

Hybrid Transformer Model for Transient Simulation: Part II – Laboratory Measurements and Benchmarking

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Abstract — The topological structure and basic approaches for parameter estimation for a new hybrid transformer model are presented in Part I of this two-paper set. Part II deals with the model benchmarking and also discusses additional methods for parameter estimation based on laboratory measurements. The simulation results confirm the validity of the model for the low- and medium-frequency range.

Index Terms — Duality, EMTP, Transformer Models, Transient Simulations

I. INTRODUCTION

AMONG the many existing power transformer models developed in recent decades, only a few have the ability to simulate behaviors over a broad range of frequencies. Therefore, there is a strong need to develop advanced models [1]–[6]. Various possible coil and core configurations and inclusion of magnetic saturation effects make the derivation of a general model even more complex.

The use of simple models may be justified by lack of available information. However, nowadays it is not uncommon to have a very detailed transformer test report, and additional data can be requested from the manufacturer at the time of purchase. In cases when information is not available, typical parameters based on nameplate ratings can be obtained from reference textbooks, standards and common design practice.

The work presented in this set of papers provides advancements beyond the traditional transformer models in EMTP [7], [8].

This paper discusses parameter estimation methods for the model described in Part I based on the additional laboratory tests that have been performed for two particular transformers and provides the results of model benchmarking.

II. LABORATORY MEASUREMENTS AND PARAMETERS

The Part I paper developed the model and presented

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parameter estimation methods based on three typically available sources of information: factory test reports, design data, and basic ratings. Here this methodology is further extended to include one more option, namely direct laboratory measurements.

Two transformers with different core configurations were tested in the lab: a three-legged 15-kVA, wye-delta dry-type transformer with voltage ratio 208Y/120:240, and a five-legged 150-kVA, grounded wye-wye oil filled transformer, with voltage ratio 12470GRY/7200:208Y/120.

The model has also been implemented for large 2-winding and 3-winding transformers.

Several general approaches can be used to obtain the parameters of the model when transformers are available for testing in the laboratory. The first option is a traditional approach based on RMS measurements of voltages, currents and average power losses. Another option is to record the waveforms of voltages and currents, and determine the parameters by post-processing the waveforms.

Both of these methods were applied to the 3-legged and 5-legged transformers [9], [10]. The methods are summarized as follows:

A. Test Measurement Strategy

The tests were implemented in a similar way for both the 3-legged Δ -Y and the 5-legged Y-Y transformers. The delta winding was opened in the case of the 3-legged transformer.

1) Short Circuit Tests

The short-circuit tests were performed on the high voltage side with the low voltage windings shorted; the voltage V_H , current I_H , power P_H and power factor were measured and used to determine the short circuit impedances, i.e., the winding resistances and the leakage reactances of the three phases.

DC resistance tests were also performed. These results were used to approximate the ratio of high-voltage winding AC resistance to low-voltage winding AC resistance. The total AC resistance values were all calculated from the measured RMS voltages and currents and average power.

2) Open Circuit Tests

The tests were done in a slightly different way as compared to the typical factory tests, in order to enable determination of the individual saturation curves of the different core limbs. This strategy of exciting one phase and strategically shorting or open-

circuiting other phases to isolate on individual sections of the core was developed and explained in [4] and [11].

In this case, open circuit tests of the center limb and outer limb were performed, and from these tests the parameters for the core and yoke sections were determined as described below.

The configuration for the open circuit test on the center leg is shown in Fig. 1. Here X_3-X_0 is shorted, X_1-X_0 was excited and the voltage V_{20} across X_2-X_0 was measured, as was the current I_3 . The short X_3-X_0 effectively bypasses the core and yoke of the 3rd limb such that I_3 is essentially equal to the exciting current of the center core leg. Hence, using several measured values of V_{20} and I_3 the values of R_1 and the saturation curve for L_1 were determined.

The behavior of the core limb resistance R_1 , which models the hysteresis and eddy current losses, is also nonlinear. Following the recommendations of [6], the value of the resistance at rated voltage was used as an approximation.

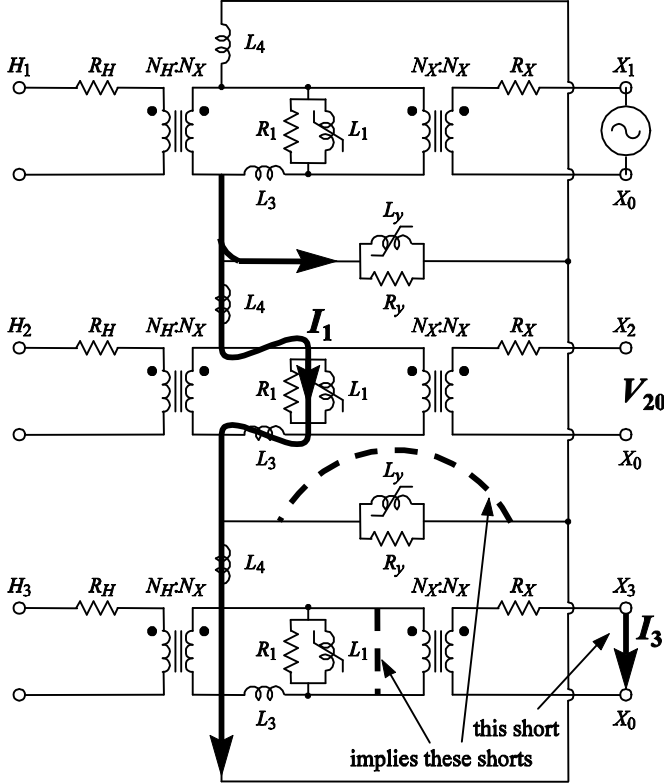


Fig. 1. Circuit configuration for the open-circuit test of center leg.

The configuration for the open-circuit test used to obtain parameters for the outer leg is shown in Fig. 2, where X_2-X_0 was shorted, X_1-X_0 was excited and the voltage V_{30} across X_3-X_0 was measured, as was the current I_1 . The short across X_2-X_0 effectively bypasses the core of the center leg. Consequently, the measured current I_1 was equal to the sum of the currents I_2 (flowing in the R_y-L_y combination representing the yoke sections between the center limb and the third limb) and I_3 (flowing in the R_1-L_1 combination representing the core of the third limb). The measured voltage V_{30} was equal to the voltage drop across the combined impedance Z_C (shown in Fig. 2), which represents the parallel combination of leg and yoke.

Hence, using a family of values of V_{30} and I_1 , the values of $R_1||R_y$ and the saturation curve for $L_1||L_y$ were determined.

Since R_1 and L_1 had been determined from the open-circuit test of the center leg, these could be separated from the parallel combinations to yield R_y and L_y for the yokes.

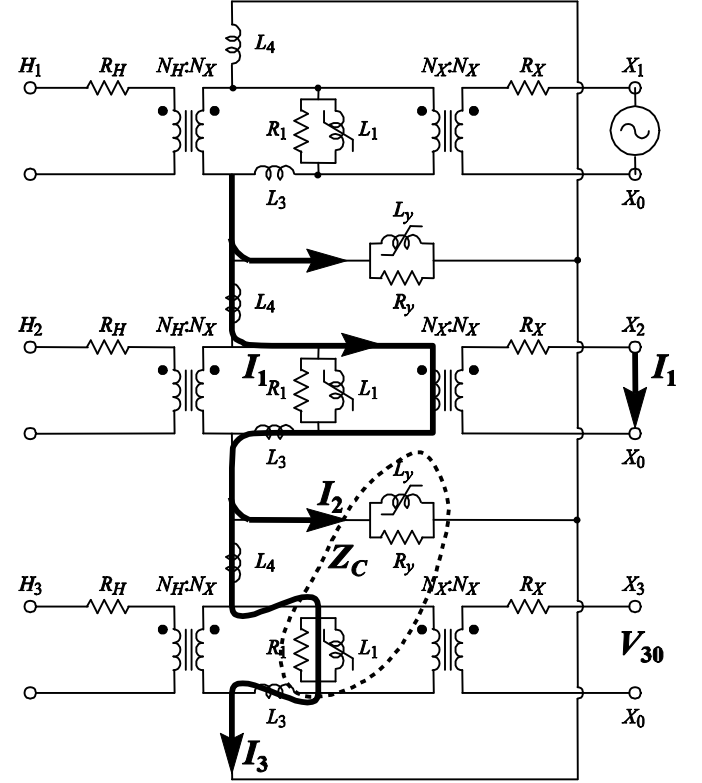


Fig. 2. Circuit configuration for the open-circuit test of outer leg.

3) Zero Sequence Test

The three low-voltage phases were connected to the same AC supply as shown in Fig. 3. This had the effect of placing the applied voltage across the series combination of R_X , L_4 and L_3 of every phase. Therefore the applied voltage, one third of the total current drawn from the source, and the angle between the voltage and current provide values of R_X and L_3+L_4 . Recall that R_X is the winding resistance, and L_3 and L_4 are, respectively, the leakage reactance and the zero sequence reactance, which are both linear. Since R_X and L_3 were determined from the short circuit tests described in section I.A.1 above, the zero sequence inductance L_4 were easily determined.

For the three-legged transformer, the delta-connected high voltage winding was opened up.

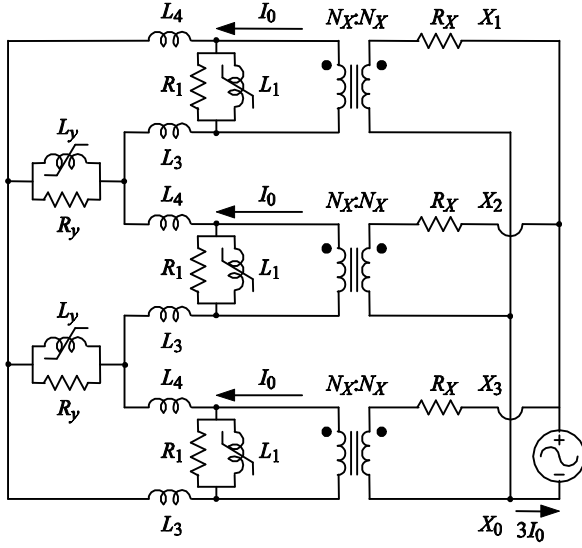


Fig. 3. Circuit configuration for the zero-sequence test.

B. Waveform-based Tests

In the case when the waveforms have been recorded during the tests, the configuration was similar to that of Figs. 1–3; however, a zero-crossing method first proposed in [12] and further developed in [13] has been used for parameter determination based on the waveform data.

For obtaining the parameters of the core, the method is based on v - i and λ - i relationships at the instant when a zero crossing of either voltage or flux linked occurs. For the case of the short-circuit parameters, the voltage and current waveforms are sinusoidal, and the waveforms can be directly used to obtain the short-circuit impedances.

1) Short Circuit Tests

For this test configuration the equivalent circuit can be reduced to a resistance R_{SC} in series with an inductance L_{SC} as shown in Fig. 4.

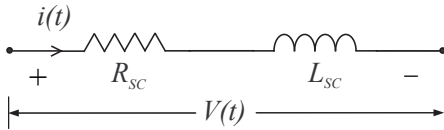


Fig. 4. Equivalent circuit for the short-circuit test.

Given the voltage and current waveforms as shown in Fig. 5 the values of R_{SC} and L_{SC} can be calculated as shown below. The voltage $v(t)$ across the winding can be expressed as

$$v(t) = R_{SC}i(t) + L_{SC} \frac{di(t)}{dt} \quad (1)$$

Hence:

$$R_{SC} = \frac{v(t)}{i(t)} \bigg|_{\frac{di}{dt}=0} \quad (2)$$

$$L_{SC} = \frac{v(t)}{di(t)/dt} \bigg|_{i(t)=0} \quad (3)$$

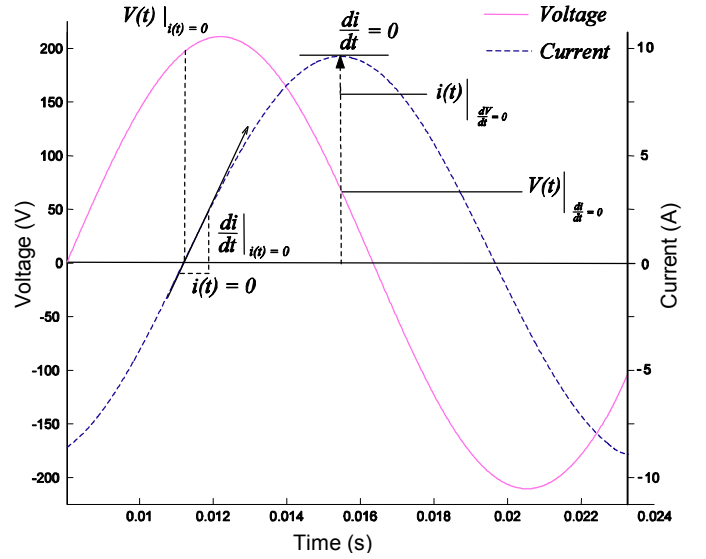


Fig. 5. Voltage and current waveforms for the short-circuit test.

The resistance R_{SC} can be estimated from equation (2) by measuring the voltage and current at the instant at which the slope of the current waveform is zero while the inductance L_{SC} is estimated from (3) at the instant at which the current waveform passes through zero by determining the voltage and slope of the current at that instant.

2) Open Circuit Tests

The open circuit test for the center leg was performed by shorting X_3 - X_0 and exciting X_1 - X_0 leaving the high voltage side open.

For this test configuration the equivalent circuit can be reduced to a resistance R_1 in parallel with an inductance L_1 as shown in Fig. 6.

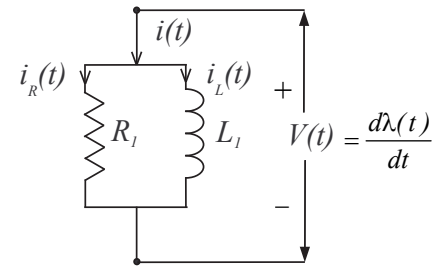


Fig. 6. Equivalent circuit for the open-circuit test: center leg.

This zero-crossing method from [4], [13] is based on the simple realization that when $v(t) = 0$ then $i_R(t) = 0$ and $i(t) = i_L(t)$. This happens at two instants each cycle, when the flux-linked λ is at its peak. Further, when $\lambda(t) = 0$, $i_L(t) = 0$ and $i(t) = i_R(t)$. This happens when the voltage is at its peak. This is depicted in Fig. 7 and (4) – (6).

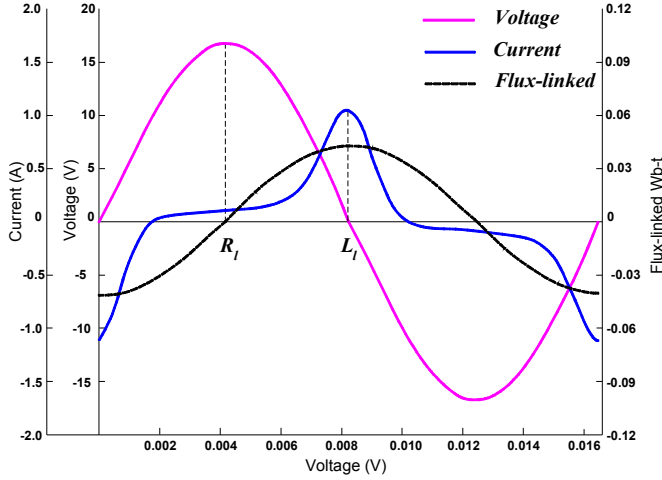


Fig. 7. Voltage, current and flux-linked waveforms for the first zero-crossing method.

$$\lambda(t) = \int v(t) dt \quad (4)$$

$$R_l = \frac{v(t)}{i(t)} \Big|_{\lambda(t)=0} \quad (5)$$

$$L_l = \frac{\lambda(t)}{i(t)} \Big|_{v(t)=0} \quad (6)$$

As documented in [4], a family of excitation measurements will yield the λ - i normal magnetization curve. Note that L_l values are taken at the instant of peak flux and R_l at peak voltage.

The flux linkage $\lambda(t)$ was then obtained by performing a trapezoidal integration of the sampled voltage waveform. The magnetizing current $i_L(t)$ is obtained by subtracting the current through the resistor from the total current, point by point. The peaks of the $\lambda(t)$ and $i_L(t)$ waveforms thus define the normal magnetization curves of the various core sections.

Any appreciable component of capacitive current must first be removed from the exciting current $i(t)$ before proceeding. Capacitive effects are negligible for small dry-type transformers, but increase with the size and voltage level of the transformer.

The open-circuit test for the outer leg was performed by shorting X_2 - X_0 and exciting X_1 - X_0 leaving the high-voltage side open (Fig.2). The procedure of obtaining the parameters for parallel combinations of the resistances and inductances for the leg and the yoke is similar to the center leg case. Then, the calculation of the individual inductances is performed, which is the same strategy used in the RMS-based method.

III. MODEL BENCHMARKING

Model performance is evaluated by comparing ATP-EMTP simulation results with the waveforms recorded during the tests.

Model benchmarking has provided some additional insights on how to estimate the parameters and demonstrated general correctness of the hybrid model behavior for the proposed

application. As already mentioned, a couple of different parameter estimation techniques have been used. The first method [9] based on the calculation involving the RMS measurements, while the other one [10] relied on the advanced waveform post-processing applied to obtain the parameters and investigate the frequency-dependency of the different pieces of the model. Large quantities of test data obtained in [10] were also used to benchmark the model in the 30-1200 Hz frequency range, the upper limit imposed by the equipment limitations.

The total number of cases run for the purposes of benchmarking was 34 for the three-legged transformer and 26 for the five-legged transformer.

A. Steady-state tests

1) Short-circuit test

The details of the circuit configuration for the first series of single-phase tests are provided in [9], [10]. Frequency has been varied from 30 Hz to 1200 Hz, and then the frequency-dependent resistances represented as Foster equivalent circuit with two cells were obtained based on the test results for both high- and low-voltage windings. The [A]-matrix has also been calculated by applying the developed technique to the results of 60 Hz short-circuit tests. Parameter estimation methods are outlined in Part I and [5].

The results of the simulation as compared to the lab measurements for the short-circuit test of the 3- and 5-legged transformers are shown in Tables I and II. This shows the frequency-dependent behavior of the winding resistances. Higher frequencies are not reported for the 5-legged transformer case, since the lab source could not provide enough voltage, and the measured RMS currents were less than 1 A. The errors reported in the tables are due to the fact that the Foster equivalent circuit, used in the model to represent frequency-dependency, cannot be fitted precisely throughout the whole frequency range.

2) Open-circuit test

The open-circuit tests for parameter estimation were performed as described in [9], [10] for both the center and outer legs of the transformers. From these tests, the values of non-linear inductances for legs and yokes have been calculated.

As an example, the results of the simulation as compared to lab measurements for the outer leg of the 3-legged transformer are demonstrated in Fig. 8.

As expected, the saturation curves for both transformers

3) Harmonics test

For the harmonics tests, 2nd, 4th, 5th, 7th and 9th harmonics were consecutively superimposed over the rated voltage at 60 Hz, and applied to all the phases uniformly. For both transformers, the voltage was applied to the 120 V, Y-connected sides.

The results of the simulation for the 3% 3rd harmonic injection are demonstrated for the 5-legged transformer in Fig. 9.

TABLE I
SHORT-CIRCUIT TEST RESULTS. 3-LEGGED TRANSFORMER

Frequency, Hz	Applied Voltage, V RMS	Current in the high voltage winding, A RMS		Error, %
		Lab	Simulation	
30	4.0	11.93	11.88	0.42
60	3.4	11.52	11.59	0.61
120	3.8	11.79	12.14	2.97
300	5.3	11.92	11.99	0.59
600	10.0	11.97	12.05	0.67
1200	15.8	12.30	12.53	1.87

TABLE II
SHORT-CIRCUIT TEST RESULTS. 5-LEGGED TRANSFORMER

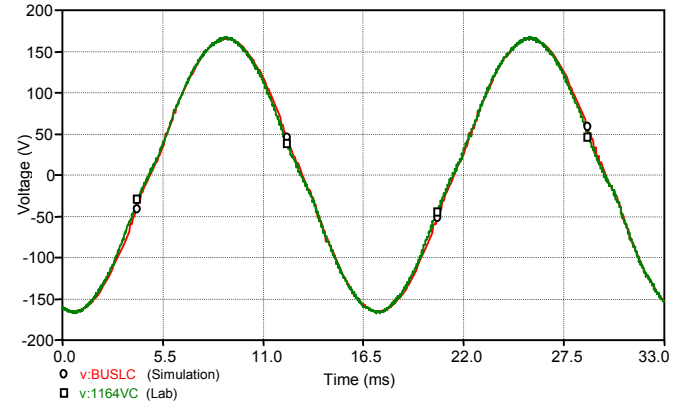
Frequency, Hz	Applied Voltage, V RMS	Current in the high voltage winding, A RMS		Error, %
		Lab	Simulation	
30	87.3	4.66	4.88	4.72
60	148.8	4.53	4.54	0.22
120	304.8	4.76	4.77	0.16
300	298.4	1.86	1.89	1.61

For both 3- and 5-legged transformers some transient is observed during the first cycle of the simulation. This is due to the fact that harmonics source in the model was initiated at $t=0$ s, and therefore only 60-Hz source was used for steady-state initialization in EMTF. It is also noted that lab sources were not exactly balanced three-phase sources, and this might have had a contribution to the current's mismatch.

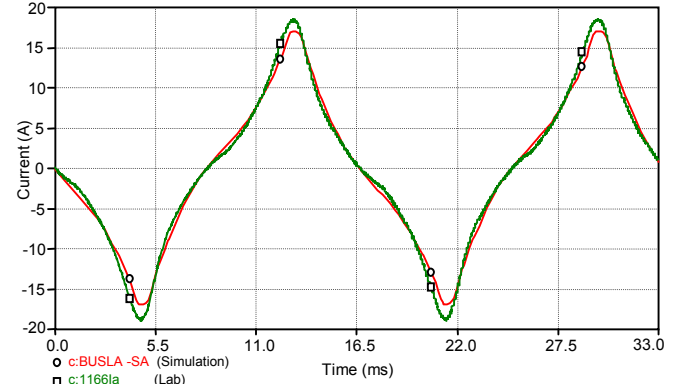
4) Phase-unbalance test

For phase unbalance tests, the transformers were excited at no load with unbalanced sources, and the voltage and current waveforms were recorded. All these tests were performed at nominal frequency of 60 Hz. Unbalanced excitation is a good way to benchmark the transformer model's core representation and saturation curves.

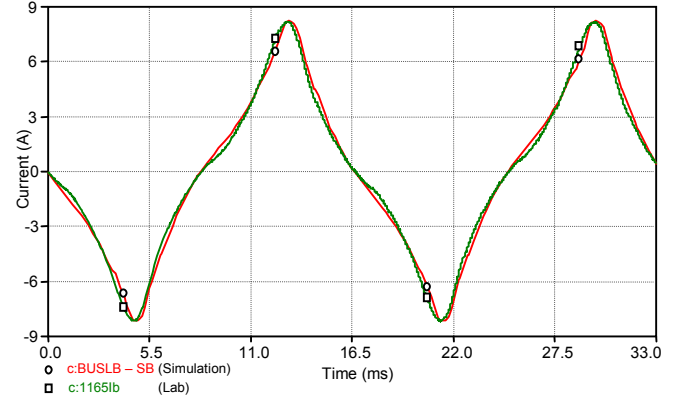
Several cases of two- and three-phase unbalance were benchmarked. The results of a two-phase unbalanced excitation test for the 3-legged transformer with an open delta are shown in Fig. 10.



a) Source voltages



b) Phase a line current

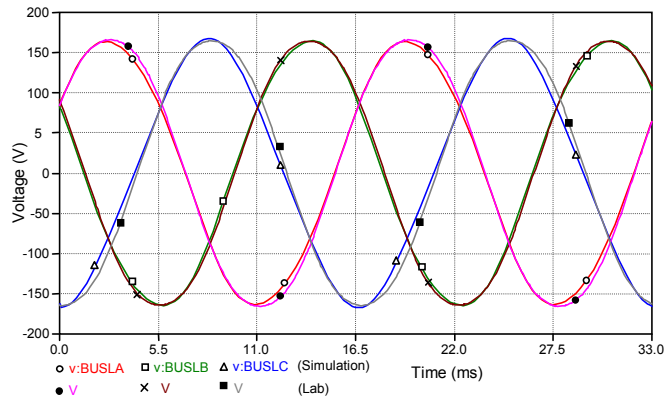


c) Phase b line current

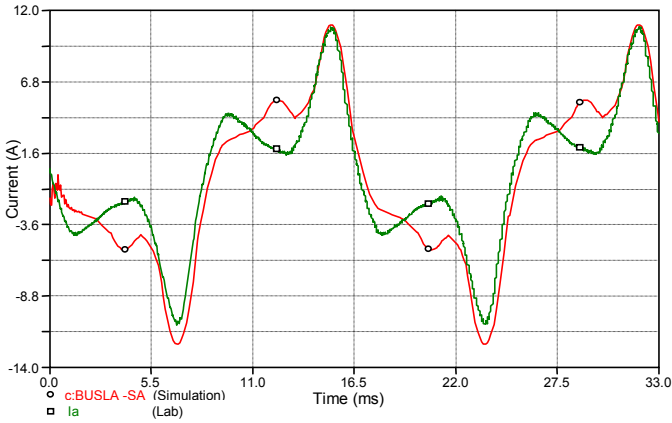
Fig. 8. 3-legged transformer. Excitation at 100% voltage. Outer leg.

B. Inrush

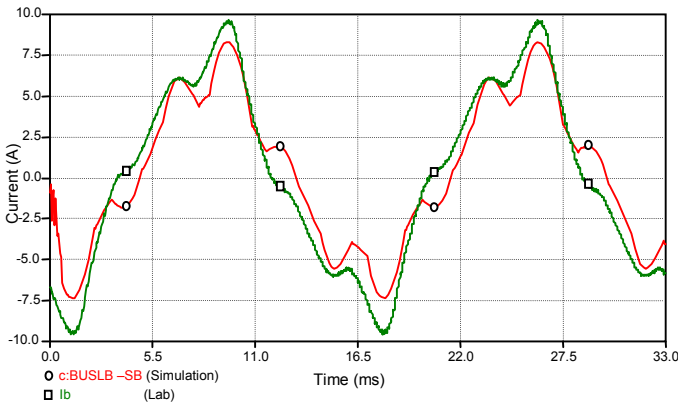
Benchmarking of model performance during inrush and de-energization has also been conducted. For the inrush case, 120 V at 60 Hz was applied to the low-voltage side of both the 3- and 5-legged transformers. Various configurations for single-phase inrush and de-energization were also used for testing [14].



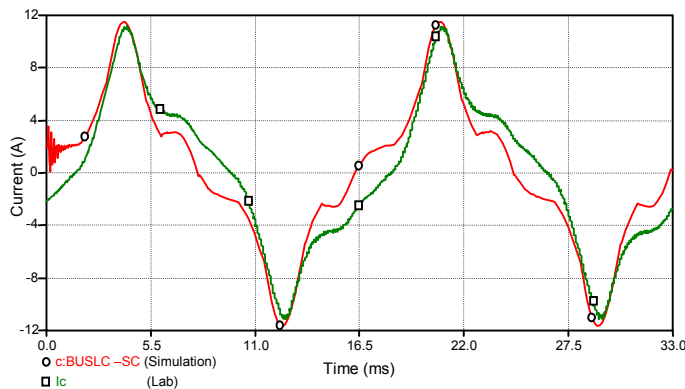
a) Source voltages



b) Phase a line currents

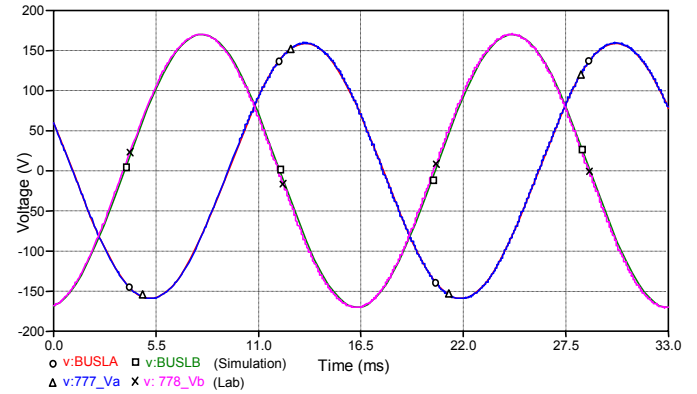


c) Phase b line current

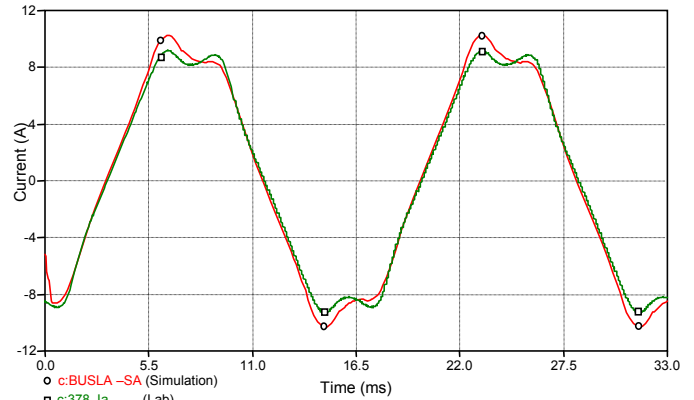


d) Phase c line current

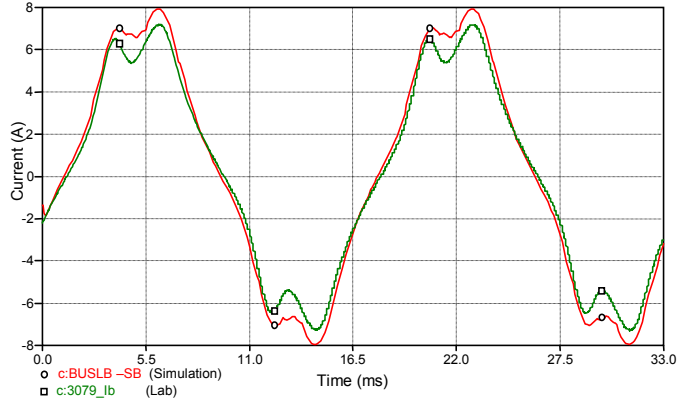
Fig. 9. 5-legged transformer. Harmonic injection (3% 3rd harmonic).



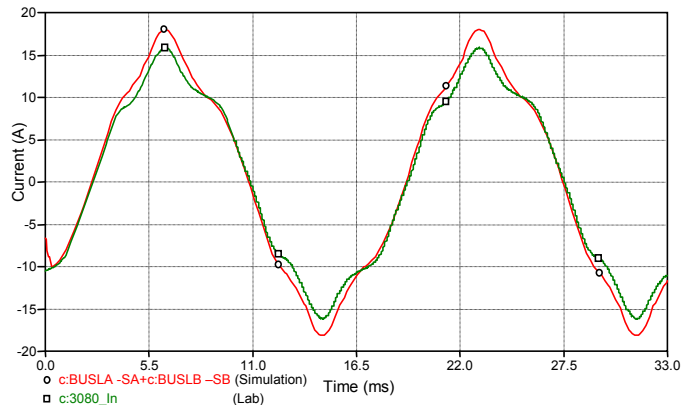
a) Source voltages



b) Phase a line current



c) Phase b line current



d) Neutral current

Fig. 10. 3-legged transformer. Two-phase unbalance. $V_a = 114 \angle 0^\circ V_{RMS}$ and $V_b = 120 \angle 120^\circ V_{RMS}$; phase c open.

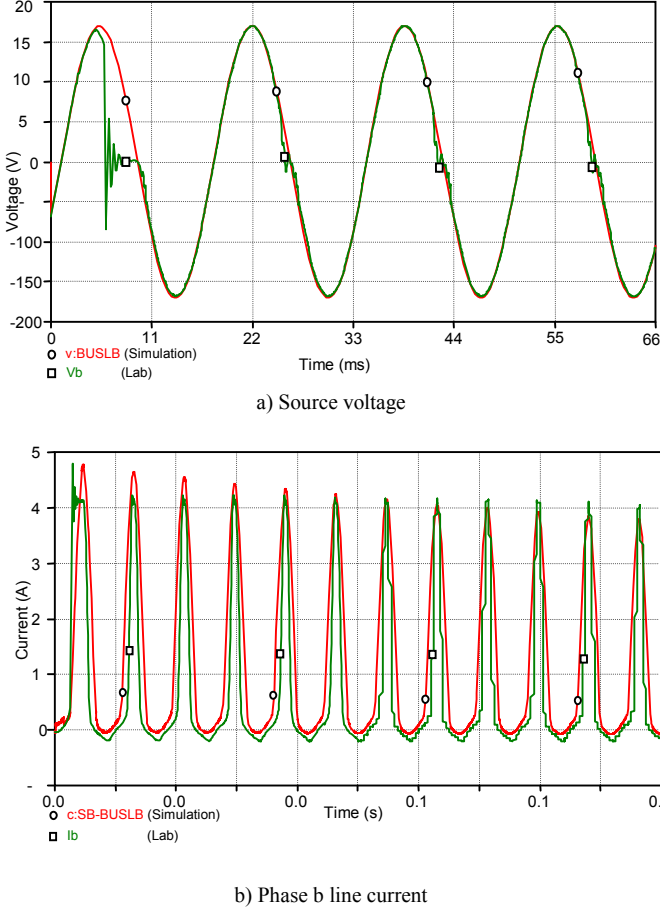


Fig. 11. 3-legged transformer. Single-phase inrush. Energize phase b with other phases disconnected.

The results of simulation vs. the lab measurements for inrush tests of the 3-legged transformer with closed delta winding are shown in Fig. 11. Note that the laboratory source impedance is relatively high, explaining the drop in voltage during the first half cycle of inrush. Even so, the simulated peak inrush current is very close to the laboratory measurement.

IV. CONCLUSIONS

A new hybrid transformer model for low- and mid-frequency transient simulations has been presented in Part I of this two-paper set.

A specific method of calculating model parameters from the lab measurements data complements the more general methods described in Part I.

Benchmarking results presented in the paper prove the general correctness of the model structure by comparison of the simulation results with the waveforms captured in the laboratory.

V. FUTURE WORK

A vital direction for future research is to verify the

suitability of parameter estimation techniques based on lab measurements for larger power transformers, including autotransformers. Some additional testing at transformer factories is required for this task with possible extension of the methods proposed here.

Additional papers, addressing each of the four parts of the model (core, leakage, coil resistance and capacitance) shall detail the parameter estimation methods and benchmarking of those aspects of the model.

VI. ACKNOWLEDGEMENT

The modeling project is led by Michigan Technological University (MTU) in collaboration with North Dakota State University (NDSU). NDSU laboratory measurements have been useful for model development, benchmarking, and parameter estimation.

VII. REFERENCES

- [1] C.M. Arturi, "Transient simulation and analysis of a five-limb step-up transformer following an out-of-phase synchronization," *IEEE Transactions on Power Delivery*, Vol. 6, No. 1, pp. 196-207, January 1991.
- [2] T. Henriksen, "How to avoid unstable time domain responses caused by transformers models," *IEEE Transactions on Power Delivery*, Vol. 17, No. 2, pp. 516-522, April 2002.
- [3] P. Holenarsipur, N. Mohan, V.D. Albertson, and J. Christofersen, "Avoiding the use of negative inductances and resistances in modeling three-winding transformers for computer simulations," in *Proc. 1999 IEEE Power Engineering Society Winter Meeting*, Vol. 2, pp. 1025-1030, January 31 - February 4, 1999.
- [4] B.A. Mork, "Five-legged wound core transformer model: Derivation, parameters, implementation, and evaluation," *IEEE Transactions on Power Delivery*, Vol. 14, No. 4, pp. 1519-1526, October 1999.
- [5] B.A. Mork, F. Gonzalez-Molina, and D. Ishchenko, "Parameter estimation and advancements in transformer models for EMTP simulations. Task/Activity MTU-6: Parameter estimation," report submitted to Bonneville Power Administration, Portland, USA, December 23, 2003.
- [6] D.L. Stuehm, "Three-phase transformer core modeling. Final report," Bonneville Power Administration, Award No. DE-BI79-92BP26700, Portland, USA, February 28, 1993.
- [7] H.W. Dommel with S. Bhattacharya, V. Brandwajn, H.K. Lauw and L. Marti, *Electromagnetic Transients Program Reference Manual (EMTP Theory Book)*, Bonneville Power Administration, Portland, USA, 1992 - 2nd Edition.
- [8] K.U. Leuven EMTP Center, *Alternative Transients Program Rule Book*, Leuven EMTP Center, Heverlee, Belgium, July 1987.
- [9] J. Mitra, "Advanced transformer modeling for transient simulation. Task NDSU-1: Laboratory testing of three-leg and five-leg distribution transformers," Bonneville Power Administration, Portland, USA, December 20, 2002.
- [10] J. Mitra, R.D. Mascarenhas, and M.A. Jayachandran, "Advanced Transformer Modeling for Transients Simulation. Task/Activity NDSU-3 Frequency Dependence of Parameters of 3-leg and 5-leg Distribution Transformers," Bonneville Power Administration, Portland, USA, September 23, 2003.
- [11] D.L. Stuehm, B.A. Mork, and D.D. Mairs, "Five-legged core transformer equivalent circuit," *IEEE Transactions on Power Delivery*, Vol. 4, No. 3, pp. 1786-1793, July 1989.
- [12] L. O. Chua and K. A. Stromsmoe, "Lumped circuit models for nonlinear inductors exhibiting hysteresis loops," *IEEE Trans. Circuit Theory*, vol. CT-17, pp. 564-574, Nov.1970.
- [13] B. A. Mork, "Ferroresonance and chaos," Ph.D. dissertation, Dept. Elect. Eng., North Dakota State Univ., Fargo, 1992.
- [14] J. Mitra, R.D. Mascarenhas, and M.A. Jayachandran, "Advanced Transformer Modeling for Transients Simulation. Task/Activity NDSU-4, 5 and 6 Additional Laboratory Testing of 3-leg and 5-leg Distribution

Transformers for Parameter Refinements,” Bonneville Power Administration, Portland, USA, September 26, 2003.

VIII. BIOGRAPHIES

Bruce A. Mork (M'82) was born in Bismarck, ND, on June 4, 1957. He received the BSME, MSEE, and Ph.D. (Electrical Engineering) from North Dakota State University in 1979, 1981 and 1992 respectively.

From 1982 through 1986 he worked as design engineer for Burns and McDonnell Engineering in Kansas City, MO, in the areas of substation design, protective relaying, and communications. He has spent 3 years in Norway: 1989-90 as research engineer for the Norwegian State Power Board in Oslo; 1990-91 as visiting researcher at the Norwegian Institute of Technology in Trondheim; 2001-02 as visiting Senior Scientist at SINTEF Energy Research, Trondheim. He joined the faculty of Michigan Technological University in 1992, where he is now Associate Professor of Electrical Engineering, and Director of the Power & Energy Research Center.

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In October 2002, Dr. Gonzalez was awarded a Postdoctoral Fellowship from the Spanish Government and spent two years working as Postdoc at Michigan Technological University. In 2005 he joined El Sewedy Cables, Cairo, Egypt. His research interests include transient analysis of power systems, lightning performance of transmission and distribution lines, FACTS, power quality, and renewable energy.

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Don L. Stuehm (M'62) was born in Anamosa, Iowa on Feb. 5, 1941. He received the B.S. degree from Northrop University, Inglewood, CA, the M.S. degree from North Dakota State University, Fargo, and the Ph.D. degree from Colorado State University, Ft. Collins, in 1962, 1967, and 1972 respectively.

From 1962 to 1965 he was with McDonnell-Douglas Corporation, St. Louis, working on spacecraft systems. From 1965 to 1966 he was with Honeywell, Minneapolis, working on control systems. From 1973 to 1976 he taught at South Dakota School of Mines and Technology, Rapid City. In 1976 he joined the faculty of North Dakota State University, where he taught and did research in power systems. He retired in 2002 at the rank of Professor, and is now Professor Emeritus.

Dr. Stuehm is a member of the IEEE Power Engineering Society, Eta Kappa Nu, Sigma Xi and Phi Kappa Phi.

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