductor are also mounted directly in the strands of the different layers as described in the previous paragraphs. To do this we accessed the inner layers by forcing adjacent strands in the upper layers to open. This introduces some local plastic deformations in the conductor. In the comments section we discuss about the advantages and disadvantages of this procedure as well as its possible effects on the measurements. The internal thermocouples were mounted in

Fig. 1. Core (top black line) and surface (bottom blue line) temperature of the AAAC conductor as a function of time after a decrease in the current. The red lines span over the stability region of the temperature as determined by a trend following algorithm. The dashed red lines indicate the average temperature over the stability region and the continuous ones correspond to one standard deviation from the mean ($30.08 \pm 0.02^\circ\text{C}$ for the core temperature, and $29.34 \pm 0.06^\circ\text{C}$ for the surface temperature).

the middle of the 9 m long conductor to avoid any heat-sink effects from the clamps. The ambient temperature in the laboratory is controlled by a ventilation system to 21°C . The local temperature near the center of the line (and below it) was observed to slowly increase when higher currents were applied to the conductor. It is for this reason that this temperature is continuously monitored during the measurements using a T-type thermocouple attached to a thin copper plate which is suspended by nylon threads inside a Stevenson screen.

Temperature measurements were performed as a function of current and tension. After a change in the conditions we waited for the transient effects in the temperature and tension to disappear. Then we recorded data for periods of approximately 40 minutes. For our data analysis we implemented a trend-following algorithm to extract the region over which the measured temperatures were stable. The mean and standard deviation of each variable were calculated for this stability region, and these numbers were used as value and error for data analysis and graphic purpose. In Figure 1 we show a sample of raw data for the core and surface temperature of the conductor. In this figure the higher stability of the core with respect to the surface temperature can be noted. The greater variability in the surface temperature reading may be due to turbulence in the natural convective cooling.

III. RADIAL HEAT CONDUCTION IN THE STEADY STATE

The temperature of an energized transmission line is higher in the inside since the internal heat generated has to flow to the surface to leave the conductor. This problem can be modeled by assuming a homogeneous and cylindrically symmetric conductor. The heat equation for this problem is:

$$\frac{c\rho}{\pi r_s^2} \frac{\partial T}{\partial t} = k \frac{1}{r} \frac{\partial}{\partial r} \left(r \frac{\partial T}{\partial r} \right) + \frac{P_j}{\pi r_s^2} \quad (1)$$

where c is the specific heat of the material (909 J/kg K at 20°C for our aluminum alloy), ρ is the linear density of the line (1.528 kg/m for our conductor), r_s is the conductor radius,

¹Estimation performed using data from a breaking load test of individual strands performed by the manufacturer of the conductor.

²Tension in transmission lines changes with time and weather conditions, but a typical operating tension for this type of conductor in the Swiss transmission network would be 15 kN

k is the thermal conductivity of the material, T is the local conductor temperature and P_J is the Joule heating per unit length of conductor. To solve Eq. (1) we will assume that the resistance of the conductor is constant through its cross section, even though thermal gradients create differences in resistivity. As we will show, the temperature gradients are small enough to justify this assumption (Ref. [19] presents the full treatment which simplifies to our result when the gradients are small). In the steady state, the left hand side of Eq. (1) is zero, and a solution can be found using the following boundary conditions:

$$\left. \frac{\partial T}{\partial r} \right|_{r=0} = 0 \quad (2)$$

$$T|_{r=r_s} = T_s \quad (3)$$

where T_s is the temperature at the surface of the conductor. This solution is:

$$T = T_s + \frac{P_J}{4\pi k} \left(1 - \left(\frac{r}{r_s} \right)^2 \right) \quad (4)$$

From this equation we get that the core to surface temperature is [19], [11]:

$$\Delta T = \frac{RI^2}{4\pi k} \quad (5)$$

where R is the AC-resistance of the conductor per unit length evaluated at T_s and I is the conductor current. In this article we evaluated the line resistance at the surface temperature, and not at the more accurate average conductor temperature, because in most cases T_s is a good estimator of the average conductor temperature and because in most real life applications the core temperature is not available. Using experimental data we have estimated the error on R from using the surface temperature instead of the average conductor temperature to be 1.5% when the surface temperature is 83.3°C and ΔT is 5°C.

We want to note that ΔT in Eq. (5) depends only on the line current and not on external factors such as wind cooling (provided that these factors do not break the cylindrical symmetry of the problem). Because of this sole dependence in the current, ΔT can be close to 10°C for conductors operated under a DLRS. This is specially the case under high wind conditions where the large forced-convective cooling allows for a higher load on the conductor [14], [11].

Real conductors are not homogeneous cylinders though, they have air voids and contact points between the strands. Nevertheless, it has been observed that real conductors follow Eq. (5) provided that the thermal conductivity of the conductor in the radial direction is reduced from k to a value k_r which is approximately 100 times smaller [11], [20], [16]. Indeed, in Figure 2 we show that the quadratic function suggested by Eq. (5) provides a satisfactory description of the experimental data. Published values of k_r for AACs range from 0.73 W/m K to 4 W/m K; and for AAACs from 1.45 W/m K to 6.98 W/m K [14], [17], [16]. We have calculated k_r for our line by fitting

Fig. 2. Current dependence of $4\pi\Delta T / (R(1 - (r_1/r_s)^2))$ at 3 kN (red triangles) and 30 kN (blue dots). The lines are quadratic fits to the 3 kN (continuous red line) and 30 kN (dashed blue line) data.

Fig. 3. Tension dependence of the radial thermal conductivity. Top axis shows the equivalent tensile stress for our conductor.

the temperature difference between the surface and the first conductor layer (at $r_1 = 2r_s/9$) as a function of the electrical current. In Figure 3 we show the value of k_r as a function of tension. It can be seen that, as expected from Refs. [19] and [17], increasing the tension has the effect of incrementing the thermal conduction through the layers. The reason for this increase is the higher radial force among the conductor layers at higher tensions (see ahead). Our values for k_r are 1.5 to 8 times smaller than those reported for AACs in the comment section of Ref. [14], but its size and tension dependence is very close to that reported more recently in Ref. [17] for a hard-drawn AAC.

We would like to address now the question of how well does the continuous model represents the real temperature profile of the conductor. For this, we will assume that the temperature is constant within the strands of a given conductor layer and, therefore, our measured temperature represents the temperature of that layer. In fact, it has been proposed that the heat conduction among strands in different layers is dominated by the heat flow through contact points between the strands with areas of $\approx 1\text{mm}^2$ and effective thermal conductivities much lower than that of aluminum [17]. Then, for a given heat flow, the thermal gradient should peak around these contact points due to a reduction of the area available for the heat flow. Therefore, a layered temperature structure for the conductor

Fig. 4. Temperature profile of the line for a current of 909 A and a tension of 5 kN. The step-like profile originates from the layered structure of the line and the width of each step represents the experimental uncertainty in the temperature measurement. The red curved line is Eq. (4) with $k_r=0.88$ W/m K, and with the electrical resistance of the line evaluated at the surface temperature (80.4°C).

(with constant temperature for the layer and sharp temperature changes between the layers) can be a valid picture. In Fig. 4 we show the experimental layer profile of the conductor temperature and the prediction from Eq. (4). It can be observed that the model crosses all the experimentally measured steps, and that the core temperature is well estimated. Furthermore, we found the measured core temperatures always to be within 0.23°C from the estimation using Eq. (5).

To gain further insight in the heat conduction through the contact points between the layers, we write the heat balance equation for the interface between two conductor layers as:

$$N_{ij}h_{ij}\Delta T_{ij} = \sum_{l < j} R_l I_l^2 \quad (6)$$

where N_{ij} is the amount of contact points per unit length between layer i and j ($j = i + 1$), ΔT_{ij} is the temperature difference between layer i and j , R_l is the electrical resistance per unit length of layer l , I_l its current, and h_{ij} can be written as:

$$h_{ij} = \frac{k_p A_{ij}}{\delta} \quad (7)$$

where k_p is the effective thermal conductivity of the contact point, A_{ij} is the area of the contact point and δ the effective width of the contact point. We will refer to h_{ij} as the “heat conductance” of the contact points between layer i and j , and we will enumerate the layers from 0 (the core) to 4 (the surface layer). We want to note that A_{ij} is expected to increase with tension since, as the tension increases, so does the radial forces between the strands (see ahead). N_{ij} is given by:

$$N_{ij} = \frac{N_i N_j (s_i + s_j)}{s_i s_j} \quad (8)$$

where N_i is the amount of strands in layer i and s_i is the pitch of the helix formed by the strands in layer i . We want to note that Eq. (6) through (8) are not meaningful for the interface between the core and the first layer, since in this interface

there are contact lines instead of contact points. Nevertheless, equivalent equations can be formulated provided that N_{01} is only the number of strands around the core ($N_{01} = 6$) and h_{01} is a “heat conductance” per unit length.

Using Eq. (6) we can calculate h_{ij} from our data as a function of the tension of the conductor. Nevertheless, h_{ij} does not depend directly on the conductor tension but on the radial force among the strands in layers i and j . Following the steps in Ref. [19], we can find this force by first noticing that the radial compressive force per unit length of a strand in layer i is given by:

$$\frac{df_i}{dl} = \frac{\Gamma_i \kappa_i}{N_i} \quad (9)$$

where l is the arc length of the strand, Γ_i is the total tension in layer i and κ_i is the radius of curvature of the helix formed by the strand. Then, the total radial force per unit length exerted by layer i in the layers below is given by:

$$F_i = \frac{\Gamma_i \sin^2 \beta_i}{r_i} \frac{((2\pi r_i)^2 + s_i^2)^{1/2}}{s_i} = \frac{2\pi \Gamma_i \sin \beta_i}{s_i} \quad (10)$$

where r_i is the radius of layer i and β_i is given by:

$$\sin \beta_i = \frac{2\pi r_i}{((2\pi r_i)^2 + s_i^2)^{1/2}} \quad (11)$$

Then, since our conductor has four conductor layers (plus a core wire), the total force per contact point between layer i and j is given by:

$$\Phi_{ij} = \frac{\sum_{k=j}^4 F_k}{N_{ij}} \quad (12)$$

For the core wire we can write the force per unit length between the core wire and a strand in layer 1 as:

$$\Phi_{01} = \frac{\sum_{k=1}^4 F_k}{6} \quad (13)$$

In Eq. (10), Γ_i can be found using Eq. (61) in Ref. [19] which we reproduce here:

$$\Gamma_j = 6j\Gamma \cos^3 \beta_j \left(1 + \sum_{i=1}^4 6i \cos^3 \beta_i \right)^{-1} \quad (14)$$

where Γ is the tension applied to the conductor.

Equations (10) through (14) can be used to determine Φ_{ij} . In Figs. 5 and 6 we have plotted h_{ij} as a function of Φ_{ij} . As expected, h_{ij} increases with increasing radial force since the area of the contacts increases accordingly. Note that, for the same radial force, the contact points at the interface between layers 1 and 2 have a higher conductance than those on layers above. As we will discuss later, this can be due to the contact points between layer 1 and 2 having larger permanently deformed areas.

The measured “heat conductances” can be used to create a layer model for the conductor in the following way. For a given line tension, Eqs. (10) through (14) can be used to find

Fig. 5. “Heat conductance” as a function of radial force for contact points between layer 1 and 2 (h_{12} , blue circles), layer 2 and 3 (h_{23} , green squares) and layer 3 and 4 (h_{34} , red triangles). Lines are shown to mark the different trends.

Fig. 6. “Heat conductance” per unit length as a function of force per unit length for the contact lines between the core wire and layer 1.

the radial force per contact point for the different layers. Then, using Figs. 5 and 6 appropriate values for h_{ij} can be obtained. After this, the “heat conductances” can be used to numerically calculate all the heat fluxes among the layers using some given boundary conditions. Then the temperature of the layers can be found from the known amount of heat in the layer at a given time. In the next section we show the result from this procedure applied to a time dependent heating process of the line.

IV. DYNAMIC RADIAL HEAT CONDUCTION

We have studied the radial heat conduction in the line following step-like changes in the electrical current. In Figure 7 we show the increase of the surface and core temperature upon an increase of the electrical current from zero to 863 A, and a subsequent temperature decrease following a reduction of the current to zero. In this figure we also show the numerical solution of Eq. (1) using the measured k_r values interpolated at the measured instantaneous tensions, and subjected to the following boundary conditions:

$$T(r, t = 0) = T_0 \quad (15)$$

$$\left. \frac{\partial T}{\partial r} \right|_{r=0} = 0 \quad (16)$$

Fig. 7. Temperature evolution of the line after a step up of the electrical current from zero to 863 A, and a subsequent step down to zero. The initial tension is 10 kN and the maximum surface temperature of the line was 76°C. Top lines correspond to the temperature of layer 1, and bottom ones to the surface temperature. Continuous lines are measured data; red dotted lines are numerical solutions to the continuous model in Eq. (1); and black dotted lines are numerical solutions to a layer model using the thermal conductivities in Figs. 5 and 6.

$$k_r \left. \frac{\partial T}{\partial r} \right|_{r=r_s} = - \frac{P_R + P_N}{2\pi r_s} \quad (17)$$

where T_0 is the measured temperature of the conductor at the beginning of the measurement, P_R is the heat per unit length lost by the conductor as black body radiation and P_N is the heat per unit length lost by the line due to natural convection (we found the warming contribution from the absorption of radiation from the laboratory lights to be much smaller than P_J , P_R or P_N during most of the warming up and cooling down cycle). Expressions for P_R and P_N were taken from Ref. [8].

In Figure 7 we also show the numerical solution of a layer model which uses the thermal conductivities shown in Fig. 5 and 6. It can be observed that the continuous model and the layer model are very close to each other, with only a difference of 1°C in the maximum core temperatures. This difference might be a consequence of the extrapolation required to calculate k_r and h_{ij} down to 1.8 kN. With respect to the measured data, both models follow the temperature trend closely. Nevertheless, between 0.5 hrs and 2 hrs, both models fail to follow closely the temperature evolution differing by up to 1.7°C from the measured values. Then, during cooling, the maximum difference between the models and the measured values is 0.8°C. These differences can be due to an inaccuracy in the temperature-dependent specific heat and thermal expansion coefficient used in the simulations. In fact, in our simulation we have used the specific heat and thermal expansion coefficient of aluminum since we do not know the exact values for the aluminum alloy. Also, we do not know the emissivity of the line, so we adjusted its value to match the maximum surface temperature of the line (76°C). We found this value to be 0.32, which is within the range of values expected for AAACs published in Ref. [20].

In Table I we present the time constants for both, the warming up and cooling down processes (the time constant

is the time at which 63% of the temperature increase/decrease has taken place). It can be seen that slightly smaller time constants are observed when the initial tension is higher. The slightly smaller time constants for higher initial tensions is due to the higher radial thermal conductivity during the heating/cooling process (in the 10kN warm up process the tension dropped to 1.8kN, while in the 30kN one it dropped to 8.1kN).

TABLE I
TIME CONSTANTS FOR WARMING UP AND COOLING DOWN PROCESSES AT DIFFERENT INITIAL TENSIONS.

Initial tension	Warming up	Cooling down
10kN	26.0±0.4 min	29.3±0.3 min
30kN	23.4±0.5 min	24.6±0.4 min

In Figure 7 it can be seen that the core of the line reaches a given temperature faster than the surface. This point is important for applications since, for example, during fault conditions it is desired to know the time at which the core reaches the maximum allowed temperature and not when the cooler surface does. After a step-up change in the line current, the difference between the time at which the surface reaches the maximum allowed temperature and that at which the core does, can be estimated from:

$$\Delta t = \tau \left(\ln \left(1 + \frac{\Delta T}{T_s - T_{\max}} \right) - \frac{\Delta T}{T_s - T_0} \right) \quad (18)$$

where τ is the time constant for the warming process, T_0 is the line temperature at time zero, $T_{\max}$ is the maximum allowed temperature of the line, ΔT is given by Eq. (5), and T_s is the final surface temperature ($T_s > T_{\max}$). In writing Eq. (18), we assumed that the initial core to surface temperature is small and also that $(T_s - T_0) \gg \Delta T$. In practice τ , T_s and the time at which the surface reaches $T_{\max}$ can be estimated by quickly solving numerically the zero-dimensional heat equation:

$$c\rho \frac{\partial T}{\partial t} = P_J - P_R - P_N \quad (19)$$

with the initial boundary condition $T = T_0$ (a solar radiation term is needed in this equation if it is applied to real transmission lines [8], [9], [10]). We used this method to estimate Δt for the case of a current step from zero to 1120 A and found 268 s; while the measured value was 238 s. We believe that the 13% difference is mostly due to a drift down of the current by 14 A during the measurement time.

V. CORE TEMPERATURE AND LINE RATING

We want to address now the effect of the higher core temperature in the maximum current capacity of a transmission line, and we will do this using a specific example. To rate a line in the low lands of north Switzerland during the winter season, it is common to assume an air temperature of 10°C, a solar radiation of 900 W/m² and a wind speed of 0.5 m/s. If we use these ambient conditions to make a seasonal rate of our conductor (see Ref. [8], [9], [10]) with a maximum surface temperature of 80°C and an absorption coefficient of

0.4, we find that the maximum current capacity is 1200 A. If we further assume that $k_r=1$ W/m K, then the core temperature reaches 88°C under these conditions. If instead we rate the line such that the maximum core temperature is 80°C, then the maximum current capacity of the line has to be decreased by 5% to 1140 A.

A higher current capacity for the line is expected if a DLRS is used. Indeed, using weather data for the same geographic region during January 2013, we found that a DLRS would have an average maximum current capacity of 1330 A during the day and 1360 A during the night. To perform this DLRS, we used the maximum ambient temperature for a given day (or night), the maximum solar irradiance (equal to zero for the night) and a constant wind speed of 0.5 m/s to make the thermal rating for a given day (or night). The highest maximum current capacity which we found was 1380 A and it happened during a night when the maximum ambient temperature was -2.6°C. As in the previous paragraph, we did the DLRS assuming a surface temperature for the conductor of 80°C. This means that the core temperature would be at 91°C when the line is at its maximum current capacity of 1380 A. This higher core temperature would increase the average conductor temperature to $\approx 85.5^\circ\text{C}$. If a homogeneous piece of this cable is used to cover a span between two towers separated by 300 m, and if we assume that the weather conditions mentioned in the previous paragraph apply all along the span, then the real sag (*i.e.* that calculated using the average conductor temperature of 85.5°C) at the maximum current capacity of 1380 A would be 16 cm (approximately 1% of the total sag) larger than the one calculated assuming an average conductor temperature of 80°C³. The higher core temperature would not be of concern for the annealing of the line though. This because annealing effects are negligible below 100°C for Aldrey alloys⁴ [21]. If the maximum temperature limit is imposed to the core of the conductor instead of to the surface, then we get that the highest maximum current capacity of the line has to be decreased by 4% from 1380 A to 1320 A. Before finishing this section we want to note that the DLRS which we used is based in a constant low wind condition. If the wind speed is also allowed to vary in a DLRS, even higher core temperatures can arise if not taken into account for the rating.

VI. DISCUSSION

As mentioned in section II, our method of implanting the thermocouples by forcing adjacent strands to open creates plastic deformations in the structure of the line. It is hard to estimate the effect of these distortions on the temperature distribution of the line. It is most probable that these plastic deformations remove contact points between the strands, reducing locally the radial heat conductivity and, therefore,

³The effect of the higher internal temperature of the conductor on the sag needs to be further studied. In particular the effects of different weather conditions, the effect of the catenary shape of the conductor, conductor defects and comparisons to experimental measurements are need to be done.

⁴Nevertheless the higher core temperature can be relevant for annealing-limited transmission lines, since the tensile strength decrease at a faster rate with higher conductor temperatures.

increasing locally the temperature. Now, if temperature is increased locally in a continuous strand, there will be a flow of heat along the strand with thermal conductivity 200 times larger than the radial one. This would likely keep the local temperature distortion at a low value. Still, and as we mentioned before, our values of k_r are consistent with what has been measured before by drilling holes in AACs, and also our tension dependence is similar to that found there [17].

In our experiment the surface temperature of the conductor is slightly higher at the top because the air heated at the bottom of the conductor drifts upwards. We found the temperature difference to be $\approx 1^\circ\text{C}$ when the temperature at the top was $\approx 65^\circ\text{C}$ and the line current was $\approx 650\text{ A}$. Then, for the case of natural convection cooling and no solar radiation, the asymmetry in the surface temperature is so small that an axially symmetric approach to the problem is valid. This might not be the case for external transmission lines though. Strong wind or solar radiation would contribute to creating an asymmetric surface temperature in external lines, but the thermal conductivity along the helical strands would contribute to attenuate this effect. If asymmetric environmental conditions are important for the line temperature profile of a transmission line is something that needs to be further studied.

In Figures 5 and 6 we show that h_{01} , h_{12} and h_{23} increase with increasing radial force (*i.e.* axial tension of the transmission line). This is expected as the contact area between the strands increases with increasing tension. Nevertheless, the values of h_{12} are higher than those of h_{23} for the same radial force. This difference can be caused by permanent area deformation of the contact points, which is higher for contact points between layer 1 and 2. In Figure 5 we show that the maximum radial force for these points is about 150 N. In Ref. [19], the areas of contact points between hard-drawn aluminum wires are reported for different compressive forces. Using these areas scaled by the ratio of the yield stresses of hard-drawn aluminum and aluminum alloy ($f_{\text{AAC}}/f_{\text{AAAC}}$) we can estimate that at 150 N, the contact-point area is about 0.3 mm^2 and therefore the pressure at the contact point is $\approx 500\text{ MPa}$. This is higher than the yield stress of the material ($\approx 300\text{ MPa}$), and therefore substantial permanent area deformation is expected for the point-contacts between layers 1 and 2. Since the maximum radial force experienced by the contact points between layers 1 and 2 is bigger than that of the contact points between layer 2 and 3, these points have larger permanently deformed areas. Therefore, their thermal conductivities are higher even for the same radial force. In Figure 5 it can also be seen that h_{34} is rather constant as tension increases. The radial forces for the contact points between layer 3 and 4 do not reach very high values and, therefore, its permanent deformed areas should be smaller than those of the contact points at lower interfaces. Also the range of radial forces which the contact points between layer 3 and 4 experience is limited, and then h_{34} should not be expected to change much when increasing the line tension from 5 kN to 30 kN.

Before finishing this section we want to note that the force

per contact point derived in Refs. [22] and [23] using a more precise and complex conductor model, are equal to those obtained using Eq. 12 in the limit of $r_i/r_{ij} \approx 1$, $r_j/r_{ij} \approx 1$ and, for the expression derived in Ref. [22], $r_w/r_{ij} \approx 0$ (here r_{ij} is the radius of the contact cylinder between layers i and j , and r_w is the wire radius). If the model in Refs. [23] is used instead of Eq. 12 to calculate the radial forces, then this would amount to shifting the red triangles in Fig. 5 to the right by 4%, the green squares by 7% and the blue circles by 14%. We want to note that regardless of this correction, which can be traced back to a simplification in the derivation of Eq. 8, the conclusions derived from Fig. 5 would not be changed. Mainly, that the contact points between the layers are permanently deformed and that the larger contact point deformation of the inner layers is responsible for their larger “heat conductances”.

VII. CONCLUSIONS

We have studied the radial heat conduction in an AAAC. We found that the effective radial thermal conductivity of the line increases from 0.88 W/m K at 3 kN to 1 W/m K at 30 kN. Our values are smaller than those reported for AAACs in the comments section of Ref. [14], but they agree well with those reported for a hard-drawn AAC in Ref. [17]. Based on the ideas proposed in Ref. [19], we have developed a layer model of the conductor where the heat flow between the layers happens through point contacts with effective “heat conductances” (between the core and the first layer the heat conduction happens through contact lines). The tension dependence of these “heat conductances” was found to be different for the different interfaces probably due to the higher permanent deformation of the contact points in the innermost layers. We have also shown that a continuous model based on the heat equation provides very similar results to the layer model; and that both of these models reproduce very closely the time dependence of the core and surface temperature upon step-like changes of the electric current. Finally, using an applied example we found that the maximum current capacity of a line has to be decreased by $\approx 5\%$ to account for the higher temperatures in the core under low wind conditions.

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