
Saturation Effects in 3p3L Power Transformers due to Geomagnetically Induced Currents



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October 2023

*A Dissertation in fulfilment of the requirements for a Master of Science in Engineering
Submitted to the Faculty of Engineering and the Built Environment*

Department of Electrical Engineering

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ABSTRACT

Geomagnetically induced currents (GIC's) caused by solar storms or other sources of dc excitation can impact the normal operation of power transformers in transmission networks. The impact is caused by the dc current introduced into the windings of a transformer energised with ac, which leads to a simultaneous dc magnetomotive force (mmf) resulting in part-cycle saturation of the core and so, heat related ageing and failures.

A generally accepted theory on the impact of GIC's on transmission networks is that three-phase three-limb (3p3L) power transformers are not affected by this phenomenon. The basis is that a three-limb core forms balanced return paths which oppose the dc flux evenly in each limb, thereby neutralising the effect. The challenge is this theory only remains true if the circuit resistance of the three-phase lines connected to the transformer are balanced, which has proven difficult to achieve in practice.

This study set up laboratory experiments to evaluate the effect of simultaneous ac and dc in the windings of an unloaded 3p3L transformer while simulating realistic unbalanced conditions. To ensure a valid test protocol, the transformer was designed as a scaled-down model of a power transformer with a core constructed using four step-lap mitred joints and a high quality electrical steel. This was to closely resemble the performance of a normal power transformer. The source supply for the test circuit was carefully designed and tested to ensure that it was sufficiently robust that it would not affect the results of the transformer under test (TuT).

The initial testing was of symmetrical ac conditions to establish a performance benchmark at normal operation and the TuT nameplate rating, and to emulate core saturated conditions by over-exciting the core for exploratory core stray flux recordings. The dc testing that followed required adjustable levels of dc current and the unbalance to emulate a range of GIC conditions. At each setting of dc and unbalance, the excitation parameters such as THD were recorded using a power analyser. Simultaneously, the core stray flux was recorded using search coils, while tank 'hot-spot' temperatures from stray flux eddy current heating were recorded. Finally, the audio levels of the 3p3L transformer in balanced and unbalanced GIC conditions were analysed.

The results of the tests challenge the assumption that 3p3L power transformers are immune to GIC primarily because it is not possible to ensure ideally balanced conditions in the field. The importance of the transformer core design was highlighted by showing its significant contribution to the GIC vulnerability. The core joints of a four step-lap core was shown to saturate before the bulk core areas that emphasised the need for continual careful attention to core design and construction in an effort to reduce inherent design vulnerabilities.

ACKNOWLEDGEMENTS

I would like to thank my supervisor Professor Gaunt for his support, guidance and assistance, making this project possible. The coronavirus pandemic brought challenges that included changes in plans and, even so, his many years of experience brought with it the foresight that turned the challenges I had in my research plan into viable solutions.

I would also like to thank Professor Folly for his role as co-supervisor, without this invaluable support the research project could not have started, and his questions and guidance at the weekly Power Seminar reminded me of the importance of continuous improvement.

I would like to thank Dr. Les Borrill for his valuable, and insightful guidance and friendship that helped overcome obstacles and challenges on the way.

I would like to thank Dr. Hilary Chisepo for his invaluable assistance and guidance with the documenting of my research and his practical assistance with my tests. He regularly reminded me of what to expect as seen from his software simulations of the hardware tests.

Finally, I would also like to thank my fellow students Ngcebo Sithebe and Melissa-Jade Williams that selflessly shared their time, thoughts, space and equipment.

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NOMENCLATURE

<i>CME</i>	Coronal Mass Ejection
<i>CRGO</i>	Cold-Rolled Grain Orientated (core steel)
<i>FEM</i>	Finite Element Method
<i>FFT</i>	Fast Fourier Transform
<i>GIC</i>	Geomagnetically Induced Currents
<i>GMD</i>	Geomagnetic Disturbance
<i>GOES</i>	Grain Oriented Electrical Steel
<i>HV</i>	High Voltage
<i>IEEE</i>	Institute of Electrical and Electronics Engineers
<i>I_{mag}</i>	Magnetizing current
<i>Idc</i>	dc Current
<i>LV</i>	Low Voltage
<i>MM</i>	Multimeter
<i>MMF</i>	Magnetomotive Force
<i>MSL</i>	Multi step-lap
<i>MV</i>	Medium Voltage
<i>NLL</i>	No Load Losses
<i>NSL</i>	Non-Step-lap
<i>PA</i>	Power Analyser
<i>PVC</i>	Polyvinyl Chloride
<i>SSL</i>	Single Step-Lap
<i>SC</i>	Air Search Coil
<i>THD</i>	Total Harmonic Distortion
<i>TRFR</i>	Transformer
<i>TuT</i>	Transformer under Test
<i>V_{knee}</i>	Knee point voltage
<i>WT1800</i>	Yokogawa WT1800 Power Analyser (6 Element)
<i>WT500</i>	Yokogawa WT500 Power Analyser (3 Element)
<i>3p3L</i>	A three phase three limb transformer
<i>3p5L</i>	A three phase five limb transformer

1. INTRODUCTION

Power transformers are fundamental components in an electrical transmission and distribution network with significant impact on the efficient functioning of the network, and generally carry the highest single-component cost in the system. As such, there is continual effort by transformer designers to maintain adequate performance while decreasing their material and manufacturing cost. In some cases, performance is actually improved by development in material and manufacturing processes while reducing cost, and in other cases, research to improve the understanding of transformer stressors which led to failure, and specific protection against those stressors, achieves the objective.

The transformer core, as one of the critical parts of the transformer with significant performance impact, is often the subject of these improvement initiatives. To this end, most modern power transformers use grain oriented electrical steel (GOES) for their cores. The use of laminated cores in both shell and core-type designs results in step-lap or butt-joints that have been the focus of improvements due to their potential to affect core performance.

This study evaluates a typical three-phase three-limb (3p3L) step-lap transformer core to the effects of saturation at the joint and bulk-core material, with ac and dc over-excitation similar to that experienced from geomagnetically induced currents (GIC).

1.1 Background

Solar activity gives rise to abnormal space weather conditions which, through a complex interaction of events, lead to geomagnetic disturbances (GMDs). This then causes GIC to flow through the earth which pose a risk to manmade structures such as transmission networks. These are particularly vulnerable as they extend over very long distances in many cases, and because they contain sensitive electrical devices which could interact directly with the GIC, such as power transformers.

GIC are quasi-dc in nature [1]-[3], and so they create a dc magneto-force inside transformers which can drive their magnetic cores into saturation.

Abnormal space weather has led to multiple events with power transformer failures [4],[5]. It is therefore accepted that GIC will remain a threat to power systems due to the potentially high dc currents that flow through the high voltage windings of power transformers. As such, mitigation of this risk can only be fully exercised once the impact of this phenomena on all types of power transformers is fully understood. It is discussed in an early report in the southern African context [6], that three-limb core-type transformers should have no sensitivity to GIC since they offer no return path for the quasi-dc initiated flux. This is because the flux is shared equally in the three phases which has a cancelling effect by inducing equal and opposing dc flux. Measurements have, however, contradicted the theory because the assumption is that most transmission systems are sufficiently balanced. The question then

remains as to how much unbalance in a 3p3L transformer and power system is sufficient to result in an impact on 3p3L power transformer making it vulnerable to the effects of GIC.

This study addresses this question by reviewing established thinking in literature, and through experimentation assesses the effect of simultaneous ac and dc in the windings of a transformer. The core configuration of the test transformer resembles a real 3p3L power transformer with mitred four step-lap core joints, and is fabricated using the same type of high-quality grain-oriented electrical core steel (GOES). Power transformers generally use 4 or 6 step-lap designs in favour of the lower cost butt-joint designs, so this study will use a model power transformer with a four step-lap core instead of butt-joints as used in many past model transformer research initiatives [1],[7]. In the study by Chisepo [7] extensive use was made of Finite Element Method modelling of transformers of different core designs using an iterative approach to verifying and validating the results of the FEM. This methodology involved comparing the results of real practical tests to the FEM results and, in the case where there were differences, the differences were analysed and the model was ‘calibrated’ accordingly. This effectively improved the validity of the FEM for future studies of similar transformer designs. The scaling between large power transformers and bench-scale transformers were also further understood in this study.

The Finite Element Method similar to Chisepo’s study [7] could be used in future research to verify and validate the results of this study of the 3p3L transformers using a scale model laboratory transformer.

The response of the model power transformer core is observed first with symmetrical ac over-excitation, and then under GIC-like dc conditions. This project develops appropriate test circuits, test protocols and electrical and magnetic parameter measurements for the examination of the transformer response during varying degrees of core saturation.

It is anticipated that controlled experimentation with a physical model representative of actual 3p3L power transformers will improve the understanding of the response of a 3p3L transformer in the presence of GIC, which could assist to improve the modelling and simulation of transformers exposed to the GIC phenomenon.

1.2 Proposal

Based on the aspects outlined above, the hypothesis of this study is as follows:

Phase unbalances of less than a typical practical unbalance on power system networks could increase the effect of GIC initiated dc magnetisation in 3p3L transformers with multi step-lap cores, resulting in saturation above normal levels and elevated levels of stray flux, with increases in heating, harmonic distortion, the draw of conventional reactive power, and noise.

In summary, symmetrical ac testing i.e., experiments using only applied ac voltage at power frequency, is required initially to document the symmetrical core saturation conditions of a 3p3L multi step-lap transformer core. After this, testing with a dc-bias on the windings will be performed to document and analyse the impacts paying special attention to the degree of unbalance where core saturation is first observed.

1.3 Research Questions

The following research questions will be used to test the validity of the hypothesis:

1. What is stray flux and how is it measured?
2. What does saturation depend on and how does it develop?
3. What is the normal operational level of flux beyond which saturation is excessive?
4. How does GIC affect unbalance and how does it affect the magnetisation of a 3p3L transformer?
5. What is the significance of the core-joint design?
6. How can core saturation be measured?
7. What are the negative effects on transformers and the power system due to unbalance in the presence of GIC?

1.4 Thesis Outline

Chapter 1 introduces the topic under investigation, and presents the motivation for the research, the hypothesis, and the research questions to test the hypothesis.

Chapter 2 presents an analysis of the literature relevant to the research, identifying the findings from the current knowledge-base in the context of the research questions, and identifying the knowledge gaps for specific study.

Chapter 3 presents the theoretical basis of this research including descriptions of the testing equipment, and the test transformer.

Chapter 4 presents the required testing methodology and laboratory testing protocols.

Chapter 5 describes the tests including the limitations, test results and discussion in the context of the literature review performed in Chapter 2.

Chapter 6 is the assessment of the study results in the context of the research questions, discussion on the validity of the hypothesis, and implication of the work.

2. LITERATURE REVIEW

2.1 Background

Approximately every 11 years, solar activity gives rise to abnormal space weather conditions which lead to geomagnetic disturbance (GMD) effects on earth. The solar activity consists of solar flares and in some instances, very high energy coronal mass ejections (CME) which normally take a few days to reach earth. These CMEs interact with the Earth's magnetosphere where periodic changes in the resultant geomagnetic field and ionospheric currents lead to changes in the electric field intensity penetrating the Earth's surface. According to Faradays law of electromagnetic induction, GICs flow through the ground and enter power networks through grounded transformer neutrals, and in pipelines.

When two power transformers are widely separated by high voltage (HV) overhead transmission lines and a geomagnetic disturbance (GMD) occurs, a GIC is induced and traverses the 'true earth' [12]. This GIC then flows into the neutral of a transformer, along the transmission line, and then into a second transformer, and out through its neutral. The frequency of this GIC is in the order of millihertz (mHz), but typically less than 1 Hz, whereas the nominal operating frequency of power systems in south Africa is 50 Hz, and 60 Hz in the U.S.A. and Canada. GICs are therefore termed as quasi-dc currents in power engineering as they induce the same effects as those of dc excitation in transformers. Dc excitation causes interference with the co-ordination of the transformers magnetic circuit, driving it into the half cycle saturation of its core, bringing about instantaneous and cumulative damaging effects Heindl, et al. [67] and Slothower and Albertson [8].

As discussed by Kirkham *et al.* [9], Kappenman [10], and Radasky and Kappenman [11], CMEs are regarded as a common space weather phenomenon and so the understanding and mitigation of any negative effects is essential. To this end, the effects of CMEs on power systems have been closely observed more than a dozen times between 1980 and 2020. Large-scale electrical system failures happen less frequently, but some have caused collapse of large parts of electrical networks due to failure of equipment and system protection initiation as discussed by Gaunt & Coetzee [12] and Jayasinghe [13]. Severe CME events occurred in 1859 affecting telegraph systems, then in 1921, and in 1989 when U.S. and Canadian utilities lost power transmission capability over wide areas as discussed by Tessier and Simard [14].

Long transmission lines are particularly vulnerable to this effect as discussed by Boteler and Pirjola [15], where the cross-sectional area of the single-turn loop represented by the power line and a ground return is large, resulting in some measured currents well in excess of 100 A. High-latitude regions have generally been considered to be most susceptible to GIC but, as discussed by Gaunt and Coetzee [12], research in mid-latitude South Africa shows a strong correlation between transformer failures and GIC recorded in the region.

Girgis and Vedante [5], give two examples of typical GIC current signatures. One is estimated based on the var consumption at the Salem Generating station during the event recorded there in 1989, and the other is the current measurement obtained at a Canadian generating station in 2011.

Figure 2.1 shows a typical GIC waveform that have some high peaks of short time duration. The GIC signature shows several consecutive 20 A peaks over a period of 90 minutes and two high peaks of 80 A & 100 A with a duration of 2 minutes each. Another event recorded in 2011 at the Salem generating station was of a very low magnitude that showed several short duration low amplitude peaks of less than 5 A over a 5-minute period and one 8 A peak for less than a 1-minute period.

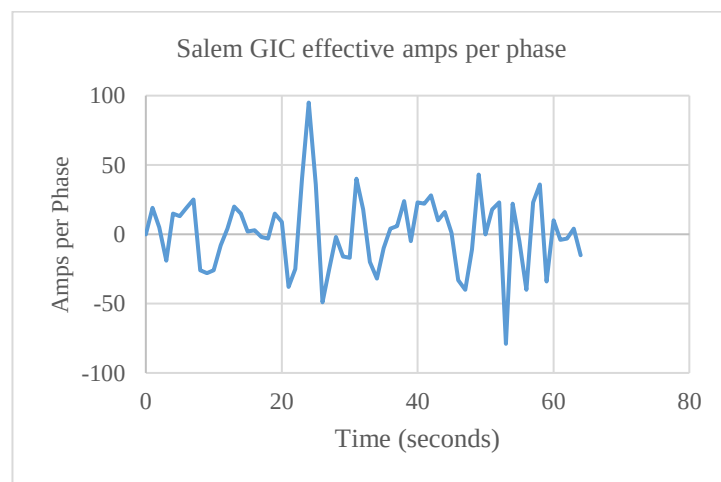


*Figure 2.1 – Estimated GIC current at Salem generating station 1989
(adapted from [5])*

Their conclusion from this study was that the majority of failures linked to GIC is not the GIC alone but a combination of factors linked to the specific transformer design, age, and condition. These conclusions, however, are not consistently substantiated with evidence obtained from in-depth research or testing of the cases which were referenced.

The failures of transformers due to GIC are either catastrophic failure without warning, or delayed failures due to repeated exposure to GIC and other grid operating transients and fault stresses which cause gassing and other symptoms as discussed by Gaunt and Coetzee [12]. The effects of GIC on power systems include changes in system var requirements, increases in harmonic current magnitudes, increased transformer stray and eddy losses, and problems with system voltage control. Repeated exposures to GIC lead to cumulative damage which can ultimately lead to transformer failure, and units at base load power stations are more vulnerable because they are relatively heavily loaded and have less margin for heat stress. It is emphasised in [12] that various stresses are related to the final failure, but it is stated that it

is useful to consider the processes by which GIC could weaken the condition of a transformer and the symptoms which would indicate the contribution of GIC to failure.

2.2 Stray flux on a 3p3L dry-type transformer

Research Question 1: What is stray flux and how is it measured?

Stray flux

Kulkarni [16] states that as ratings of transformers increase, the proportion of stray losses in their load loss may increase significantly. These losses in structural components may exceed the stray losses in windings in large power transformers with a major portion of these stray losses occurring in tank plates and core clamping elements. It is also discussed that an overexcitation condition leads to an over-fluxing condition in a transformer causing additional losses due to core saturation. This resulting spill-over flux from the core can cause intense local losses in conducting parts leading to hot spots. The stray flux causes eddy current by 'cutting' through conductors of windings and metallic structural parts where unlaminated structural parts can overheat rapidly, and the condition of adjacent insulation could deteriorate.

Alexander and Corbin [17] discuss that the flux in a transformer core, produced by the exciting current in a winding, is confined to the core because of its greater permeability compared to the surrounding volume. When flux density in the core increases beyond its saturation point, a substantial amount of flux will exit the core into the surrounding air and metallic structure causing hot-spot heating due to localised eddy currents. With overexcitation, the flux lines of the primary coil respond like an air-core inductor so the flux 'cuts' the conductor of the coils in other directions than at normal excitation levels, causing its heating. It is stated that this is similar to 'placing an air-core reactor inside the tank' of a power transformer. Appreciable flux flows in the air paths only during the portion of the cycle when the core is saturated so air flux is pulse-like in form, as is the exciting current. The pulse-like waveform has a high harmonic content which increases its heating effect and the exciting current of the same magnitude as full-load current can occur for more than 140 percent of the rated excitation level. For the same rms value, however, the heating effect of the exciting current is much greater than that of a sinusoidal load current because it is rich in harmonics.

Mulasalihovic' *et al.* [18] discuss that planar eddy currents (PECs) are generated by stray flux, mainly due to strong increases of excitation currents from geomagnetically induced currents on the magnetic performance of transformer cores.

Search coils

Search coils have proved useful in studies of transformer cores to determine their saturation behaviour and to compare their design strengths and weaknesses such as core joints, bulk core material response to saturation, and fringing flux determination. In [1], the search coils were used for exploratory analyses and then fixed in locations of interest to study the relative

response of the various core locations to determine the extent of stray flux and the core saturation response in the different parts.

Different angular orientations of the search coil were performed to further refine the flux readings to obtain the highest voltage possible at the SC terminals.

It is also discussed in [1] that by applying equation $E_s \propto \Phi_m$ the calculated voltage output of a search coil is proportional to the peak flux in the core at the search coil location. As a result, single turn search coils placed strategically around the core will assist with determining core symmetry, actual core flux distribution for both normal and saturated operation, and the core response to flux produced by both ac and dc components of current.

To determine the level of stray flux at the different parts of a transformer core under saturated conditions Borrill [1] made extensive use of search coils in conjunction with no-load v-i excitation graphs. This could then determine the induction level at which the knee point occurred and then the stray flux response of the core analysed at specific excitation points to compare the level of induction at which stray flux became evident at the various locations.

Preliminary tests were conducted at induction levels corresponding to linear and non-linear core operation with various shapes of air search coil with differing numbers of turns to find a suitable coil for detecting stray flux in the transformer air space. This was necessary as the stray flux magnitudes were unknown at that stage of the research. A 20-turn search coil was found to give readable output voltages in line with the induction levels used for the preliminary tests.

Jones, Moses, and Thompson [19] used search coils in a similar investigation of core stray flux to those discussed above. Air search coils were used by them as tools to determine the relative performance of mitred step-lap core joints in power transformers. In this study the sizes of the coils were chosen so that the flux densities could be measured over as small an area as possible while still retaining good accuracy. The leads to the coils were twisted so as to avoid any stray fields, and they were mounted firmly in position. It was also noted that it was important to know the direction of the flux relative to the search coils orientation in the overlap region to ensure that the search coil outputs were correctly interpreted.

Summary of key points in literature relevant to research:

- Significant stray flux is introduced into the air and surrounding transformer structural parts when the core is excited beyond the normal rated excitation level.
- Search coils serve as effective devices for core saturation analysis.
- The positioning of the search coils is important and their location can only be finalised after initial exploratory tests.
- Twisted leads on the search coils are important to reduce signal interface.
- Air search coil results analyses should be done in conjunction with I_{mag} and THD curves.

Points of uncertainty or disagreement

The previous test using search coils have been performed on open, transformer cores without tanks and with easy access to the core so exploratory tests would be required.

In the case of Borrill [1], the study test transformer core was constructed using butt-joints, and not multi step-lap joints as is the case in this study.

The number of turns applied to the search coils would need to be confirmed by exploratory tests to determine the positioning and required accessibility of a 3p3L core constructed using mitred step-lap joints.

2.3 Core saturation of a 3p3L power transformer

Research Question 2: What does saturation depend on and how does it develop?

The point where a transformer core enters a saturated condition is primarily determined by the core design, its construction method, and the material used, as described below.

Transformer losses

Transformer losses are primarily categorised as no-load losses and load-losses [20]. Load losses vary according to the load on the transformer and include heat and eddy current losses in the primary and secondary windings of the transformer. The dominant load-loss is the heat loss in the windings, or I^2R loss.

No-load losses are associated with magnetizing the core and are independent of the load on the transformer. The no-load stray losses, dielectric losses and I^2R losses are often ignored as they are negligible when compared to the hysteresis and eddy current losses.

No-load current

With the secondary winding open-circuit, only the no-load current (I_o) flows in the primary winding as shown in Figure 2.2, which consists of the magnetizing reactive component (I_u) and the active component I_w . As discussed by Rupala [21], the no-load current of small distribution transformers at their rated voltage is in the order of 3 to 5% of the full load current. It can be <1% for large power transformers with high-quality electrical core steel and staggered multi step-lap mitred core joints.

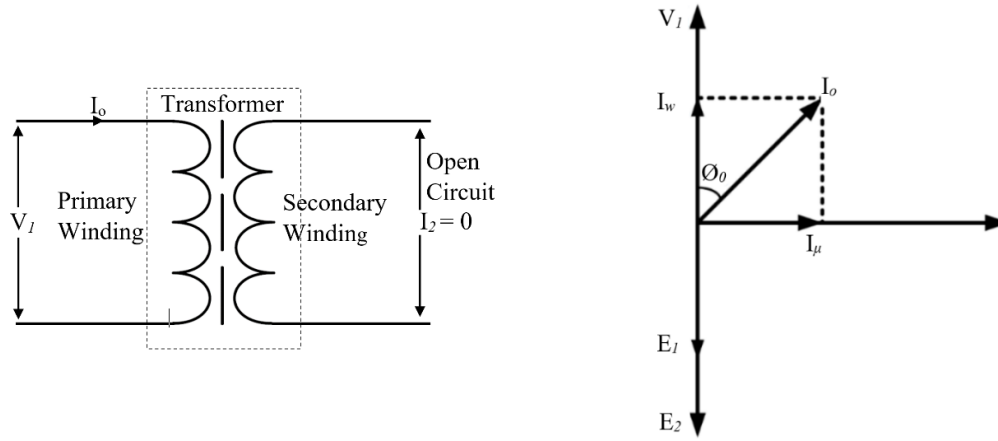


Figure 2.2 - Practical Transformer on No-Load Vector Diagram

No-load magnetisation currents are shown in Figure 2.3 plotted from tests on a three-limb transformer (3p3L) [15]. The transformer was supplied at rated voltage but there is a significant 3rd harmonic component, so it is suggested that even under rated conditions the transformer has started entering the non-linear saturation region. This observation agrees with other transformer studies [1] and [11], which would then lead to the de-rating of the transformer to ensure linear operation without dc.

In [22] the reluctance path differences were noted in the magnetising currents recorded during no-load tests of a 3p3L transformer that shows a lower magnetizing current amplitude on the centre phase when compared to the external phases (Figure 2.3). It is stated that the centre limb of the core structure requires less magnetizing current as indicated due to the lower reluctance path compared to the other phases.

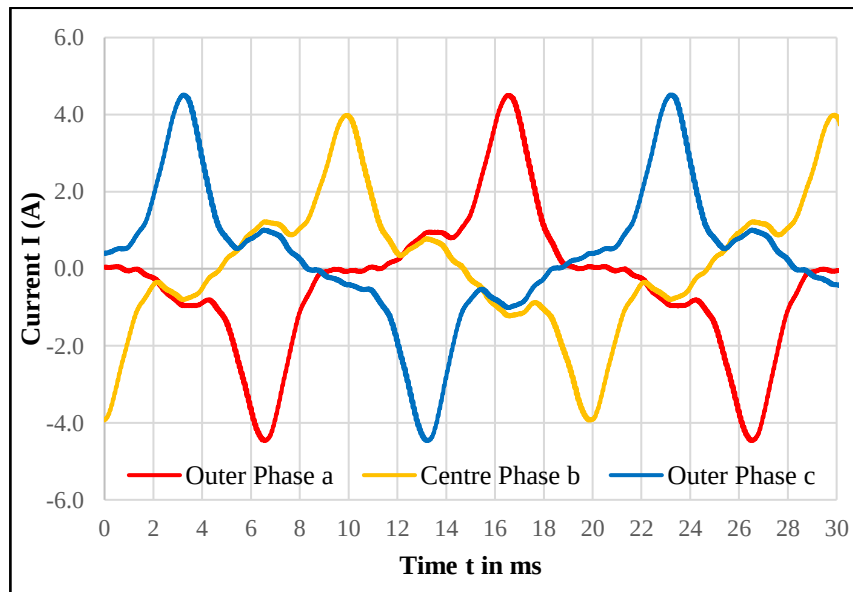


Figure 2.3 - No-load primary current of 3p3L transformer

In a study on transformer no-load current harmonics by Al-Haj and El-Amin [13] it was found that the harmonic components of the no-load current are influenced by flux density, the degree

of saturation and the core stacking technique. A slight over-excitation resulted in a substantial increase in the THD level and the relationship between the THD and flux density was not linear.

Core Material

As discussed by Kulkarni & Khaparde [20], the development of modern GOES for transformer core material started with non-oriented silicon steel, hot rolled grain-orientated silicon steel, cold rolled grain-orientated silicon steel (CRGO), and then improved CRGO steels including Hi-B and laser or mechanically scribed steels. It is stated that the saturation flux density has remained constant around 2.0 Tesla for the CRGO grades, however, sophisticated manufacturing technologies have resulted in better steel grades, and significant improvements in the watts/kg and volt-amperes/kg characteristics of the transformer nameplate ratings. In [7] it is stated that conventional grain oriented (CGO) electrical core steel has a nominal operating flux density of 1.72 T, and a safe operation upper limit of 1.95.

Mechler and Girgis showed that with a nominal operating flux density B of 1.7 T, the B can rise to 2.7 T for a conventional single step-lap (SSL).

T. Jazebi *et al.* [23] states that in grain oriented steels a transformer is driven to ‘deep’ saturation at levels higher than 2.0 to 2.2 Tesla.

Core magnetisation and symmetrical distortion

When a transformer is supplied by an ac voltage source, a magnetizing (excitation) current flows in the primary windings to produce the magnetisation (excitation) current for the magnetic flux in the core of the transformer, and to overcome the core-loss component.

The point that a transformer reaches saturation is determined by the core design, construction method, and the material used, which, as described by McLyman [27], all affect the permeability (μ), resistivity-core loss (p), remanence (B_r), coercivity (H_c), and saturation response (B_s).

When the saturation point is reached, the magnetic flux finds a transmission path by exiting the core into the surrounding material. The path is often through air (or oil in an oil-cooled power transformer), and into the frame, transformer tank, and other metallic parts, as discussed by Biro *et al.* [24], Zirka [25], and Price [26]. As the primary voltage increases and the knee-point voltage is exceeded, the saturation levels increase resulting in a significant increase of the temperature of the core and surrounding conductive material due to induced eddy currents [24]. Transformer core saturation ‘beyond the knee-point’ can be grouped into two categories, one when driven by a balanced ac source that results in symmetrical over excitation of the core, and the other when a transformer is subjected to an injected dc-bias superimposed on the ac which will result in non-symmetrical over excitation.

Figure 2.4 shows a typical B-H curve of a soft magnetic material and the relationship between the relative permeability and the flux density as it changes. McLyman [27], discussed that

when a high magnetizing force is encountered, a point is reached where further increase in H does not cause useful increase in B . This point is known as the saturation point, or knee-point of that material as illustrated in Figure 2.5 [27].

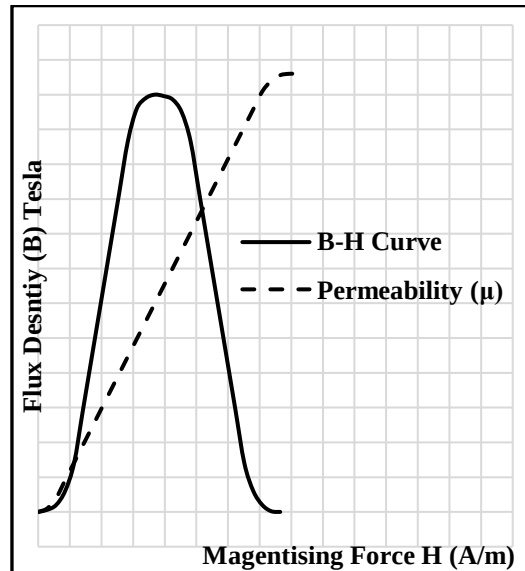


Figure 2.4 - BH Curve and Permeability of a Soft Magnetic Material (adapted from [27])

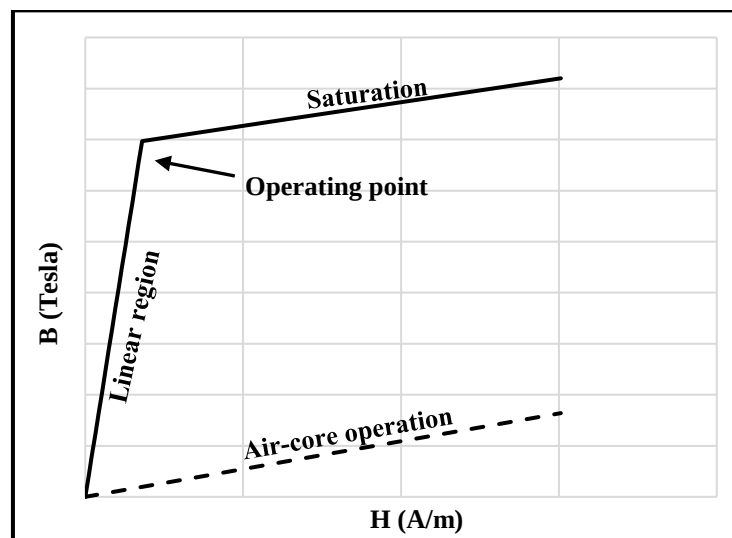


Figure 2.5 – Illustration of the ideal operating point and saturation on B-H curve (adapted from [27])

The permeability, μ , of this magnetic material is a measure of the ease in magnetizing the material and is defined by Equation (2.1).

$$\mu_o\mu_r = \frac{dB}{dH} \quad (2.1)$$

B : flux density in Tesla

H : magnetizing force in A/m

This relationship is not linear, as shown in Figure 2.4, and μ_r rises to a maximum and thereafter declines to minimum value of 1. The maximum μ coincides with the knee point of the B-H curve since it represents the slope of a line drawn between the origin and the knee point. This is significant since beyond this point as μ declines the reluctance of core components will change in such a way as to affect the distribution of the flux in the available core flux paths.

McLyman [27] states that a single-phase transformer has reached saturated conditions when the peak exciting current is twice the average exciting current, so when $I_{pk} \geq I_{ave}$ the core is in saturation. This is shown in Figure 2.6 and is a basic indicator to consistently determine the point of saturation of a transformer core during testing. In the case of the 3-phase transformer, Abdulsalam [28] evaluated a method of determining a 3p3L transformer's saturation characteristics by recording the inrush waveforms and analysing and comparing both the simulated and practical results. This method provides a characteristic curve but not the point of saturation i.e., knee-point.

There is a relative permeability of 1 as saturation is 'approached' (i.e., non-magnetic or air) as discussed by Del Vecchio *et al.* [29]. The flux would therefore 'spill' into all areas other than the core, such as the air spaces, oil space, frame and tank. This is significant since stray flux can be expected in the transformer airspace prior to complete saturation.

These references point to saturation as being achieved when the permeability beyond the knee of the B-H curve drops off dramatically and the core material is no longer the preferred path for the flux.

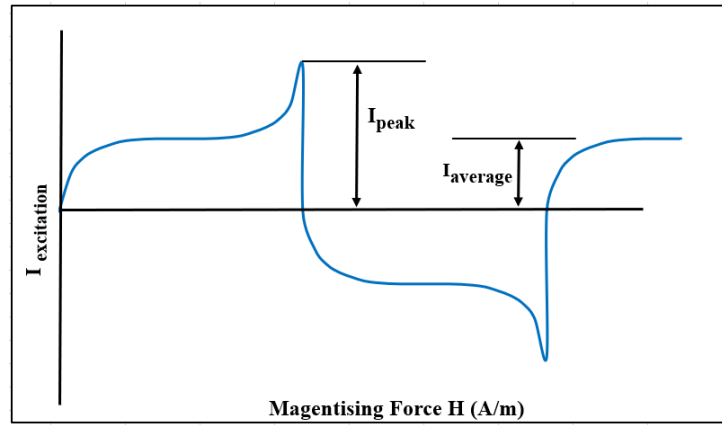


Figure 2.6 - Definition of symmetrical distortion (adapted from [27])

As the excitation is increased the laminations adjoining a core air gap will start to saturate. As the excitation is increased further and the adjacent lamination bridging the air gap approaches saturation its permeability drops and the flux goes in a straight line and crosses the small air gap.

Another phenomenon that occurs with the core response prior to saturation is fringing [1],[26]. As the reluctance seen by the flux increases, the flux seeks alternative paths and at the sharp lamination edges the flux exits the core producing the phenomenon called fringing. Fringing decreases the total reluctance of the magnetic path and therefore, increases the inductance as is evident when examining Equation (2.2).

$$L = \frac{N^2}{R} \quad (2.2)$$

L : inductance

N : number of turns of the energizing coil

R : reluctance

Fringing occurs at air gaps and also along the core length as μ approaches unity. In cores with air-gaps, if the fringing flux is not carefully considered there could be premature core saturation. Fringing flux can be deliberately induced by designing a gap into the magnetic path, or will develop parallel to the magnetic path as saturation is reached.

Borrill [1] discusses the fringing-type effect on transformers related to GIC, and Price [26] discusses the fringing-type effect on a three-limb core caused by a GIC, stating that the three GIC mmf's will sum in the top yoke and have to drive flux through the high-reluctance combination of the zero-sequence air gap and the shunting tank path so the tank effectively 'connects' the core magnetic circuit by virtue of a large magnetic fringing effect.

Summary of key points in literature relevant to research:

- Nominal flux density conventional grain oriented (CGO) electrical core steel has a nominal induction of 1.72 T.
- Flux density of grain-oriented steels at ‘deep’ saturation is >2.0 to 2.2 T.
- No-load current of a power transformer at rated voltage is 3 to 5% of the full load current.
- A 3p3L core is inherently magnetically unbalanced.
- Stray flux can be expected in the transformer airspace prior to complete saturation.
- Stray flux readings in conjunction no-load v-i curves can be used to determine linear and non-linear regions of operation of a transformer.
- Fringing flux is present outside of the core prior to in the inception of stray flux and increases as the core excitation increases.
- The importance of the distinction between fringing flux and stray flux.

Points of uncertainty or disagreement

The recording the inrush waveforms provides a characteristic curve of a transformer but not the point of saturation i.e., knee-point so cannot be used to clearly distinguish between the linear and non-linear region of operation.

Gaps in literature relevant to research

In this literature review, no conclusions were drawn or tests found discussing a 3p3L transformer with a multi step-lap core under symmetrical ac conditions;

- demonstrating the differences between the inception point of stray flux at the centre limb compared to the inception of stray flux at the joints, and;
- showing when the inception of stray flux at the joints occurs when compared to the stray flux in the bulk core material at the centre of the winding, and;
- what the differences are between the flux signal characteristic of the centre limb when compared to the outer limbs, as observed by air search-coils.

2.4 Defining the rating of a test transformer

Research Question 3: What is the normal operational level of flux beyond which saturation is excessive?

Determining the voltage rating using the $B_{\max}$ of the core

After the linear operating region of the magnetisation curve (Figure 2.5), the core enters a state of partial saturation characterised by the non-linear knee point of the curve continuing onward toward ‘deep’ core saturation.

The flux density of a transformer is influenced by the induced electromotive force (emf) [16], the frequency and the number of turns on the winding. As described by Kulkarni and Khaparde [16], for a given number of turns and frequency, the flux and flux density in its core is entirely

determined by the applied voltage. In addition, as demonstrated by Zirka [30], the rated voltage can be determined by calculation for a transformer under no-load conditions using the core cross-sectional area, the flux density specification of the core steel, and the number of turns of the primary winding.

This formula will therefore be applied to evaluate the nominal voltage rating of the transformer core to confirm a flux density of 2.02 T that signifies the start of ‘deep’ core saturation at the limits of M5 GOES core steel.

The basic relationship between induced emf (E) in a coil winding of N turns is given by:

$$\begin{aligned}
 E &= N \frac{d\phi}{dt} & (2.3) \\
 E &= N \times \omega \times \phi_{max} \times \cos(\omega t) \\
 E_{max} &= N\omega\phi_{max} \\
 E_{rms} &= \frac{N\omega}{\sqrt{2}} \times \phi_{max} = \frac{2\pi}{\sqrt{2} \times f \times N \times \phi_{max}} \\
 \therefore E_{rms} &= 4.44fN\phi_{max} \\
 &\text{or} \\
 \phi_{max} &= \frac{E_{rms}}{4.44fN}
 \end{aligned}$$

f – flux frequency in Hertz, $= \omega/2\pi$

N – number of coil windings.

B or ϕ_{max} – flux in webers

Determining the rating using the core saturation response

As discussed previously, Borrill [1] made extensive use of search coils to determine cores saturation that, in conjunction with no-load v-i excitation graphs, could then determine the induction level at which the knee point occurs.

The stray flux combined with the v-i curve can then be used to determine the optimum rating of the transformer to ensure valid dc injection test results.

Determining the rating using a standard knee point definition

The knee points of current transformers to determine their comparative core performance is defined in the IEEE Std. C57.13-2008 and IEC 61869-2 standards as described in Figure 2.7 and Figure 2.8. The IEEE knee point is determined by drawing a line 45° to the abscissa of the graph and positioning it at the point of intersection with the curve as described in Figure 2.7. The IEC knee point is determined on the same magnetisation curves by drawing a point

where the ensuing slope of the curve shows a 10% increase in voltage and a 50% increase in current as described in Figure 2.8.

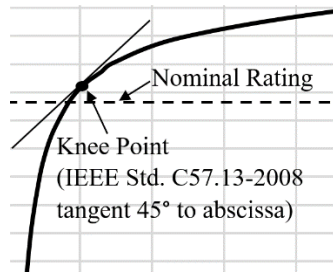


Figure 2.7 – IEEE defined knee point

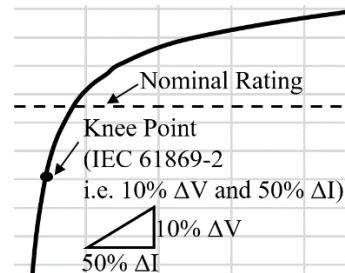


Figure 2.8 – IEC defined knee point

Summary of key points in literature relevant to research:

- The transformer emf equation can be effectively used in combination with the analysis of the no-load v-i excitation graphs to distinguish between the linear and non-linear regions of operation of a transformer i.e., knee point, and the optimal voltage rating of the test transformer.
- A combination of the I_{mag} knee-point evaluation and the core saturation response can be used to determine the rating of a transformer.
- IEEE and IEC standards exist for comparative core performance of current and voltage transformers used for measurement.

Points of uncertainty or disagreement

It is not certain whether the IEEE and IEC standards can be applied to determine the knee points of power transformers.

2.5 GIC affect on power system unbalance and transformer magnetisation

Research Question 4: How does GIC affect unbalance and how does it affect the magnetisation of a 3p3L transformer?

Impact of GIC or dc on unbalanced 3p3L transformer magnetisation

The transformer's core configuration has been shown to influence the effect of GIC. An example of which is discussed by Chisepo [31], where three-phase three limb transformers have been widely understood to be immune to GIC when compared to other power transformer configurations.

Mulasalihović *et al.* [18] and the transformer inductance study by Chisepo [31] demonstrated that even with a low level unbalanced GIC of less than 10 A/phase, the transformer model

was forced into saturated conditions with increased current harmonic distortion and with significantly higher reactive power when compared with the balanced cases. The unbalanced GIC in the 3p3L transformer resulted in much higher current distortion, reactive power, and neutral current distortion than when the GIC was balanced, and the increase in reactive power, Q , was not due to the phase displacement of reactive power but due to the unbalance that led to distortion and considerably lower instantaneous inductances. It was stated that GIC have the potential to make a significant contribution to the failure of grid connected transformers and that even with extensive research the mechanisms and extent are not thoroughly understood.

The discussion included a tanked FEM model to a balanced GIC/phase which was in agreement with literature reports indicating a low response to GIC/dc, as discussed by Masoum and Moses [32]. In the same references, it is suggested that even a small amount of unbalance in the GIC between the phases will result in significant distortion of the excitation current (I_{mag}) and ‘unipolar saturation’. It was discussed how these studies, however, did not clearly explain why the effect of the dc bias is significantly more pronounced in the system with phase resistance unbalance. It then investigated creating an unbalance between the phases by unbalancing the transmission line impedances ($r_1 = 1\Omega$, $r_2 = 2\Omega$, $r_3 = 3\Omega$). This showed a significant impact on the I_{mag} at a high level of dc entering the transformer via its grounded neutral.

It was anticipated by Takasu *et. al* [33] that 3p3L transformers would be relatively immune to GIC because there is no return path for the quasi-dc initiated flux shared equally in the three phases and inducing opposing, equal flux. Further insight was gained and discussed in the study on network disturbances due to geomagnetically induced currents by Koen and Gaunt [3], which expanded on the effect of GIC on 3p3L transformers.

The magnetising current of a large power transformer can be less than 10 amps and the magnitude of a GIC can be in excess of 100 amps [26]. It is therefore likely that a GIC will move the operating point of the transformer core on the B-H curve into the saturated region. The extent of the saturation depends on the operating point at the time of the event, and the transformer’s design specification, including the core material and core configuration. The combination of the transformer design and the alternating current (ac) from the connected transmission line would then result in a saturated core condition shown to be on one-half of the ac waveform i.e., part-cycle saturation.

Generally, the distortion of the magnetizing current waveform will increase as the dc level is increased. Unlike the symmetrical distortion shown in Figure 2.3, the distortion caused by dc is asymmetrical.

As described by Chisepo [7] in Figure 2.9 the arrows show the stray flux paths of a partially saturated 3p3L power transformer with GIC at full load. This response was also reported by Mulasalihović *et al.* [18] in their planar eddy loss study. The research demonstrated the

importance of the proximity of the tank to the core, the magnitude of the GIC/dc, and the degree of GIC/dc unbalance in a 3p3L with dc injection levels of up to 10 A/phase.

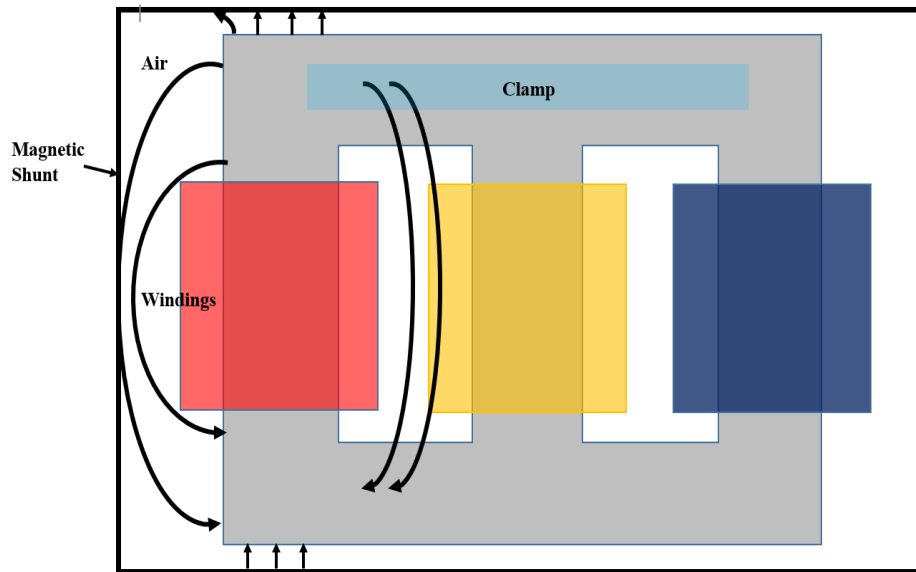


Figure 2.9 – Stray flux in a partially saturated 3p3L power transformer (adapted from [34])

In the FEM study [7] transformer-GIC simulations are discussed showing that 3p3L transformers can saturate at low levels of dc, and that the tank is severely affected in ‘deep’ core saturation so manufacturers need to improve the understanding of stray flux paths inside the transformer, under half-cycle saturation.

It is discussed by Fouad, Nehl and Demerdash [35], that inductance of a transformer decreases markedly as the level of core saturation increases with the decrease in voltage. This emphasises the importance of monitoring and adjusting the phase voltages to maintain a ‘firm’ supply during the unbalanced ‘Transformer under Test’ (TuT) tests with dc, where one or more of the phases could vary markedly from each other affecting the saturation response results from dc injection.

Masoum and Moses [32] investigated the difference between the balanced and unbalanced response of a 3p3L transformer to GIC, and addressed the varying dc bias in the windings and the respective dc mmf in the limbs of a 3p3L transformer. From this analysis, it was concluded that the generation of dc fluxes is highly sensitive to unbalanced dc biases. However, in practice, even if balanced dc bias currents could be achieved precisely, there is still the problem of inherent core asymmetry, non-linearity and residual magnetism including hysteresis in the leg reluctances. If a small degree of unbalance exists, larger dc fluxes can develop in some legs resulting in increased unipolar saturation.

Their investigation considered the varying strength of the dc mmf in each limb depending on the dc current in its windings, they demonstrate how a ‘strong’ mmf would initiate opposing

mmf in the 'weaker' limbs resulting in significantly lower levels of dc flux, and saturation, in those limbs. This could cause the reversal of the mmf in a limb as shown in Figure 2.10.

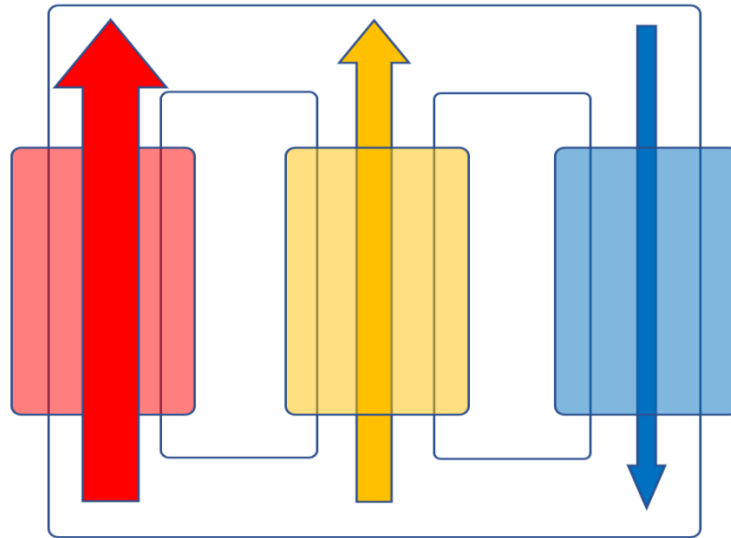


Figure 2.10 – Unbalanced GIC with the highest dc mmf in the left limb [31]

Unbalanced three-phase transmission system

Transmission line transposing

In [36] the impact of untransposed transmission lines and the reasons for, and impact of, the requirement is analysed. This considers two parallel lines (i.e., double-circuit) on the same tower, with and without series capacitor compensation. It is stated that the implications of current-unbalance varies from situation to situation but often setting changes in the protection relays may be sufficient for satisfactory performance under all circumstances and contingencies. As a result, even though it is accepted that transposition will eliminate current unbalance entirely, the need for transposition or phase rearrangements of the circuits is not always required.

In addition, modern transmission lines are generally not transposed because the difference in the inductance of the phases are made negligibly small due to asymmetrical spacing but many transmission line conductors are still spaced symmetrically.

Transmission line unbalance from ageing

In [37] it states that overheating of the transmission lines is one of the major factors that causes conductor aging due to annealing, the deterioration of insulation, and hotspots. This gradually damages the physical and electrical properties of the conductor affecting their resistance, and therefore efficiency.

The conclusion being that line resistance variances are a likely scenario resulting in phase unbalances in transmission lines.

Transmission line unbalance from unbalanced loads and power electronics

Masoum and Moses in [32] state that in general, transmission networks strive to operate in a state of near balance. However, sources of unbalances typically impractical to control may arise. For example, the presence of unbalanced load currents with dc components such as unbalanced rectifiers and power electronic devices in separate phases and imperfect line transpositions can impact transformers under dc bias. The three-leg core structure itself is also a source of unbalance with different centre and outer leg (phase) non-linear reluctances. As shown theoretically, such small unbalances can cause the three-phase three-leg transformer to be highly sensitive to dc bias.

System operator conclusion on transmission line unbalance

When considering resistance differences between the three phases of a transmission line, in an interview with System Operator at Simmerpan in South Africa it was stated as accepted that resistive differences do exist due to the practice of avoiding the transposing of transmission lines. As a result, the dc resistance of phases on long transmission lines vary because of differing phase line lengths from line turns due to the varying topography and line catenary profile differences.

The System Operator concluded that a $\leq 5\%$ unbalance is regarded as an acceptable level of phase unbalance in their network studies.

Emulating a three-phase unbalance in a laboratory

In the [1] the impact of unequal division of dc currents in a 3 phase bank of single phase transformers was discussed.

Figure 2.11 illustrates the parallel paths available for the dc to flow between the source and a test transformer star point.

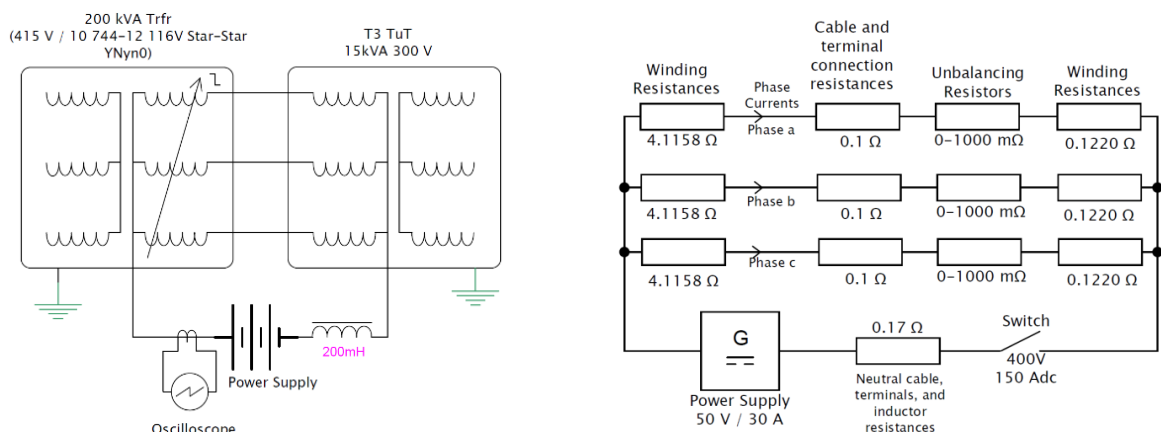


Figure 2.11 – Resistance paths as seen by dc [1]

It was discussed that considerable effort was made to keep the cable lengths equal and terminal connections firm for good electrical contact, but it was noted that the dc does not split equally between the three test transformers. The individual transformer winding resistances were

closely matched to within 0.3 m Ω and all component resistances values were in the milliohm range with a difference of 3 or 4 milliohm noted as being sufficient to cause unequal dc splitting at the star points of the source and test transformer bank.

Each electrical component and connection played a role in increasing the series dc resistance of each phase in a three-phase system. The dc resistance of a series path could be affected by components such as the power transformer winding resistances, high voltage bushing resistances, bus bars, circuit breaker contact resistances, instrument transformers, overhead phase jumper connections of unequal length, overhead line phase wires of unequal length and numerous electrical contact resistances. It was stated that this experimental finding was therefore acceptable and to be noted by power system engineers when conducting GIC studies.

Summary of key points in literature relevant to research:

- GIC in unbalanced 3p3L transformers resulted in an increase in current distortion, reactive power, and neutral current distortion than when the GIC was balanced.
- 3p3L transformers are highly sensitive to unbalance.
- The saturation of 3p3L transformers can occur at low levels of dc of less than 10 Adc, and the tank is severely affected in ‘deep’ saturation.
- The magnetising current of a large power transformer can be less than 10 amps and the magnitude of a GIC can be in excess of 100 amps.
- There is significant stray flux recorded outside of a transformer core even in partially saturated conditions.
- Importance of the flux path including metallic structure of the transformer.
- The particular response of an unbalanced 3p3L transformer to GIC.
- The inter-relationship between the I_{mag} knee-point and level of saturation.
- The requirement of maintaining a ‘firm’ ac supply during dc injection tests.
- Distinction between balanced ac over-excitation and dc bias saturation.

Points of uncertainty or disagreement

There is uncertainty around the precise level of dc where saturation starts in a 3p3L power transformer which is not clearly stated.

2.6 Core joint design significance

Research Question 5: What is the significance of the core-joint design?

Multi step-lap core joints

Transformer core joints are interleaved laminations where the flux can bridge the joint air gap by traversing to a neighbouring lamination. Core joints reduce the effective core cross-sectional area of a core section but this can be alleviated by increasing the number of steps per joint, the number of laminations per step, the air gap length, and overlap length as discussed by Borrill [1].

The importance of carefully constructed core joints was realised during the development and evolution of the core material which included the practical application of non-mitred, mitred, and step-lap joint configurations.

In preparation for the use of a scaled-down model of a larger transformer, as discussed by Borrill *et al.*[38] it was shown that to ensure representative results to those which would be obtained from a full scale power transformer, the joint design correlation is vital. As such, the same multi step-lap joint design in a laboratory transformer is required to ensure reliable test results.

In addition, Kulkarni [16] discussed that the mitred step-lap core construction performs better than the standard mitred joint without steps, under overexcitation conditions.

This is also of relevance in the application of positioning holes drilled through each lamination to align the stacking of the core since the air hole in the core introduces an additional undesirable saturation effect similar to the inappropriate application of butt joints with irregular gaps in the transformer design.

Carrander, C., 2017 [39], says that higher reluctance paths are created in transformer core joints. The flux uses the surrounding core material bypassing the air gap because it provides a lower reluctance magnetic path than the air gap [39]. This effectively reduces the overall core cross-sectional area. The reluctance change influenced by the change in magnetic path length is illustrated by calculation (2.4).

$$R_m = \frac{MPL}{\mu_r \mu_o A_c} \quad (2.4)$$

R_m : reluctance

MPL : magnetic path length

A_c : cross – section of core, cm^2

μ_r : permeability of magnetic material

μ_o : permeability of air

As described in the Transformer Book [40], transformers are designed to operate at a flux density based on the core material chosen by the designer and the requirements of the customer's system. As described by [1] and [41], the performance of a transformer's core is directly related to its joints. Joints being interleaved laminations which allow the flux to negotiate the joint air gap by traversing to a neighbouring lamination which bridges the joint gap. Core joints effectively reduce the core cross-sectional area of a core section but counter measures such as the number of steps per joint, the number of laminations per step, the air gap length, and overlap length play a significant role in dealing with joint saturation during normal

operation [Jones *et al.* [19]; Nakata *et al.* [42]; Nakata & Kawase [43]; Kulkarni *et al.*[16]; Mechler & Girgis [44]]

In [1] a method for determining core joint performance is discussed where indicators are derived to predict joint performance in transformers, including those using mitred step-lap joints. It is postulated that the larger effective area of mitred joints due to their geometry makes them less susceptible to saturation before the limbs themselves. It is stated that a method of determining the effective joint area and effective flux density, B, is needed to understand the core's response. Otherwise, the first component to saturate could be mistaken to be a bulk core component, such as the wound limb, and the joints could be erroneously ignored.

It is discussed how Du *et al.* [45] proposed an equivalent (effective) magnetic path length which differs from the geometric mean magnetic path length as the flux density changes inside the laminated core. It should therefore be possible to determine an effective core joint area that differs from the perfect joint of its particular type, and, among other factors, is affected by the core stacking process.

Borrill discusses [1] that flux distortion, the non-uniform flux profile and fringing complicate the joint response. A simple approach to these complexities could be the use of an averaging factor which is unique to a particular joint type or position in the core structure. If the flux density and applied voltage at which the joints start to saturate are known, and the peak flux through the joint can be measured then the effective core area can be determined. The critical flux density at which a core joint saturates is termed B_c ; therefore, the corresponding applied voltage the study applied was termed V_c , the critical voltage.

It was stated that the effective core area will vary from transformer to transformer according to the quality of the core build and core joint type. In a perfect joint the available joint area is given in equation (2.5). The study proposed determining a utilization factor based on an average V_c for similar transformers for use in transformer core response studies in the non-linear portion of the B-H curve.

$$A = l \times t \times b \times \frac{N - 1}{N} \text{ (mm}^2\text{)} \quad (2.5)$$

A_{eff} : effective core area using $B_{sat} = 2.03T$

l : number of laminations

t : lamination thickness

b : lamination breadth

N : number of lamninations in step group

The utilization factor was then calculated as:

$$U = \frac{A_{eff}}{A} \text{ so } B_{pk} = \frac{\phi_{pk}}{A \times U}$$

It is stated that the benefit of using multi step-lap (MSL) joints instead of non-step-lap (NSL) butt joints is demonstrated by theoretical calculation that MSL mitred joints with a step-lap group of 5 or more are recommended if the core joints are not to affect the saturation characteristics of the transformer. Since practical power transformers use MSL mitred joints, laboratory scaled models for GIC or dc investigations should also use them for proper representation. Alternatively, when butt joints are used, the analysis must consider the joint effect.

Applying this theory, the study demonstrated (Table 2.1) the effect of joint type and step-lap group number on available joint area in p.u. of the wound-limb cross-sectional area.

Table 2.1: Effect of joint type and step-lap group number on available joint area [1]

Step-lap group number (N)	Per unit area (the wound limb is the base cross-sectional area)			
	Butt joint		45° mitred joint	
	Wound limb joint (p.u.)	Return limb or interconnecting yoke joint (p.u.)	Wound limb joint (p.u.)	Return limb or interconnecting yoke joint (p.u.)
2	0.50	0.25	0.71	0.35
3	0.67	0.33	0.94	0.47
4	0.75	0.37	1.06	0.53
5	0.80	0.40	1.13	0.57
6	0.83	0.42	1.18	0.59
7	0.86	0.43	1.21	0.61

Nakata *et al.* [1982] explain that step-lap joints in a transformer core show improved magnetic characteristics such as magnetising current, core losses and sound level when compared to butt-lap joints. They demonstrate that the equivalent magnetizing current length increases with length of the joint overlap and air gap length in step-lap core joints with increasing flux density and an infinite number of laminations per step used in the simulation. Furthermore, they indicate that at a flux density of 1.73T, when the number of laminations per step group, N, is small, saturation of the laminations facing the air gap of the joint will occur with some flux in the air gap. Nakata indicates that as ‘N’ increases so the percentage of the flux passing through the air gap decreases. Ilo *et al.* [41] supports the theory that B_c increases with N.

Summary of key points in literature relevant to research:

- Core joints are an area of focus when investigating core saturation.
- Flux distortion, non-uniform flux profile and fringing complicate the joint so search coil placement should consider simulations for accurate placement.
- Step-lap joints in a transformer core show improved magnetic characteristics such as magnetising current, core losses and sound level when compared to other type of joints.
- 3p3L transformers can saturate at low levels of GIC, and the tank can be severely affected in ‘deep’ saturation.
- It is important that the laboratory transformer have the same multi step-lap joint design found on most large power transformers in a power system network. As shown in Table 2.1, a 4 step-lap mitred joint is adequate to avoid flux restriction in the joints of the test transformer. A large power transformer’s core joints could be of a 6 step-lap construction but a 4-step construction is regarded as adequate, and substantially different to the butt joints used in many laboratory transformers.
- Prior to any laboratory testing, to ensure that the test transformer’s performance is consistent with other transformers in industry its performance would be confirmed according to the IEEE Std C57.12.91-2011 [46].

Gaps in literature relevant to research

- No literature was identified that compared the saturation of the centre limb joints to the joints on the outer limbs of a 3p3L transformer, with a multi step-lap core with simultaneous ac and dc in the windings.
- No literature was identified that compared the saturation of the bulk core material at the centre of the winding to the stray flux inception at the centre limb and outer limb joints of a 3p3L transformer, with a multi step-lap core with simultaneous ac and dc in the windings.

2.7 Measurement of core saturation

Research Question 6: How can core saturation be measured?

Harmonics

As a result of two days of severe GIC activity in March 1989, a reactor failed at the Grassridge 400/220/132 kV substation located on the south-east coast of South Africa. Subsequently, one three-phase, three-limb, 400/132 kV, 500 MVA transformer manufactured in 1991 was installed and monitored due to the potential risk of GIC in the area.

A limited number of geomagnetic activity events were noted up to 2001 and then on three occasions in 2001 magnetic activity which exceeded selected thresholds was observed in March and November of that year lasting over one minute, three hours and 24 hours.

A linear relationship was noted between the second and fourth harmonic measured on the Grassridge transformer in March which showed a strong correlation between these two harmonics and the presence of GIC in the transformer winding.

The current in the transformer neutral showed evidence that the GIC reached approximately 5 A leading to transformer core saturation and there were indications of saturation observed from measurements of the sixth harmonic current that exceeded 8 A. This result confirmed that a very small dc current from a GIC is sufficient to saturate the core.

It was also shown that the reactive power demand of the transformer increased during the main period of activity of the GIC. In the instance described, it doubled from 12 MVAR to 24 MVAR, that illustrates the significant increase in reactive power demand for saturated transformers and reactors.

A statement was made that the results obtained from the event affecting the Grassridge transformer appeared to contradict the prevailing theory that three limb, core type transformers are not susceptible to GIC saturation.

The study further discussed that there was again a linear relationship observed between the 2nd and 4th harmonic currents in the phase windings shown during the storm on the 6th November. In addition, it was found that the GIC also affected the phase angle of the harmonic currents. As an example, the phase angle of the 2nd harmonic in the Grassridge transformer changed by 38° which was a significant new observation which could possibly be used as an indicator of GIC activity in the transformer.

Hydra substation which lies approximately 500 km from the Grassridge substation was previously shown to have susceptibility to GIC by simulation. In the study, it was also observed that low level saturation of a transformer in the Hydra substation was evident from the linear relationship of the 2nd and 4th harmonic currents, with the phase angle change of the 2nd harmonic, and the presence of the 6th harmonic. This saturation occurred even though the transformer was loaded at less than 10 % of its rated load during the event. This indicates that reducing the load of a transformer during geomagnetic events may not prevent transformers on the network from saturating but it may limit thermal damage if the GIC event is of a short duration.

Harmonic signature

In [38], the difficulty of testing real transformers with dc injection is discussed stating that injecting dc into transformers with various winding and core structures demonstrate responses to part-wave saturation, including the generation of harmonics and non-active power. It is also stated that dc injection tests in power transformers, however, are not routinely performed by manufacturers because of fear of damaging the transformer, or limitations in the capacity of the reactive power required for the tests with high levels of dc. The discussion further states that very high values of dc or GIC displace the operating flux of the core into the linear

operating region of ‘deep’ saturation. The effect of this is that the THD initially increased but then decreases again as the dc component increases. The study used FEM modelling which allows conditions of saturation to be simulated beyond practically achievable values in a laboratory. The FEM model identifies the incremental inductance of the transformer during a power cycle reaching the saturation level of L_{sat} , and allows the inductance during distortion by the GIC or dc to be compared with the normal operating condition.

In discussing the distortion of the primary and secondary currents of transformers at increasing levels of dc bias, [1] discusses that the second harmonic is the largest harmonic, besides the fundamental, with the general trend for all harmonics increasing initially as the dc increases. Beyond the 6th harmonic, the magnitudes were stated as negligible.

It was noted that the 5th and 6th harmonics show a decline in magnitude at higher dc levels while the 7th and 8th harmonics show a slight increase.

H Kirkham *et.al.* [9] discuss that harmonics can resonate with inductances and capacitances in the power system, and so create voltages which are much higher than nominal, which also could lead to transformer failure. Harmonics also might cause erratic behaviour in voltage regulators and relays, resulting in system voltage decreases and, consequently, adverse effects of the power transfer capability of lines. In other cases, harmonic blocking measures may be activated, thus preventing some relays from operating when they should, and the harmonics may cause trips which should not happen.

In [32] it was found that an unbalanced GIC in a transformer results in much higher current distortion, reactive power, and neutral current distortion than when the GIC is balanced. It was discussed in [31] that with varying levels of GIC injected in the FEM models, the THD was analysed up to an unrealistic GIC injection of 1.2 kA in the neutral to see the effect of the THD and the so-called ‘air core’.

The THD responses in Figure 2.12 [1] indicate saturation of the joints first before the bulk components go into ‘deep’ saturation for the unbalanced case (with the highest GIC in phase A). The balanced case has a much lower level of distortion expected but each phase current has a different THD%.

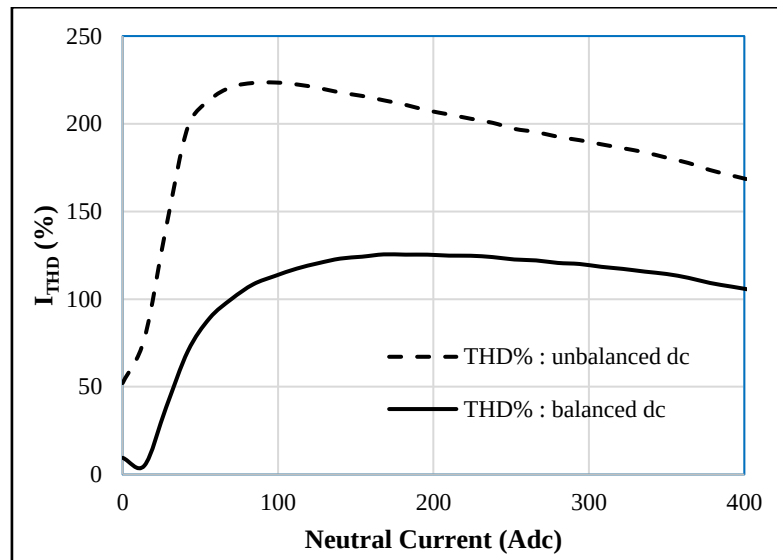


Figure 2.12 - THD showing the difference between the balanced and unbalanced cases to describe the bulk core and joint saturation (adapted from [31])

Harmonic signature obtained by practical experiment

It was stated that in section A in Figure 2.13 the saturation of the core joints is reflected. The THD increases sharply with increasing dc up to approximately 1 A dc.

Through B the THD declines as the joints are now in ‘air-core’ operation. Section C is then described as representing the bulk core material moving into deeper saturation, with the THD increasing once more. Through D the THD declines as the test transformer as a whole is now in ‘air-core’ operation. These results agree with the modelling and experimental results reported by Masoum & Moses [47] and Chisepo [48], which observed that the voltage and current harmonics stopped increasing but rather gradually decreased with excessively increasing dc. It is then stated that this THD graph differs because the test transformer has two distinct parts saturating, initially the joints and then followed by the bulk core material.

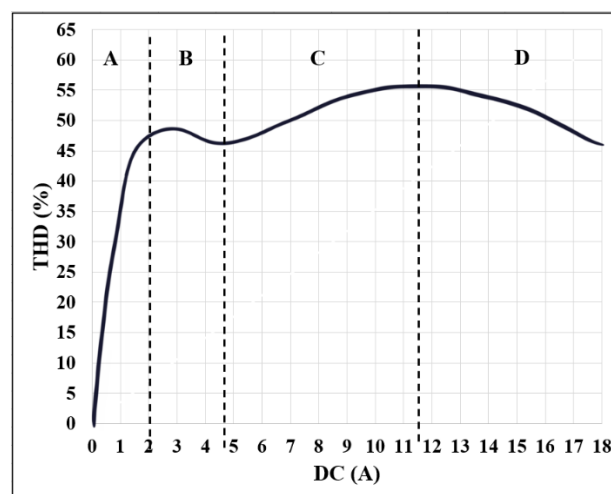


Figure 2.13 - Primary current percentage THD versus dc ampere-turns of transformer (adapted from [49])

The importance of confirming that the test circuit is not inadvertently affecting the results of the device under test (Figure 2.14) [1] demonstrated that although the THD of the current in the ac test circuit (I THD 1) of the transformer displayed the symptoms of a saturated core whereas the THD of current in the dc test circuit (I THD 2) did not. The THD of the secondary ac current being very low compared to the primary current and so not displaying the typical trend associated with a saturated transformer. It is stated that due to this, it proves that the single-phase inductor is not saturating and so will not mask the symptoms of the test transformer.

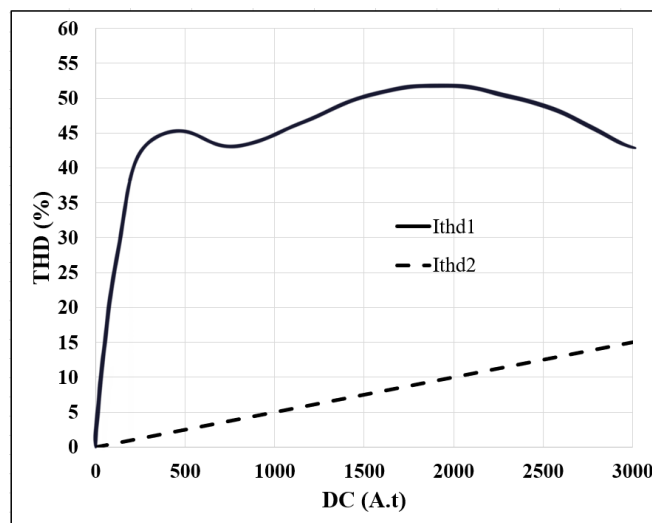


Figure 2.14 - Primary and secondary currents as a percentage of THD versus dc ampere-turns (adapted from [1])

Half-wave saturation

It was shown that the axis of rotation of the half-wave saturated primary current changes as the dc bias level changes. The axis of rotation is the average value of the current in the windings over a 360° period and is equal to the value of the dc bias current. It was stated that the high peak magnetising current pulse seen in the positive half-cycle was due to the dc bias polarity which drives the transformer into half-wave saturation. Walling & Khan [50], Molinski [4] and Girgis & Vedante [5] reported a similar finding.

Mousavi [51] discusses that dc current induces a dc offset in the core flux which forces the core magnetisation over the knee point in one of the half-period of the power cycle. As a result, the magnetisation current, core saturation, and harmonics, increase significantly during the period when the core is magnetized over the knee point.

Summary of key points in literature relevant to research:

- THD is a fundamental tool in the analysis of the response of a transformer to core saturation.
- There are distinct changes in harmonic distortion as the flux paths change when stray flux exits the core, and starts finding alternative paths.

Points of uncertainty or disagreement

No points of uncertainty were identified in the literature review related to this research question.

Gaps in literature relevant to research

No gaps were identified in the literature review related to this research question.

2.8 Impact on power systems and transformers with unbalance and GIC

Research Question 7: What are the negative effects on transformers and the power system due to unbalance in the presence of GIC?

In [51] there is a discussion on dc currents injected in three phase transformers with a grounded neutral causing the transformer core to saturate during each supply half cycle. It was noted that a small dc current can create a large magnetomotive force inside the core due to the number of windings. This phenomenon increases heat from core and winding losses, stray losses in tank and metallic constructions and noise levels.

Ramírez-Niño *et al.* [52] discuss that several undesirable effects, produced by GIC in electrical power grids and transformers, have been reported. When the dc magnetic flux is superimposed on the ac flux, the magnetic cores in the transformers are asymmetrically saturated [53],[33]. They state that the reported failures in transformers due to GIC are mainly dielectric and not as a result of overheating. The primary impact on the grid is that the system protection activates when the reactive loads in the system are changed in the presence of GIC, and the internal resonant frequencies of the transformer change. This process generates voltage surges that potentially degrade the insulation, and the magnetization impedance decreases while the magnetizing current and the no-load losses increase. Reactive load absorption can cause instability in the electric grid [54], and harmonics are contributed to the system, with a significant increase in even harmonics.

2.8.1 Heating

The heating effect of GIC on transformers have been shown by simulation and testing and have the potential impact on power system networks as discussed in the review of literature as follows.

It is understood that transformer core designs which support the flow of zero sequence current are not vulnerable to any negative effects of GIC or dc flux but unbalanced dc current in their windings can imitate the same undesirable effects as other core configurations.

T Ngnegueu *et al.* [55] discuss that in 3p3L core type transformers the zero-phase sequence flux must find a return path outside the yoke which will effectively lower the magnitude of the impedances. The zero-phase sequence impedance will also vary with the magnitude of the current so depending on the magnitude of this zero-phase sequence current, the transformer tank wall will offer return paths to the leakage flux. Eddy currents will develop in it and hot spots may arise.

In [16] it is discussed that the stray loss in tank plates forms a major part of the total stray loss in large power transformers. Stray flux departing radially from outer windings gives rise to eddy current losses in the tank.

In [56] the vulnerability of a 3p3L transformer tank is inferred. In the effort to reduce the height of large three-phase transformers and to avoid tank heating problems due to zero sequence flux, the five-limb core design is preferable over the three-limb. A disadvantage of this is due to the vulnerability of the five-limb transformers to geomagnetically induced currents (GIC) than their three-limb counterparts. This explains the attention continuously paid to studying five-limb transformers under GIC impact by means of circuital and finite-element models.

It is further discussed that in five-limb transformers the presence of the tank can be effectively accounted for by linear inductances representing the off-core fluxes from yoke to yoke. The same conclusion was drawn from accurate modelling of three-limb transformer, which is more sensitive to the tank influence than its five-limb counterpart.

Distortion caused by dc generates harmonic currents, which in turn generate localised heating is discussed by H Kirkham *et.al.* [9]. Albertson (1973) *et al.* [19] established that localized heating can occur shortly after exposure to a GIC, and the effects may be cumulative. It was also stated that overheating can shorten the life of a transformer, and if severe enough, would result in early catastrophic failure.

Berge [57] discusses that the principal threat to transformers during a GIC event is heating of the transformer core due to harmonic currents, and the use of THD as a ratio with respect to actual fundamental current is not accurate if the transformer is lightly loaded, because it will over represent the level of harmonic currents present.

Lahtinen and Elovaara [53] studied the GIC impact on two 463 MVA transformers connected to the grid at Toivila in Finland in 2001 by injecting dc into their neutral. The temperatures were monitored while injecting dc and at 200 Adc the highest temperature they recorded approached 130°C on the inside of a top yoke clamp while the ambient temperature was about

2°C. The steepest temperature gradient recorded was 110°C on the inside of a bottom yoke clamp over a 10-minute period.

In 1985 using a laboratory experiment Hock-Chuan and Swift [58] placed iron bars so as to simulate structural metallic parts of a transformer and under a ‘deep’ saturation condition, from dc they recorded a maximum temperature rise of 6°C. The general conclusion was that core saturation due to a GIC would not cause significant overheating in structural metallic parts because the level of stray flux is not sufficiently large. They concluded that the excitation current could be large and overheating due to winding copper losses was possible. This experiment was not conclusive because the actual placement of the bars was considering GIC simulations on three phase 3 limb transformers.

Picher, Bolduc and Olivier [59] investigated the heating effect of dc in a balanced three-phase 3-legged core-type transformers and concluded that they are “slightly or not at all” influenced by the presence of direct current because the zero-sequence flux is blocked by a strong reluctance. They state that an excessive temperature rise in the transformer's structural parts cannot therefore be attributed to the presence of direct current.

Vakhnina *et al.* [60] studied the influence of GIC on the tank thermal processes. The study calculated that the most heated point on the tank of the power transformer with dc from a GIC in its windings. The temperature rate-of-change in the most heated point on tank with a GIC of 10 Adc, results in a 0.02°C/s and for 110 Adc the change was 0.74°C/s. The conclusion was therefore that when a GIC value increases 11 times, the speed of temperature increases 37 times.

Gupta [61] discussed that stray flux initiated heating of a transformer tank was only potentially harmful when the power rating of the transformer was above 100 MVA, in this discussion on the loading of power transformers.

There is some conflicting evidence in literature on the impact of heating in structural components but most agree due to the fact, as discussed by Biro *et al.* [24], Zirka [25], and Price [26], when the saturation point is reached, the magnetic flux finds a transmission path by exiting the core into the surrounding material.

Summary of key points in literature relevant to research:

- Heating is localised in specific areas of the tank as demonstrated by simulation and exploratory tests.
- Various heating patterns of a 3p3L transformer with GIC or dc flux have been observed in simulations and a few in practice, particularly involving the transformer tank, so is a valid test to perform in the scope of this research.
- The hot-spot temperatures that will be generated due to the induced eddy currents are dependent on the degree of eddy currents and stray flux which is dependent on the level of saturation of the core.

Points of uncertainty or disagreement

It is not certain that the thermal response of a test transformer will be entirely representative of a real power transformer due to the large design margins applied to a test transformer to meet the level of robustness required in a laboratory environment.

2.8.2 Reactive power response

The reactive power response of power transformers from GIC, and its potential impact on power system networks is discussed in the following review of literature.

The reactive power absorption of a transformer increases when that transformer's magnetic core is saturated [57] and the relationship is described as almost linear and depending nearly entirely on the saturated reactance of the transformer.

In [62] it is discussed that the variation of the MVar consumption is also linear with the increase of GIC when considering the fundamental component of the exciting current. It states that the MVar drawn by the transformer can be estimated using (2.6):

$$Q_{MVar} = k_1 \times GIC + Q_0 \quad (2.6)$$

Q_0 – reactive power consumption during at nominal excitation

GIC – dc current in neutral

k_1 – Table 2.2

Table 2.2 – Value of k_1

Core Design	k_1
Single phase	1.18
3p – shell type	0.33
3p3L – core type	0.29
3p5L – core type	0.66

Importantly, in [31], it was discussed that with varying levels of GIC injected into the FEM models, and the reactive power Q calculated, that the balanced case produces a linear Q-GIC characteristic (Figure 2.15). The unbalanced case, however, yielded a non-linear Q-GIC characteristic and the Q draw was significantly higher than the Q for the balanced case. In conclusion, it was shown that the actual increase in reactive power Q was not due to the phase displacement of reactive power but due to the unbalance that leads to distortion and considerably lower transformer instantaneous, incremental, inductances.

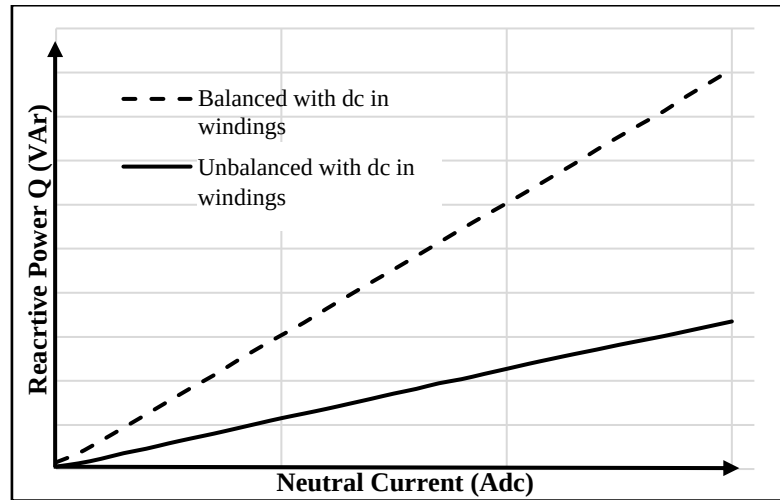


Figure 2.15 - Calculated reactive power showing the difference between the balanced and unbalanced cases (adapted from [31])

Berge, Marti and Varma [54] stated that in the laboratory transformer under study where dc was injected into the windings, it was shown that the absorbed reactive power of a transformer is linearly proportional to the magnitude of the saturating current. Overbye *et al.* [63] concurred with this.

Summary of key points in literature relevant to research:

It is consistently stated that the reactive power increases with the increase in core saturation, and is linear in nature. This should be analysed in conjunction with the stray flux and the harmonic distortion measurements to understand the overall transformer response to core saturation with as and dc in the windings.

Note: The literature reviewed takes a conventional view of reactive power. There are alternative views about the validity of the concept of reactive power and energy (Kirkham and White [64], Malengret and Gaunt [65]), but the conventional approaches will be retained for this project.

2.8.3 Noise

The noise produced by power transformers from GIC, and the possibility of its use in the analysis of GIC in power system networks is discussed in the following review of literature.

Table 2.3 illustrates the varying noise levels of four transformers with each having different corner-jointing methods. Otherwise, the cores are dimensionally identical, stacked laminates of the same steel grade, at nominal operating conditions and, in this case are 3-phase, 3-limb.

Table 2.3 – Decreasing noise level in multi step-lap joints in a transformer core [66]

Corner Joint Type	Butt-Lap (2-steps)	Half Mitre (2 steps)	Single Step-lap (2 Steps)	Multi step-lap (5 steps)
Noise (dbA)	Reference	-4 dbA	-6 dbA	-9 dbA

Kulkarni and Khaparde [16] also discuss the benefit of step-lap joints for core construction to achieve a lower core loss and noise level.

Heathcote [40] discusses transformer noise in some detail describing a dominant noise level of at 100 Hz. This is at twice the fundamental operating frequency of the transformer and indicates magnetostrictive strain in the transformer core producing a longitudinal vibration in the laminations at this measured frequency. This is not truly sinusoidal so also leads to harmonics.

Deviation from a ‘square-law’ magnetostrictive characteristic would result in even harmonics (at 200, 400, 600 Hz, etc.), while the different values of magnetostrictive strain for increasing and decreasing flux densities lead to the introduction of odd harmonics (at 300, 500, 700 Hz, etc.).

The other main source of noise from the transformer core is due to alternating attractive and repulsive forces between the laminations caused by flux transfer across the air gaps at the leg to yoke and inter-yoke joints. These forces can be reduced by special building and design techniques of which the best known and most widely used is the step-lap form of construction.

In cold-rolled laminated cores operating at flux densities between 1.6 and 1.8 T variations of 10 per cent in flux density have been shown to produce changes of noise level of the order of only about 2 dB, although the ‘character’ of the noise may vary appreciably.

Turning to other possible sources of noise emitted by a transformer, as indicated earlier, the forces present between the individual conductors in the winding when the transformer is loaded must be considered. These forces are, however, of a sinusoidal nature so that any vibration consists of a fundamental at 100 Hz with negligible harmonics. The fundamental is thus effectively dwarfed by the much greater 100 Hz fundamental generated by the core, while there are no harmonics to add to the annoyance value. Acoustic measurements confirm this conclusion by showing that, except on very large transformers the noise level increases by no more than 2 dB (15 per cent rise in loudness) from no load to full load. Any variation is in fact attributable more to changes in flux density than to variations in the forces in the windings.

Noise is a factor documented by Kappenman & Albertson [67] where it is stated that a partially saturated transformer is characterized by a significantly increased asymmetrical magnetizing current, the generation of unwanted harmonics, noise and overheating, large reactive/non-active power (Mvar) consumption, and voltage depressions.

Heindl *et al.* [68] discusses that GIC on power transformers can be observed by vibration measurements that are caused by voltage-dependent and load-dependent effects, which lead to oscillations in mechanical structures of power transformers. The voltage-dependent vibrations are said to originate from magnetostriction oscillations of the core lamination sheets due to the Weiss Domain effect. The vibration is at 100 Hz, or double the electrical frequency.

In [69] Girgis *et al.* gave a detailed analysis of the relationship between the level of noise in a transformer at different locations on a network, and the level of the GIC. They also differentiated that to the noise levels of transformers at different load levels.

Summary of key points in literature relevant to research:

- The noise levels of an unbalanced 3p3L transformer increases significantly when there is dc in the core.
- The frequency of 100 Hz should be clearly visible in the recording of audio levels.

Gaps in literature relevant to research

No literature was identified discussing that the noise signature of a 3p3L transformer with a multi step-lap core with simultaneous ac and dc in its windings could be used as an analysis tool to determine its saturation levels.

3. THEORY DEVELOPMENT

3.1 Theory outline

There are many studies on the effect of GIC on 3p3L transformers, and many more on transformers with other core configurations such as Biro et al. [24], Chisepo, Borrill and Gaunt [31], Zirka [56],[25] and Price [26]. These studies have performed extensive analyses of the core response to saturation using the transformers electrical, magnetic flux, thermal and mechanical characteristics. Even though these studies have investigated core saturation levels at different parts of the core, no study has performed a detailed practical experiment to study the degree to which a 3p3L saturates with slight unbalances as expected in a real power system, and the related core saturation response at the joints, compared to the bulk core material.

To address this, it was theorised that controlled experiments would be needed with a carefully designed test transformer, integrated into a purpose-built test circuit to practically emulate a real-world GIC on a 3p3L power transformer. To achieve this, a scale model of a power transformer (TuT) was designed for laboratory-type testing, closely replicating the electrical, magnetic and mechanical characteristics of a real 3p3L power transformer. The test circuit for the TuT needed to replicate the conditions in a power system network to emulate normal conditions to establish a baseline for the GIC research, and then also allow normal and high-level GIC tests. The circuit would need to be carefully designed to emulate a GIC but, at the same time, not be affected by the GIC itself. This would ensure that all the test results were from the response of the TuT, and not from other circuit interactions.

The required test conditions would be;

- symmetrical ac excitation at the TuT's nominal rating;
- symmetrical ac that exceeds its nominal rating, to emulate an over-excited condition, and;
- asymmetrical conditions with ac at the TuT's nominal rating, and superimposed, controllable dc of varying levels, on each of the windings.

The varying level of dc would be to emulate a real-world unbalance in power system networks. The dc tests would require balanced dc in each winding to study a 'baseline' GIC condition, and then various controllable levels of current in each of the phases of the transformer to emulate a range of real-world GIC conditions, to perform a comparative analysis.

3.2 TuT

The basic TuT requirement specification to replicate a power transformer but with practical considerations to enable measurements, was as follows:

Windings

The winding design was based on the following requirements (Figure 3.1 and Figure 3.2):

- Three winding per phase 3p3L transformer with winding ratios of 1:1:1.
- All nine windings constructed using the same conductor thickness.
- Windings made up of three separate concentric copper wire coils per phase.
- The terminals of each coil terminated on an accessible front connection board to allow for wiring various combinations of 3 phase winding configurations, i.e., Yy0y0, Yy0y6, Yy0, Yd1, Yd1d1, 2:1 autotransformer with delta tertiary Yy0d1, 2:1 autotransformer with parallel 'LV' windings, 3:1 autotransformer Yy0 with series 'LV' windings.
- The connecting board should not interfere with tank installation and tank temperature measurements.

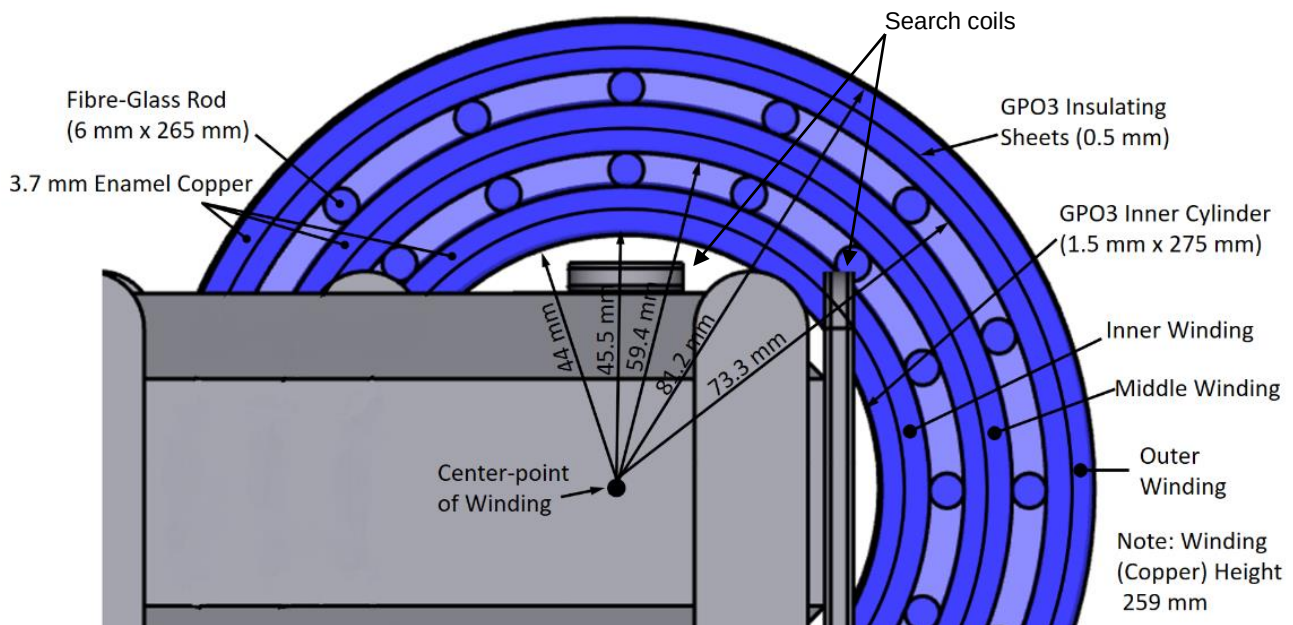


Figure 3.1 - Winding Construction and Dimensions

The power rating of the transformer was restricted by the physical limitations inherent in the design and configuration. To define its capability, the inner winding was considered the limiting component because it is located within two other windings on each of the phases and enclosed in two layers of 1 mm insulating material. This is the most limiting operational cooling environment of the windings.

Table 3.1 – Transformer T3 (TuT) specification

T3 (TuT)
Rated Power: 15 kVA
Voltage: 380 Vac
Current: 22.8 Aac
Insulation: 1000 V
System Frequency: 50 Hz
Vector Group: YNyn0yn0
Impedance: 5.08%
Tap Changer: None
Core/Winding Configuration: 3p3L
Core Cross-Section Area: 0.0035m ²
Winding Material: Cu/Cu
Core Material: M5 CRGO Silicon Steel
Core Lamination thickness :0.3 mm
Core Corner Joints: Mitred 4-step-lap
Core Flux Density: 1.703 T
Core supported by a 304 grade stainless-steel (austenitic material)

In addition, the transformer current rating for any experiments must consider that the windings are wound with 4 mm round enamelled copper with a cross-sectional area of 12.5 mm². To conservatively represent the most restrictive current rating, a ‘clipped direct’, 10 mm² copper PVC insulated cable is used, which has a 58 A rating (SANS 1507) at an operating temperature of 70°C (30°C ambient).

Core design

The core design was based on the following requirements (Figure 3.2 and Figure 3.3):

- Fully exposed and vertically orientated for ease of work and for installing search coils and instrumentation around the core.
- No holes in the core lamination as commonly used on small transformers for core stacking.
- Core material and joints to be representative of a real power transformer, i.e., 45° 4 step-lap mitred joints. The feasibility of a 6 step-lap design, such as that used in large power transformers must be investigated and if it is possible on such small a unit.
- Core clamps and beams to be representative of a real power transformer.
- Core construction should allow a mild steel bar with search coils, to be inserted without being obstructed by the winding assembly.
- Factory-installed search coil at centre phase winding mid-point with twisted leads and adequate protective insulation for measurement of core induction level.

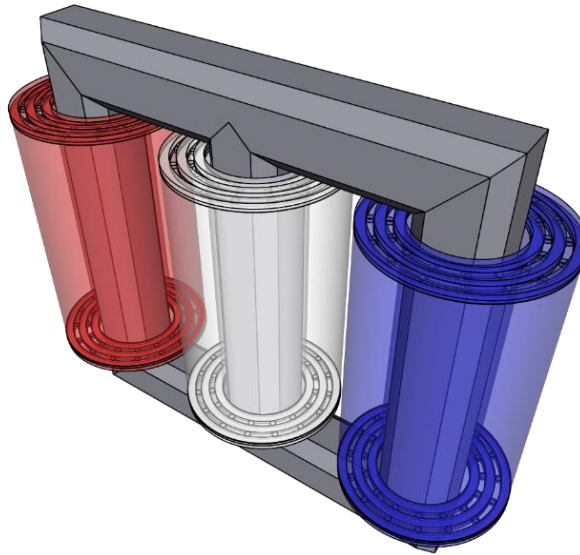


Figure 3.2 - Winding and Core Configuration

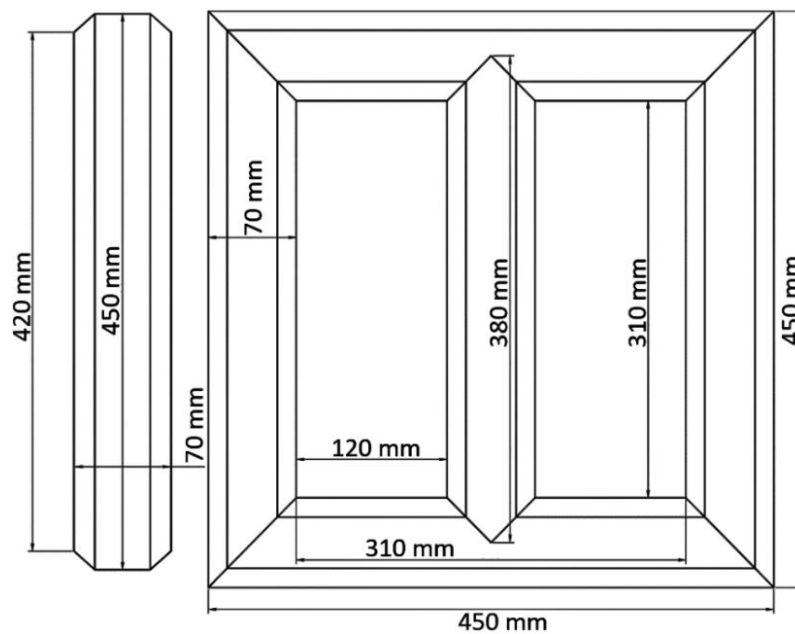


Figure 3.3 - Transformer Core Dimensions

Tank

The tank design was based on the following considerations (Figure 3.4):

- Removable tank with clearances between windings and tank, and core and tank scaled to that of a real power transformer.
- Material of standard hot-rolled mild steel.

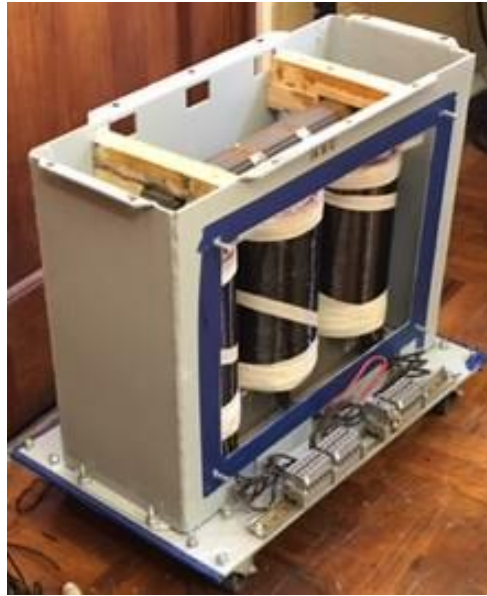


Figure 3.4 – Model 3p3L power transformer (TuT) at delivery (lid and front cover removed)

Winding and core mounting

To ensure that all the measurements of the TuT would be consistent, and could be performed repeatedly without variance in the results it was required to stabilise the core and measurement devices where possible. Commercially available power transformers are well braced using yoke and core clamps and the cores are solidly bound. In the case of the TuT, however, to make it as accessible as possible for core flux and temperature measurements the top limb of the transformer core was only bound with transformer binding tape. To remedy this, braces machined from PVC (Figure 3.5) were installed on the top yoke of the core and additional tape was used in the sections between. The concern was that this could restrict access to the transformer core for the search coil readings so permanent search coils were installed at each of the braces so readings could be taken on the core joints.

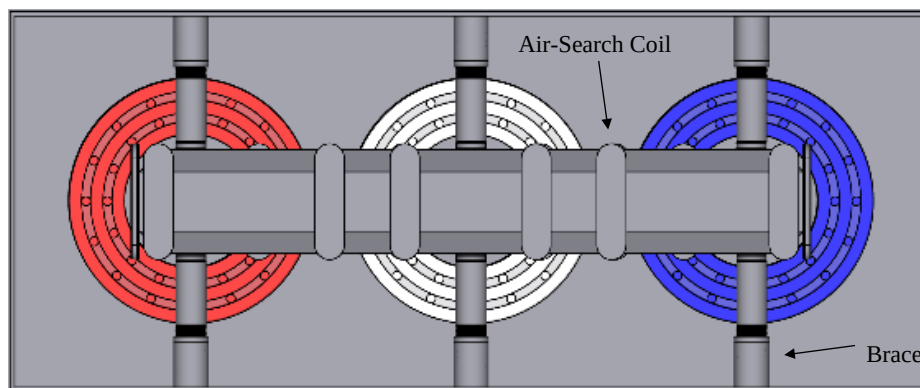


Figure 3.5 – Upper yoke bracing

Finally, prior to any tests it was required to measure the position of each of the windings on the transformer, and fix them rigidly using wooden wedges. This was to ensure that the transformer windings would not shift during the tests and invalidate previous test results, and to ensure as much magnetic and inductive symmetry as possible.

3.3 Test circuit for symmetrical ac tests

As the symmetrical ac tests were performed with the TuT under no-load conditions a harmonic-free stable power supply would be particularly important as described in the IEEE Std C57.12.91-2011 [46]. The standard states that no-load (excitation) losses are particularly sensitive to instability or variation of the test supply source voltage, so, considering this, a primary requirement was that the test circuit energise the TuT with a stable and robust power supply with minimal distortion. According to IEEE Std 519; 2014 [70], and IEEE Std 1459; 2010 [71], a system is required to operate within a maximum THD of less than 5% at $V \leq 1.0$ kV. To this end, an isolated supply using a generator as the source as shown in Figure 3.6 was required for all the TuT tests. The tests circuit in Figure 3.6 was therefore designed for the symmetrical ac tests of the TuT.

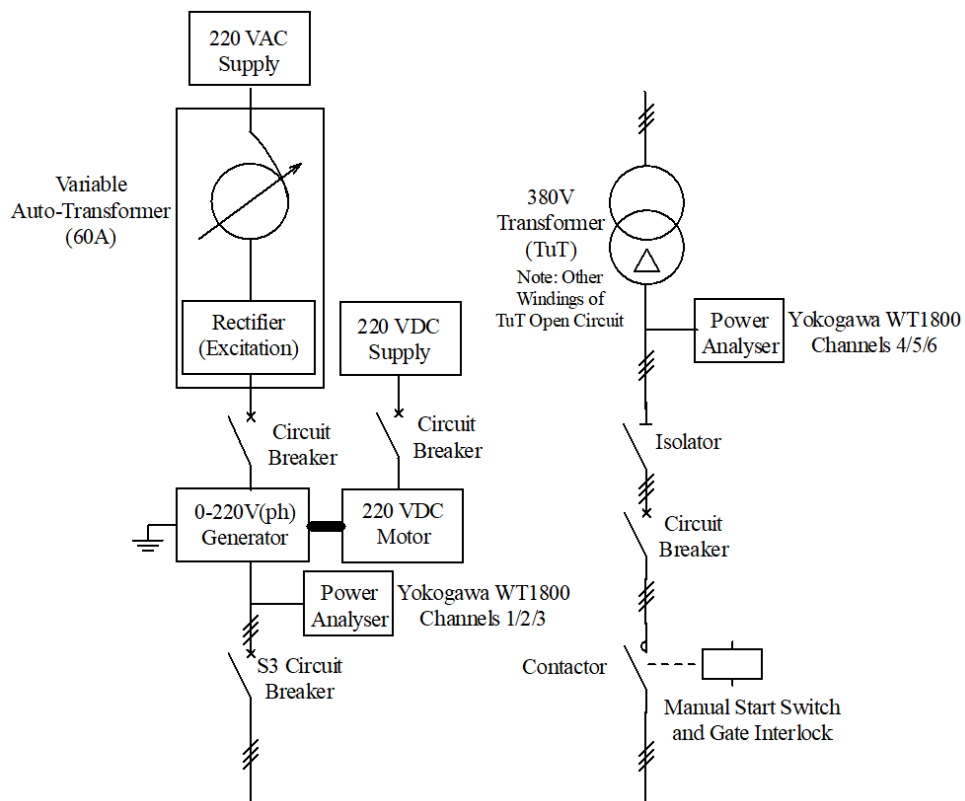


Figure 3.6 – Test circuit for symmetrical ac tests: Read from Top-Left to Top Right

3.4 Test circuit for asymmetrical dc tests

The design of the power supply for the asymmetrical tests with simultaneous ac and dc, was required to conform to the same specification as the ac power supply for the symmetrical ac tests (Figure 3.6). This circuit (Figure 3.7 to Figure 3.9) must, however, enable the controllable injection of dc into the windings of the TuT without any other component being affected by the dc.

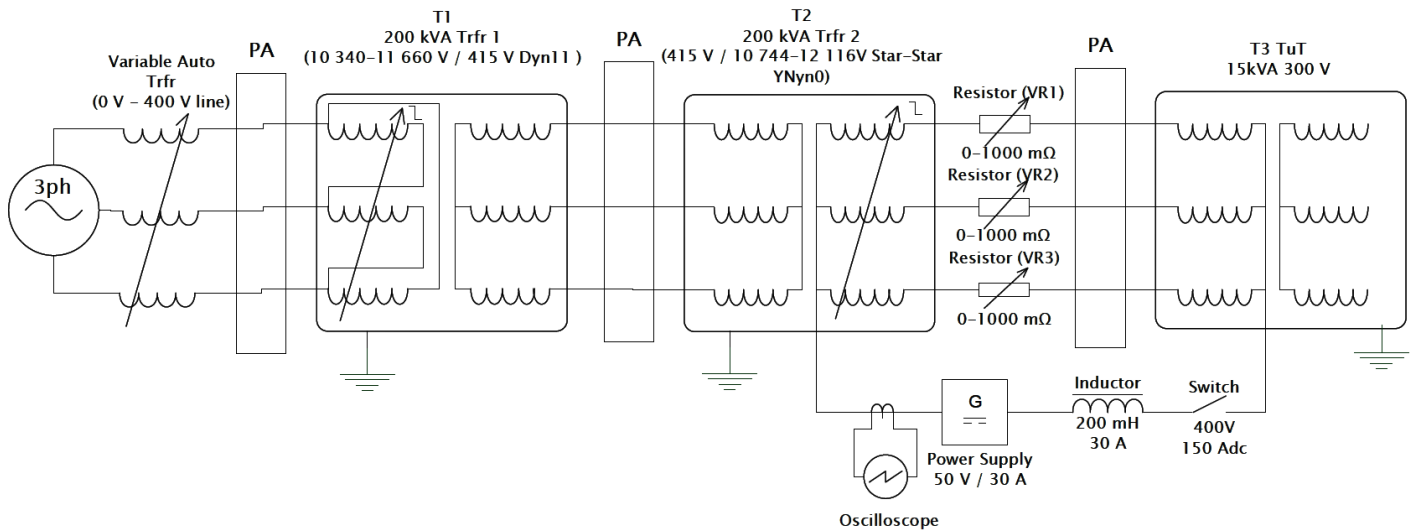


Figure 3.7 – Test circuit for asymmetrical dc injection tests

To achieve this two 200 kVA, 11 kV to 415 Vac power transformers in a back-to-back configuration were used as shown in Figure 3.7 and Figure 3.8.

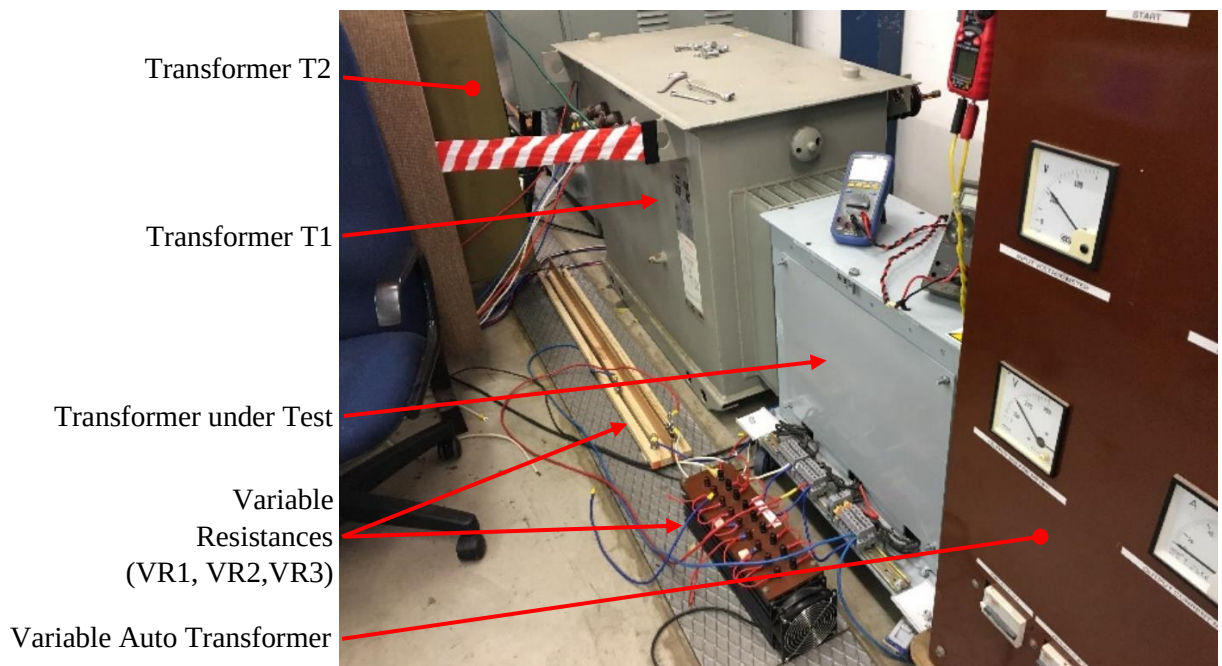


Figure 3.8 – Physical Layout of Test Circuit Supply Transformers, Resistors, and TuT

The purpose of the large MV power transformer (T2) supplying the TuT with ac and dc was to minimise the possibility of it saturating during the test of the TuT as it would be operating at relatively low induction levels when compared to the TuT. To verify this was the case, tests were performed to confirm that T2 was not saturating under test conditions (section 3.5). Transformers T1 and T2 were standard 3-phase distribution transformers used in industry, but transformer T2 was configured star-star during manufacture to make the neutral accessible on the MV winding to enable dc injection at its neutral.

The copper current carrying capability of the T2 MV winding included an ac excitation current of 5 Aac [1] of the TuT superimposed on the dc power supply that had a maximum output of 30 Adc. With this circuit configuration, the sum of the ac and dc currents in each phase would not exceed a peak current value of 20 A per phase. The MV winding of T2 (Figure 3.7) has a cross-sectional area of 4 mm² which, therefore, has an adequate continuous current rating of 37 A. The wiring of the circuit was 6 mm² PVC insulated wire which has a continuous current rating of 47 A. It was therefore concluded that all the conductor material ratings exceeded the maximum current rating requirement.

In the case of the line connections between transformers T1 and T2 due to a maximum possible current of 170 Aac, the wiring was required to be at least 35 mm² PVC insulated copper [72].

Other key components in the circuit are the resistors in series with each TuT winding to control the unbalance of dc in the windings, the dc power supply to control the dc current, and the 200 mH inductor filter to suppress the level of ac in the neutral line. The oscilloscope current clamp (Figure 3.9) is to monitor the neutral line during the dc injection tests to measure the levels of ac in the neutral, to ensure that the ac current in the neutral line, and therefore through the dc power supply, is negligible.

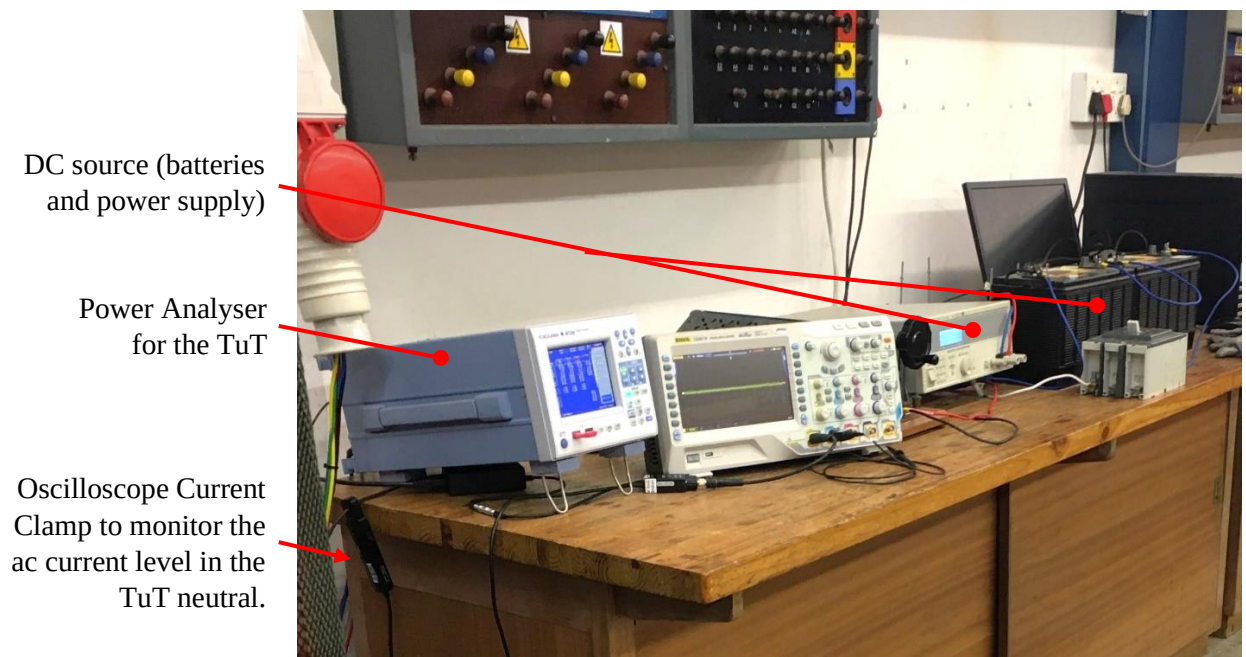


Figure 3.9 – Test Circuit Oscilloscope, TuT Power Analyser, and DC Power Supply

Note: Assuming balanced load conditions, the additional benefit of T1 being a delta-star configuration was that the delta winding would remove, by filtering, possible triplen harmonics from the source supply.

3.5 Test circuit for ac power supply validation for dc tests

The open-circuit testing of the supply transformers in the test circuit would require a basic test to confirm that they are in good condition.

To ensure that the complete magnetisation curves of transformers T1 and T2 could be plotted, an excitation voltage well in excess of their nominal rating was required. To achieve a higher voltage, transformer T3 (TuT) was proposed to be configured with two of its windings in series (Figure 3.10) to provide a stable 500 Vac (line) supply required for this test, connected in a Y-Y configuration.

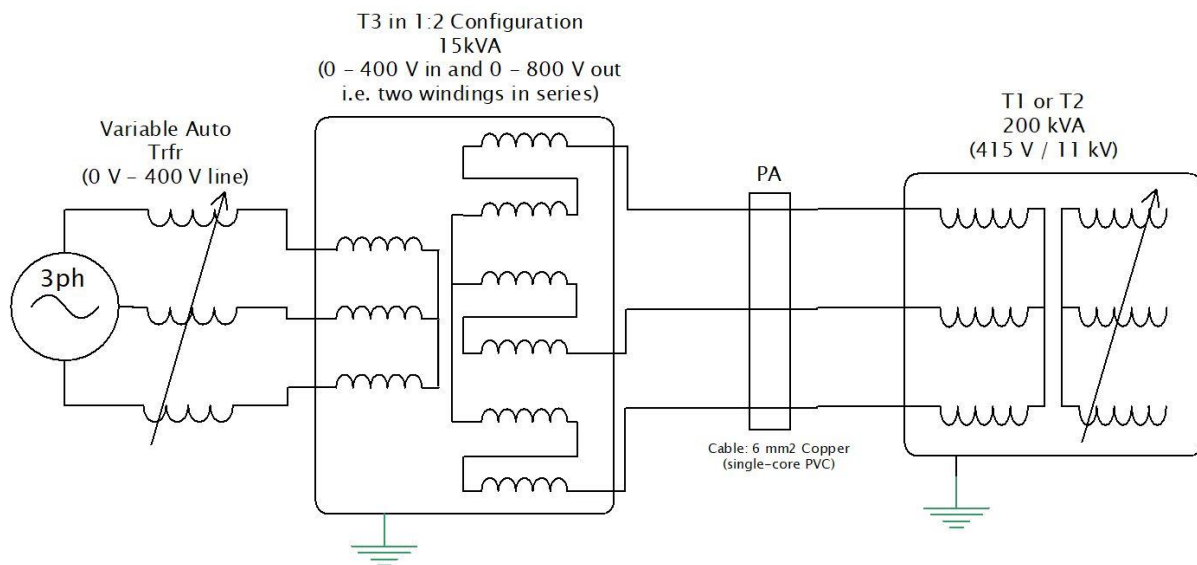


Figure 3.10 – Test circuit to determine transformer T1 and T2 magnetisation curves

The nameplate specifications of transformers T1 and T2 in Table 3.2.

Table 3.2 – Transformer T1 and T2 specification

T1	T2
Rated Power: 200 kVA	Rated Power: 200 kVA
Voltage: 11 000 V / 415 V	Voltage: 11 430 V / 415 V
Current: 10.5 A / 278 A	Current: 10.1 A / 278.2 A
Core/Winding Configuration: 3p3L	Core/Winding Configuration: 3p3L
Core Cross-Section Area: Unknown	Core Cross-Section Area: 0.01663m ²
Winding Material: Al/Al	Winding Material: Al/Cu
Core Material: Unknown	Core Material: 030122 Steel
System Frequency: 50 Hz	System Frequency: 50 Hz

T1	T2
Vector Group: Dyn11	Vector Group: Ynyn0
Impedance: 4.26%	Impedance: 4.08%
Insulation Level: 95 kV	Insulation Level: 95 kV
Tap Changer: 10 340-11 660V (94 – 106%)	Tap Changer: 10 744 -12 116 V (94 – 106%)
11 000 tap 3 (100%)	11430 tap 3 (100%)
Turns LV: 38 MV: 1109 tap 1 (106%) – 1046 tap 3 (100%)	Turns LV: 38 MV: 1109 tap 1 (106%) – 1046 tap 3 (100%)

The specification of transformer T3 (TuT) is recorded on Table 3.1.

To validate their performance, the flux density, B , for transformers T2 and T3 could be calculated using the transformer emf equation, $U_{rms} = B_m \times 4.44 * N * f * A * \sqrt{3}$, to compare their relative values, and compare their values to those expected for a core constructed from GOES on the excitation curves. This could then be evaluated in conjunction with the IEEE and IEC knee point definitions as described in literature in Figure 2.7 and Figure 2.8. The IEEE and IEC knee point definitions are specifically used for comparative tests of current transformers so their validity when applied to power transformers will be evaluated in this study.

Validation test to confirm that T2 does not saturate

The test circuit in Figure 3.11 was designed to confirm that the source transformer T2 would not saturate for all the future supply and test conditions required by the TuT. The circuit enables dc injection into the windings of T2, while T2 is supplying a purely resistive load that cannot saturate. The resistive load is specified to ensure that no harmonics could be introduced into the circuit from the load, and so invalidate the results of the test, as could be the case if the load had a reactive power response.

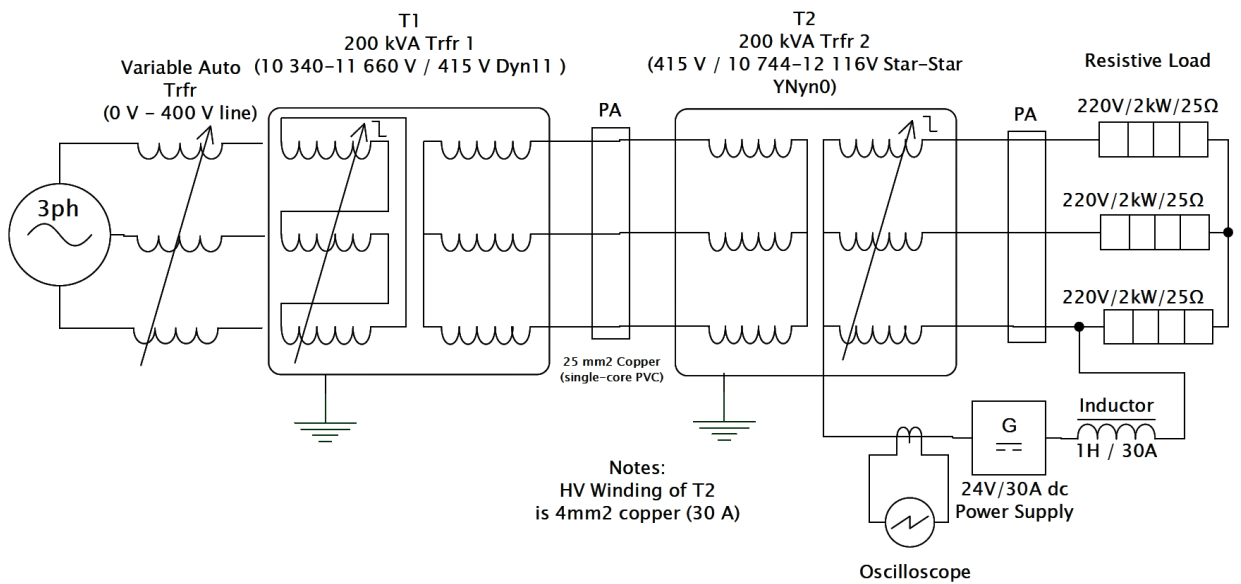


Figure 3.11 – Test circuit to confirm T2 could not saturate during dc tests of TuT

A maximum phase unbalance of 169% (the current in a single phase being 1.69 times the current of the other two phases) calculated for the unbalanced condition of T2 in this test circuit. The maximum unbalance that the TuT and T2 would be exposed to in the planned test protocol is 23.5% due to the maximum variable resistances available in the laboratory. The 23% unbalance will demonstrate an extreme condition, with the majority of the tests under 5% to replicate the unbalance of normal transmission lines in a national network.

Note: An unbalance of 169% is excessively high but a more realistic unbalance was not possible due to the available dc power supply in the laboratory that could not provide a sufficiently high dc voltage to achieve the required dc current through the resistive load. To overcome this limitation, the dc current was injected directly into a single phase of T2 as shown in Figure 3.11, initiating the high unbalance.

A current of between 3 and 5 A_{dc} would then be injected into the neutral of T2 to test if any core saturation is evident. This dc current is well into the non-linear region of operation on its magnetisation curve, as measured using the test circuit in Figure 3.11. In addition, to ensure that T2 was not saturating during a test at a high unbalance, a power analyser would be connected to monitor the primary LV side of T2 during a dc test on the TuT at a high unbalance. This is to confirm that there are no anomalies related to the increase in THD, typical of core saturation on T2 during the test.

3.6 Unbalance for asymmetrical dc tests

Determining of transmission line unbalance

To emulate unbalanced transmission lines, unbalancing resistors were placed in series with each winding of the TuT (Figure 3.7).

The unbalancing resistors were sized based on a discussion with the System Operator at Simmerpan in South Africa that concluded $\leq 5\%$ unbalance as a reasonable level of resistive phase unbalance in their network studies. So, for the purposes of this study a worst-case resistive unbalance of $\leq 5\%$ would be applied to emulate a normal unbalance, and some higher unbalances would be required to investigate higher-level GIC conditions.

For the dc test circuit (using Phase a as an example), the actual dc current in Phase a for a GIC in the neutral was calculated using (3.1), and the percentage unbalance of a particular phase was calculated using (3.2). The formula simply calculates the percentage variance of a particular phase from its nominal current, which is generally accepted method, and is applied by Eskom transmission. In this case the nominal current is the mean of the total current of the three lines.

$$R_{phase\ a*} = R_{imb\ a*} + 4.3378\ \Omega$$

$$\frac{1}{R_t} = \frac{1}{R_{imb\ a} + 4.3378\ \Omega} + \frac{1}{R_{imb\ b} + 4.3378\ \Omega} + \frac{1}{R_{imb\ c} + 4.3378\ \Omega}$$

$$I_{phase\ a*} = \frac{R_t}{R_{phase\ a*}} \times I_n \quad (3.1)$$

$$IMB_{phase\ a*} = 1 - 3 \left(\frac{I_{phase\ a*}}{I_n} \right) \times 100 \quad (3.2)$$

$R_{phase\ a}$ = phase a resistance

$R_{imb\ a}$ = unbalancing resistance in phase a

$R_{imb\ b}$ = unbalancing resistance in phase b

$R_{imb\ c}$ = unbalancing resistance in phase c

R_t = total resistance

I_n = neutral dc current

$I_{phase\ a}$ = phase a dc current

* Substitute phase a with phase b or phase c as required

3.7 Search coils

Air-search coil design

Air search coils (SCs) are required to enable recording of the relative magnitude of stray flux surrounding the core after the transformer reached saturated core conditions [1]. The SCs were wound using 0.315 mm (diameter) cold drawn, annealed, round copper wire, overcoated with polyamide-imide. The SCs in Figure 3.12 each had 130 turns with a dc resistance of 5.1 Ω , and the 250-turn SC in Figure 3.13, for the centre of the winding, had a dc resistance of 4.2 Ω . The leads from the search coils were twisted to improve their electromagnetic noise immunity.

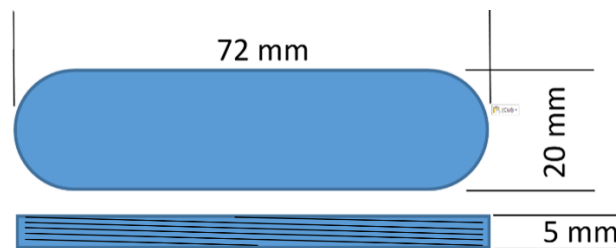


Figure 3.12 – Dimensions for 130-turn air search coil to measure stray flux at the core joints

It is important that all of the 130-turn search coils' sensitivity is the same so that they would detect the same relative level of stray flux at each location around the core. The tolerance of the air-search coils was therefore confirmed during manufacture and measured in the 50 Hz alternating electromagnetic field where they would be used, to within 1% of each other.

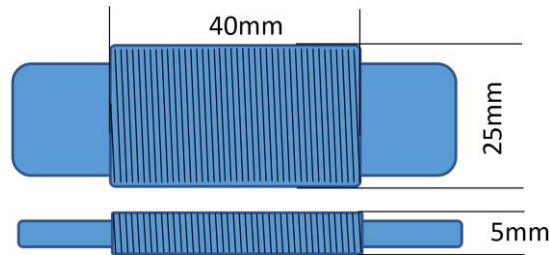


Figure 3.13 – Dimensions for 250-turn air search coil to measure stray flux at the centre of the winding

Search coil placement

To measure the stray flux density, locations around the transformer were identified so that consistent, repetitive measurements could be taken. The positions used in the single-phase transformer tested in [1] showed the optimum orientation of the SCs in specific areas of the core (Figure 3.14). This was taken into consideration in the new TuT that specifically allowed for access around the core to make optimum use of search coils and instrumentation.

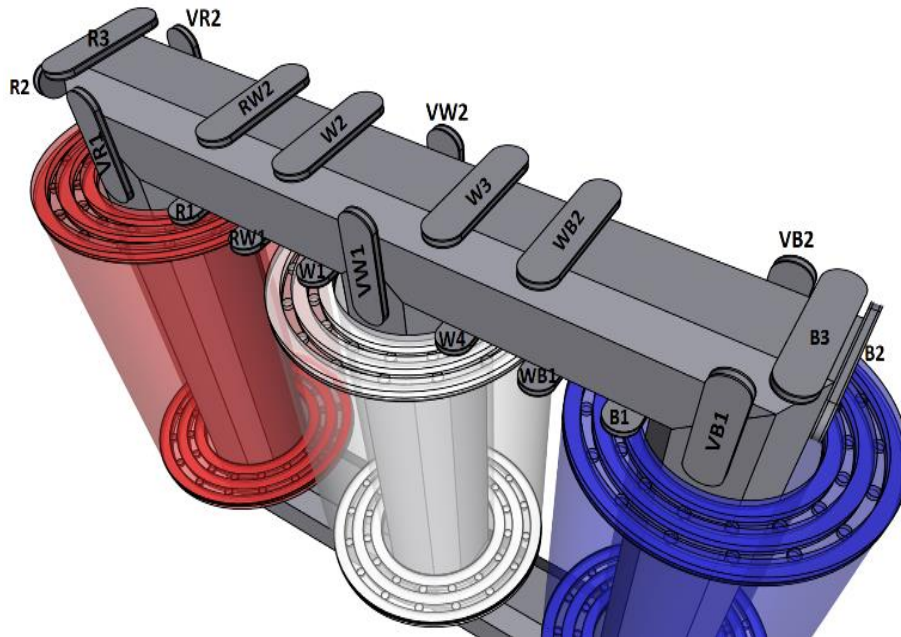


Figure 3.14 – Air Search Coil Deployment

The initial exploratory tests could be performed without a closed tank because the symmetrical 440 Vac (line) source would not result in sufficiently 'deep' core saturation levels [1] that the tank would have an effect, or be affected. To confirm this, it would be required to perform

initial tests at 440 Vac (L) making sure that no stray flux was detected near the tank wall or beyond approximately 10 mm from the top surface of the top yoke.

The search coil positions which were selected as indicated in Figure 3.14 were based on a symmetrical layout, and after observing results from other studies [7] where the stray flux is expected, as is demonstrated in Figure 2.9.

Note: The naming convention of the search coils is as follows:

- 'R', 'W' or 'B' indicates that the search coil is horizontally orientated at either the red, white, or blue phases.
- 'V' indicates that the search coil is vertically orientated e.g., 'VR' is vertically orientated at the red phase winding.
- The search coils marked with two phase colours are located centrally between two phases e.g., WB is located between the white and blue phases and RW is located between the red and white phases.

The air-search coil placed at the centre of the winding was required to measure the inception of bulk core stray flux (Figure 3.15).

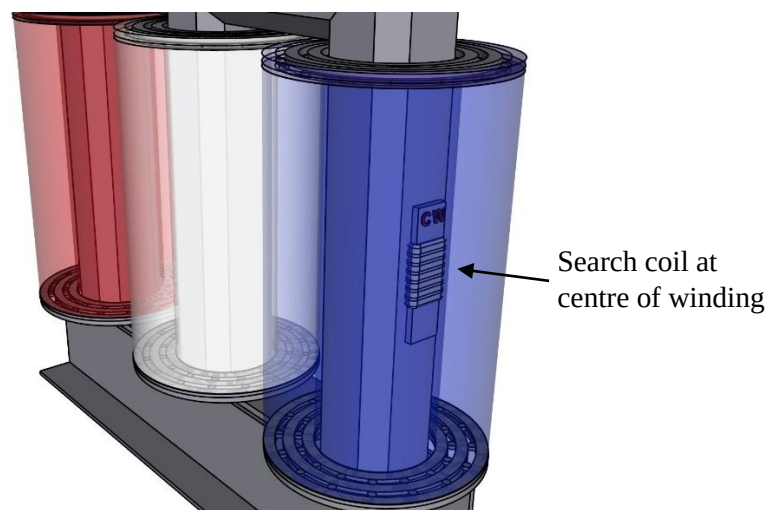


Figure 3.15 – Search coil at centre of winding

Note: For tests that result in 'deep' core saturation, the tank would be required to be closed. This is to test the effect that the tank has on a 3p3L power transformer where stray flux has exited the core and entered the tank as is expected from half-cycle saturation when dc is

injected into the circuit. Due to this, the measurement of all the SCs mounted on the core would not be practically possible, so, an initial confirmation test would be required to select the appropriate search coils to monitor for all the dc tests.

Bar search coil

A bar with 5 search coils (Figure 3.16) was proposed and constructed to measure the presence and profile of stray flux inside the winding. This is to evaluate the winding symmetry and to compare it to the stray flux inception points at the joints and other areas of the core. Each coil was wound with 60 turns of 0.315 mm round copper wire.

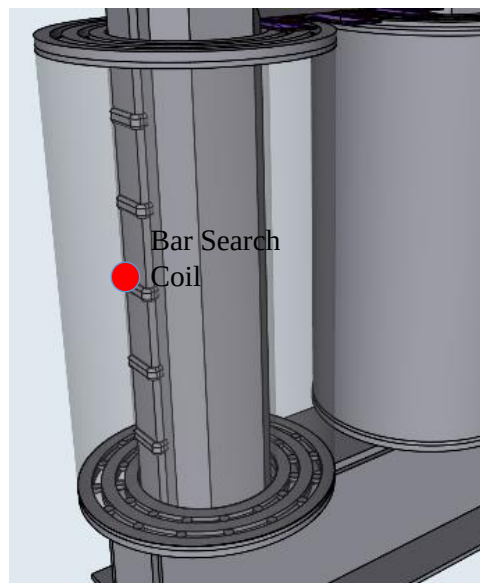


Figure 3.16 – Bar Search Coil

3.8 Temperature measurements

It was shown in literature in section 2.8.1 that measurements of the TuT tank and windings would be required in studying the effects of GICs on transformers. To achieve this the temperature probe requirements and their locations would be needed, based on literature and simulation results. It was therefore concluded that four temperature readings would be required.

Temperature probe 1 would be located after considering the areas of highest stray flux at the tank walls shown by simulation (Figure 2.9) to measure the tank heating effect from eddy currents (Figure 3.17). Temperature probe 2 would be placed in a region with low stray flux levels established during initial exploratory tests. This would be required to measure the normal convective tank heating from the winding temperature increase to ensure that ‘hot-spot’ heating from the stray flux could be differentiated from the normal convective heating of the tank. The third temperature probe would be for the winding temperature to confirm the conductive heating of the winding to validate the tank temperature changes (Figure 3.18). The fourth temperature probe would be to measure the ambient temperature to confirm that changes in ambient temperature was not the cause of any temperature changes of the transformer tank.

The resolution of the temperature probes would be 0.1°C to provide reasonable accuracy for the temperature monitoring to ensure that small changes could be recognised.

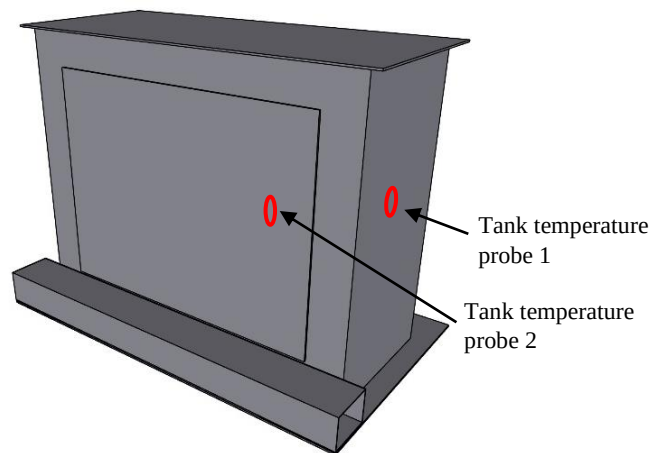


Figure 3.17 – Temperature probe placement on TuT tank

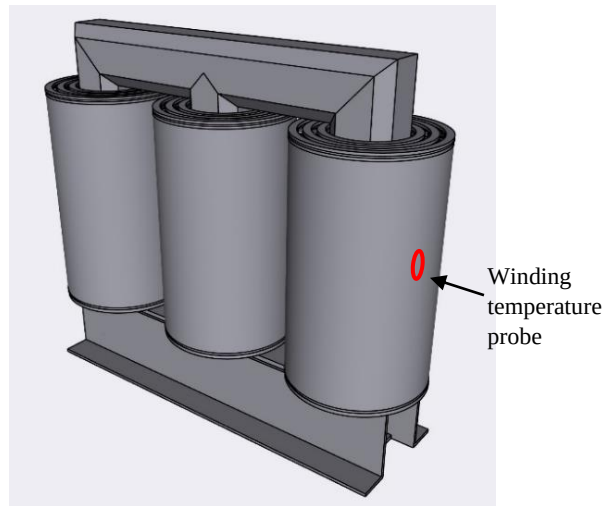


Figure 3.18 – Temperature probe placement on TuT winding

4. LABORATORY TEST PROTOCOLS

This chapter describes the ac and dc tests and the testing required to answer the research questions relevant to flux mapping of the core of a 3p3L transformer, the distortion caused when its core saturates, and the measurement of the transformer parameters that affect power systems when exposed to GIC. To this end, test protocols were developed to ensure accurate and repeatable test results using methods which have been applied by other similar studies so the results could be compared.

4.1 Symmetrical ac tests

Before any research questions could be answered concerning asymmetrical dc tests, a number of tests and observations with symmetrical ac were required:

- Performance test of the ac test source supply and test circuit (Figure 3.6).
- TuT acceptance tests, magnetisation curves, and THD curves.
- Core saturation response with ac over-excitation.
- Oscillograph signatures of air-search coils.
- Capability rating of the TuT as a baseline for future tests.

4.1.1 ac Power supply validation

As the symmetrical ac tests were performed with the TuT under no-load conditions a harmonic-free stable power supply was particularly important as described in the IEEE Std C57.12.91-2011 [46]. This test was to confirm that the ac Power supply used in the test circuit for symmetrical ac tests (Figure 3.6) met the requirements as introduced in the theory development for this project.

The measurement equipment selected was based on proven performance as experienced from these studies [1]. A Yokogawa WT1800 power analyser was used to ensure a high degree of confidence in the test measurements with high quality, accurate and dependable electrical power measurement instruments for electrical circuit power measurements up to 1000 V. The same type of instruments have been used in other studies requiring high accuracy electrical measurements such as the study on the measurement of air-core inductance by de Leòn *et al.* [73].

The test required the use of 3 elements (channels) of the WT1800 power analyser to measure the voltage, current and THD on the configured primary winding of the TuT. As the TuT has 3 windings on each phase, each was connected to the power analyser as a primary winding to test and compare their performance. The power analyser was configured using the 3-phase 4-wire (3P4W) connection method for a Y-connected system measurement.

To ensure that the specification of the power analysers exceeded the minimum requirements for this and future test circuit measurements, the basic instrument specifications in Table 4.1 were confirmed:

Table 4.1 – Instrument specification evaluation

	Test Requirement	WT1800	WT500
Voltage Range	10 to 450 V	1.5 to 1000 V	15 to 1000 V
Current Range	0.1 to 40 A (ac or dc)	10 mA-5 A or 1 to 55 A	0.5 to 40 A
Voltage Accuracy	-	±0.1%	±0.1%
THD (%)	U_{thd}, I_{thd}	$U_{thd}, I_{thd}, P_{thd}$	$U_{thd}, I_{thd}, P_{thd}$
Current Accuracy	-	±0.1%	±0.1%
Power Accuracy	-	±0.05%	±0.1%
Freq. Bandwidth	dc or 0.1 Hz to 1 kHz	dc or 0.1 Hz to 1 MHz	dc or 0.5 Hz to 100 kHz
Elements	9	6	3

It was confirmed that the cable sizing, insulation and connection materials used on the test circuit met the South African National Standards (SANS); 10142-1: Wiring of premises: Low-voltage installations [72], and 10142-2: Wiring of premises: Medium-voltage installation [74].

The parameters required to measure were the voltage (U), current (I), I_{thd} and U_{thd} on the TuT's primary winding as the supply was increased in 10-volt increments from 0 to 440 Vac (line) at no load. The purpose of the upper voltage level requirement was to study the saturation response of the TuT beyond its nominal rating, so the ac supply was required to enter the nonlinear region of the transformer's magnetisation curve.

The THD readings on the primary winding of the TuT were required to remain within the power system requirement of $\leq 5\%$ as stipulated in IEEE Std 519; 2014 [70], from 0 Vac to 420 Vac.

4.1.2 TuT acceptance tests

Prior to tests being performed on the research model power transformer (TuT), acceptance tests were required to confirm its design specification to use as a 'baseline' to verify possible future transformer damage during testing, and to confirm the initial transformer specification. Analysis of these results could also lead to possible performance specification changes to ensure that research test results would yield consistent and valid results representative of 3p3L power transformers in the field.

The testing methodology applied was based on the IEEE Standard Test Code for Dry-Type Power Transformers, IEEE C57.12.91-2011 [46], and covered;

- Winding resistance measurements.
- Winding configuration and polarity checks.
- Initial ac energization, magnetisation curve tests, and ratio tests.
- No-load loss and excitation current test i.e., open-circuit tests to obtain I_{mag} and THD.
- Load-loss and impedance tests i.e., short-circuit tests.

Table 4.2 – Measurement equipment list for open circuit tests

Quantity	DESCRIPTION
1	Yokogawa WT1800 High Precision Power Analyzer (6 Element)
1	Gossen Metrawatt Metrahit true rms Multimeter
1	Rigol DS4014 digital oscilloscope
20	130-Turn Air-Search Coils
1	250-Turn Air-Search Coil for centre of winding
1	1 Ω , 500W resistor bank i.e., for winding resistance test.

4.1.3 TuT core saturation response and defining of nominal rating

Finally, the core magnetic saturation response of the TuT with symmetrical ac could be determined, and its nominal voltage rating for all future tests. The nominal voltage rating was determined using the open-circuit results of IEEE C57.12.91-2011 [46], the core saturation response tests using search coils, and the IEC 61869-2 and IEEE Std. C57.13-2008 knee-point definitions for current transformers.

This test of the TuT core magnetic saturation response with symmetrical ac started by plotting the I_{mag} curves and identifying the knee point on the curve according to IEC 61869-2 and IEEE Std. C57.13-2008 where the core saturation departs from the linear operating region. The curve was then analysed in conjunction with the core saturation curves obtained from the search coil recordings of the stray flux surrounding the different parts of the core, to determine the optimal voltage rating of the TuT, to ensure dependable and repeatable results as a test transformer. The search coils to be used for the dc tests were then required to be identified based on the analysis of core saturation during the symmetrical ac tests.

The THD curves were also plotted concurrently to evaluate them in relation to the I_{mag} curves and core saturation.

The test equipment used for measurement in the test circuit is listed in Table 4.2.

Concurrent with the magnetisation curves, oscillographs were taken of the search coils' voltage signals using an oscilloscope. This was done to confirm the relationship between the stray flux exiting the core, the magnetisation current, and the influence of the flux path reluctance and discussed in literature [22].

4.2 Asymmetrical dc tests

As the magnetising current of a power transformer is significantly less than the magnitude of a GIC, it was determined that a GIC is likely to move the operating point of a power transformer core on the B-H curve into the saturated region of operation. These tests aimed to emulate a real-world situation by energising the model 3p3L power transformer (TuT) at its nominal voltage and then introduce a GIC-like dc current into the neutral of the transformer.

As demonstrated by Masoum and Moses [32], even with low levels of unbalance in the phases of a 3p3L transformer they could still be forced into a saturated condition, even though these transformers are widely understood to be immune to GIC when compared to other core configurations. To confirm this, a test circuit was configured to emulate different levels of balance, from 'balanced' to a relatively high-level unbalanced condition, while injecting incrementally adjusted dc current into the neutral of the TuT. Simultaneously, at each level of dc current, the level and location of stray flux surrounding the core was recorded using air search coils (SCs). This data was used to compare and analyse the core saturation response under balanced symmetrical over excitation, and saturation under unbalanced dc.

4.2.1 Power supply and test circuit validation

A number of preparatory tests and analyses were required before asymmetrical dc tests on the TuT to analyse the effect on the saturation response of a 3p3L transformer. The ac source supply for the dc tests was significantly different to the ac supply configured for the symmetrical ac tests. As a result, the performance of the new ac supply configuration was required to be validated by confirming that it was sufficiently robust with a low level of harmonic distortion so it would not affect the results of any of the asymmetrical dc tests. Once the ac supply was validated it was required to validate the dc power supply to confirm its capability.

The size and placement of the resistors to create a current unbalance in the TuT windings was previously determined theoretically but an initial test was required to confirm the current unbalance was as expected.

Due to the requirement to keep the tank closed for the dc tests, only certain fixed search coils can be monitored so initial exploratory tests were required to select the appropriate permanent search coils to monitor during the dc tests.

After these preparatory tests the dc injection tests can be performed so that the saturation response of the TuT can be determined with a high degree of confidence in the results.

The testing methodology of the tests described in this section covered the following;

- recording of the magnetisation curves of each of the transformers using the same equipment and methodology as for the symmetrical ac tests which could be used as both a performance benchmark, and for troubleshooting in case of transformer failures in the future;

- basic visual comparison of the T1, T2, and T3 transformers' magnetisation curves to observe anomalies for possible further analysis by testing and simulation;
- application of the IEEE and IEC knee points on the T1, T2, and T3 transformer magnetisation curves to compare these standard definitions to the actual specification of each transformer, and;
- test of transformer T2 (Figure 3.7) to confirm that it would not saturate when injecting dc into the circuit neutral of T2 and the TuT, and so possibly affect the results of the tests of the TuT.

ac Power supply validation for dc tests

Similar to the validation of the ac power supply for the symmetrical ac tests, this test was to confirm that the source supply would meet the requirements of the test circuit by documenting both source transformers' operating characteristics and to verify that their performance was as expected. This would prove their suitability as a robust 400 Vac, 50 Hz source supply with low levels of harmonics from other loads, or from the source transformers saturating. With this assurance, the research questions could be answered without introducing areas of doubt due to possible anomalies introduced by the power source.

Magnetisation curves and knee point definition

The ac supply tests required the use of 3 channels of the power analyser (PA) to measure the voltage, current and THD on the 415 Vac primary windings of transformers T1 and T2 (Figure 3.10). As this is an open circuit test the secondary MV windings were left disconnected.

To obtain the magnetisation curves of the transformers, the ac source supply was increased in 10 Vac increments by the variable auto-transformer from 0 Vac to 500 Vac (line), while the voltage (U), current (I), U_{thd} and I_{thd} were monitored on the PA and recorded. The PA was configured using the 3-phase 4-wire (3P4W) connection method for a Y-connected system measurement.

The IEEE knee points were identified on the magnetisation curves for T1, T2, and T3. The IEEE and IEC knee point definitions are specifically used for comparative tests of current transformers so their validity when applied to power transformers was evaluated in conjunction with this test.

Validation test to confirm that T2 does not saturate

This test was to confirm that the source transformer T2 did not saturate and introduce any extraneous harmonics into the test circuit. To do this, recordings of the primary voltage and current waveforms were required to ensure there was no evidence of wave distortion.

Note: In addition, a power analyser would be connected to monitor the primary LV side of T2 while injecting 0 to 30 Adc into the neutral of the TuT at a high unbalance of 18.7% to confirm

that there are no anomalies related to the increase in THD during the test, typical of core saturation of T2.

dc Power supply and test circuit validation

The objective of the test of the dc power supply in the test circuit (Figure 3.7) was to confirm that its voltage and current rating was sufficient to drive the TuT well into the non-linear region of its magnetisation curve and so into saturation beyond its knee point. The dc injection part of the test circuit was designed to superimpose dc magnetizing flux and ac flux in the TuT without potentially influencing the test results by introducing harmonics into the ac supply. It was therefore required to confirm that the dc supply was void of harmonics over the full range of injected dc current.

To ensure sufficient dc current capability, the required dc voltage and power capability had to be confirmed practically during the dc tests (Figure 3.7). In addition, the ac in the dc injection circuit was continually monitored to confirm whether the reactive filter components in the tests circuit i.e., inductors, were performing their function as expected.

Confirmation of the wiring capacity for the interconnections of the test circuit for dc injection was also considered at this stage.

Unbalancing resistor sizing and placement

To determine the size of the resistors required to unbalance the circuit for dc injection (Figure 3.7), dc winding resistance tests were conducted on transformer T2 according to the IEEE test code for liquid-immersed transformers[75], and for T3 (TuT) according to the IEEE test code for dry-type transformers [46]. This was to accurately determine the dc resistance of the test circuit so that the precise values of the unbalancing resistances could be calculated. A tolerance of 1% is required from the expected current, by calculation of the unbalance in (3.2). Concurrently, for record purposes and for possible circuit analysis, the winding resistances of T1 were also recorded.

4.2.2 Core saturation response of 3p3L transformer with GIC or dc

To emulate a fundamental component of a GIC condition in a 3p3L power transformer it was required to inject dc into the primary windings of each of the phases of the TuT [1]. As discussed by S. Lu and Y. Liu [76], the level of dc current in at least one of the windings must be lower than the other windings i.e. an unbalanced condition, for a GIC to have any significant impact on a 3p3L transformer.

This test was to investigate the performance of core joints as described by Borrill [1] where the significance of core joint performance was shown using a method of comparing the relative performance of butt-joints to mitred multi step-lap joints. It was postulated that the

core joint performance of multi step-lap mitred joints was superior due to their larger effective cross-sectional area, when compared to a butt-joint, and was stated that the outer limb bulk core material of a mitred four step-lap core has similar susceptibility to saturation as the outer limb joints. The centre limb yoke joints, however, were shown to be significantly more susceptible to saturation than the bulk-core material.

To confirm these statements, the test required the stray flux to be observed at both the core joints and the bulk core material at the centre of the winding, and to record the voltage of the embedded single turn winding around the centre limb. At specific levels of unbalanced dc current in the neutral of the transformer, the stray flux inception at the joints could then be compared to the stray flux inception at the centre of the winding.

Table 4.3– Measurement equipment list for dc tests

Quantity	DESCRIPTION
1	Yokogawa WT1800 High Precision Power Analyzer (6 Element)
3	Gossen Metrawatt Metrahit true rms Multimeter
1	Rigol DS4014 digital oscilloscope
1	Pintech PT-350 Current Clamp Probe (0.1 V/A conversion)
20	130-Turn Air-Search Coils
1	250-Turn Air-Search Coil for centre of winding

To perform these measurements, it was required to inject dc into the neutral of the TuT energised at nominal operating voltage while incrementally increasing the level of dc current. At each increment of dc current the stray flux was required to be recorded at specific points predetermined by careful analysis of the core saturation response during the symmetrical ac over-excitation tests, and the preliminary dc tests of the TuT described previously. This test was then repeated with the unbalance on different windings of the transformer, and then in a balanced configuration for comparative analysis. Concurrently, the I_{thd} and U_{thd} were monitored on the LV windings of T2 to ensure that it wasn't entering saturated conditions when injecting dc into the TuT.

4.2.3 THD in relation to saturation level of a transformer core with GIC or dc

A fundamental response of a transformer core saturation to GIC and dc is an increase in harmonics as discussed by Dong and Kappenman [62]. Masoum and Moses [32], Chisepo [31], and Price [26], determined by simulation that an unbalanced GIC in a transformer resulted in much higher current distortion than when the GIC is balanced. This test was the analysis of the harmonics of a 3p3L power transformer constructed using multi step-lap core joints to assess the correlation between harmonic distortion and saturation of the transformer core.

In discussing the distortion of the primary and secondary currents of transformers at increasing levels of dc bias, [1] states that other than the fundamental, the second harmonic is the largest harmonic, with the overall trend for all harmonics to increase initially with increasing dc. It was observed that the THD initially increased with a steep gradient which was attributed to the saturation of the core joints, and then as the stray flux enters other lower reluctance paths surrounding the core the rate of increase of THD reduces substantially.

This test therefore aimed to measure the harmonic levels at the primary windings of a 3p3L power transformer and evaluate the THD by comparing it to the saturation level of the core as observed by the search coils. This was done by observing the stray flux from the search coils at the identified joints and bulk core areas of the core and analysing the behaviour relative to the THD levels at the same unbalance and the same level of dc in the transformer neutral.

4.2.4 Heating, reactive power and noise response of a 3p3L transformer with GIC or dc

Heating, noise and the increase in reactive power, Q , when a transformer enters the non-linear region of operation is discussed in many studies. These symptoms of the abnormal magnetic behaviour of transformer cores caused by GIC could impact the operation of power systems, and cause possible damage impacting their recovery after a large GIC event.

These tests used the dc test circuit and required that the transformer be driven into part cycle saturation with dc injected into the neutral of the TuT. As the dc in the neutral was increased the temperature of the tank, noise levels and the reactive power was then required to be recorded for analysis.

Heating in relation to transformer core saturation in an unbalanced system

The heating of components outside of the core is a well-documented phenomena in transformers under saturated conditions exposed to GIC. In the case of a 3p3L transformer such as the TuT, the tank wall in the regions on either side of the outer limbs was determined as the appropriate place to test for increases in temperature under saturated conditions. The SCs indicated stray flux in this area during exploratory tests, albeit relatively low levels, as demonstrated by the illustration of the FEM depiction of stray flux in Figure 2.9 and in literature.

To determine the effect as described in literature, this test aimed to measure the temperature of the transformer tank at strategic locations (Figure 3.17 and Figure 3.18) and one of the windings, using laboratory temperature probes measuring to a resolution of 0.0625°C . The results were then evaluated by comparing them to the saturation level of the core as observed by the search coils and the THD level recordings.

Reactive power in relation to transformer saturation in an unbalanced system

In [31], it was discussed that with varying levels of GIC injected in a balanced transformer it produced linear Q-GIC characteristic (Figure 2.15) whereas the unbalanced case produced a non-linear Q-GIC characteristic, and the Q reactive power was significantly higher than for the balanced case.

This test therefore aimed to measure the Q reactive power at the primary windings of a 3p3L power transformer and evaluate its response by comparing it to the saturation level of the core as observed by the search coils and THD response of the transformer. This was done by observing the stray flux from the search coils at the identified joints and bulk core areas of the core and analysing the behaviour relative to the reactive power at the same unbalance and the same level of dc in the transformer neutral.

Noise in relation to transformer core saturation in an unbalanced system

Noise in transformers is an inherent characteristic, and is affected by dc in the windings, but it is well documented that it is reduced significantly in cores constructed using multi step-lap core designs. This test is to study the effect of GICs on 3p3L transformers.

Heathcote [40] discusses transformer noise in some detail describing a dominant noise level at 100 Hz which is the 2nd harmonic of the 50 Hz electrical supply. This is agreed to by Heindl *et al.* [68] where it discusses that GIC on power transformers can be observed by vibration measurements said to originate from magnetostriction 100 Hz oscillations of the core lamination sheets due to the Weiss Domain effect. In [51] there is a discussion on dc currents creating a large mmf inside the core resulting in an increase in noise levels.

Considering the literature discussing transformer noise characteristics, this test aimed to measure the noise levels of the 3p3L power transformer using an audio microphone, and then evaluate its response. This includes comparing it to the saturation level of the core as observed by the search coils, and the THD levels at specific levels of dc injection.

5. TEST RESULTS AND DISCUSSIONS

5.1 Symmetrical ac tests

The symmetrical ac tests were to define and document the specification of the TuT under balanced phase conditions. All the required tests were performed with no load except the Load-Loss and Impedance, i.e., ‘Short-Circuit’ test.

The test circuit for these tests was as described in Chapter 3 and repeated here in Figure 5.1.

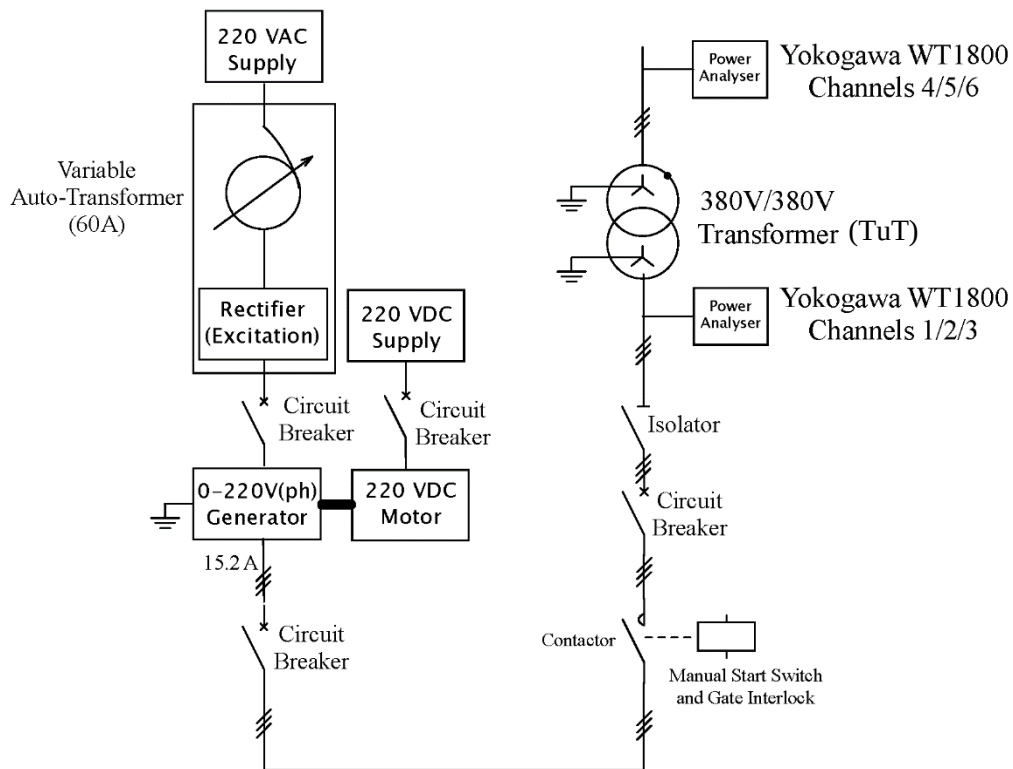


Figure 5.1 - Test circuit for symmetrical ac tests: Read from Top-Left to Top Right (Proficad)

5.1.1 ac Power supply validation

Test description

The ac power supply and test circuit required initial validation to ensure that it was sufficiently robust to ensure that the results of the symmetrical ac tests were dependable and repeatable.

Test results discussion

At initial power-up of the test circuit the source was adjusted incrementally to the maximum voltage and the U_{thd} readings on the primary winding of the TuT remained <1.6% from 70 Vac to 420 Vac.

The U_{thd} were initially shown to be erratic relative to the mean of the curve at low voltages of 0 to 200 Vac (line) (Figure 5.9). This anomaly was regarded as not representative of the actual U_{thd} after discussion with the laboratory staff. It was concluded the slightly erratic U_{thd} measurement on the generator supply was likely due to the exciter slip rings and brushes at low levels of excitation. In addition, the accuracy of the readings at these low excitation voltage levels from 0 to 200 Vac are not vital for this test as it is still at the lower linear region of the magnetisation curve of the TuT.

The results of the tests confirmed that the THD levels were acceptable by remaining well below the requirement of $\leq 5\%$ THD as stipulated in IEEE Std 519; 2014 [70], so the power supply was determined as acceptable for the purposes of the symmetrical ac tests.

5.1.2 TuT acceptance Tests

Winding resistance test

Test description

The voltmeter-ammeter method (Figure 5.2) is the most common method used for transformer winding resistance measurements. In this method, a test current is injected into the winding and corresponding voltage drop across the winding is measured. i.e., $R_x = V/I$ gives the value of resistance. It is expected that the same winding on each phase (i.e., outer, middle, or inner) of the TuT should be within 5% of each other.

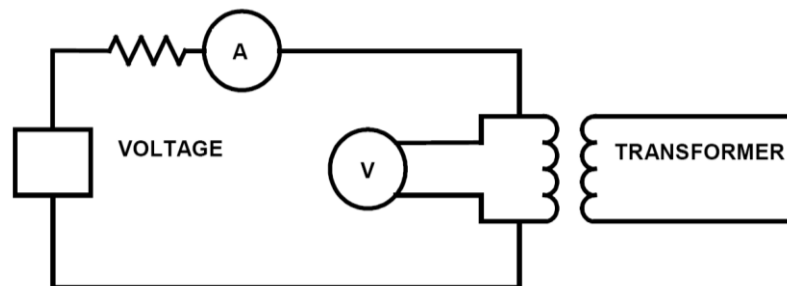


Figure 5.2 - Connections for voltmeter-ammeter method

The purpose of this test is to calculate the I^2R losses in the transformer as a benchmark for assessing possible future damage.

Test results discussion

Table 5.1 - Winding resistance

	Resistance Test								
	Phase A			Phase B			Phase C		
	mV	mA	mΩ	mV	mA	mΩ	mV	mA	mΩ
Outer 1	66.25	558.00	118.73	67.50	553.00	122.06	65.50	548.00	119.53
Middle 2	55.54	558.00	99.53	54.30	551.00	98.55	53.50	547.00	97.81
Inner 3	42.68	558.00	76.49	42.10	551.00	76.41	41.66	548.00	76.02

The inner and middle windings were within 1% of each other the outer windings were within 3%. The results were as expected and can be applied to calculate the I^2R losses in transformer, as required.

Polarity and phase relation test

Test description

Polarity and phase-relation tests were performed on the test transformer because of their bearing on paralleling or banking of two or more transformers such as 3-phase transformers. Phase-relation tests are made to determine angular displacement and relative phase sequence.

Polarity and phase relation tests are of particular interest with the test transformer because it has three windings on each phase so, to take advantage of the various possible winding configurations, knowledge of each winding's polarity and phase relation is critical.

The polarity test was performed using the 'inductive kick' method as described in IEEE C57.12.91-2011.

Test results discussion

The polarity and phase relation test shows that the windings are configured in subtractive polarity as described by the IEEE standard C57.12.91-2011, and the winding leads are connected to the transformer terminal block as shown in Table 5.2.

Table 5.2 - Polarity and phase marking

Phase a						Phase b						Phase c					
I [•]	I	M [•]	M	O [•]	O	I [•]	I	M [•]	M	O [•]	O	I [•]	I	M [•]	M	O [•]	O
Outer – O						Middle Winding – M						Inner Winding - I					

Ratio Test

Test description

The ratio test is to determine the turns ratio of the transformer. The test is performed by energizing the transformer with three phase power on a single winding of each phase and then raising the voltage in 10 Vac (line) steps to 420 Vac (line), recording the voltages of the two ‘secondary’ windings at each step.

The basic acceptance requirement is that all voltages measured at no-load on each of the secondary windings of the transformer shall be correct within 0.5% of the nameplate marking. As the test transformer is designed to have a 1:1 ratio, all results should all be within 0.5% of the supply voltage, and each other.

Test results discussion

Table 5.3 – Ratio test

	Ratio Test Outer Winding / Middle Winding								Ratio Test Outer Winding / Inner Winding							
	Vin (Outer)			Vout (Middle)			Ratio		Vin (Outer)			Vout (Inner)			Ratio	
	A	B	C	A	B	C			A	B	C	A	B	C		
1	5.073	5.099	5.11	5.059	5.109	5.117	1	0.9998	5.234	5.241	5.252	5.232	5.282	5.25	1	0.9977
2	20.77	20.78	20.82	20.75	20.79	20.84	1	1.0000	20.34	20.35	20.38	20.29	20.51	20.4	1	0.9977
3	40.19	40.21	40.23	40.19	40.21	40.22	1	1.0001	40.39	40.43	40.44	40.39	40.71	40.45	1	0.9976
4	80.88	80.9	80.91	80.89	80.89	80.91	1	1.0000	80.28	80.36	80.35	80.29	80.93	80.34	1	0.9976
5	120.9	121	120.9	120.9	120.9	120.9	1	1.0000	120.5	120.6	120.6	120.6	121.5	120.6	1	0.9977
6	160.5	160.5	160.5	160.5	160.5	160.5	1	1.0000	160.5	160.5	160.5	160.5	161.7	160.5	1	0.9977
7	200.5	200.5	200.6	200.5	200.5	200.6	1	1.0001	200.6	200.6	200.7	200.5	202	200.7	1	0.9978
8	220.5	220.6	220.8	220.5	220.5	220.8	1	1.0002	220.4	220.4	220.6	220.3	221.9	220.6	1	0.9979

No-Load loss and excitation current test

Test description

The no-load losses and excitation current are measured during the same test. The no-load losses are caused by the repeated re-orientation of the magnetic field in the core steel grain, and the magnetostrictive movement of the core i.e., hysteresis loss [77],[78]. Losses are directly proportional to the core mass and the flux density in the core.

Test results discussion

Table 5.4 - No-Load loss and excitation current

No-Load Loss of TuT						
Outer Winding Primary and Inner Winding Secondary						
* ambient temperature 22°C						
	Phase A	Phase B	Phase C	Average		
U_{rms} (line)	173.73	172.97	173.66	173.45		
I_{rms}	0.186	0.1517	0.1678	0.1685	Excitation Current (Arms)	0.1685
P	17.18	12.17	11.74	13.90	No- Load Loss (W)	41.09
S	32.31	26.24	29.14	29.23		

- No-load losses are the sum the individual phase power readings.
- Excitation current is the average of the primary currents.

Load Loss and Impedance Test

Test description

The load losses and transformer impedance are measured during the same test. The load losses are the losses caused by the phase currents in the transformer windings and the impedance of a transformer is the series impedance seen by the supply due to the mutual inductance between the primary and secondary windings. Losses are proportional to the square of the current (or current density) and directly proportional to the winding resistance.

The transformer is energised by a variable auto transformer with wire links connected across the terminals of each secondary phase. The maximum design current rating of the transformer is 30 Aac so the cross-sectional area of the links should be at least 6 mm².

- The load losses are the calculated sum of the individual phase power readings.
- The short circuit voltage is the calculated average of the primary voltages.
- The impedance p.u. is the short circuit voltage divided by the rated primary voltage and multiplied by 100 to calculate the impedance in percentage.

Test results discussion

Transformer ratings

Power (kVA): 15

Voltage (V phase): 173.2

Full Load Current (A): 28.868

Table 5.5 - Load loss and impedance test

Load Loss and Impedance Test of TuT						
Outer Winding Primary and Inner Winding Secondary						
* ambient temperature 22°C						
	Phase A	Phase B	Phase C	Average		
Urms	11.329	11.523	10.957	11.270	Impedance (%)	6.51
Irms	29.42	28.783	27.976	28.726		
P	219	217.8	199.4	212.10	Load Loss (W)	636.2
S	333.3	331.7	306.5	323.83		
PF	0.657	0.6566	0.6505	0.655		
Uthd	2.669	2.321	2.18	2.390		
Ithd	0.792	0.692	0.686	0.723		
Outer Winding Primary and Middle Winding Secondary						
Urms	9.108	9.315	8.872	9.098	Impedance (%)	5.25
Irms	29.178	28.792	28.16	28.710		
P	232.87	235.71	217.55	228.71	Load Loss (W)	686.13
S	265.74	268.21	249.84	261.26		
PF	0.8763	0.8788	0.8708	0.875		
Uthd	2.683	2.232	2.209	2.375		
Ithd	1.058	0.893	0.922	0.958		
Middle Winding Primary and Outer Winding Secondary						
Urms	9.291	9.338	8.918	9.182	Impedance (%)	5.30
Irms	29.704	29.148	28.332	29.061		
P	242.6	239.4	220.16	234.05	Load Loss (W)	702.16
S	276	272.2	252.66	266.95		
PF	0.879	0.8793	0.8714	0.877		
Uthd	2.731	2.279	2.176	2.395		
Ithd	1.066	0.906	0.878	0.950		
Middle Winding Primary and Inner Winding Secondary						
Urms	7.751	7.778	7.269	7.599	Impedance (%)	4.39
Irms	29.692	28.87	27.759	28.774		
P	204.34	197.79	177.9	193.34	Load Loss (W)	580.03
S	230.13	224.54	201.78	218.82		
PF	0.8879	0.8809	0.8816	0.883		
Uthd	2.668	2.255	2.03	2.318		
Ithd	1.115	0.919	0.855	0.963		

Innner Winding Primary and Outer Winding Secondary						
Urms	11.471	11.477	10.912	11.287	Impedance (%)	6.52
Irms	29.947	28.97	28.044	28.987		
P	226.7	218.3	198.7	214.57	Load Loss (W)	643.7
S	343.5	332.5	306	327.33		
PF	0.6599	0.6566	0.6494	0.655		
Uthd	2.723	2.197	2.078	2.333		
Ithd	0.791	0.643	0.636	0.690		
Innner Winding Primary and Middle Winding Secondary						
Urms	7.749	7.794	7.222	7.588	Impedance (%)	4.38
Irms	29.889	28.83	27.702	28.807		
P	205.7	197.79	176.31	193.27	Load Loss (W)	579.8
S	231.61	224.7	200.06	218.79		
PF	0.8881	0.8802	0.8813	0.883		
Uthd	2.759	2.256	2.191	2.402		
Ithd	1.14	0.867	0.904	0.970		
Middle Winding Primary with Inner and Outer Configured in Parallel						
Urms	6.023	6.33	6.0634	6.139	Impedance (%)	3.54
Irms	28.546	28.925	28.46	28.644		
P	164.15	174.75	164.12	167.67	Load Loss (W)	503.02
S	171.92	183.11	172.56	175.86		
PF	0.9548	0.9543	0.951	0.953		
Uthd	2.645	2.159	2.026	2.277		
Ithd	1.345	1.057	1.015	1.139		
Middle Winding Primary with Inner and Outer Configured in Series						
Urms	5.9366	5.7182	5.4475	5.701	Impedance (%)	3.29
Irms	30.497	28.983	28.429	29.303		
P	171.65	156.2	146.18	158.01	Load Loss (W)	474.03
S	181.05	165.73	154.87	167.22		
PF	0.9481	0.9425	0.9439	0.945		
Uthd	2.668	2.226	2.119	2.338		
Ithd	1.293	1.043	1.023	1.120		

5.1.3 TuT core saturation response and defining of nominal rating

Magnetisation curves

Test description

Finally, after the power supply validation and acceptance tests, the no-load test to obtain the core magnetic saturation response of the TuT with symmetrical ac could be determined, and its nominal voltage rating for all future tests.

For this test, each winding of each phase of the TuT was consecutively configured as the primary winding, and its test was performed while the other two windings were open circuit.

The test circuit for symmetrical ac over-excitation tests was used for this test (Figure 5.1). The source voltage and the magnetisation current of the TuT were measured and manually recorded using a Yokogawa WT1800 power analyser as the source voltage was increased in 10 V increments from 0 to 440 Vac.

Test results discussion

The magnetisation curves of the TuT obtained from the tests (Figure 5.4, Figure 5.5, Figure 5.6, and Figure 5.7) follow the typical profile of M5 CRGO laminated core 3p3L transformers. It is interesting to note that, as described in Figure 5.3 and literature [22], the magnetisation current of the two outer limbs, Phase a and c are identical in the linear operating region of the magnetisation curve, with slightly higher excitation current than Phase c.

The knee points identified on Figure 5.4, Figure 5.5, and Figure 5.6 are according to the IEC and IEEE definitions applied to current transformers. The slight imperfections between windings were noted as shown by the knee points which differed slightly between the three windings on each of the phases. The result was 302 Vac for the inner winding, 300.5 Vac for the middle winding, and 297 Vac for the outer winding using the IEC 61869-2 definition of the knee point. This correlated with the IEEE definition where the inner winding indicated the highest and the outer winding, the lowest.

Note: The IEC 61869-2 and IEEE Std. C57.13-2008 knee-point definitions are discussed and their applicability to power transformers is established in the validation of the dc test circuit power supply in section 5.2.

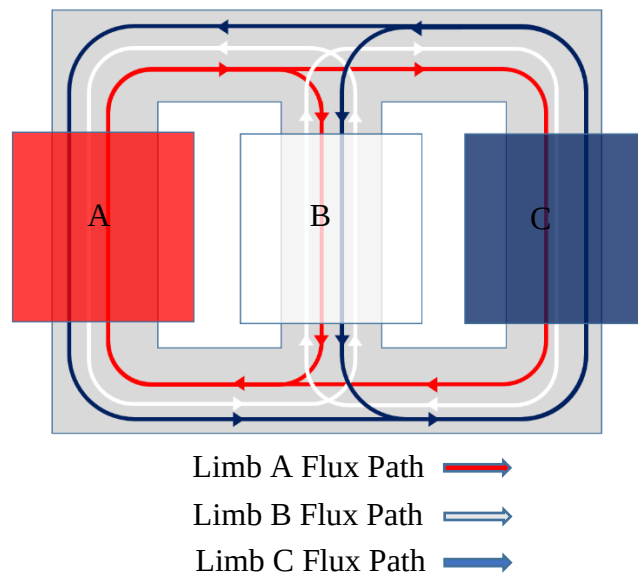


Figure 5.3 - Core Flux Flow Direction (One Half-Wave)

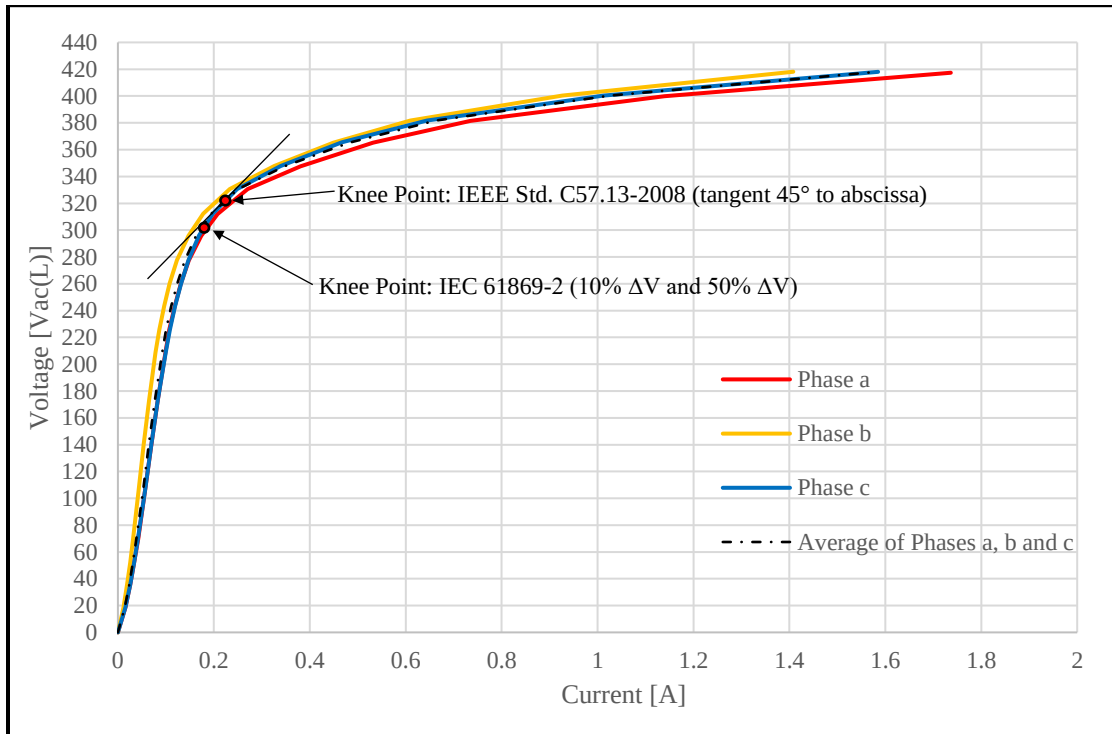


Figure 5.4 - Inner Winding Magnetisation Curve

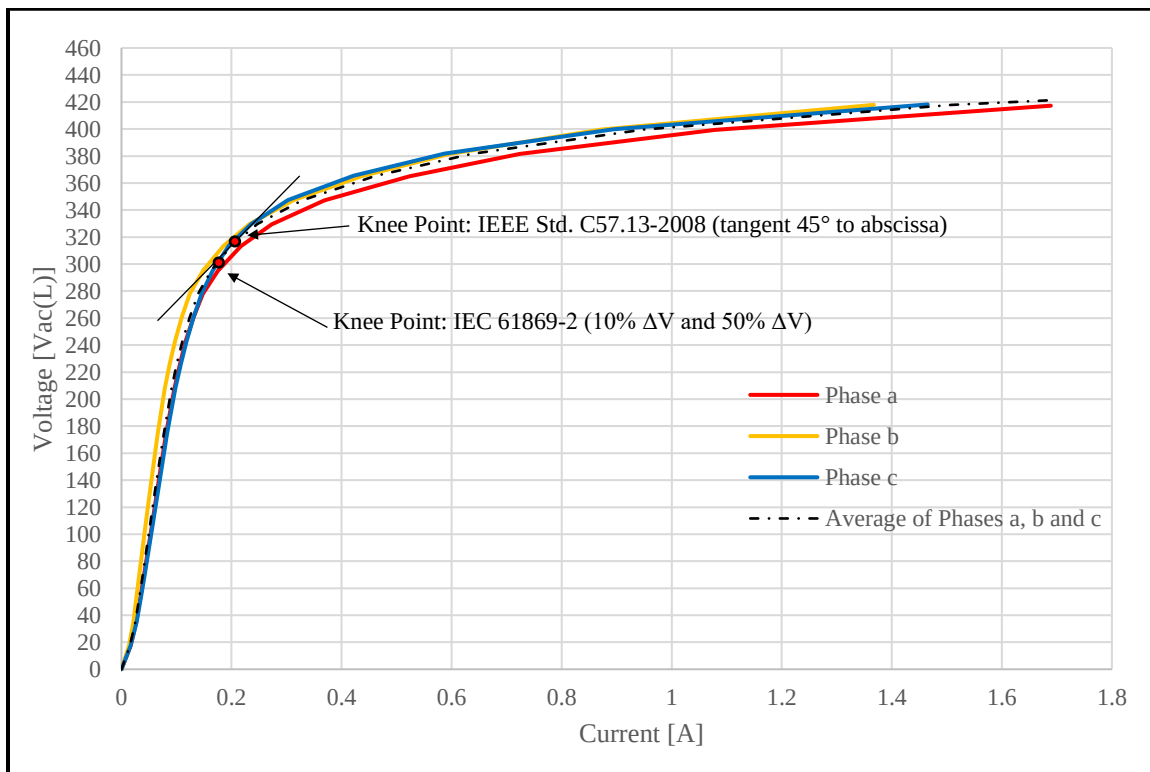


Figure 5.5 - Middle Winding Magnetisation Curve

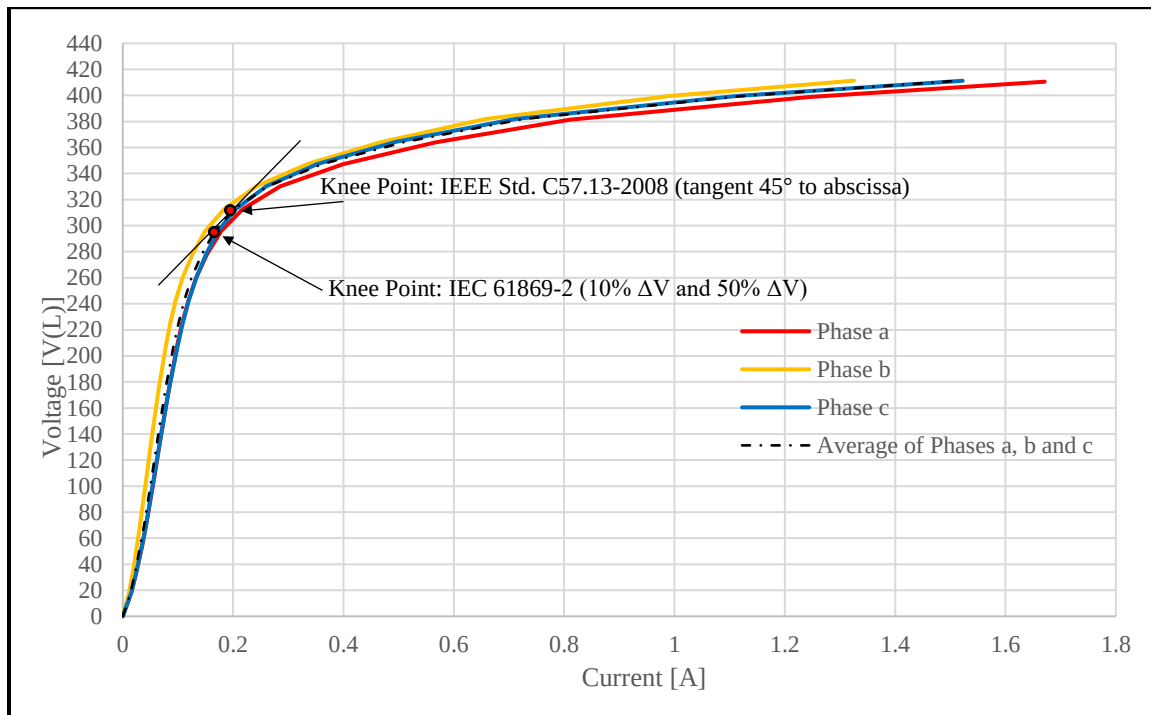


Figure 5.6 - Outer Winding Magnetisation Curve

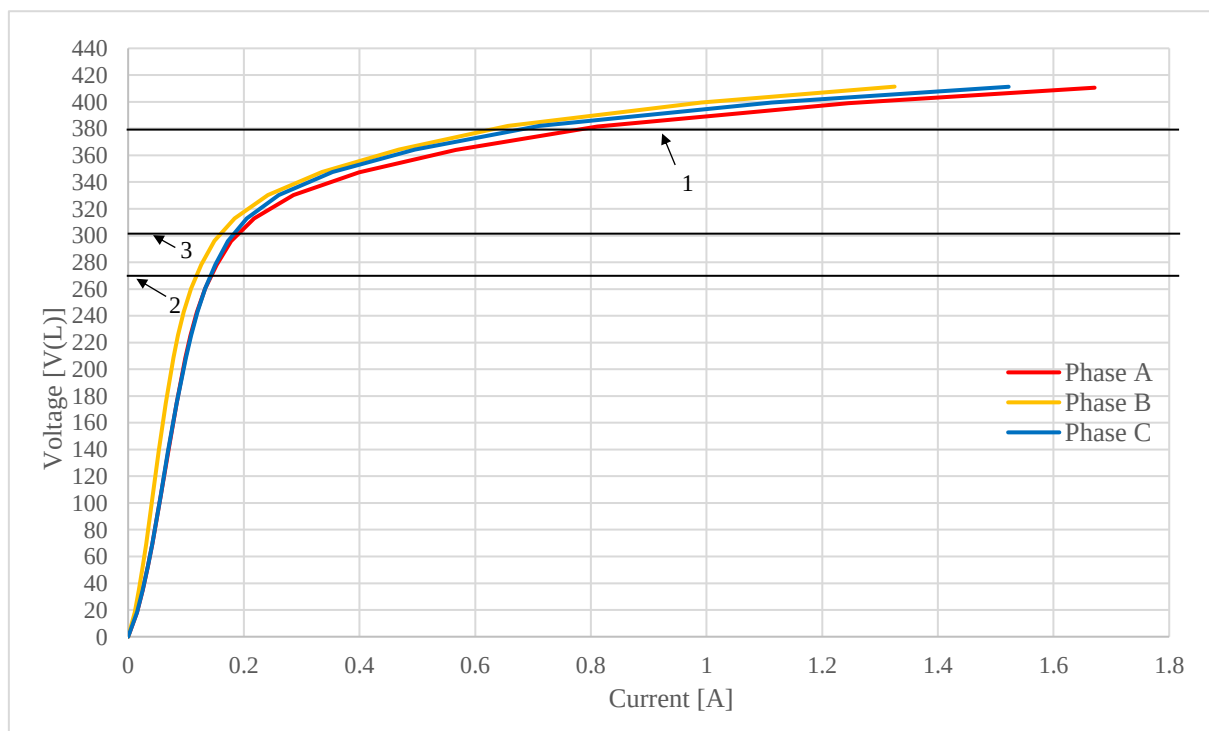


Figure 5.7 - Transformer Magnetisation Curve

As shown on the horizontal line at 270 V (point '2') the start of divergence of the outer limb curves for phases A and C was observed. This is one of the indicators that the core is entering the non-linear region of operation. The divergence continued in magnitude at the 300 Vac

(point '3'), and beyond, so it could be stated that the transformer had entered the region of non-linear operation close to the maximum limits of CRGO (typically 2.03 T) [1]. From Figure 5.7 it can be seen that the TuT is well into the non-linear operating region at the nameplate rating which should not be the case at a transformer's nominal rating, i.e., 380 V (point '1'), and so needed to be investigated.

Harmonic distortion

Test description

As a 'benchmark' for future dc tests, concurrent with the magnetisation curve tests, the harmonic distortion was recorded to determine the core saturation response relative to the level of harmonics generated as the core moves from partial to 'deep' core saturation.

Test results discussion

No-load losses are based on sine wave voltage excitation but the non-linearity of the transformer core as it saturates introduces harmonics into the excitation current resulting in distorted excitation voltage and flux waveforms. The level of voltage waveform distortion is determined by the output impedance of the voltage source and the level and harmonics of the excitation current.

As shown in Figure 5.8 and Figure 5.9 the harmonic distortion increases significantly as the excitation voltage exceeds the rated voltage of the TuT and the core moves from partial saturation into higher levels of core saturation as the excitation current increases in the non-linear part of the excitation current curve. Figure 5.7 describes the flux paths by its visible changes similar to the harmonics changes with stray flux described in Figure 2.13. This shows a distinct change at the 300 Vac rating of the transformer as the core reaches its knee point and partial core saturation.

Figure 5.8 and Figure 5.9 show that the U_{thd} and the I_{thd} of phases B and C have very similar curve traces, and the traces of phase A are outliers in both the U_{thd} and the I_{thd} graphs. This demonstrates the small differences between the core and windings of the three phases of the transformer due to the imperfections in the manufacturing process, which is likely to be present in all commercial power transformers. There is harmonic 'noise' evident at 0 to 200 Vac therefore the U_{thd} readings appear to be erratic but these harmonics are generally introduced from other non-linear devices close to the source circuit, or possibly from the generator exciter slip rings and brushes. The actual harmonics are likely to be 'normalised' around the mean of the curve so the noise is disregarded in this case.

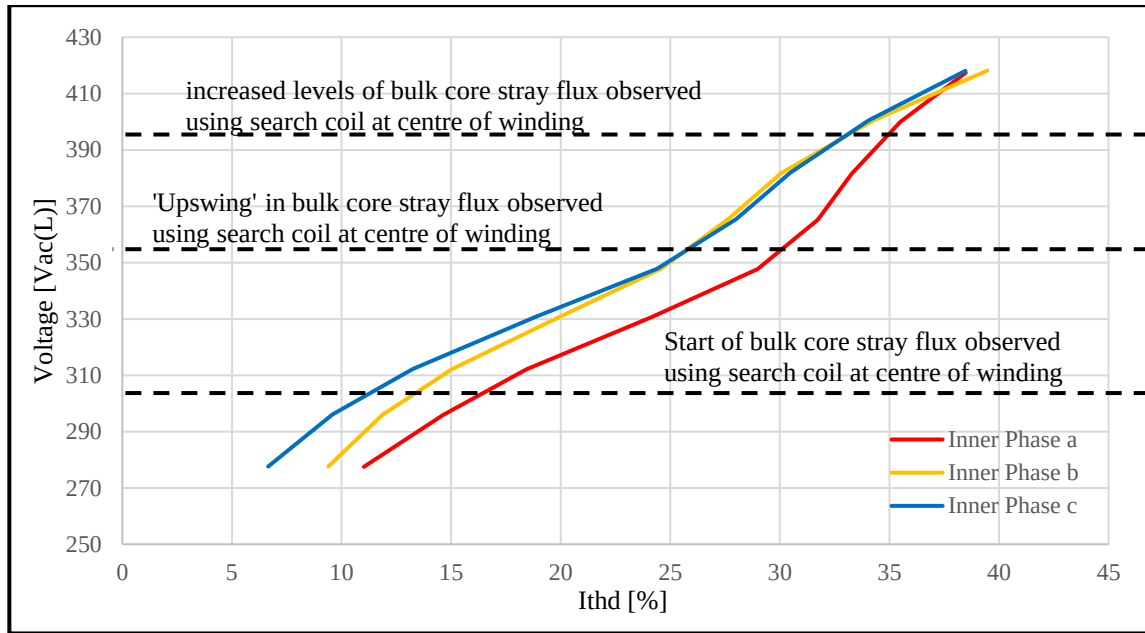


Figure 5.8 - Inner Winding I_{thd}

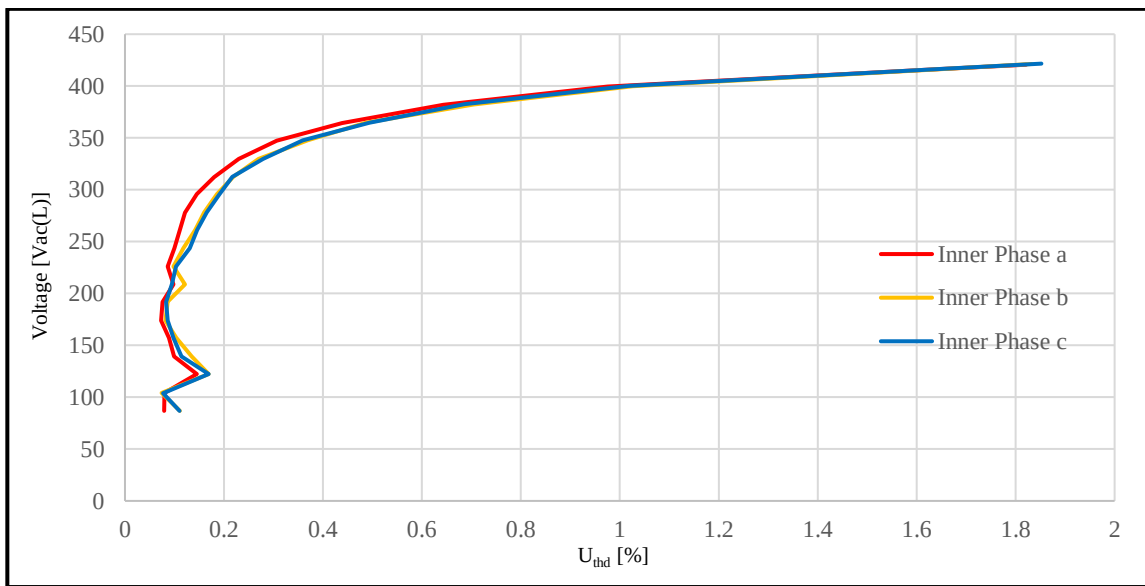


Figure 5.9 - Inner Winding U_{thd}

Search coil readings

Test description

Concurrent with the magnetisation curve tests, the core saturation response was determined so that it could be evaluated in conjunction with the I_{mag} curves, THD, and knee point analysis to ultimately determine the rating of the TuT.

The stray flux recordings were performed with the TuT under no-load conditions while the relative magnitude of the stray flux was recorded at strategic locations on the transformer core.

The test was performed by stepping the source voltage in 10 V increments from 0 to 430 V (line). The WT1800 power analyser was required to monitor the primary windings' voltage and current, and the multimeter set on rms volts to measure the SC voltage at each voltage step. The values were manually recorded at each step. The objective was to determine the stray flux inception points at each specific core location and each voltage step, and the relative magnitude of the stray flux exiting the joints and bulk core material regions. This core saturation 'map' of the symmetrical core saturation response could then be used to ensure that the appropriate core locations were monitored during future dc injection tests.

Test results discussion

The stray flux inception points can be observed in Figure 5.10 where there was evidence of stray flux as low as 220 Vac (line). These levels could be partially attributed to the fringing flux phenomena on the surface of the core [1] and [34].

Figure 5.10 shows that the SC at the mid-point of the Phase A winding registers stray flux leakage at 320 V indicating that the bulk core material appears to reach the defined level of saturation well after the inception of stray flux at the joint areas (Figure 5.10 and Table 5.6 – Centre Winding), whereas the SCs at the corners of the core windows R1, W1, W4, and B1 (Figure 3.14) show evidence of stray flux at a significantly lower voltages.

The transformer emf equation (5.1) computes a U_{rms} of 320.9 V.

$$\begin{aligned}
 U_{rms} &= B_m \times 4.44 \times N \times f \times A \times \sqrt{3} \\
 U_{rms} &= 1.703 \times 4.44 \times 140 \times 50 \times 0.0035 \times \sqrt{3} \\
 &= 320.9 \text{ V}
 \end{aligned}
 \tag{5.1}$$

U_{rms} : emf

B_m : magnetic flux density

N : number of winding turns

f : frequency

A : core cross-sectional area

The transformer emf had approached the ideal value as defined by (5.1) and was close to this value in the bulk-core material regions of the core. The joints appear to have 'compromised' the real capability of the transformer further due to the typical air gap anomalies inherent in a multi step-lap joint structure, reducing the effective cross-sectional area of the core [1].

From evidence using the stray flux recordings (Figure 5.10), the majority of the joint areas start showing evidence of flux outside of the core at around 300 V with two outliers at 220 Vac. It can therefore be concluded that the transformer voltage de-rating should not be at the region between 220 Vac and 270 Vac at the first evidence of flux outside the joints that is

attributed to fringing flux, but only where there is a clear ‘kick-up’ of stray flux at the corner joints at 300 Vac. This precisely correlates with the IEC definition of the knee point on the magnetisation curve (Figure 5.4, Figure 5.5, and Figure 5.6). At this point, there was still no evidence of stray flux in the bulk core material in the winding.

It was also observed that there was evidence of consistent symmetrical stray flux patterns (Figure 5.10) observed in the TuT. It was interesting to note that previous transformer studies using model transformers showed core asymmetry that was attributed to core stacking holes. This demonstrates the effect of ‘air gaps’ in cores. The new TuT was specifically designed with no stacking holes for this reason, so a symmetrical flux distribution pattern was expected, and observed during tests.

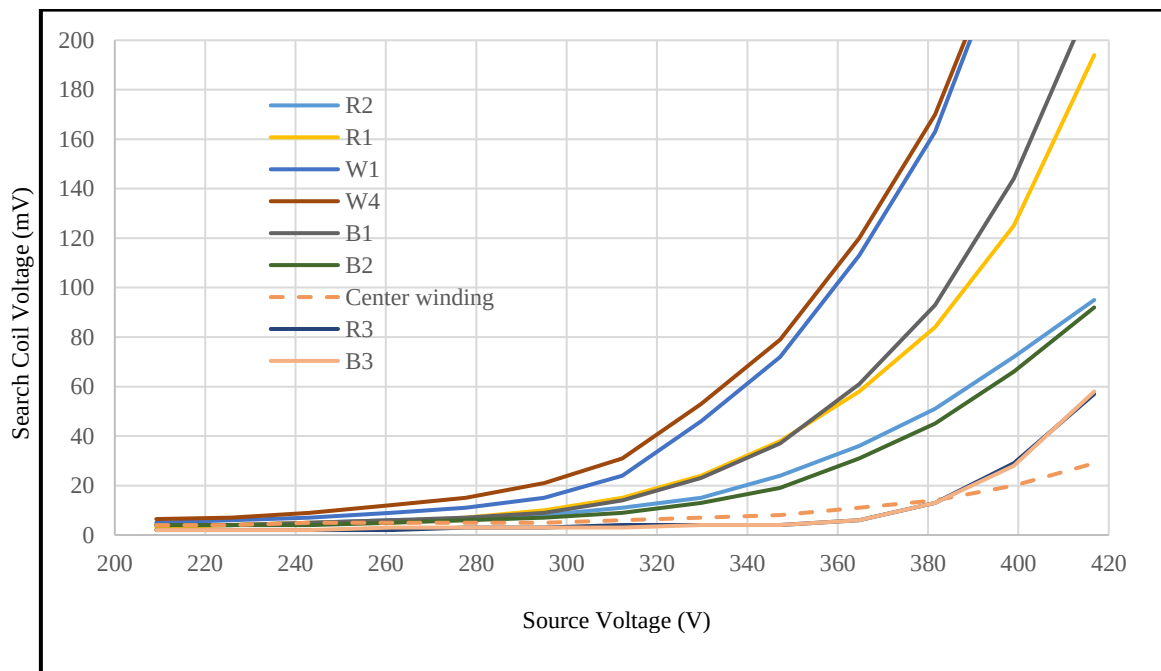


Figure 5.10 - Stray flux recordings of SCs at regions with highest magnitude

The stray flux traces (Figure 5.10) show three distinct regions of stray flux, the inside corners next to the centre limb, W1 and W4, (Figure 3.14), corroborated by simulation (Figure 2.9) [7], then the inside corners next to the outer limbs, R1 and B1. In comparison, all the other areas are grouped in relatively low-level stray flux regions. This low-level region can then be further subdivided into two groups, the corners on the outer limbs, R2, R3, B2 and B3, and the top of the yoke directly above the centre limb, W2 and W3. The rest are considered low-level regions similar to that experienced in the bulk core material away from any of the mitred joints.

Figure 5.11 shows the regions of inception in more detail with specific explanations and the precise voltages where the inception of stray flux was noted at the different parts of the core.

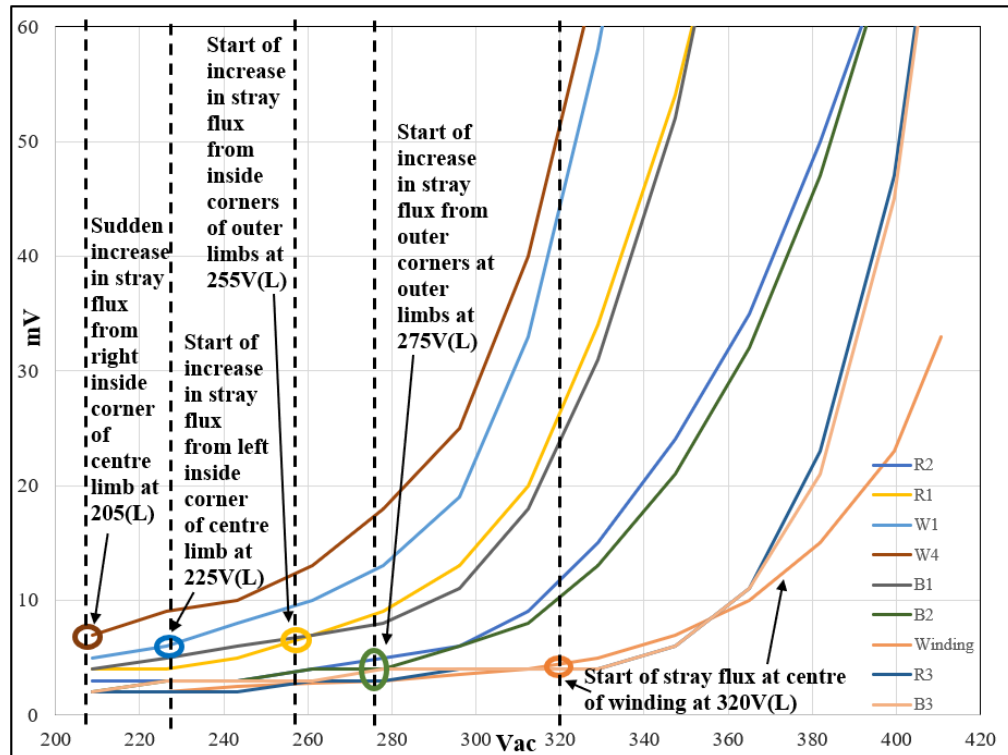


Figure 5.11 –Search coil recordings with explanation on when stray flux became evident on the different parts of the core

Table 5.6 - Search Coil Stray Flux Point of Inception

	Phase Voltage (L)	Search Coil Voltage (mV)	Search Coil ID
1	226.29	9	W4
2	243.2	8	W1
3	278.03	8	B1
4	278.03	9	R1
5	312.32	9	R2
6	312.32	9	VW1
7	312.32	8	VW2
8	312.32	8	B2
9	322.73	9	Centre Winding
10	329.11	10	VR2
11	329.11	9	VB1
12	329.11	8	VB2
13	329.11	7	VR1
14	365.03	11	R3
15	365.03	11	B3
16	365.03	11	W2
17	365.03	10	RW2
18	365.03	10	W3
19	365.03	8	WB2

Borrill, Chisepo and Gaunt [38] show that core joints distort flux patterns in transformer cores, including those with mitred multi step-lap joints. Non-symmetrical flux distribution is caused by any core anomalies that cause high reluctance flux paths due to the same phenomenon observed at the mitred corner joints. The magnetic flux distorts at the air gap finding alternative paths, including through the air outside of the core. The effective cross-sectional area of the core is reduced and so the maximum flux is also reduced.

Reluctance, R_m is;

$$R_m = \frac{MPL}{\mu_r \mu_o A_c} \quad (5.2)$$

MPL : Magnetic Path Length, cm

A_c : Cross Section of core, cm²

μ_r : Permeability of Magnetic Material

μ_o : Permeability of air

The bar search coil traces (Figure 5.12) shows a similar response to Figure 5.10 where there are indications of flux exiting the bulk core material at the centre of the winding, and increasing, at about 320 Vac.

The reading at bar search coil 3 positioned at the centre of the winding indicates that the flux inception occurs before the other 4 bar coils, followed by the position which correlates to coils 2 and 4, which are identical in amplitude i.e., superimposed on each other, and then by coils 1 and 5 which are also identical in amplitude. This demonstrates the symmetry of the flux at the centre of the winding.

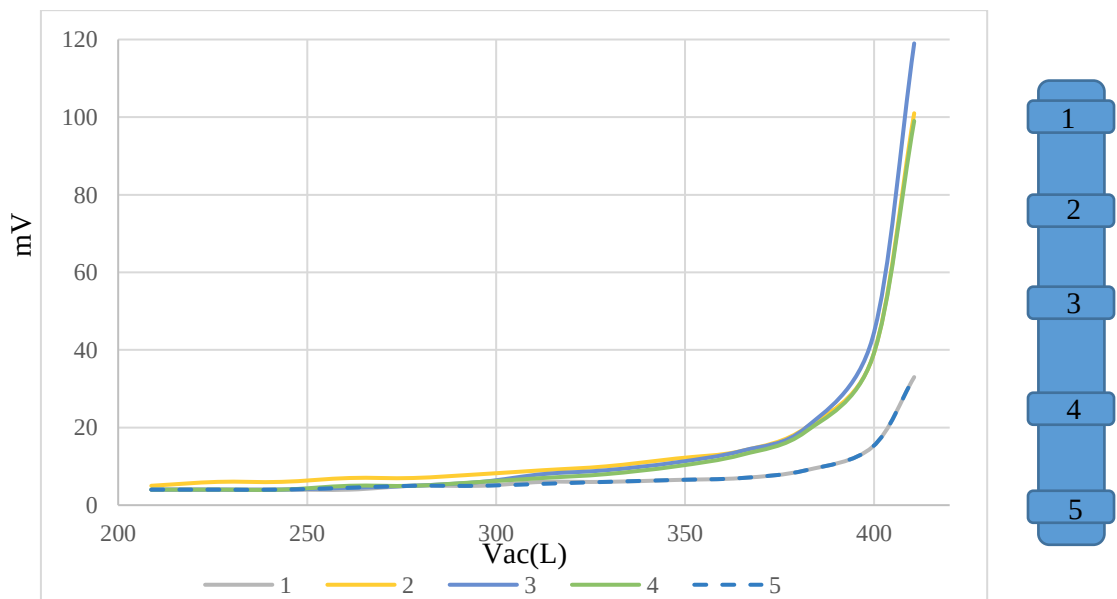


Figure 5.12 - Bar Search Coil Readings inside Winding (Phase a)

Search coils consisting of a single turn of enamelled copper wire were wound around the upper limb at the centre of the ‘windows’ between the red and white phases, and the white and blue phases.

The magnetisation curve (Figure 5.13) from the single turn search coil shows a linear curve trace with no indication that the bulk core material had reached ‘deep’ saturation. Non-linearity of this curve is expected when a dc bias is applied and half wave ‘deep’ saturated conditions are expected.

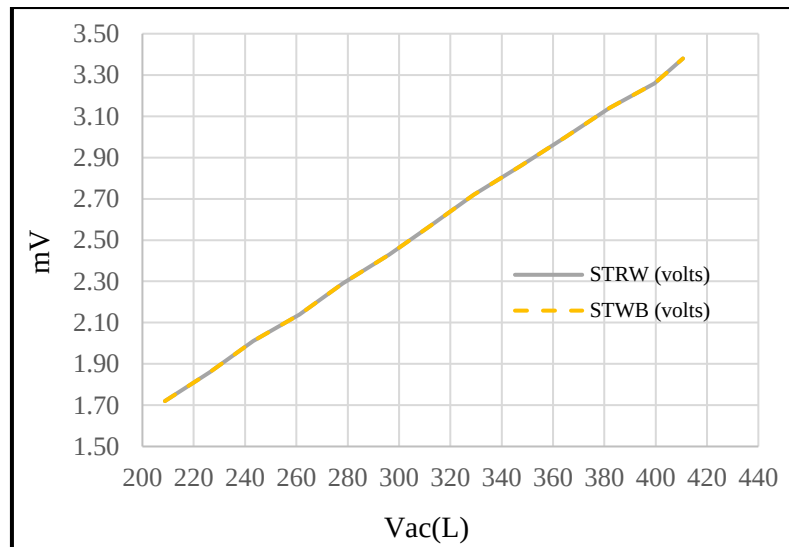


Figure 5.13 - Single Turn Readings (Centralized on windows around upper yoke)

Oscillographs of air search coils

Test description

Concurrent with the magnetisation curve tests, the dynamic interaction of varying flux densities between the bulk core material and the joint areas, oscillographs of the stray flux and the instantaneous magnetisation current were taken. This was to further understand the saturation behaviour of the transformer core joints as shown in Figure 5.10.

This was a no-load test to record the instantaneous magnitude of the stray flux of SCs R1, W1, W4, and B1 on the transformer core with the source voltage at 420 V, or maximum possible excitation. The WT1800 power analyser was required to store the primary winding input voltage and current waveforms, and an oscilloscope set to store the search coil waveforms, both set at 32 020 samples per second to place both waveforms on the same time base. The objective was to analyse the instantaneous level of magnetisation currents in the three limbs and compare them to the stray flux waveforms recorded by the oscilloscope.

Test results discussion

Instantaneous current is a function of the rate of change of the flux as shown by the formula:

$$e = Ri + \frac{\Delta\Phi}{\Delta t}$$

$$\therefore i = \frac{e}{R} + \frac{\Delta\Phi}{R\Delta t}$$

In support of this, points 1 to 13 of the search coil oscillographs in Figure 5.14 demonstrate that the peak stray flux presence and, therefore, the peak flux density is at the highest rate of change (slope) of the magnetisation current.

Peaks 1, 6, 7, and 12, are the stray flux peaks related to the magnetisation current induced from the winding of the centre limb recorded by search coils W1 and W4, also shown in Figure 5.15 as point '1'.

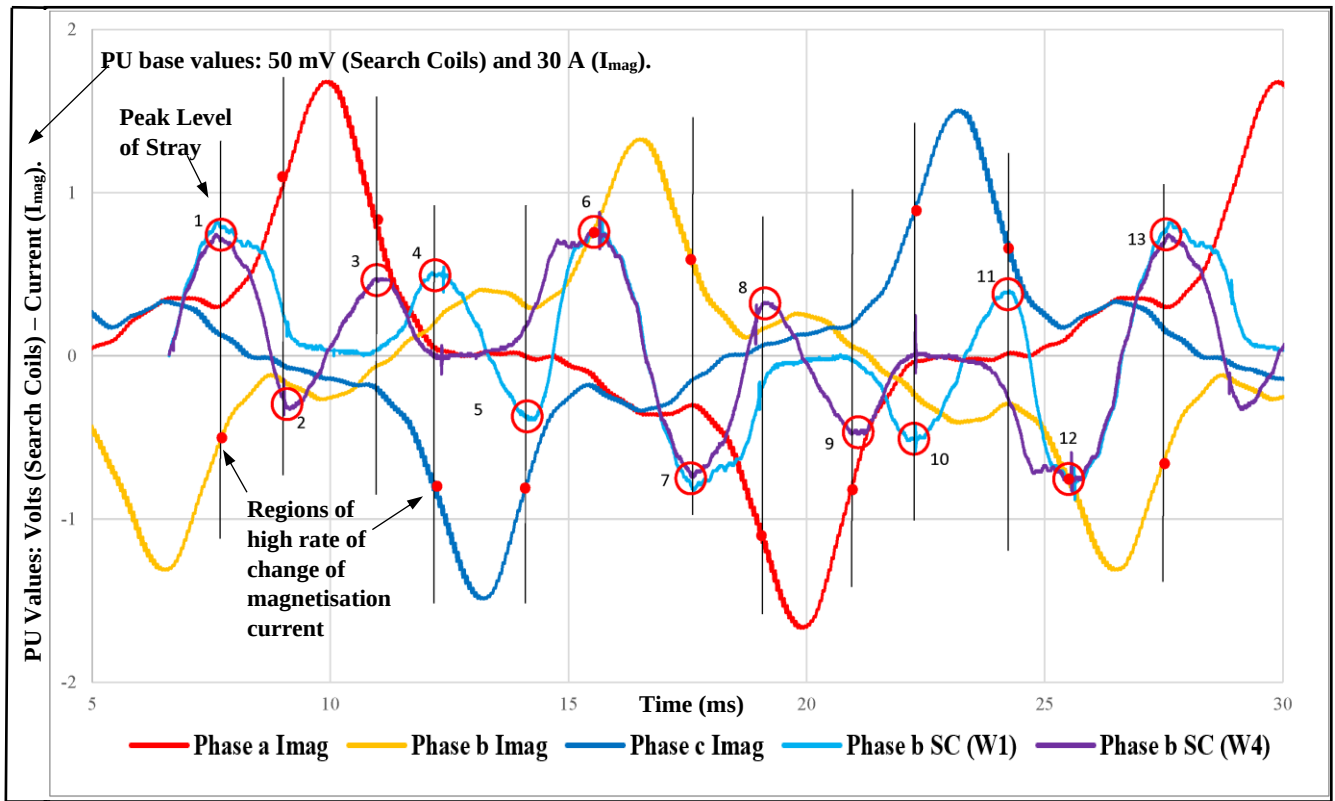


Figure 5.14 - Oscillographs of the Search Coils W1 and W4 and the I_{mag} of Phases a, b and c

Point 1 on Figure 5.15 demonstrates the regions of stray flux which are related to the phase b I_{mag} where search coils W1 and W4 are mounted i.e., centre limb, whereas the regions which are influenced by the interaction of flux from phases a and c, is visible in Point 2.

The uneven reluctance paths of phases a and c (Figure 5.3) do not provide even opposing flux at phase c, and so there is evidence of flux from those phases at the centre limb, Phase b (point

‘2’ on Figure 5.15). This amalgamation of flux in the centre limb is also seen from the higher levels of stray flux measured at W1 and W4.

The oscillographs of the search coils highlights the complex interaction which exists at the centre limb between all three phases of the transformer.

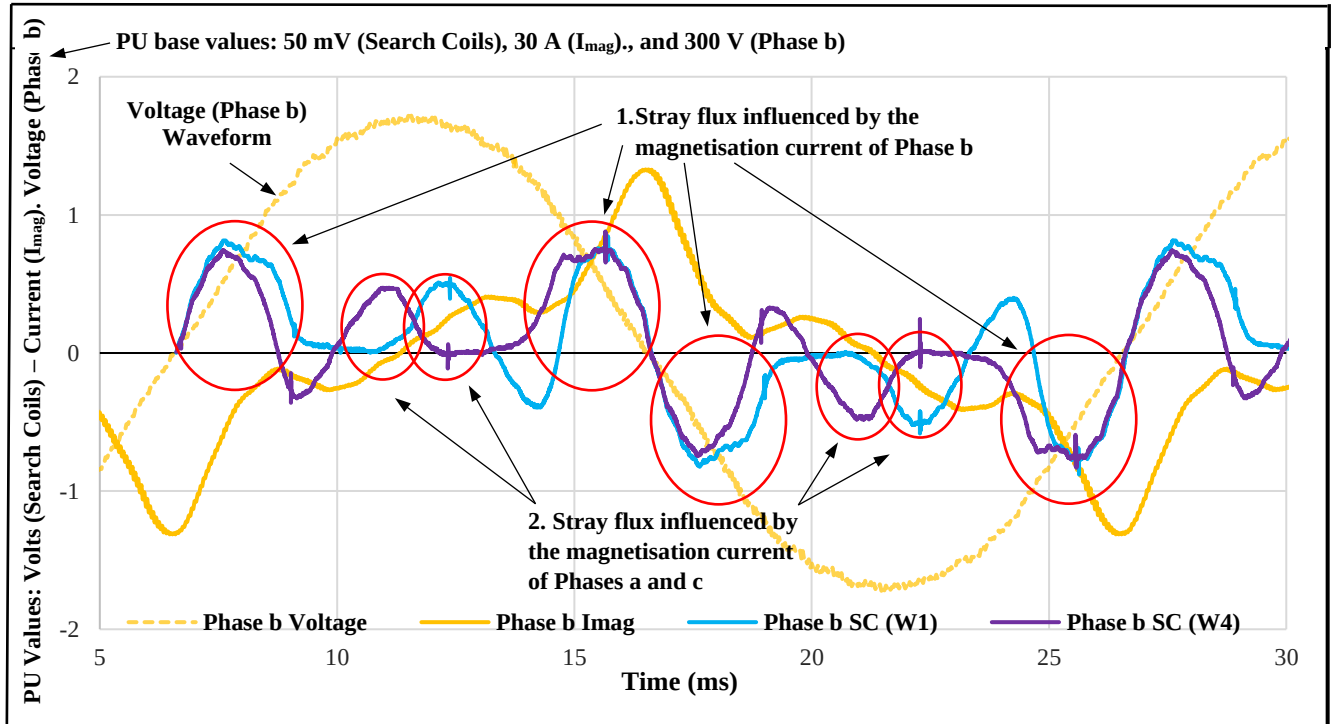


Figure 5.15 - Oscillographs of the Search Coils W1 and W4 with Phase b Voltage and I_{mag}

In conclusion, demonstrated by these tests, as described below, core saturation of a transformer core is related to the nominal operating flux density as identified on its magnetising current (I_{mag}) curve. When the excitation current exceeds the nominal operating point, the transformer enters partial core saturation and stray flux is detected outside of the core. This demonstrates the limits of the transformer's performance i.e., its rating.

The results of these tests highlighted the following:

Magnetisation current: It was noted that the magnetisation current of the outer limbs, Phase a and c, are identical in the linear operating region of the magnetisation curve but diverge in the less controlled and less defined non-linear core saturation region of operation. The centre limb, Phase b, in contrast, has a lower magnetisation current than the outer two limbs due to the inherent lower reluctance path of the centre phase.

Harmonic distortion: Harmonic distortion increases significantly as the excitation voltage exceeds the rated voltage of the TuT and the core moves from partial saturation into 'deep' core saturation. A distinct change is noted at the 300 Vac rating of the transformer as the core reaches its knee point and enters partial core saturation.

Stray flux density measurement: The TuT had a symmetrical stray flux distribution, and by inference, flux distribution and flow patterns. This balanced symmetrical flux distribution is due to a well-balanced core and therefore, balanced reluctance paths for the magnetic flux.

It was shown that the stray flux, and related level of saturation, was highest in the centre limb joints followed by the corner joints. The bar search coil traces showed that the flux starts exiting the bulk core material at the centre of the winding at 320 V.

Oscillographs of air search coils: The oscilloscope readings were taken to observe when stray flux exits the core, and the instantaneous level of magnetisation current.

The oscillograph measurements of the search coils showed a distorted flux pattern at the centre limb joints due to the amalgamation of flux from phases a and c in the phase b centre limb, whereas the stray flux from phases a and c was clear of the congested flux observed in the centre limb.

Nominal rating: The results of the core saturation tests showed that the search coil at the mid-point of the Phase A winding registered stray flux leakage at 320 Vac and at the joints there is evidence of significant stray flux at around 300 Vac. The start of low levels of flux outside of the core below 300 Vac was attributed to fringing flux and there was a distinct increase in flux outside of the core, or stray flux, at the corner joints at 300 Vac. This correlated with the IEC definition of the knee point on the magnetisation curve. At this point, there was still no evidence of stray flux in the bulk core material in the winding.

This led to a reasonable rating of 300 Vac to ensure reliable a repeatable future testing.

Notes:

- The dependability of the IEC knee point definition needed to be corroborated on other transformers (section 5.2) before any conclusion could be made as to its useful applicability to power transformers.
- The nominal current rating of a 15 kVA transformer is 22.8 A ($15\text{kVA}/380\text{V}/1.732$), which is well within the current rating of the wound copper. At a de-rated voltage of 300 Vac, the current rating is 28.8 A ($15\text{kVA}/300\text{Vac}/1.732$) and since the copper is conservatively rated for 58 A the conclusion can be made that it is not required to de-rate the power of the TuT.

5.2 Asymmetrical dc tests

This test is to confirm the core saturation response to an unbalanced condition and to evaluate the results of the effect. This was done for normal unbalances found on transmission networks from ‘balanced’ to 5% unbalance. In addition, tests at more than double the normal unbalance on transmission networks were performed to evaluate and compare the effect under high-level unbalance conditions with dc or GIC. Even though unbalanced phase voltage, or voltage ‘sag’, is present as a GIC effect this study was based on a firm voltage supply and studying the primary conditions highlighted in the literature study including current unbalance, heating, harmonic generation, noise vibration and increased reactive power demand.

5.2.1 Power supply and test circuit validation

ac Power supply validation for dc tests

Prior to the tests of the TuT it was required to test the components of the source power supply to confirm that the ac supply for the TuT test was robust and performed according to the intent of the test protocol. This was to ensure that the tests could be performed without introducing possible areas of doubt due to the power source affecting the test results.

Test description

This test obtained the current magnetisation curves (I_{mag}) T1, T2, and T3 for visual analysis, identified the knee points on the curves, and analysed them for their applicability to power transformers. Finally, tests were performed to confirm that T2 would not saturate when injecting dc (Figure 5.16).

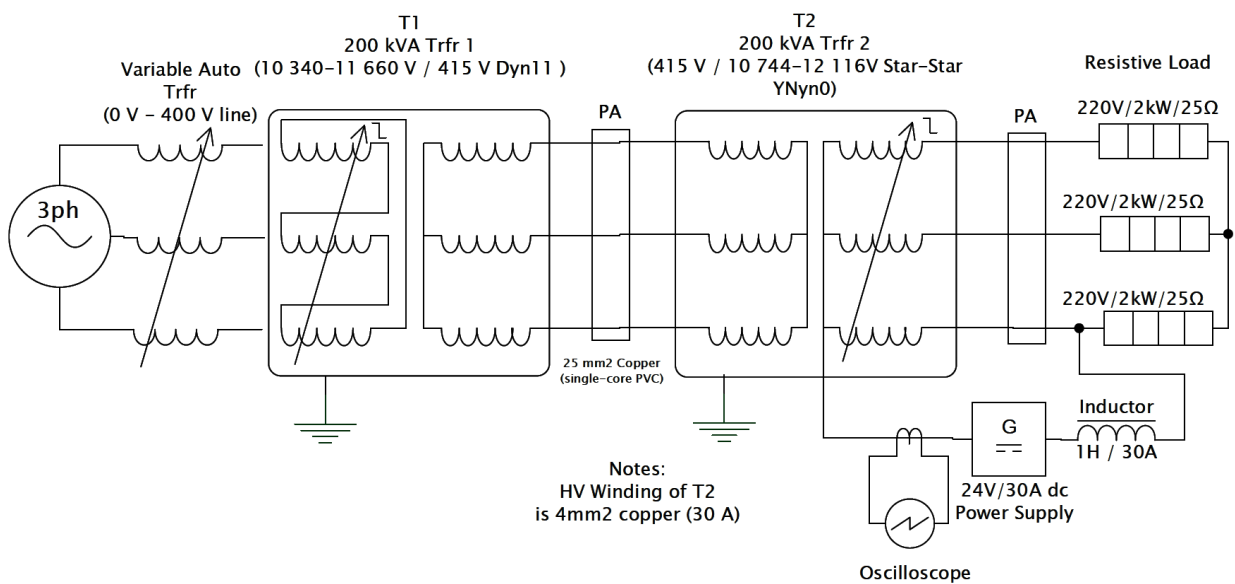


Figure 5.16 – Test circuit to confirm T2 could not saturate during dc tests of TuT

Test results and discussion:

Magnetisation curves and knee point definition

The first test was to record the magnetisation curves of each of the transformers as confirmation of their performance and as a troubleshooting benchmark for possible transformer failures in the future.

Figure 5.1 shows the test circuit to plot the magnetisation curves of the TuT and Figure 5.17 shows the test circuit applied to plot the magnetisation curves of transformers T1 and T2.

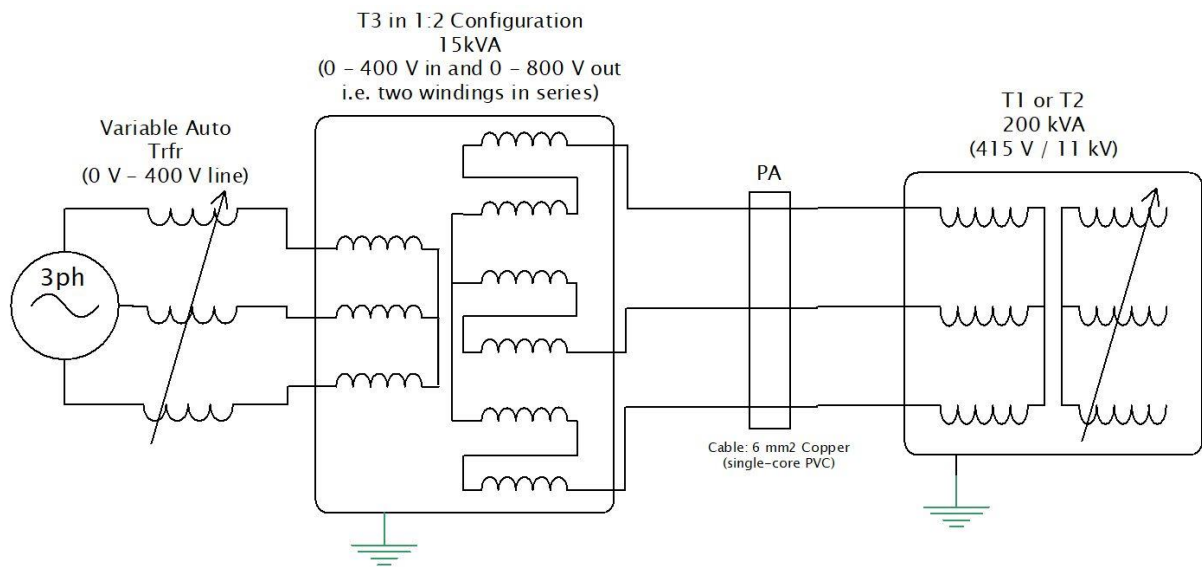


Figure 5.17 – Test circuit to determine transformer T1 and T2 magnetisation curves

The knee points shown on the magnetisation curves (Figure 5.18, Figure 5.19, and Figure 5.20) were defined according to the IEC and IEEE definitions applied to current transformers as described in Figure 2.7 and Figure 2.8. The applicability of these knee point definitions then formed part of this analysis.

Note: The THD was confirmed to remain low and was measured as <1.6% for the duration of the test similar to the levels measured for the ac supply validation for the ac symmetrical tests.

As discussed in the theory development, the transformer emf equation (5.1) for transformer T3 (3p3L 15 kVA) computes a U_{rms} of 320.9 V.

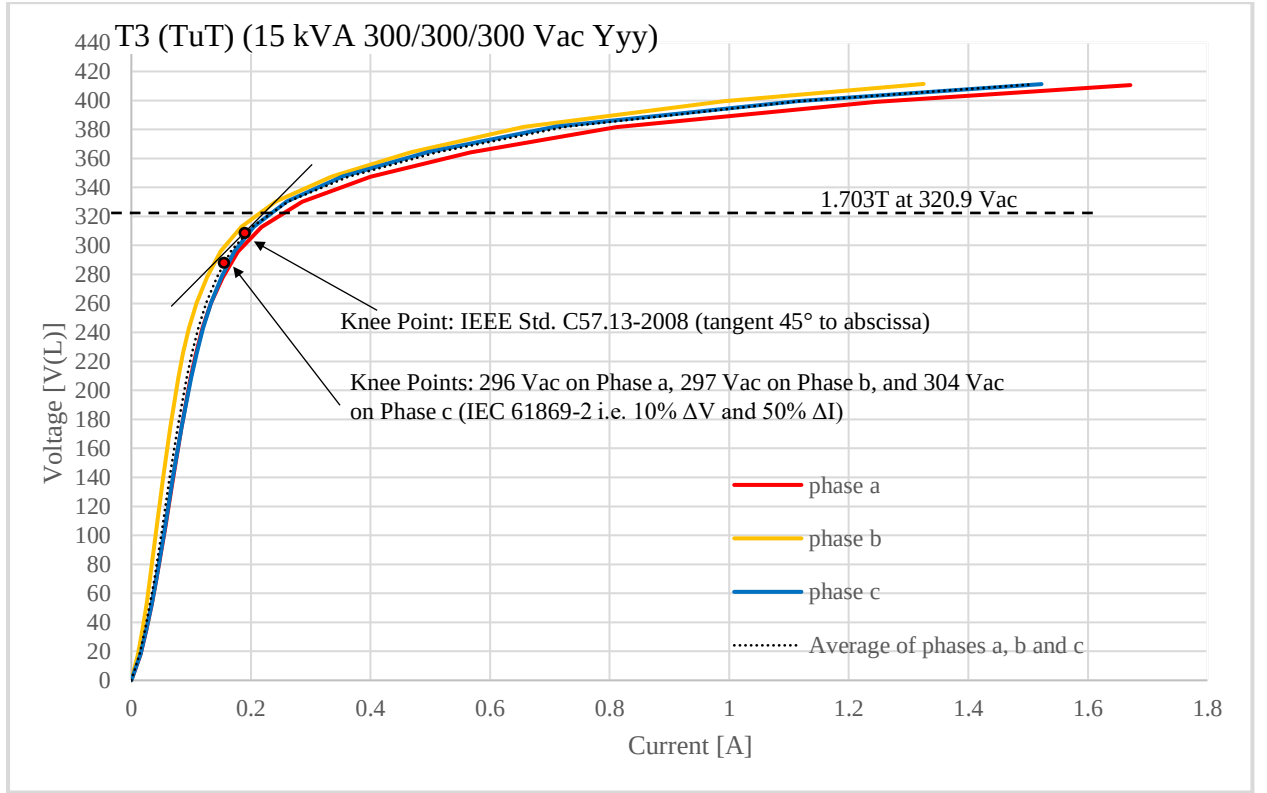


Figure 5.18 – T3 (TuT) Outer Winding Magnetisation Curve

As an important part of the dc injection circuit, the nominal flux density of transformer T2 was confirmed by calculation. The transformer emf equation (5.3) for T2 (3p3L 200 kVA) computes a $B_{m(rms)}$ of 1.708 T at the nominal voltage rating of the LV winding of the transformer using its manufacturing specifications. This is very close to the nominal operating point of about 1.7 T, as stated in literature [44].

$$B_{m(rms)} = \frac{U_{rms}}{4.44 * N * f * A * \sqrt{3}} \quad (5.3)$$

$$B_{m(rms)} = \frac{415}{4.44 \times 38 \times 50 \times 0.01663 \times \sqrt{3}}$$

$$= 1.708 \text{ T}$$

U_{rms} : emf or *rms* phase voltage

B_m : *rms* magnetic flux density

N : number of winding turns

f : frequency in Hz

A : core cross-sectional area in m^2

The nominal voltage ratings of the transformers are consistently above the knee-points as defined by both the IEC and IEEE standards (Figure 5.18, Figure 5.19, and Figure 5.20).

Note: It was confirmed that the calculation of B_m on T2 using its MV winding specification is consistent with the LV winding calculation result when performed using the MV winding's nominal tap 3 i.e., 11.43 kV and the number of turns corresponding to that tap.

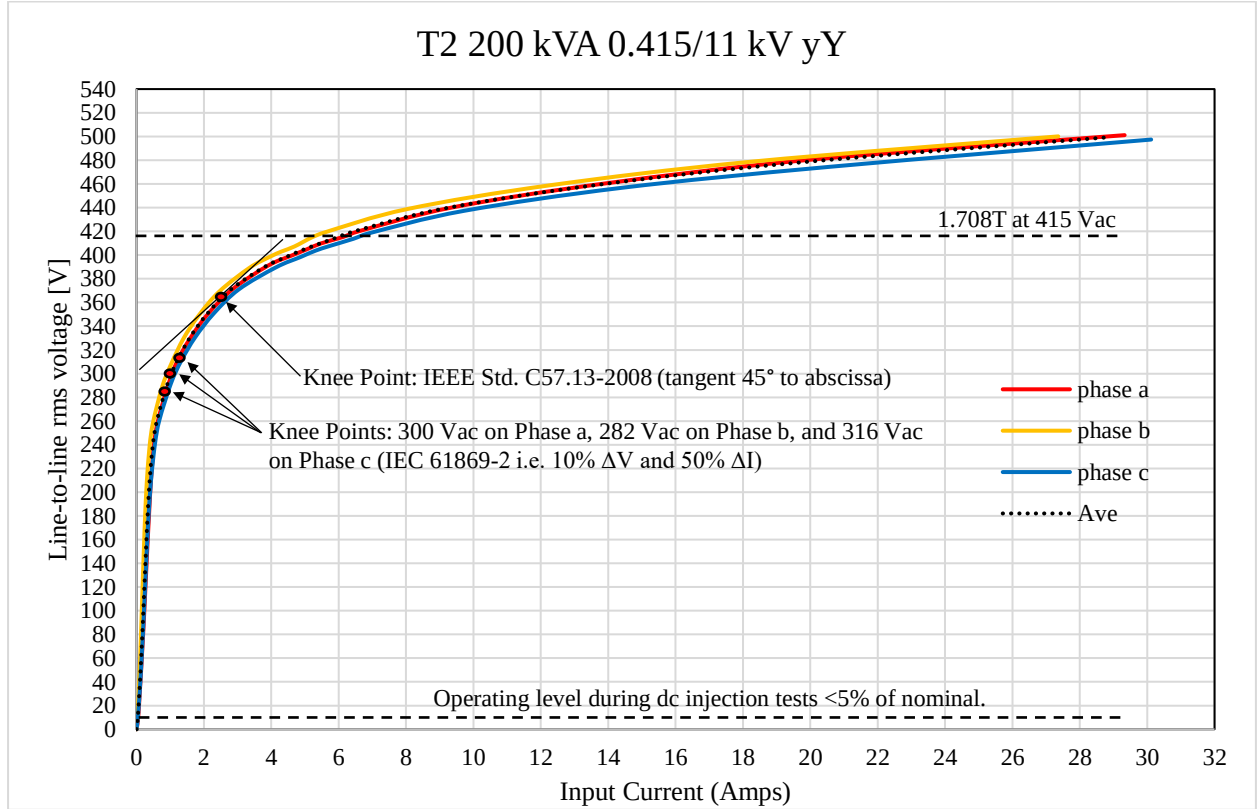


Figure 5.19 – T2 (415 Vac Winding Magnetisation Curve)

It was observed that the IEC defined knee-point of T3 (Figure 5.18) occurs at a flux density of approximately 1.6 T, whereas, the IEC defined knee-point of T2 (Figure 5.19) occurs at a flux density of approximately 1.25 T. This shows that the departure from the linear operating region of T2 occurs at a significantly lower flux density than T3.

It was also observed that, as a function of the nominal voltage rating of each respective transformer, the IEC defined knee point of T2 occurs at 60% of its nominal voltage rating, T1 occurs at the higher point of 70% of its nominal voltage rating, and T3 occurs at the highest point of 93% of its nominal voltage rating.

It could therefore, at initial inspection, be concluded that the proposal to derate T3 to 300 Vac be reconsidered. This was due to the fact that at a rating of 320 Vac there will be closer alignment with the rating of commercial power transformers that at a core flux density of 1.7. After consideration, the relatively early excursion of stray flux response from the core of the TuT in the de-rating study, indicates that the nominal rating of the TuT at 300 Vac is appropriate to avoid erroneous test results due to possible early, unexpected saturation of the TuT.

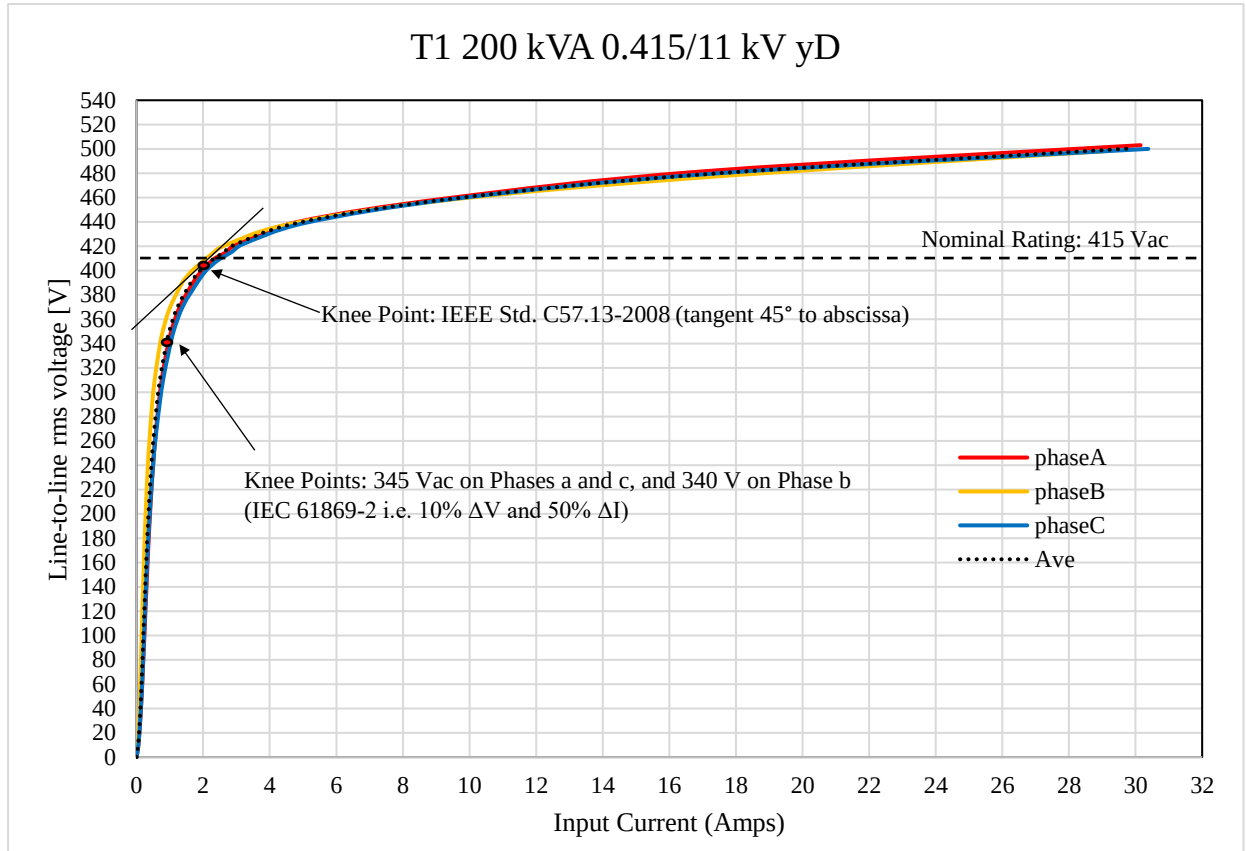


Figure 5.20 – T1 (415 Vac Winding Magnetisation Curve)

Figure 5.21 demonstrates that the results from the IEEE method [i.e., 45 deg from abscissa] for defining the knee point will change depending on the ‘shape’ of the knee point curve, and also the scales of the mantissa and ordinate, whereas the IEC method [i.e., IEC 61869-2 (10% ΔV and 50% ΔI)] references both the abscissa (x-axis) and the ordinate (y-axis) so has consistent results irrespective of the knee-point shape and scales of the mantissa and ordinate. It can therefore be concluded that, while the IEEE method can be useful for the comparative analysis of CTs as their cores are often designed for high levels of accuracy and precision, such as for instrumentation measurement. Its inconsistent results prove that this method should not be applied for the purposes of power transformers in general.

Note: The insulation level of both T1 and T2 is 95 kV which is well above the 1.2 p.u. (13 kV) level attained during the over-excitation tests.

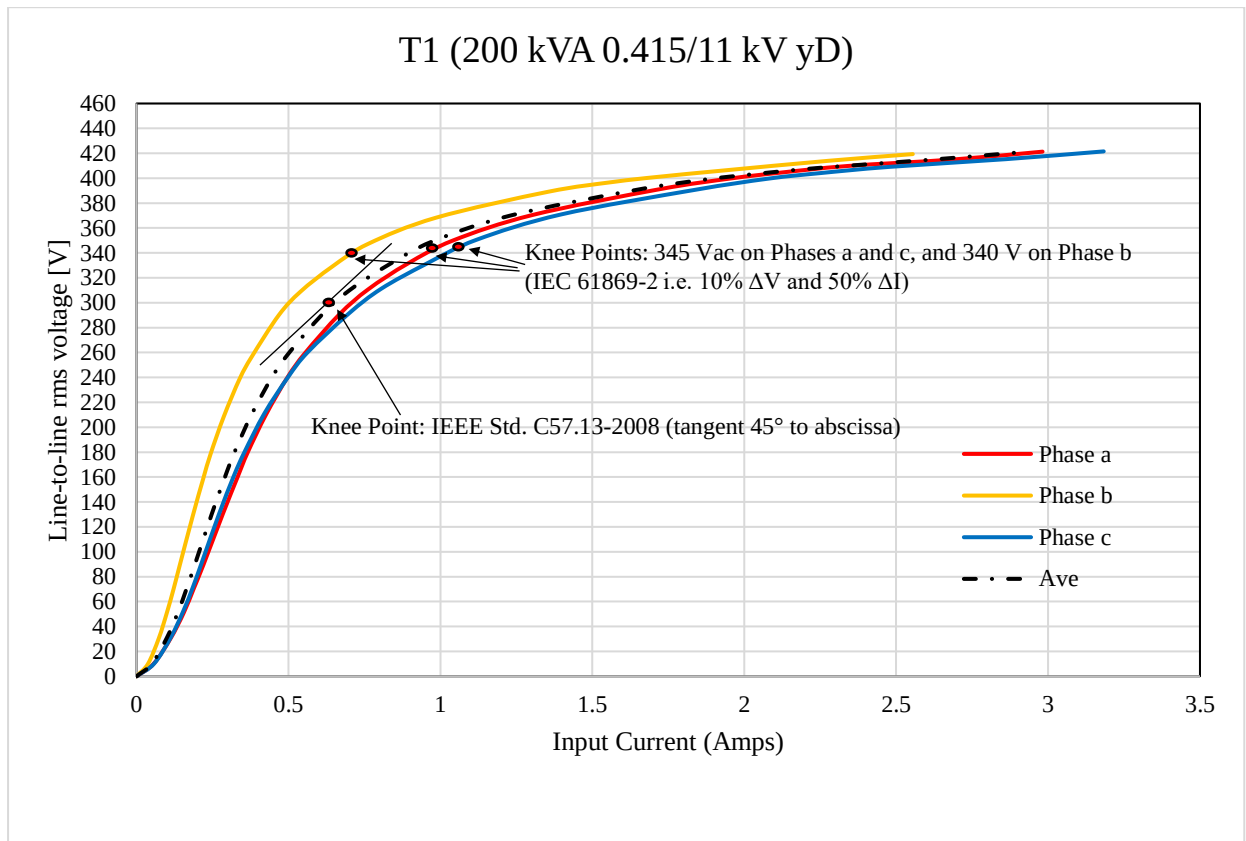


Figure 5.21 – T1 (415 Vac Winding Magnetisation Curve using 0-420 Vac Source)

Validation test to confirm that T2 does not saturate

The test circuit Figure 5.16 was designed specifically for this test to confirm that the source transformer T2 did not saturate and introduce any extraneous harmonics into the test circuit, and so invalidate the results for all the future supply and test conditions required by the TuT.

The test procedure to validate T2 was as follows:

- The ac supply was set by the variable auto-transformer at the transformers TuT's nominal rating of 300 Vac (L) on the secondary of T2.
- The dc current level from the dc power supply was set at 4.5 A and the voltage (U_{ac}), current (I_{ac}) waveforms on the primary of T2 were recorded.

Note: The tap of transformer T1 was set on the lowest tap '5' and the tap of transformer T2 was on the highest tap '1'. The tap selection was to allow the biggest voltage range when adjusting the variable auto-transformer.

The result of the test was positive in that, with the unbalanced dc injection, there was no observed distortion on the current or voltage waveforms (Figure 5.22) on the primary

windings of T2. This confirms T2 would not saturate during the execution of the test protocol on the TuT when injecting unbalanced dc into the three phases.

Figure 5.36 in section 5.2.3. confirmed this result by the test described in the test protocol to monitor the current distortion of T2 at a high unbalance of 18.7%, with dc current injected into the neutral.

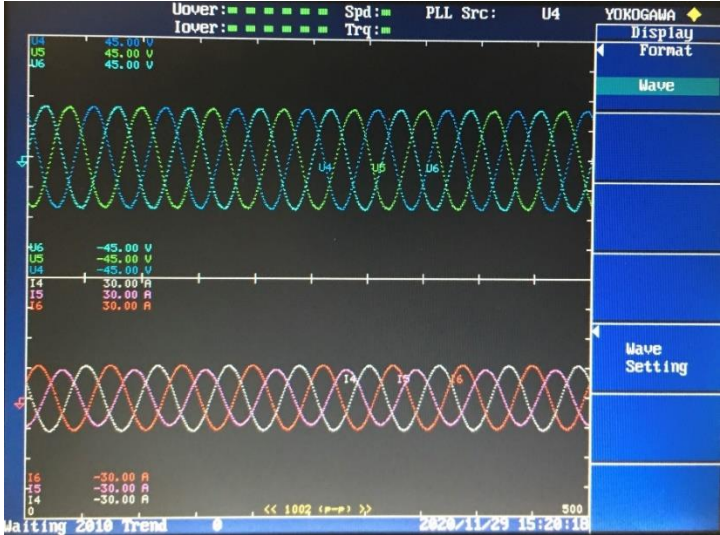


Figure 5.22 – Waveform on primary of T2

The final source transformer T2 operated at less than 5% of its nominal induction thereby showing no observable influence from possible source transformer saturation.

The waveforms in Figure 5.23 and the data in Figure 5.24 measured at the TuT show low levels of voltage distortion.

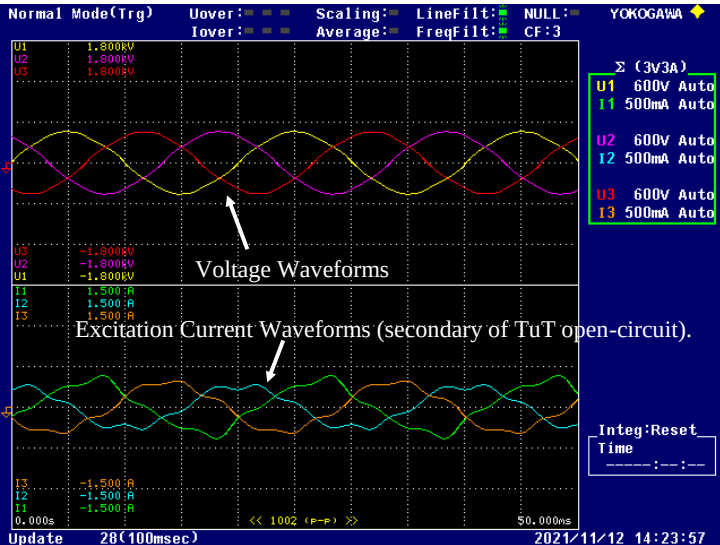


Figure 5.23 - 320.89 V ($B = 1.703T$) waveforms at TuT (Figure 5.25)

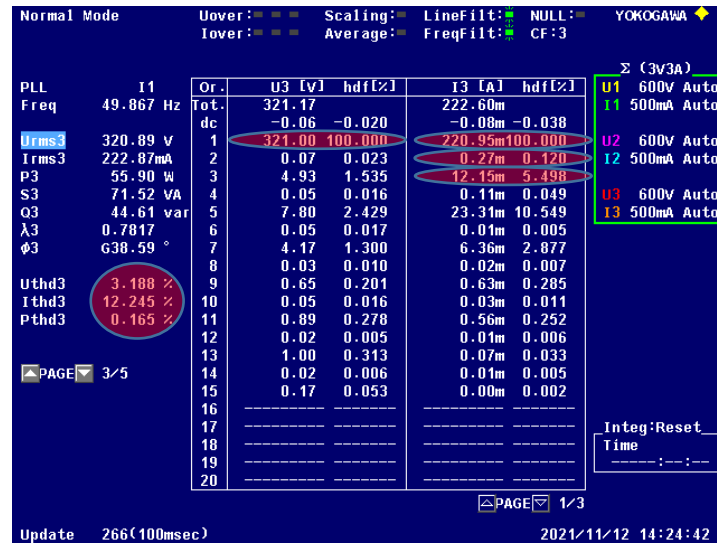


Figure 5.24 - THD values from 320.89 V ($B = 1.703T$) source supply at TuT (Figure 5.25)

Independently performed PSCAD/EMTDC and FEM simulations parameterized from the physical measurements were consistent with all the aspects of the experimental protocol (analyzed separately); including slightly unbalanced dc injection and saturation which was not unipolar.

In conclusion of these ac power supply validation tests, the health of the test circuit transformers T1 and T2 was confirmed, and it was ensured that the ac supply would perform adequacy during for the tests of the TuT with emulated GIC.

dc Power supply and test circuit validation

Test description

The following tests were required of the dc injection power supply in the test circuit (Figure 5.25) to validate the power requirement and to confirm acceptably low ac levels in the neutral of T2 and T3.

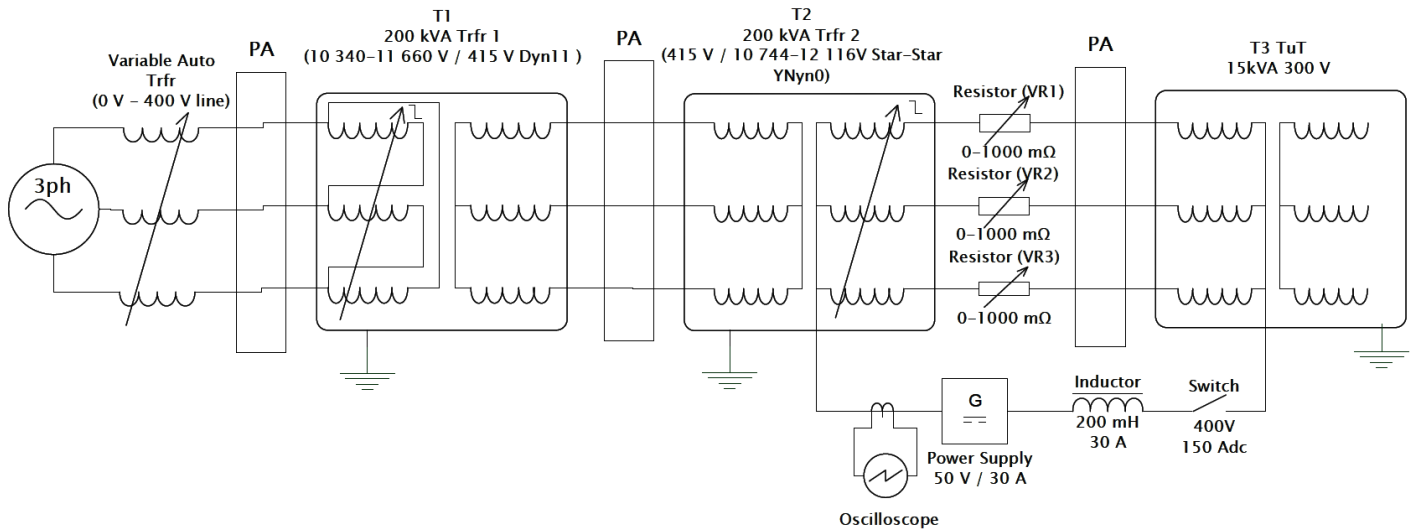


Figure 5.25 – Test circuit for asymmetrical dc injection tests

The test procedure for the dc power source test procedure using the test circuit in Figure 5.25 was as follows:

- Set the unbalancing resistance VR1 and VR3 at 300 mΩ and VR2 with no resistance (i.e., only the conductor) using a multimeter to confirm the resistances of VR1 and VR3, with the resistors disconnected from the circuit.
- Energise the ac power source and set the voltage at the TuT at 300 Vac.
- Energise the dc power supply and increase the voltage until 30 Adc is reached on the power supply as the neutral current.
- Measure the current using a current clamp multimeter at each phase connection on the TuT.
- Phase a and Phase c current should be 9.77 Adc and the Phase b current should be 10.45 Adc ($\pm 1\%$).

The measurement during all dc tests to confirm the correct level of ac in the neutral line of transformers T2 and T3 (Figure 5.25) was as follows:

The levels of ac superimposed on the dc in the neutral line were measured using an oscilloscope current-clamp. The oscilloscope input was set as ac coupled and its vertical sensitivity was set on 20 mA per division. Levels of ac were required to be $<1\%$ of the dc current monitored throughout all of the dc tests e.g., 300 mA ac when 30 Adc was injected.

Test results discussion

dc Power source test using the test circuit in Figure 5.25:

- After setting the unbalancing resistances and energizing the TuT as described in the protocol, the dc power supply voltage was increased until the neutral current was 30 Adc.
- The currents measured in Phase a and Phase c were 9.81 Adc and the current measured in Phase b was 10.41 Adc.

These results prove that the current split by the unbalancing resistors in series with the phase lines is as expected according to the circuit design and calculations.

The measurement of ac in the neutral line using the test circuit in Figure 5.25 were recorded using the current-clamp for the oscilloscope probe. The oscilloscope input was set as ac coupled with a vertical sensitivity of 10 mA per division. A maximum ac neutral current of 15 mv peak-to-peak (Figure 5.26) was measured simultaneously with 30 A dc in the neutral and it was well within the requirement of <1% of the dc current as stipulated in the protocol.

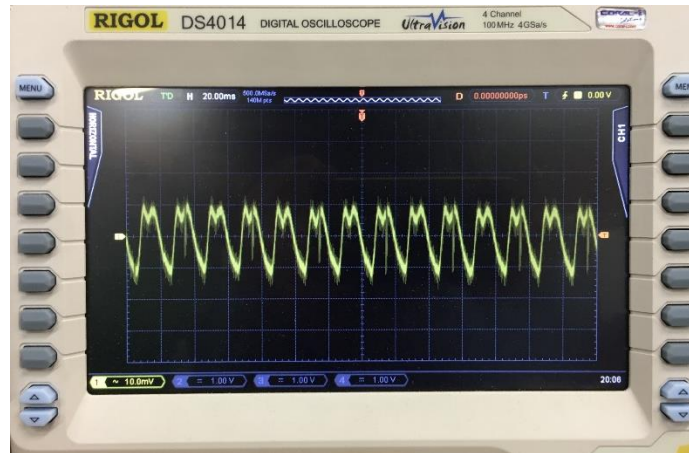


Figure 5.26 – Neutral current ac waveform of 25 mA peak-to-peak with 30 A dc injected, measured with current-clamp

Unbalancing resistor sizing and placement

Test description

As discussed by Masoum and Moses in [32] that even with low levels of unbalance in the phases of the transformer they could be forced into a saturated condition. According to the accepted unbalance of <5% in national transmission networks discussed previously, calculations were performed to determine the resistance required to create the equivalent test circuit unbalance.

Test results discussion

A maximum winding resistance of 4.1158 Ω (Figure 5.27) was measured on T2 (Figure 5.27), and a maximum winding resistance of 0.1220 Ω was measured on transformer T3 (TuT). The resistance of the wiring on each phase was 0.1 Ω and resistance of the neutral wiring was 0.17 Ω . The resistance of the dc test circuit, as seen from the dc power supply was then calculated to be 1.62 Ω . By applying the phase unbalance calculation as described previously

(3.2), an unbalance resistance of 350 m Ω in one phase is required to achieve a 5% current unbalance in the phases for the dc tests (Figure 5.25).

These resistances and configurations were confirmed in the dc power supply validation previously discussed.

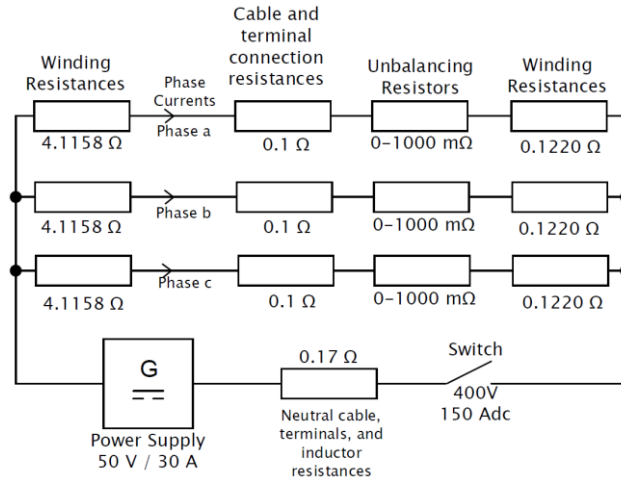


Figure 5.27 – dc Circuit resistances

5.2.2 Core saturation response of 3p3L transformer with GIC or dc

Test description

This test was to analyse the saturation response of a 3p3L power transformer in an unbalanced system with ac, and simultaneous GIC-like dc, adjustable from 0 to 30 Adc in the windings. The test was also repeated in a balanced configuration for comparative analysis.

The basic steps of this test were as follows:

- Record the U_{thd} , I_{thd} , and stray flux curves of the TuT at various levels of unbalance on different phases.
- Comparisons of the I_{thd} to the stray flux at the identified areas of the core were made i.e., W4, B2 and the search coil at the centre of the winding, as the level of dc was increased.

The configured test circuit was according to Figure 5.25. and the tests required the use of 3 elements (channels) of the WT1800 power analyser to measure the voltage, current and THD on the configured primary winding of the TuT. The remaining 3 elements of the PA were used to monitor the 2nd and 4th order harmonics of the primary winding of T2 to ensure that it did not show signs of saturating during any of the TuT tests. This is an open circuit test so there was no monitoring required for the TuT windings which were open-circuit

The test procedure was as follows:

- An unbalance was created by placing resistors according to Table 5.7, and for each placement the following steps were executed*.
- The ac supply was set at the TuT's nominal rating of 300 Vac (L).
- The dc current level from the dc power supply was increased in 1 A steps from 0 to 30 A, and at each increment the ac supply voltage was checked and then carefully maintained at 300 Vac on the phase with the lowest ac voltage reading**.
- The voltage (U), current (I), U_{thd} and I_{thd} , and the harmonic content as a percentage of the fundamental of each phase of the TuT's primary winding were monitored on the PA and recorded.
- At each 1 A dc current increment, the search coils at positions W4, B2, and the centre of the winding were recorded.

*Note: The setting of the resistances was done in discrete steps of 50 m Ω to ensure a simple and repeatable resistance setting process.

**Note: The ac voltage was required to be adjusted to maintain a 'firm' supply because as the dc increased on the phases, the ac rms voltage decreased as the level of induction decreased [35] as shown in the resulting waveforms.

The U_{thd} , I_{thd} , and stray flux curves were plotted for each of the unbalance settings.

Table 5.7 - Unbalancing resistor placement on TuT phases for dc injection test

	Phase a (Adc)	Phase b (Adc)	Phase c (Adc)	Current Unbalance (%)
1	0	0	0	0
2	0	0	100	1.5
3a	0	0	250	3.7
3b	0	100	300	3.7
4a	0	0	300	4.5
4b	300	0	300	4.5
5	0	0	500	7.1
6	0	0	600	8.4
7	0	0	800	11
8	300	1300	0	14.7
9a	0	0	1500	18.7
9b	0	0	1500	18.7
10a	0	0	2000	23.5
10b	0	2000	0	23.5

Table 5.8 – Current in phases of TuT at different levels of unbalance (30 Adc GIC)

	Neutral Current (A)	Phase a Current (A)	Phase a Current (A)	Phase a Current (A)	Current Unbalance (%)
1	30	10.00	10.00	10.00	0
2	30	10.08	10.08	9.85	1.5
3a	30	10.19	10.19	9.63	3.7
3b	30	10.30	10.07	9.63	3.7
4a	30	10.22	10.22	9.56	4.5
4b	30	9.77	10.45	9.77	4.5
5	30	10.36	10.36	9.29	7.1
6	30	10.42	10.42	9.16	8.4
7	30	10.55	10.55	8.91	11
8	30	10.37	8.53	11.09	14.7
9a	30	10.94	10.94	8.13	18.7
9b	30	10.94	10.94	8.13	18.7
10a	30	11.18	11.18	7.65	23.5
10b	30	11.18	7.65	11.18	23.5

Test results discussion

Fringing flux

Prior to discussing the core saturation response in detail, the reason for the initial ‘offset’ at the origin of the curves in Figure 5.31 and Figure 5.32 should be discussed.

In the readings of stray flux from the search coils it was noticeable that there was an initial voltage or ‘offset’ of 27 mV at the origin of the graphs for SC W4, 6 mV for SC B2, and 2 mV at the centre of the winding. This was attributed to fringing flux as described in section 2.5 that gradually increases as the excitation voltage of the TuT is increased. It is important to differentiate between the gradual increase of fringing flux, and the onset, or inception, of stray flux. This phenomenon is also well illustrated in Figure 5.10 in the symmetrical ac tests where 9 mV was present on W4 from 200 Vac (line), that gradually increased to 27 mV at the nominal rating of 300 Vac of the TuT, where the dc tests are executed.

Core saturation from symmetrical ac tests compared to core saturation from unbalanced dc

There was a significant increase in stray flux in Figure 5.31 and Figure 5.32 from about 1 Adc, and no indication of stray flux before that, at any level of unbalance. The stray flux ‘bleeding’ from the centre limb joint increased at a virtually linear rate when the resistive unbalance was <5%. With large unbalances, the rate of change of the curve increased quickly to a steep gradient which dropped significantly as the dc level increased. In contrast, with symmetrical ac over-excitation the gradient of the curve started slowly and then the slope increased at a much slower exponential rate as the symmetrical ac over-excitation of the TuT was increased linearly. The ac tests reached a maximum level of 170 mV on SC W4 at 380 Vac (Figure 3.14)

whereas the dc tests only reached a maximum of 170 mV on W4 with a 4.5% current unbalance indicating a relatively low level of core saturation impact from a 4.5% unbalance.

When considering the tests with a high level of unbalance; the ac test reached a maximum stray flux recording of 340 mV at W4 at an excitation level of 400 Vac whereas the dc tests, only reached this level at a high-level unbalance of 23.5%.

Core saturation response with simultaneous ac and dc

From observation, the stray flux curve from an unbalance of 23.5% is approaching a gradient of 0. Therefore, as shown in literature and in the harmonic distortion curves in the THD study, there is a point at which increasing the GIC cannot increase the level of core saturation any further. The level of harmonics (Figure 5.36) show a gradual decrease as the flux starts entering the structure of the transformer and the tank walls as discussed in [29].

Saturation response of limb with the highest dc current in winding compared to winding limb with the lowest dc current

Figure 5.28 demonstrates the effect on the core response at different levels of current in different phases by monitoring the core saturation response at specific areas of the core. Similar to the investigation by Masoum and Moses [32], there is an elevated level of dc current (Figure 5.29), and related dc mmf in the centre limb while the outer windings have the same, lower, dc current, thus the lower level of opposing mmf in the two outer limbs. In the other scenario (Figure 5.30) there is the same level of dc current in the Phase a outer limb and the centre limb, so there is a lower level of saturation in the Phase c outer limb.

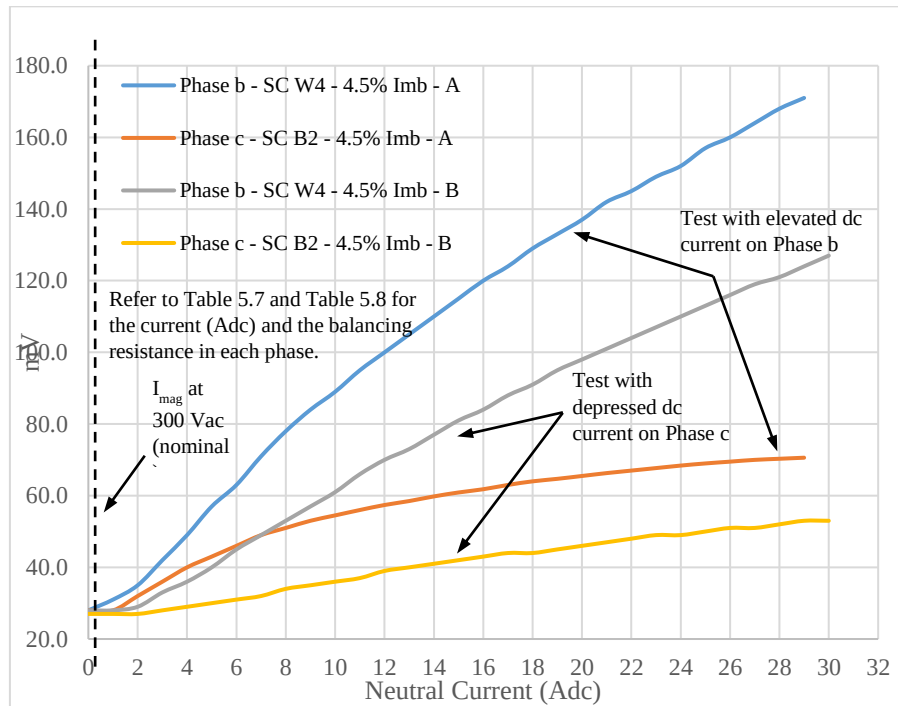


Figure 5.28– Comparison of stray flux levels with elevated current in the centre limb vs. an elevated current in the centre and an outer limb

Figure 5.28 shows a higher overall level of stray flux exiting the core when the centre limb has the highest dc current in the phase. As Masoum and Moses demonstrated, this is due to the high dc mmf in the centre phase opposing the dc mmf in the outer limbs. This analysis is similar to the discussion in the transformer inductance study by Chisepo [31], and has similar conclusions to the oscillographs during the symmetrical ac tests.

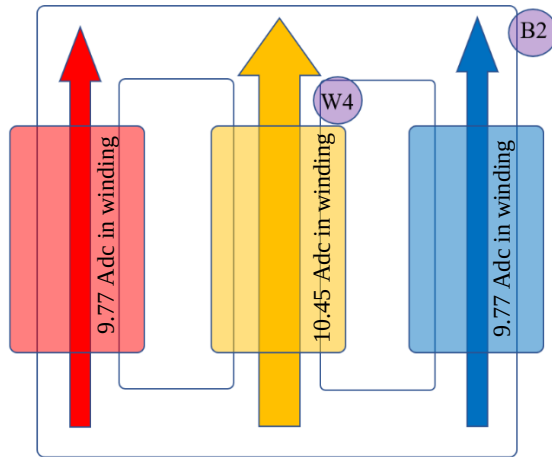


Figure 5.29 – Elevated dc mmf in centre limb

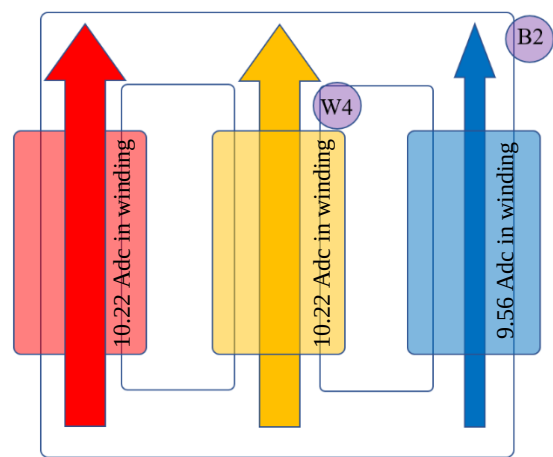


Figure 5.30 – Elevated dc mmf in Phase a and Phase b limbs

Effect of the increase of the unbalance compared to the increase of dc in the winding

Figure 5.31 shows that both the increase in unbalance, and the increase of dc affects the level of stray flux i.e., a low level of dc with a significant unbalance in the circuit results in a high level of stray flux. Conversely, a high level of dc with a small unbalance in the circuit has the same effect. All of the stray flux curves are shown to be increasing beyond the highest current level depicted of 30 Adc. If this trend continues, it could lead to significantly high levels of stray flux at high GIC of beyond 100 A, where stray flux will respond as for other transformer core configurations, and potentially cause significant damage. As a result, even with relatively small unbalances, the impact on 3p3L transformers in the field could be significant.

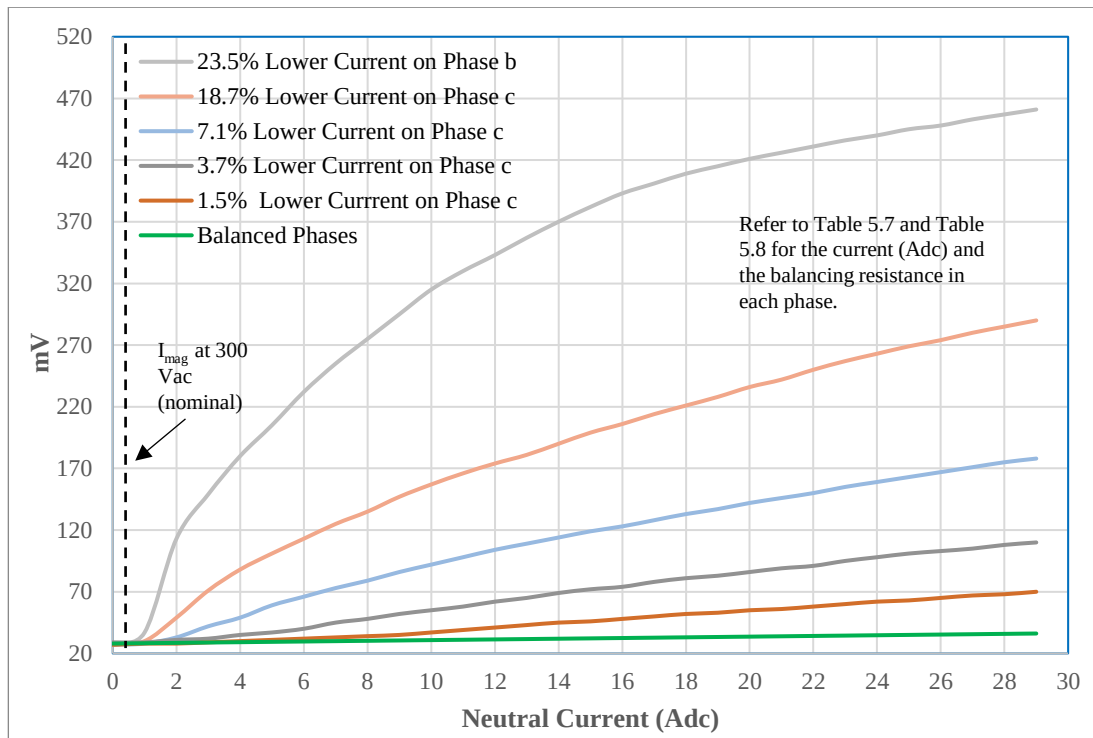


Figure 5.31 - Stray flux at SC W4 at various unbalances and various levels of dc in the phases

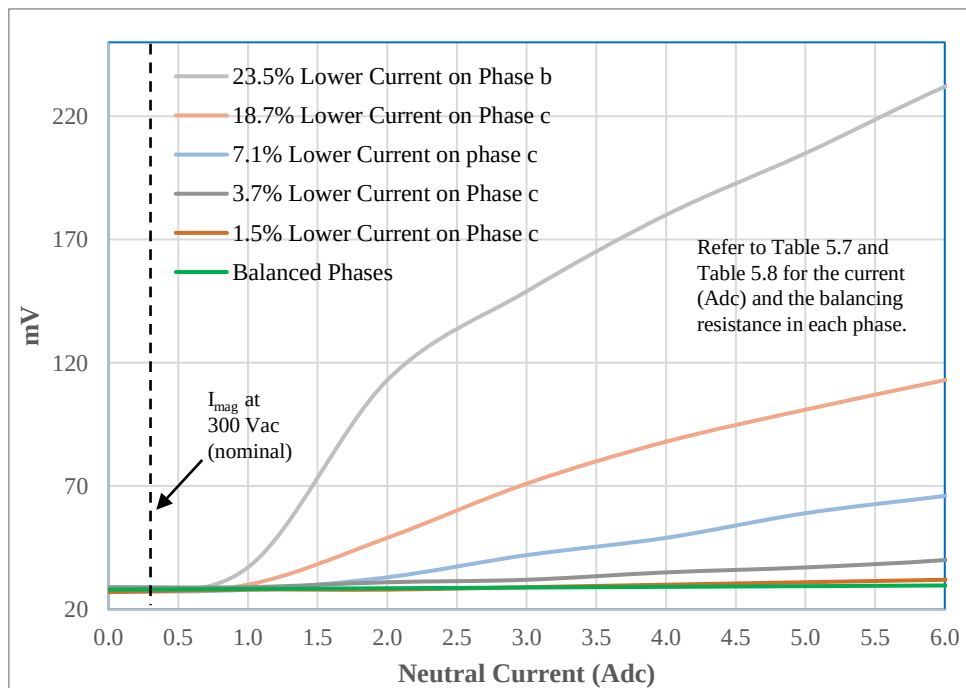


Figure 5.32 - Stray flux at SC W4 at various unbalances and various levels of dc in the phases (zoomed in to inception point)

Core saturation response at centre limb compared to core saturation response at outer limb

The saturation level at the outer limb core joints was consistently lower than the saturation level at the centre limb as shown in Figure 5.33. This is attributed to the lower reluctance of the centre limb resulting in increased dc mmf through the centre limb resulting in higher level of half-wave saturation and the resulting stray flux, similar to the observations during the symmetrical ac tests.

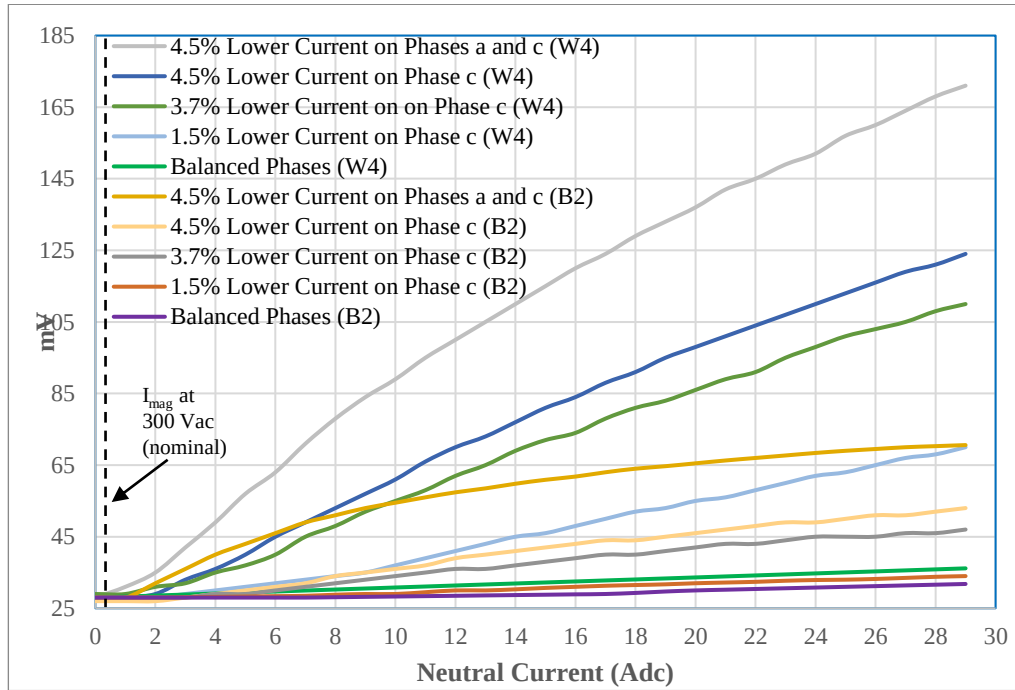


Figure 5.33 – Comparison of stray flux at the centre limb joints (W4) and the outer limb corner joints (B2)

Impact of core joints on core performance

The performance which should be expected from a mitred multi step-lap core design was discussed by Borrill [1]. A four step-lap wound limb, such as the outer limbs of a 3p3L transformer should not saturate before the bulk core material but the yoke joint should reach saturation earlier than the bulk core material. This test aimed to study this on the 3p3L model power transformer (TuT) by mapping the stray flux at the joints and at the bulk core area and then analysing the inception of stray flux at each of the areas.

Figure 5.34 shows the stray flux inception point at about 0.5 Adc with very little activity noticeable before that. The bulk core material at the centre of the winding in contrast, only showed a considerable rise in the level of stray flux much later at about 2 Adc.

In comparison, during the symmetrical ac test stray flux readings, the bulk core areas also indicated saturation significantly later than at the joints.

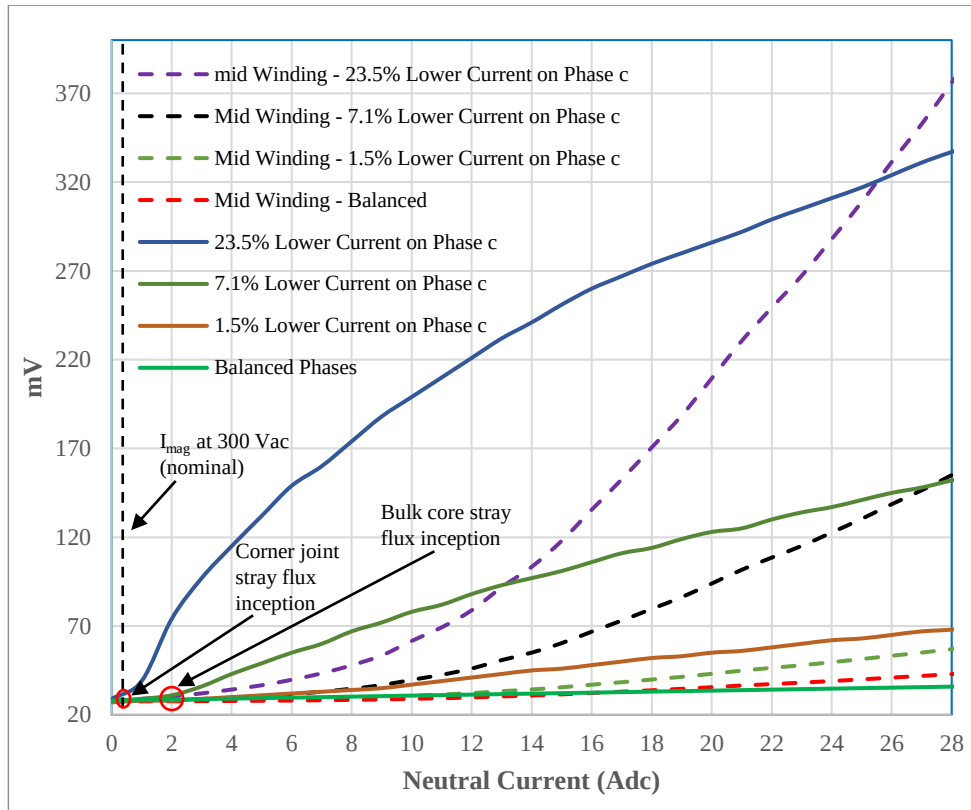


Figure 5.34 - Inception of core saturation at the core joints on the outer limb compared to the bulk-core material

In Figure 5.34, there is a clear distinction to be made as demonstrated by the 7.1% unbalance curves as an example. The gradient steepens at a later stage on the curve taken from the mid-point of the winding, than the curve from the search coil at the core joints. These curves emphasise how the inception of core saturation starts notably earlier at the mitred core joints than at the bulk core areas.

Single-turn winding

Figure 5.35 shows the results of the sinusoidal voltage (Vac) measured on the permanently installed single turn winding around the centre limb of the core at the centre winding of the TuT. At an unbalance of 1.5% the curve shows a slight non-linearity but at 7.1% at approximately 17 Adc there is a sharp turn away from linearity. This is similar to the bulk core material saturation curves in Figure 5.34 that shows low levels of saturation between 10 Adc and 15 A dc at an unbalance of 7.1% but at 1.5% there is a slow increase.

Balanced dc in windings

The final tests introduced balanced dc in the windings of the TuT for comparative analysis. Initially, careful attention was paid to ensure the current would split evenly between the three phases of the TuT by measuring the dc voltage at each phase relative to its neutral and then calculated the actual current using the resistances of each winding obtained during the TuT acceptance tests. As the dc current level was increased from 0 to 30 Adc there was clear evidence of stray flux inception (Figure 5.31), and the concurrent increase in stray flux at the

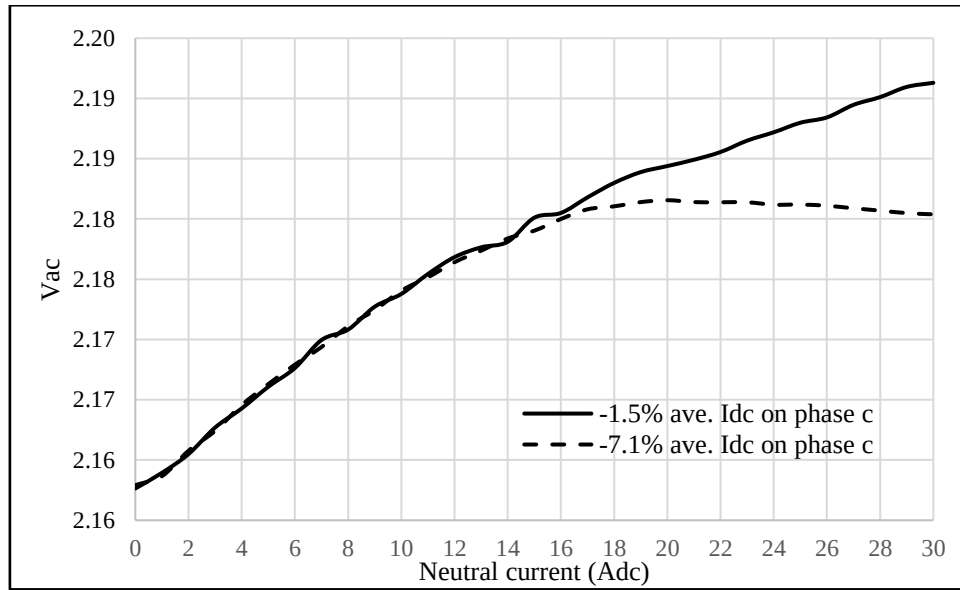


Figure 5.35 - Single turn winding around centre limb at 4.5% and 7.1% unbalance on phase c

centre limb (W4) and the outer limb corner (B2). The levels were very low as indicated but at 30 Adc there is a noticeable elevated level of stray flux visible. It was therefore postulated that as the level of dc increases beyond 30 Adc the level of stray flux will also continue to increase. As stated by Masoum and Moses [32], the conclusion made is that it is unlikely that a truly balanced system is possible so 3p3L transformers could always be vulnerable to the effects of GIC due to this. So, in ‘balanced’ conditions, it is likely that at high levels of dc, 3p3L transformers could still be vulnerable to GIC.

In conclusion, the flux distribution response of the core, for the level of electrical unbalance seen in transmission networks has proven significantly different from that seen in literature. This physical electromagnetic response indicates that the FEM studies as demonstrated by [7] and [79] are likely. It was observed that there was an immediate and notable increase in stray flux at very small levels of unbalance of less than 1% and that even balanced conditions yielded some level of stray flux as the dc current in the windings increased. High level of dc current even at extremely low levels of unbalance could therefore cause relatively high levels of stray flux, with the resulting core saturation and possible impact by GIC.

Similar to the observation during the symmetrical ac tests, the bulk core material saturation point is significantly later than at the core joints. The search coil traces showed that stray flux starts exiting notably from the bulk core material of the TuT at the centre of the winding at about 2 Adc. The corner joints, in contrast, indicated notable stray flux earlier at around 0.5 Vdc. The gradient at the inception point is also notably steeper at the corner joints than at the bulk core material.

It is noted that the effective cross-sectional area of step-lap joints improves as the number of steps increases as discussed by Borrill [1]. This means that the difference between inception

of stray flux in the joints compared to the bulk core material is likely to be smaller in cases with five and six steps, as found in many large power transformers.

5.2.3 THD in relation to saturation level of a transformer core with GIC or dc

Test description

This test was performed using the dc injection test procedure in section 5.2.2.

The following measurements were recorded at each level of dc from 1 Adc to 30 Adc:

- Voltages at primary winding of TuT, magnetising current, voltage distortion (U_{thd}), and current distortion (I_{thd}).
- Stray flux recordings of W1, B2, and the centre of the winding.

The first test was performed with a high-level unbalance to observe the worst-case scenario of unbalanced dc mmf in the core of the TuT to record the distortion levels and magnetizing current in the primary of the TuT. It was postulated that high levels of unbalance at the relatively low level of 30 Adc when compared to some GIC, could yield similar levels of dc flux in the core to relatively low unbalances at high level levels of dc. Later, this harsh test would be repeated to observe the transformer's response to extreme conditions for the analysis of core and tank heating stress with GIC, and the noise tests.

During this initial test, the harmonic distortion on the primary of the T2 supply transformer was recorded to compare its THD values to the THD values of the TuT. If there was a sudden obvious deviation on the THD curve of T2 as the injected dc current was increased then the likelihood of it being caused by a saturated core is high. This was to confirm that T2 would not enter a saturated core condition during any of the TuT tests.

Note: The current on the primary of T2 was measured with the WT1800 power analyser using a current shunt because the primary current of 150 Aac exceeds the 60 Aac capability of the power analyser.

The subsequent tests required were with unbalanced currents in the windings of the TuT by varying the resistances in the lines (Figure 5.25) and placing the test circuit in an unbalanced configuration from 1.5% to 4.5%. The aim was to understand the changes of distortion in relation to saturation of the core within the expected range of unbalances in a transmission network.

Test results discussion

Harmonic distortion and core saturation at high level of unbalance

Figure 5.36 shows a sharp increase in current distortion when the dc exceeds the I_{mag} threshold at the TuT's nominal rating of 300 Vac, as indicated by the start of area 'A'. Observing the stray flux at the joints in Figure 5.33, this is the same time that the joints start showing a definite increase in stray flux at the core joints and at the centre of the winding of the TuT.

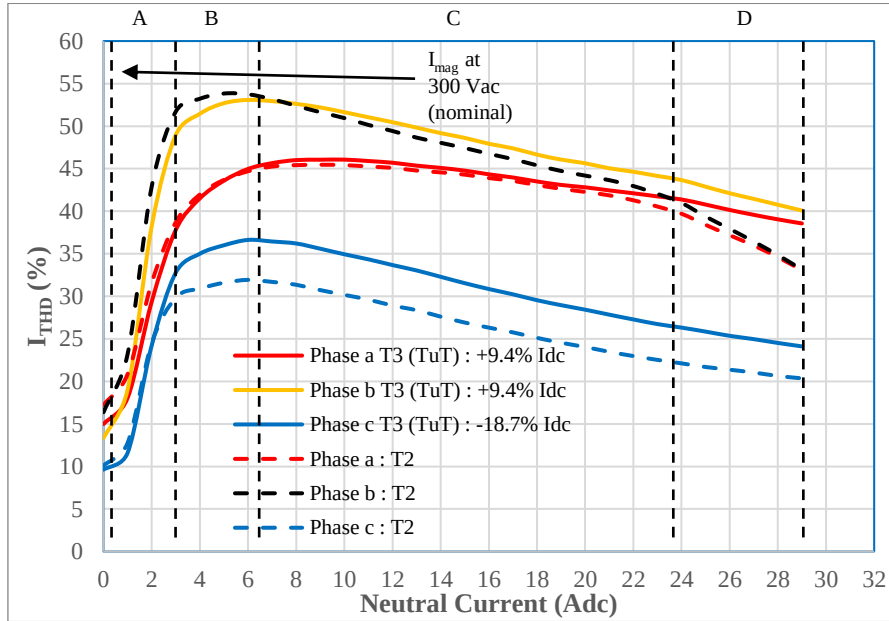


Figure 5.36 - I_{thd} at primary windings of TuT - 18.7% unbalance

Area 'B' on Figure 5.36 shows that the rate of distortion increase slows significantly between 3 A_{dc} and 6 A_{dc} on Phases b and Phase c, and at about 9 A_{dc} on Phase a. As discussed by Borrill [1], section A in Figure 5.36 reflects the saturation of the core joints and the transformer entering partial 'air-core' operation. Section B reflects the slowing in the increase of saturation where joints are fully saturated and the bulk core material moves into 'deep' saturation and, as it progresses further, into partial 'air-core' operation. This correlates with Figure 5.34 after 3 A_{dc} where the bulk core material stray flux starts increasing notably. Section C reflects the THD decline as the test transformer as a whole has now moved into 'air-core' operation. These results agree with the modelling and experimental results reported by Masoum & Moses [47] and Chisepo [48], which observed that the voltage and current harmonics stopped increasing but rather gradually decreased as the dc increased further.

The decrease in distortion remains virtually linear as the I_{dc} increases until the partial 'air-core' operation from stray flux leakage.

Figure 5.37 shows a relatively smooth and consistent increase of voltage distortion with evidence of a subtle response at each change of the current distortion at the start of areas ‘A’ to ‘D’ as identified in Figure 5.36. There is a change in response as the transformer enters the non-linear region of operation and the core saturation has increased to a point where stray flux becomes evident (Figure 5.18). The centre winding distortion starts increasing at a higher rate than Phase a and Phase c at the start of area ‘C’ which is likely due to the lower reluctance path, that is reduced further when the flux enters into the metallic structures of the tank.

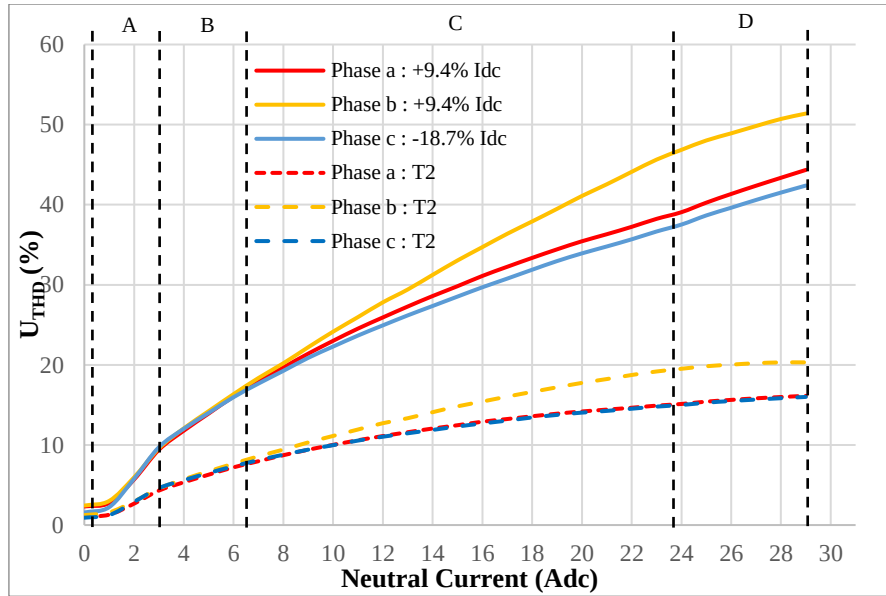


Figure 5.37 - U_{thd} at primary windings of TuT - 18.7% unbalance

Harmonic distortion and core saturation at balanced conditions

In contrast to the tests at a high level of unbalance, Figure 5.38 shows the I_{thd} distortion at balanced conditions. The dc current in the neutral was increased from 0 Adc to 30 Adc, which is 0 Adc to 10 Adc in each of the windings of the TuT, while the transformer primary winding was energised at its nominal rating of 300 Vac.

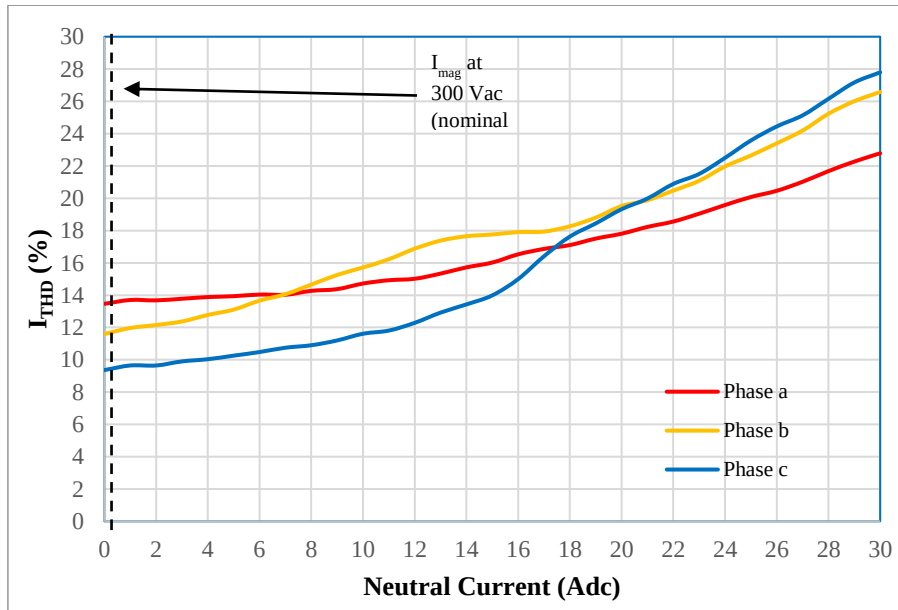


Figure 5.38 - I_{thd} at Primary Windings of TuT - balanced

The distortion levels of all of the Phases were significantly lower than for any of the tests with an unbalance as can be seen in the I_{thd} levels in Figure 5.36, Figure 5.39, and Figure 5.40 at unbalances of 18.7%, 1.5%, and 4.5% respectively. The stray flux measured at 30 Adc in the balanced configuration, showed an increase that is not attributed to fringing flux so there is evidence of core saturation. Therefore, similar to the conclusion in the stray flux recordings, even in a balanced configuration, the TuT does show evidence of a saturated core, with some flux outside of the core, as the dc current increases in the windings.

Harmonic distortion level compared at balanced and different levels of unbalance

In area 'B' of Figure 5.40 at a 'normal' unbalance of 4.5% there is evidence that the gradient of the harmonic distortion of Phase b has slowed and has reached its peak level, starting a down-turn in the I_{thd} distortion curve. This is reminiscent of the 'down-turn' in the discussion on area 'B' in Figure 5.36 at a high unbalance. This is at the same I_{thd} level of 53.7%. Phase a is still increasing but is expected to respond the same as in Figure 5.36 where its decrease in level of I_{thd} also stated after Phase a and Phase b. As discussed previously, the 'down-turn' in distortion is likely due to the flux starting to enter other metallic structures and the tank wall lowering the overall reluctance and current distortion. In contrast, the I_{thd} peaks at 47.5% (Table 5.9) on Figure 5.39 at an unbalance of 1.5% which is before the point at which the higher levels of unbalance demonstrated this behaviour. The decreasing gradient at 1.5% unbalance indicates that by extrapolation, at some point above 30 Adc it is likely to reach 53.7% and then the stray flux will start to enter the tank as well. The fact that this behaviour is demonstrated at both unbalances of 4.5% and 18.7%, but at different levels of dc, means that if the dc is of a high enough level then the point will be reached that failures of 3p3L transformers as a result of GIC are likely.

Table 5.9 – Peak I_{thd} at each level of unbalance

Peak I_{thd} – Balanced	Peak I_{thd} – 1.5% Unbalance	Peak I_{thd} – 4.5% Unbalance	Peak I_{thd} – 18.7% Unbalance
28%	47.5%	53.7%	53.7%

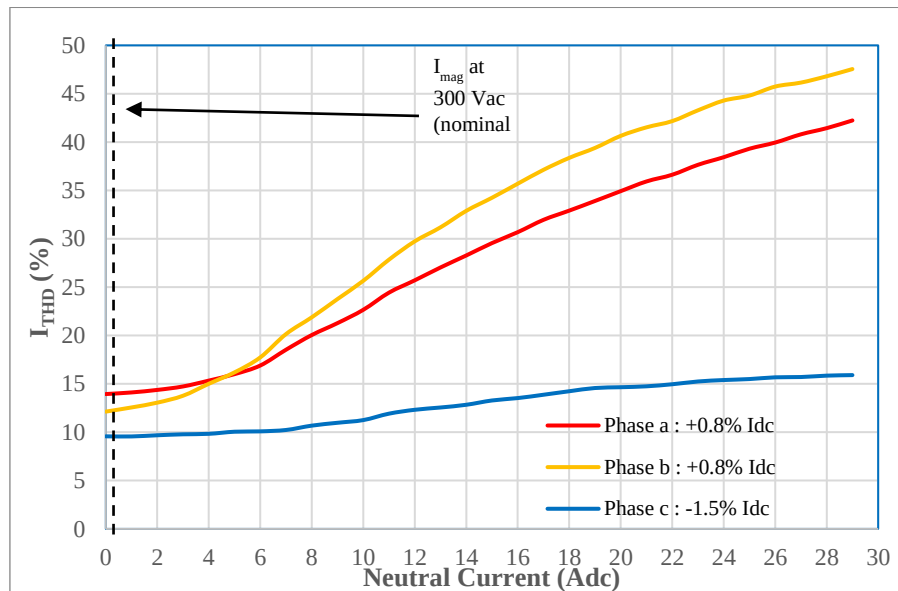


Figure 5.39 - I_{thd} at Primary Windings of TuT - 1.5 % Unbalance

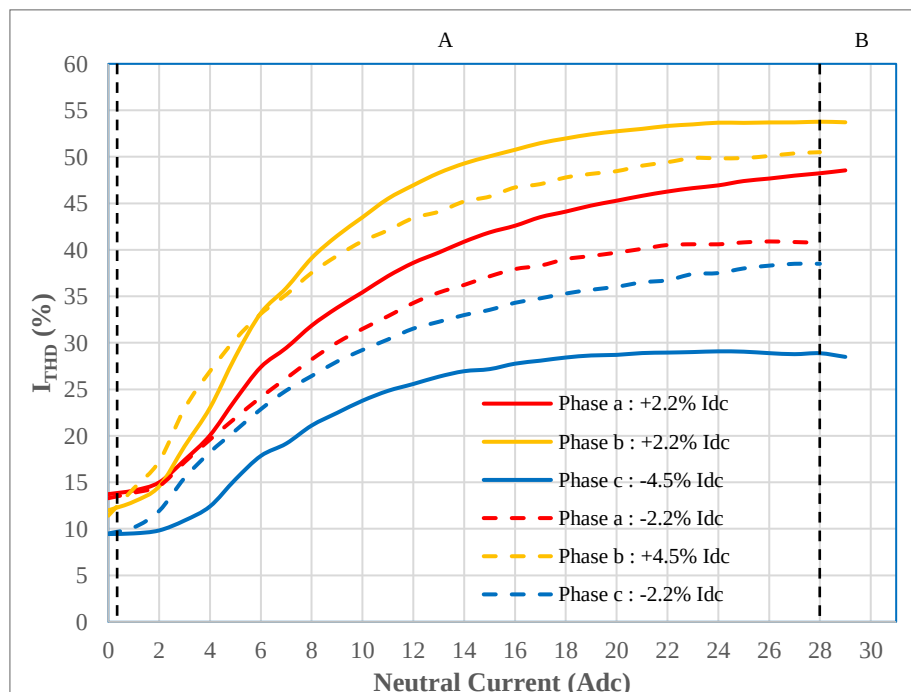


Figure 5.40 - I_{thd} at the primary windings of the TuT at a 4.5% unbalance at Phase c, and also a 4.5% unbalance on Phase a and Phase c

Figure 5.40 shows the I_{thd} curves of the TuT with two scenarios; one with the maximum unbalance on the centre limb and the other with the maximum unbalance on one of the outer limbs.

It was observed that there is a much larger variance in distortion when comparing the levels of distortion observed in each phase when the dc current in one of the outer limbs is higher than the other windings. In contrast, when the dc current is higher in the centre winding the variance in distortion of the other two phases is significantly less. When the dc mmf in the centre limb is elevated above the outer limbs it is likely that it also increases the dc mmf and flux in the outer limbs due its relatively low reluctance path and so increasing the I_{thd} distortion in those limbs as well. This is also shown by the relatively high stray flux readings on the outer limb when the unbalance is on the centre limb. The stray flux readings are significantly lower when the maximum unbalance is on the winding on the outer limb due to its higher reluctance flux path when compared to the centre limb.

Evaluation of T2 saturation

The dotted line traces in Figure 5.36 show the I_{thd} of transformer T2 as measured on the ‘primary’ LV windings of T2, indicate that the changes in distortion levels are consistent with the TuT as measured at the primary windings of the TuT. This demonstrates that T2 has not entered saturated conditions at 18.7% unbalance. If the core of T2 started saturating there would be a deviation in the current distortion trend when comparing it to the transformer it is supplying, T2. The slight deviation noted in area ‘D’ was confirmed to be due to the source power supply reaching its maximum voltage on Phases a and b. It is less pronounced on Phases a and b of the TuT due to the relatively low current on the secondary (MV) side.

Primary excitation current of TuT at high level of unbalance

Figure 5.41 shows a linear increase in the ac phase current which is proportional to the increase in dc current in the neutral and the resistive unbalance of the test circuit. Phase a and Phase c are 9.4% above the mean of the currents, and Phase c is the maximum unbalance at 18.7% from the mean. As expected, this has a direct correlation with the dc current unbalance.

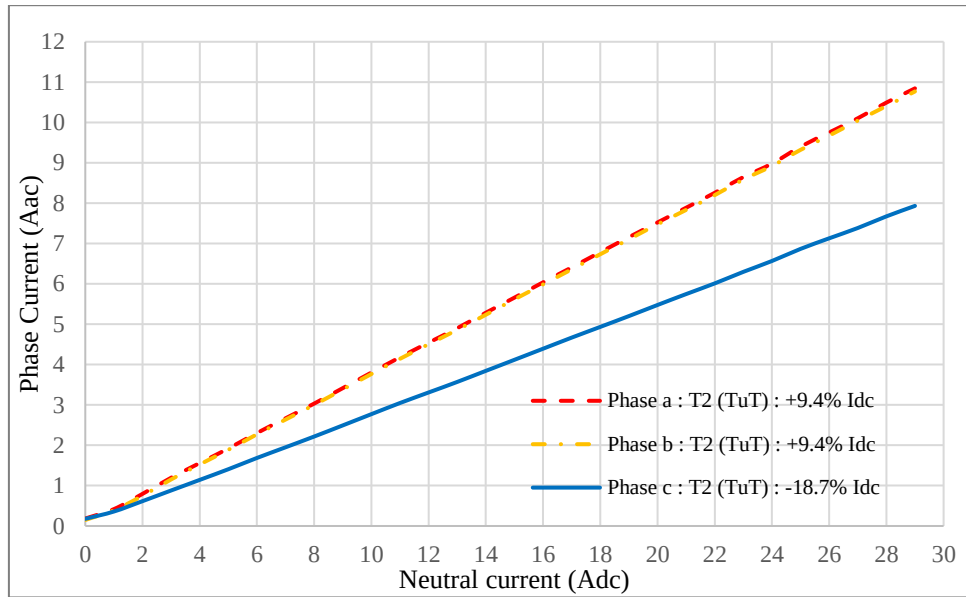


Figure 5.41 - ac Current at primary of TuT vs. dc neutral current – 18.7% unbalance

Progressive change of waveform distortion with increase of part cycle saturation

Figure 5.42 and Figure 5.43 show the current and voltage waveforms recorded with the I_{dc} of 0 Adc, 10 Adc and 30 Adc in the neutral of the TuT, at a maximum unbalance of 18.7% on Phase c. This shows the progressive change of the waveform shapes as the dc current is increased where the ‘half-wave’ is evident with its dc bias relative to the current injected into the neutral of the TuT. There is a lower level of dc evident in Phase b as the bias is of a significantly lower level than Phase a and Phase c.

Points ‘A’ and ‘B’ in Figure 5.42 and Figure 5.43 (30 Adc in the neutral) show the peak current of Phases a and b and the effect of the higher dc current on the voltage where it is likely that the low level of inductance and the high source transformer impedance attenuates the voltage. Point ‘C’ in comparison shows the lower peak current in Phase c with the less pronounced effect. Indications are that as the dc increases beyond 30 Adc, the voltage ‘collapse’ will gradually increase.

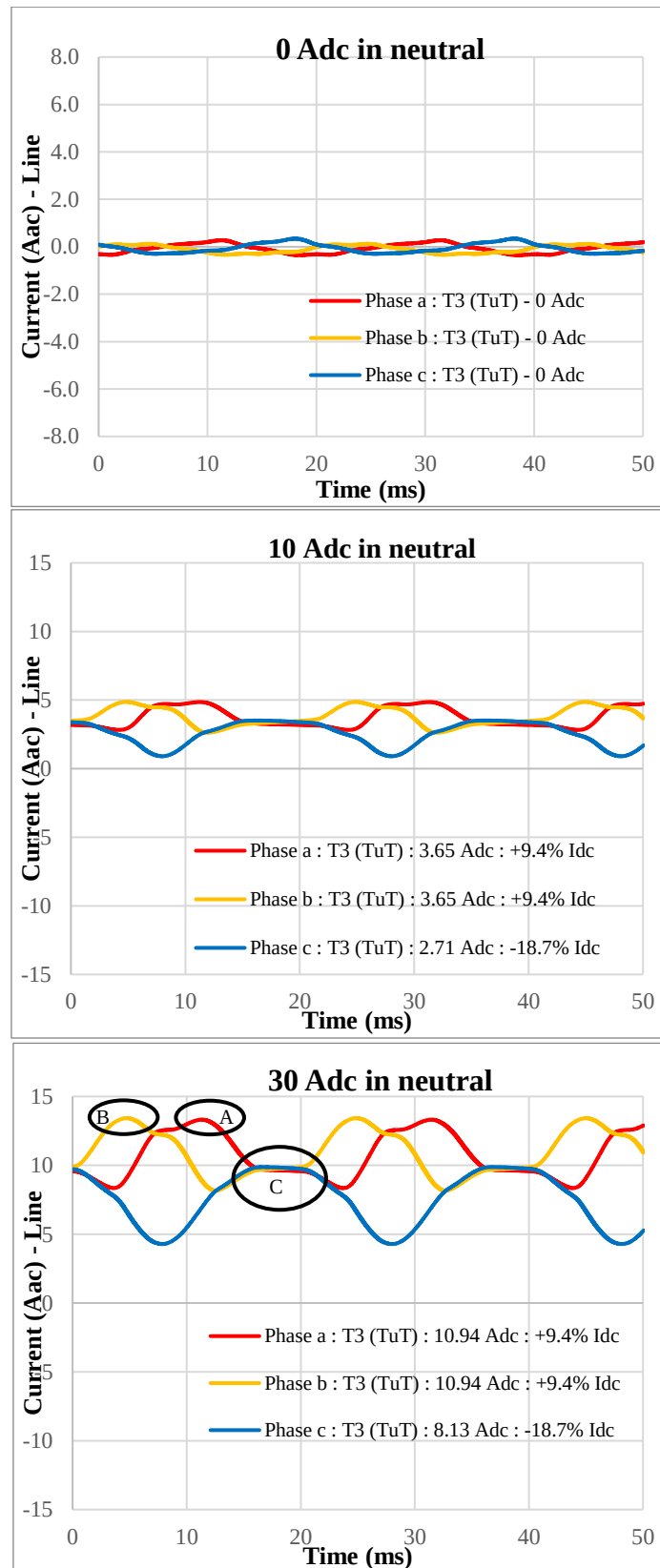


Figure 5.42 - Current waveforms at primary of TuT with 18.7% unbalance

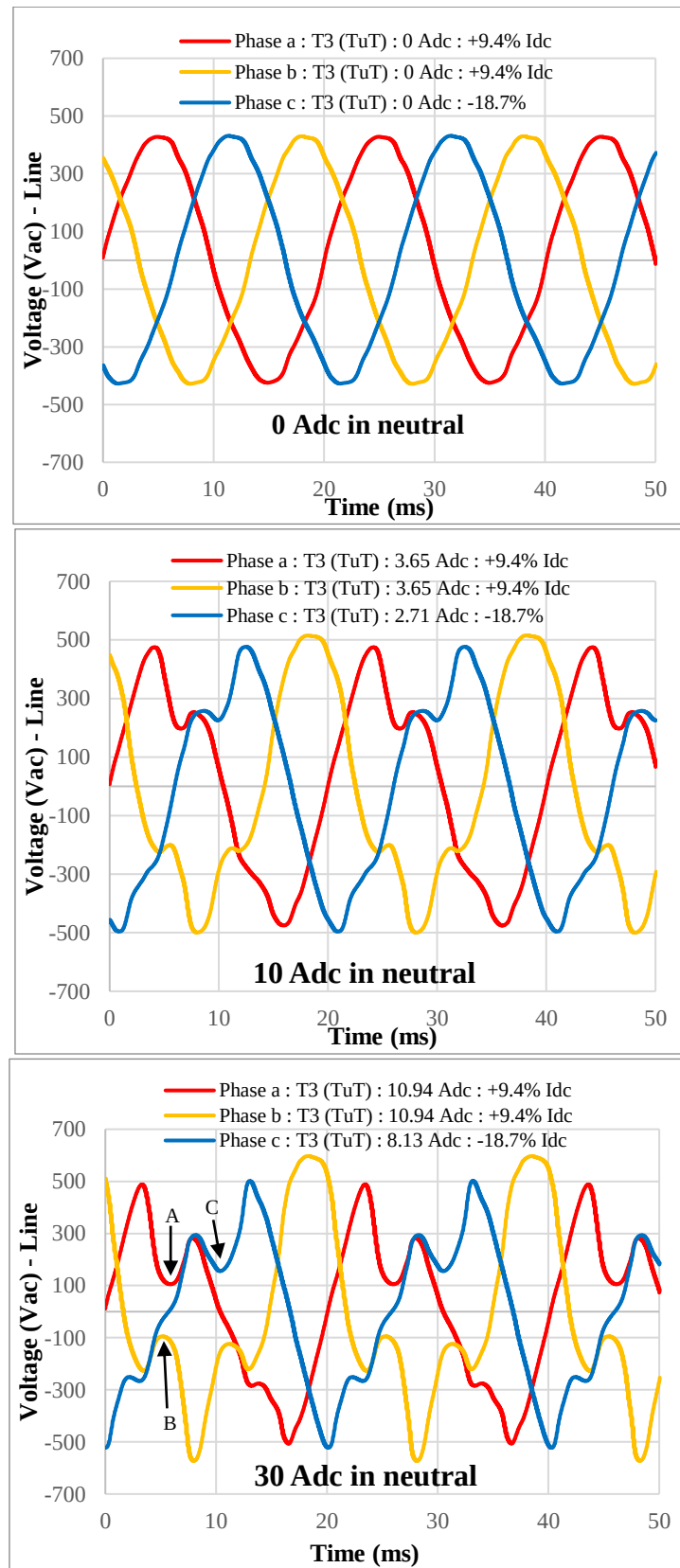


Figure 5.43 - Voltage waveforms at primary of TuT with 18.7% unbalance and 30 Adc in neutral

Waveform with balanced conditions at 30 Adc in neutral

Figure 5.44 shows the current waveforms, and Figure 5.45 show the voltage waveforms of the TuT when the I_{dc} in the neutral of the transformer is 30 Adc which is equal 10.00 Adc in the windings of each phase. It shows that at high levels of dc with the high dc mmf in the core there are low levels of distortion in both waveforms that is also reflected in the I_{thd} curves in Figure 5.38. This agrees with established thinking (section 2.7) but there is a slight increase in distortion as the dc is increased as can be seen at this high level where the waveforms are not perfect sinusoids.

It can be observed in Figure 5.43 and Figure 5.45 that there are very low levels of distortion on the voltage waveforms under relatively balanced current conditions. This is attributed to the fact that the HV winding of supply transformer T2 is connected to the TuT to achieve the back-to-back test circuit configuration (Figure 5.25). Due to this, the operating point of T2 is low on its induction curve at a relatively unstable region, resulting in this slight distortion.

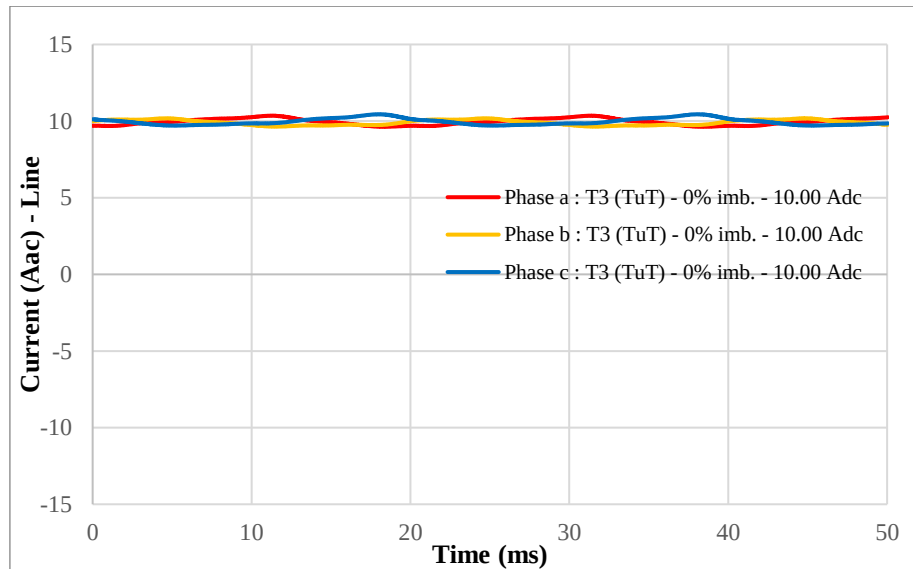


Figure 5.44 - Current waveforms at primary of TuT with 0% unbalance and 30 Adc in neutral

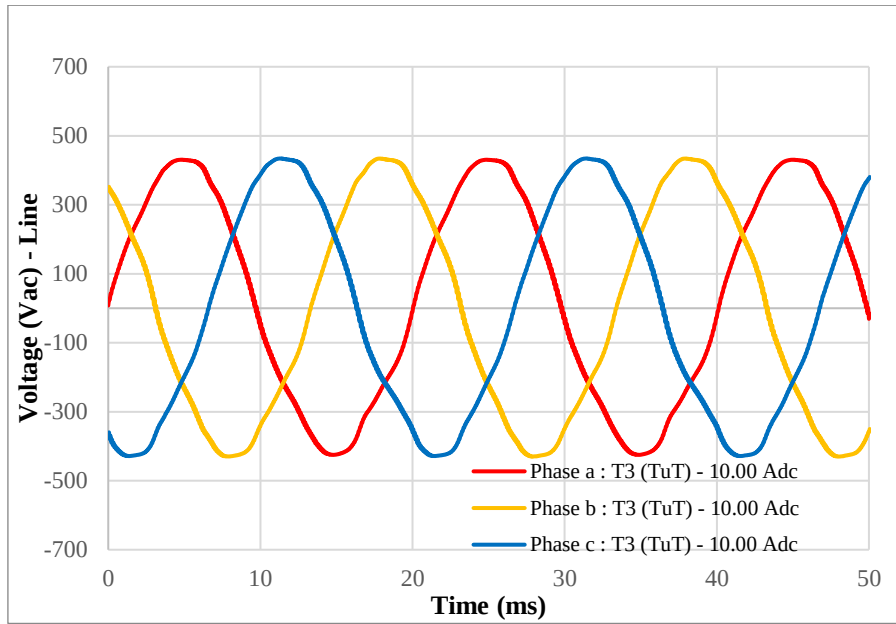


Figure 5.45 – Voltage waveforms at primary of TuT with 0% unbalance and 30 Adc in neutral

The harmonic distortion is directly affected by increasing levels of dc in the windings of a 3p3L transformer, and show a distinct ‘signature’ which is in agreement with other studies [1],[7]. This ‘signature’ (Figure 5.36 and Figure 5.39) demonstrates a strong correlation between the level of harmonic distortion, and the degree of core saturation as determined by the stray flux recordings. This is in agreement with the study by Masoum and Moses [32].

In conclusion, it has been demonstrated that the harmonic distortion measurement ‘signature’ can be a useful tool in post-GIC event analysis to determine the core saturation response of a transformer. In addition, it could be used to predict failures once data is correlated with actual failures, over the longer term.

5.2.4 Heating, reactive power and noise response of a 3p3L transformer with GIC or dc

Heating in relation to transformer core saturation in an unbalanced system

Test description

This test monitored the temperatures as described in the theory development section while repeating the dc tests described in section 5.2.2, at 0% and 4.5% unbalance, and then at the high level of 18.7% unbalance, as the dc current was increased in 1 Adc increments from 0 Adc to 30 Adc.

Temperature changes of a relatively large metallic structure could be a slow transient so it was required to perform the test over two hours pausing for four minutes between each 1 Adc increment of the current in the neutral of the TuT from 1 Adc to 30 Adc.

Test results discussion

The winding temperature at the start of the 0% unbalance test was cold at 23.750°C. Over the full dc injection range from 0 Adc to 30 Adc there was a 0.375°C tank temperature increase observed on probe 1 (Figure 3.17 and Appendix table 4), and a 0.125°C temperature increase on the tank at probe 2. Over the same period there was a 1.063°C winding temperature increase observed. The increases were generally linear in nature.

For the 4.5% unbalance test, the winding temperature at the start was 23.813°C. Over the full dc injection range from 0 Adc to 30 Adc there was a 0.375°C tank temperature increase observed on probe 1 (Figure 3.17 and Appendix table 5), and a 0.188°C temperature increase on the tank at probe 2. Over the same period there was a 1.250°C winding temperature increase observed. The increases were also generally linear in nature.

For the 18.7% unbalance test, the winding temperature at the start was 24.063°C. Over the full dc injection range from 0 Adc to 30 Adc there was a 1.188°C tank temperature increase observed on probe 1 (Figure 3.17 and Appendix table 6), and a 1.063°C temperature increase on the tank at probe 2. Over the same period there was a 2.062°C winding temperature increase observed. The increases were also generally linear in nature.

For the balanced and unbalanced cases, the tank temperature increases observed on both probe 1 and 2 as the dc current was increased from 0 to 30 Adc follow similar trends which means that the increase is unlikely to be related to localised heating due to eddy currents from high levels of stray flux in the tank area at probe 1, but rather linked to the convective temperature increase. This is corroborated by the winding temperature measurements which had all increased considerably for each test. The test at a high unbalance of 18.7% also showed similar behaviour.

Outside of the protocol, there was a final attempt to escalate the level of saturation on the outer limbs and possible ‘hot-spot’ temperature response of the tank wall. The unbalance was increased on Phase b to 23.5% to increase the level of dc in the outer limbs and possible saturation and stray flux at the tank wall at the outer limbs. The results, however, were similar to the 18.7% unbalance so the protocol was not adapted.

It was clear that the temperatures recorded during the tank temperature measurements were shown to be as a result of the convective heating of the winding and not eddy current heating caused by stray flux. These results do not agree with Biro *et al.* [24] and Chisepo, Borrill and Gaunt [31], and Price [26], that state when the saturation point is reached, the magnetic flux finds a transmission path by exiting the core into the surrounding material such as the tank which induces eddy currents causing localised heating. The unbalance tests of THD, reactive power, stray flux and noise indicate high levels of core saturation and large differences between the balanced and unbalanced cases which indicates that there should be significant hot-spot heating. The stray flux measured near the tank wall of the TuT, however, was of a relatively low magnitude in comparison to the stray flux at the centre of the winding.

In the final analysis, it was concluded that the results were due to the TuT's thermal and winding 'oversizing' to increase its thermal safety margins as a test transformer, which was inherent in its design.

Reactive power in relation to transformer saturation in an unbalanced system

Test description

This test produced reactive power graphs of the TuT to compare its reactive power consumption under balanced conditions and at an unbalance of 4.5%. This was done by taking reactive power measurements with the power analyser at each 1 Adc increment from 0 to 30 Adc injected into its neutral. The results could then be evaluated and compared to the results of the core saturation response at each level of dc from the stray flux readings and THD.

Test results discussion

The results of this test showed that as the dc in the neutral of the TuT increased (Figure 5.46), the reactive power increased linearly for both the balanced case and with an unbalance of 4.5%. In the balanced case the maximum draw of reactive power, Q, was lower by a small margin when compared to the unbalanced case. There were subsequent tests performed at higher levels of unbalance of up to 18.7 % (in Figure 5.46) where a similar trend emerged but with a clear increase in the 'diverging' of the reactive power, Q, between the phases considering the balanced and unbalanced cases.

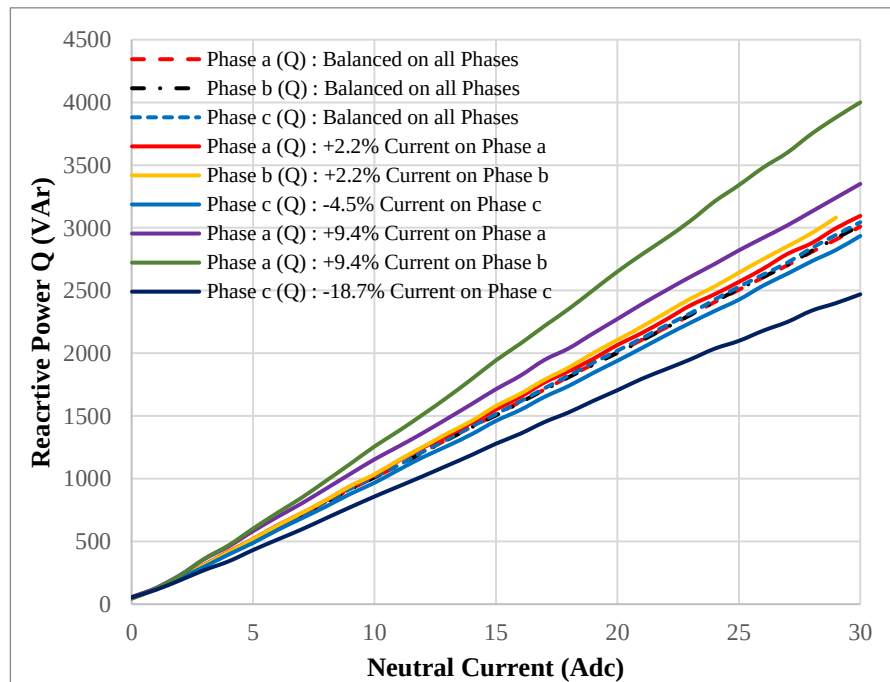


Figure 5.46- Reactive Power at Primary Winding of TuT - Balanced, 4.5%, and 18.7% Unbalance

This agrees with literature that with a balanced 3p3L power transformer, a linear uniform characteristic was obtained between phases (Figure 2.15). In the unbalanced case, however, it did not agree with literature where it states an unbalanced case yields a non-linear reactive power (Q) characteristic [31]. In literature, however, this non-linearity only becomes apparent at neutral current levels in excess of 150 Adc.

It did agree with literature that the draw of reactive power was significantly higher for an unbalanced configuration. This trend is also reflected in the I_{thd} and U_{thd} curves as dc or the unbalance between phases increase.

It was noted that the phases that have lower levels of dc current also show notably lower levels of reactive power absorption, which indicates that the total reactive power absorption may be similar when comparing the balanced and unbalanced cases. To confirm this, *Figure 5.47* shows the total reactive power. Considering the scale difference, it still indicates that there is divergence, but the degree of divergence is significantly less than in *Figure 5.46*.

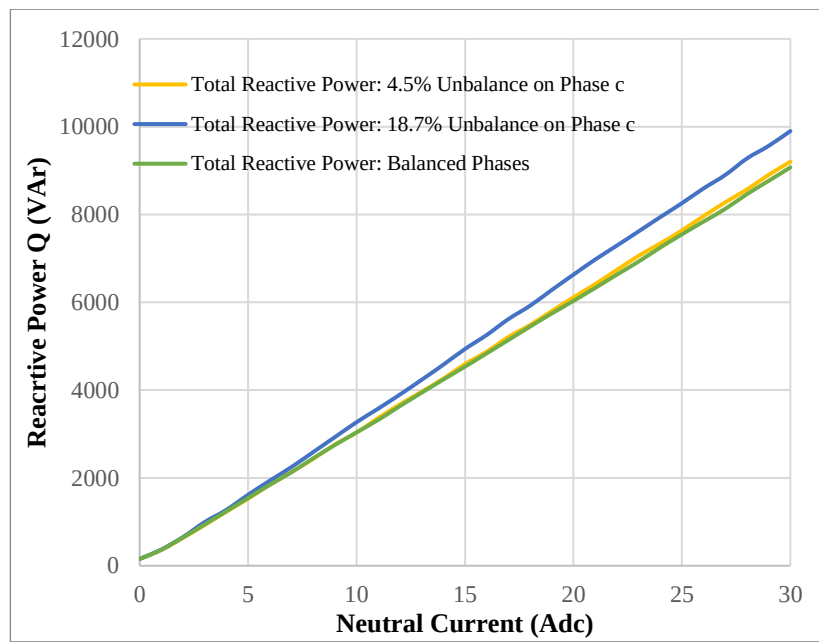


Figure 5.47 – Total Reactive Power at Primary Winding of TuT - Balanced, 4.5%, and 18.7% Unbalance

It is noted that concurrent with the increase of injected dc, similar to the linear increase of reactive power in *Figure 5.46*, there is a linear increase in excitation current (*Figure 5.48*). The phase with the lowest dc current also has the lowest reactive power as shown in *Figure 5.46*.

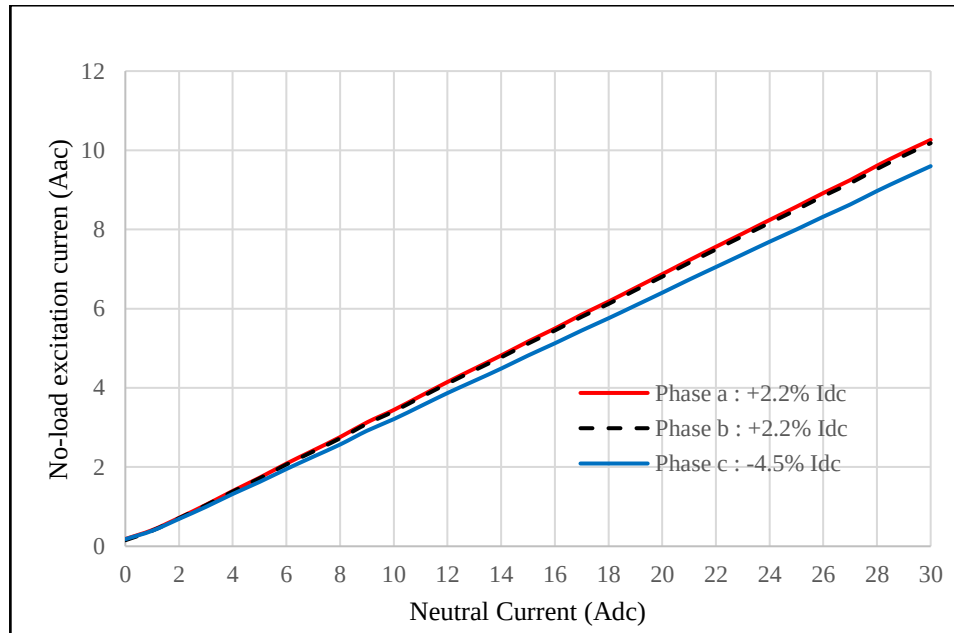


Figure 5.48 - Excitation current measured at primary of TuT - 4.5% unbalance

In conclusion, the results of the tests as shown in the reactive power, Q , graph demonstrate a strong correlation between the level of core saturation and the level of unbalanced dc current in the winding. This increase was shown as linear with the increase of dc in both the balanced and unbalanced configurations [54]. It was revealed in the experiment that the reactive power curves of the phases with an unbalance, however, diverge significantly from each other due to the varying dc current and the related decrease of inductance in the windings, whereas the reactive power curves with the balanced cases, are almost superimposed on each other.

The total reactive power measurements support this conclusion but there is a significantly lower degree of divergence.

This increase of reactive power agrees with literature, an example of which is discussed by Chisepo [31], where it was shown that even with a low level unbalanced GIC of less than 10 A_{dc}/phase in a 3p3L transformer, the transformer model was forced into saturated conditions with increased current harmonic distortion and with significantly higher reactive power when compared with the balanced cases.

Finally, the non-linear Q -GIC characteristic was not observed in this test but it is likely that this was because the dc current could not elevate the core to sufficiently ‘deep’ saturation levels for this response to become evident.

Noise in relation to transformer core saturation in an unbalanced system

Test description

This test was performed by repeating the dc injection test described in section 5.2.2 at a dc current of 10 A_{dc}, 20 A_{dc} and 30 A_{dc} at an unbalance level 0% and 4.5%. A dynamic-type

audio microphone was installed inside the transformer tank and at each dc current level, the audio levels were recorded using a personal computer sound card. This enabled the audio level to be compared to other indicators of saturation level such as the THD of the transformer at each level of dc.

Test results discussion

The recordings obtained were transformed into the frequency domain using a FFT as shown in Figure 5.49, Figure 5.50, and Figure 5.51.

It is clear that as described by Heathcote [40] there is a dominant sound level at the 100 Hz harmonic. This is not attributed to the 2nd harmonic associated with GIC but Heathcote [40], and Heindl *et al.* [68] state that this is inherent in the audio signature of all power transformers in normal operation. Heathcote [40] states that the most striking point is the strength of the component at 100 Hz or twice the normal operating frequency of the transformer. The cause of this is attributed to the magnetostrictive strain in the transformer core that produces a longitudinal vibration in the laminations at this measured frequency.

Figure 5.49 and Table 5.10 display the audio signature at balanced conditions, and a 4.5% unbalance with 10 Adc injected into the neutral of the TuT. There are clear peaks at the harmonics of the electrical fundamental frequency of 50 Hz for both the balanced and unbalanced signals, but with the 4.5% unbalance there is a general increase in amplitude at the harmonics from 50 Hz to 500 Hz, except at 200 Hz.

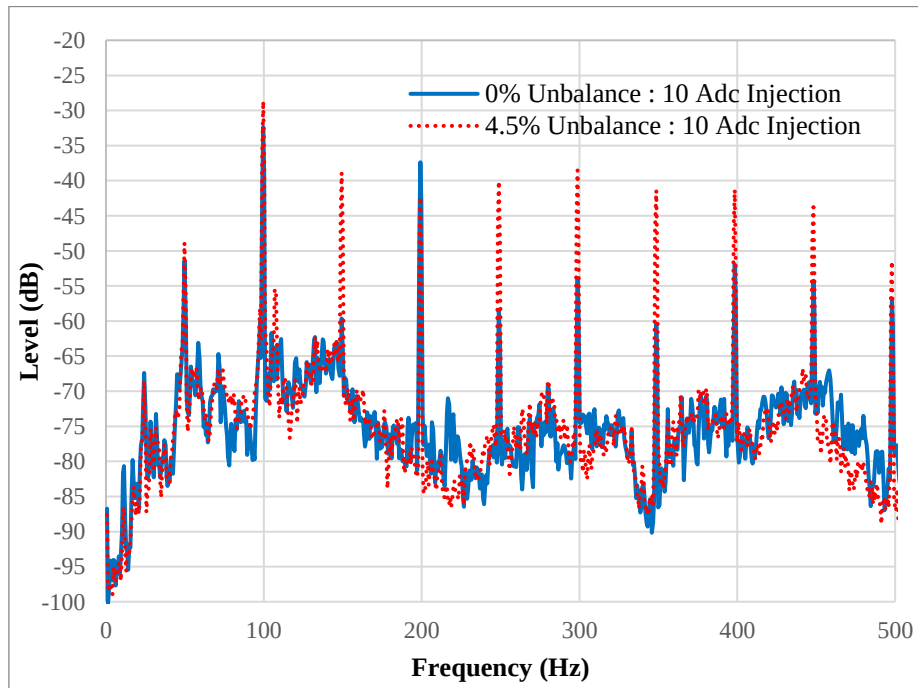


Figure 5.49 – FFT of audio recording of the TuT at 0% and 4.5% unbalances with 10 Adc in neutral of TuT

Figure 5.50 and Table 5.10 show the comparison of the audio signatures with 20 Adc injection in the TuT neutral at balanced conditions and at a 4.5% unbalance. In comparison to the audio signature with a 10 Adc injection in Figure 5.49 and Table 5.10, the fundamental frequency amplitude increases significantly by 5 dB with no change in the 2nd harmonic amplitude and a slight decrease at 150 Hz. There is a very large increase in amplitude of the harmonics from 400 Hz to 500 Hz. At this level of the dc the transformer noise started to change into a loud and urgent ‘growl’ instead of the audible ‘hum’ at no unbalance.

Table 5.10 – Sound pressure levels of TuT from 0 to 30 Adc at 0% and 4.5% unbalance

Frequency	Unbalance and Current			
	0% at 0 to 30 Adc	4.5% at 10 Adc	4.5% at 20 Adc	4.5% at 30 Adc
50	-52	-48	-45	-54
100	-33	-28	-26	-25
150	-60	-38	-40	-50
200	-37	-42	-37	-31
250	-58	-40	-38	-63
300	-54	-38	-42	-32
350	-60	-42	-37	-47
400	-52	-43	-32	-38
450	-54	-44	-36	-37
500	-57	-52	-43	-46
Legend (1-loudest to 4-softest):	1	2	3	4

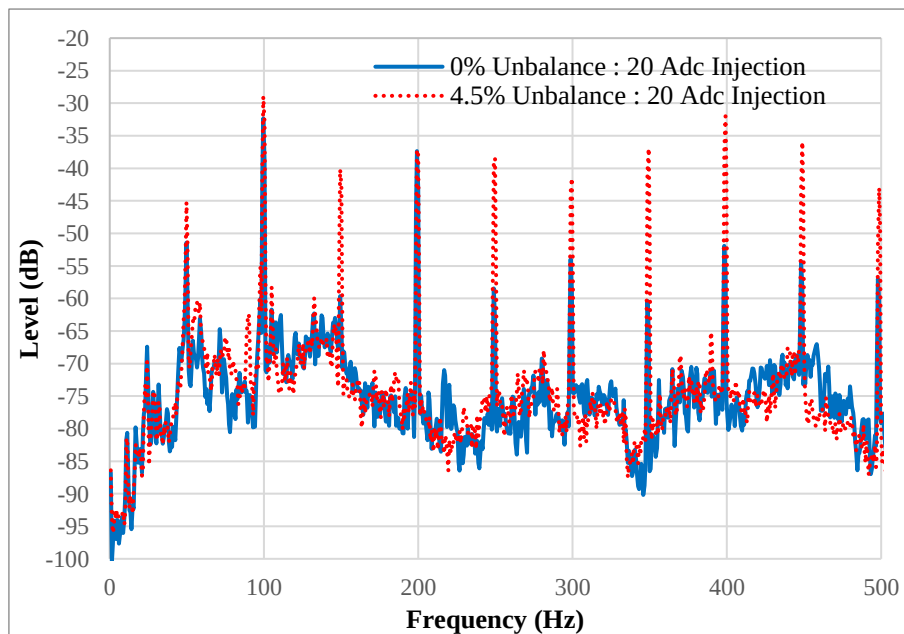


Figure 5.50 – FFT of audio recording of the TuT at 0% and 4.5% unbalances with 20 Adc in neutral of TuT

Figure 5.51 and Table 5.10 show the comparison of the audio signatures with 30 Adc injection in the TuT neutral at balanced conditions and at a 4.5% unbalance. In comparison to the audio signatures at 10 Adc and 20 Adc injection in Figure 5.49 and Figure 5.50, the 50 Hz fundamental frequency amplitude decreases to the lowest amplitude of all, recording a level lower than with no dc.

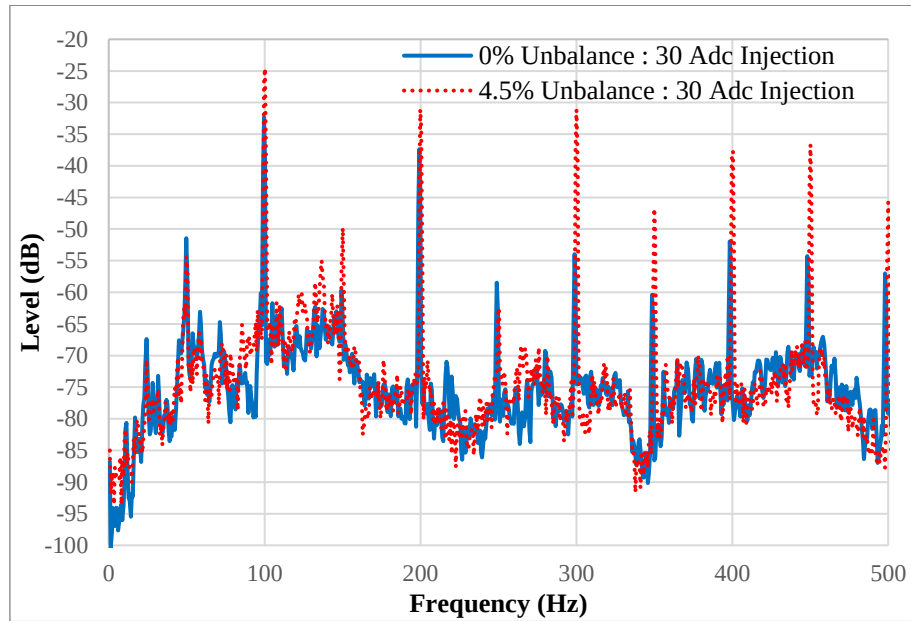


Figure 5.51 – FFT of audio recording of the TuT at 0% and 4.5% unbalances with 20 Adc in neutral of TuT

The 2nd order harmonics at 100 Hz and 200 Hz however are at their highest amplitude. This is coincident to the dominance of the 2nd order harmonics during GIC so could possibly be as a result of the large increase in these harmonics at high levels of dc. Figure 5.52 shows that the 2nd order harmonics of the phases are at their peak at the same time as the audio level at these harmonics is also at its peak.

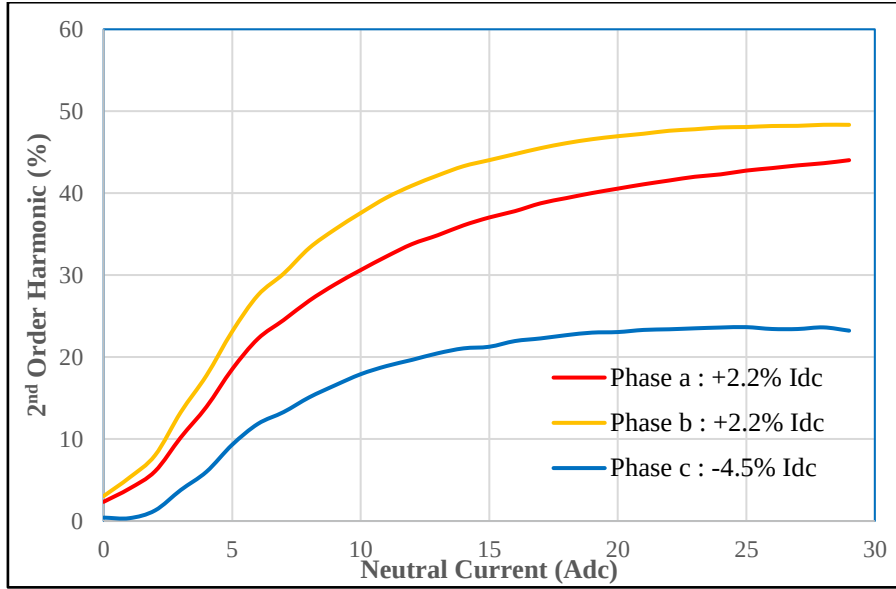


Figure 5.52 – 2nd Order harmonics at primary windings of TuT at a 4.5% unbalance

The stray flux curves as shown in Figure 5.33 and Figure 5.34 demonstrate that the core has become fully saturated at about 1 Adc in the neutral, and that stray flux is leaking from the core at an increasing rate similar to the rate increase of the sound levels in Figure 5.53.

Figure 5.53 shows that as the saturation level of the TuT core increases, as demonstrated by the increase in 2nd order harmonics (Figure 5.52), the sound level of the harmonics increase at a similar rate whereas the 50 Hz fundamental reaches a peak at 10 Adc and decreases significantly as the excitation energy is in the harmonics of the fundamental.

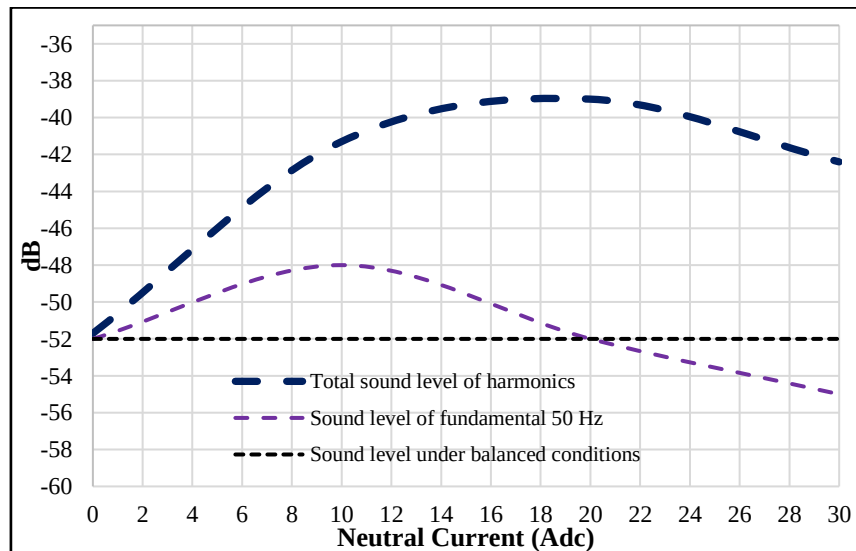


Figure 5.53 – Sound levels of TuT 50 Hz audio frequency (fundamental) and combined harmonic sound level

In conclusion, in the balanced case, there is no significant change in the audio levels as the dc current increases from 0 to 30 Adc (Figure 5.53), but in an unbalanced case, there is a

significant increase in noise level as the dc current increases. At 20 A and 30 Adc the noise is heard as an urgent ‘growl’ instead of the ‘buzz’ at 10 Adc and the audible ‘hum’ at no unbalance.

The audio levels at specific harmonic frequencies as shown in the FFT graphs are not as simple to define. It was shown that in an unbalanced case, as the dc current increased, the audio levels decreased notably at the fundamental frequency and increased significantly in the harmonic range. The FFT revealed that at an unbalance at 20 Adc the highest levels of noise are recorded at the frequencies above the 7th order harmonic at 350 Hz and at 30 Adc the highest noise levels are recorded at the even harmonics from 100 Hz. This follows a similar trend to the electrical harmonic distortion measured.

The core saturation levels as recorded by the search coils in section 5.2.2 demonstrate that the core has become fully saturated at about 1 Adc in the neutral, and that stray flux is leaking from the core at an increasing rate, similar to the rate of increase of the sound level of the harmonics as shown in Figure 5.53. It is noted that the sound level of the electrical harmonic distortion increases at a similar rate as the audio noise. The 50 Hz fundamental reaches a peak at 10 Adc and decreases significantly at 20 Adc and 30 Adc as the electrical excitation energy shifts into the harmonic frequencies.

6. CONCLUSION

6.1 Conclusion on research questions

Research question 1: What is stray flux and how is it measured?

Stray flux exits the core when the flux density in the core has increased beyond its saturation point into its non-linear region of operation and can be detected in the spaces and metallic material surrounding the core. This research confirmed, as discussed in literature, that air-search coils provide an effective means to map the stray flux paths and therefore, saturation response of a 3p3L power transformer core.

Research question 2: What does saturation depend on and how does it develop?

Saturation depends on the over-excitation of a transformer beyond its nominal operating point and into its non-linear operating region, with increases in harmonic distortion and the consumption of reactive power. It develops by increasing the excitation current beyond this point where the core enters partial, and then ‘deep’ core saturation. This was demonstrated in the experiments using search coils to map the core saturation response and comparing it to the excitation current level (I_{mag}). The precise inception of stray flux was possible that showed the differences of saturation at different parts of the core thereby addressing the three identified gaps in literature. Two related to the inception of stray flux where, at the centre limb it occurred before the stray flux at the joints on the outer limbs, and secondly, it showed the inception of bulk core stray flux leakage is significantly later than the stray flux inception at the mitred joints. The third gap in literature was addressed by the oscillograph measurements of the search coils which showed a clear amalgamation of flux from phases a and c in the phase b centre limb, whereas the stray flux from phases a and c was clear of the congested flux observed in the centre limb.

Research question 3: What is the normal operational level of flux beyond which saturation is excessive?

The normal operation level of stray flux beyond which saturation is excessive was shown to be the point where there was distinct evidence of an increase of stray flux that was not attributed to low-level fringing flux, and was below the point where stray flux was detected in the bulk core material. These experiments indicated that the IEC 61869-2 defined knee point could be a useful tool to compare cores of the same specification possibly for manufacturing quality control, but not as a reliable indicator of the core nominal operating point.

Research question 4: How does GIC affect unbalance and how does it affect the magnetisation of a 3p3L transformer?

The research confirmed what was observed in literature, that GIC affects the unbalance of a transformer by generating unbalanced dc mmf in the limbs of transformer cores resulting in an increase in harmonics and reactive power. It was shown that this phenomenon exploits the

inherent weakness of the uneven reactance paths of the 3p3L core design as demonstrated by the higher levels of harmonics in the limbs with the highest dc current. This was corroborated by the oscillographs of the stray flux at the limbs which showed an inherent unbalance in 3p3L transformers with a complex interaction of flux in the centre limb from the outer limbs, that wasn't evident in the outer limbs due to their uneven reluctance paths.

The waveforms clearly demonstrated the association between the level of the half-wave core saturation correlating with the increase in harmonics and stray flux existing the core in half-wave cycles. The magnetisation of the core cannot increase beyond this point of saturation and was recorded in the experiments as transformer losses in the form of stray flux an increase in harmonics, reactive power and voltage instability.

It was observed that there was an immediate and notable increase in stray flux at very small levels of unbalance of less than 1% and that even balanced conditions yielded some level of flux outside of the core at the joints as the dc current in the windings increased. It was therefore concluded that at high level of dc current, even extremely low levels of unbalance could cause relatively high levels of stray flux, and therefore core saturation and possible impact by GIC. This was corroborated in the study of the harmonic distortion.

Research question 5: What is the significance of the core-joint design?

As discussed in literature, these experiments demonstrated that the performance of multi step-lap core joints affect the overall response of a transformer core significantly.

It was shown that core saturation and the related stray flux develops differently at different parts of the core thereby addressing the two gaps identified in literature. Stray flux recordings demonstrated that core saturation at the centre limb yoke joints occurred before, and increased at a higher rate, than the joints at the outer limbs, and core saturation occurred at the core joints occurred before there were indications of bulk core material saturation. In addition, oscillographs of the search coils showed a more complex stray flux characteristic at the centre limb joints showing significant evidence of the outer limb flux whereas there was no evidence of the centre limb flux at the outer limb joints. This provided additional evidence of higher flux levels in the centre limb, and early saturation.

It can therefore be concluded that core joint design has significant impact on the overall performance of the core in an unbalanced 3p3L multi step-lap transformer with simultaneous ac and dc in the windings, as was demonstrated by the bulk core stray flux leakage which occurred significantly later than the stray flux inception at the mitred joints.

Research question 6: How can core saturation be measured?

From the study of the harmonics generated by the 3p3L power transformer with unbalanced dc in its windings, in relation to the core saturation at the joints and bulk core material, there was evidence of a strong correlation between the level of harmonics, and the level of dc and unbalance. It can therefore be concluded that THD can be used as a direct indicator of core

saturation, with a clear ‘signature’ of the saturation response, and can be effectively used as such in post-GIC event analysis. The harmonics with dc revealed that even under conditions where every effort was made to emulate a balanced condition, a small increase of THD resulted in a notable increase in core saturation as confirmed by the stray flux measurements.

Research question 7: What are the negative effects on transformers and the power system due to unbalance in the presence of GIC?

Reactive power

In an unbalanced case, this experiment showed that the reactive power absorption of a transformer increases when the transformer’s magnetic core is saturated. This could lead to poor voltage stability in the power system, with possible activation of protection and the consequential potential collapse of the network.

Noise

In an unbalanced case, as the dc current increased in a 3p3L transformer, the audio levels decreased notably at the fundamental frequency, and increased significantly in the harmonic range. It was noted that the rate of increase of the audio harmonics was similar to the increase of the electrical harmonic measurements. Considering this, the related gap in literature was addressed where this research confirmed that real-time monitoring of the audio range harmonic ‘signature’ of a transformer could be used to determine the cause of transformer noise to complement the post-event analysis of GIC or dc.

6.2 Research questions not answered by this research that need further work

Research question 7: What are the negative effects on transformers and the power system due to unbalance in the presence of GIC?

Heating

The temperature measurements could not deliver conclusive evidence of temperature changes of the tank related to the transformer’s core saturation due to its thermal and winding ‘oversizing’ to increase the safety margins as a test transformer. It is improbable that a 3p3L transformer is not vulnerable to this effect as there was a low level of stray flux outside of the core at the wall of the tank on the points closest to the outer windings that was in agreement with literature.

It can therefore be concluded that this test would need to be carried forward and repeated on an experiment using a different protocol, and possibly on a larger, commercial power transformer.

6.3 Validity of hypothesis

Phase unbalances of less than a typical practical unbalance on power system networks could increase the effect of GIC initiated dc magnetisation in 3p3L transformers with multi step-lap cores, resulting in saturation above normal levels and elevated levels of stray flux, with increases in heating, harmonic distortion, the draw of conventional reactive power, and noise.

It was established by this research that transmission network unbalances of less than 5% are sufficient to initiate a ‘deep’ core saturation response from a 3p3L power transformer with a multi step-lap core design. At 10 A_{dc} per phase allowed by the laboratory tests, there were notable levels of saturation as recorded by the harmonic distortion, reactive power, and evidence of stray flux. This was, however, not sufficient evidence to state that damage of the transformer was possible. It was clear by observing the graphs of the THD and stray flux recordings of the TuT, that an unbalance of <5% was sufficiently large to initiate excessively high core saturation levels in a model 3p3L power transformer. This conclusion as described would also apply to a larger 3p3L power transformer in a transmission network. These postulated levels of saturation as shown by the record of harmonics during real GIC events have led to typical medium and long-term damage from heating and insulation breakdown.

Most of the elements of the hypothesis have been proven true by this research, but the test on the transformer tank heating demonstrated no appreciable heating with asymmetrical ac and dc in the 3p3L transformer windings. It can therefore be concluded that the hypothesis is partially valid.

6.4 Implication of the work

The results of this research have given a more complete understanding of the impact of dc or geomagnetically induced currents in the windings of 3p3L power transformers. They did not contradict the prevailing theory that three-phase three-limb transformers in a balanced system are immune to GIC. The results, however, clearly demonstrated that very low levels of unbalance, well within the normal range in transmission networks, did cause a 3p3L transformer to enter saturated core conditions. Large magnitude GIC events could therefore result in ‘deep’ saturation of the core of 3p3L transformers leading to damage. There are significant numbers of 3p3L transformers in the field so if there was simultaneous failure of a large number of these transformers from a large GIC, their recovery could be very difficult, with the resulting impact of extended power outages.

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APPENDIX

Appendix table 1 - Transformer T2 winding resistance measurements

T2								
MV Winding (Star)								
Tap 1								
Winding Voltage (V)			Winding Current (A)			Winding Resistance (Ω)		
			A-N	N-B	C-N	A-N	N-B	C-N
8.2480	8.2280	8.2480	2.0040	2.0060	2.0040	4.1158	4.1017	4.1158
Tap 2								
Winding Voltage (V)			Winding Current (A)			Winding Resistance (Ω)		
			A-N	N-B	C-N	A-N	N-B	C-N
8.1280	8.1280	8.1080	2.0160	2.0160	2.0180	4.0317	4.0317	4.0178
Tap 3								
Winding Voltage (V)			Winding Current (A)			Winding Resistance (Ω)		
			A-N	N-B	C-N	A-N	N-B	C-N
7.9280	7.9480	7.9400	2.0360	2.0340	2.0260	3.8939	3.9076	3.9191
Tap 4								
Winding Voltage (V)			Winding Current (A)			Winding Resistance (Ω)		
			A-N	N-B	C-N	A-N	N-B	C-N
7.7480	7.7880	7.7480	2.0540	2.0500	2.0540	3.7722	3.7990	3.7722
Tap 5								
Winding Voltage (V)			Winding Current (A)			Winding Resistance (Ω)		
			A-N	N-B	C-N	A-N	N-B	C-N
7.5680	7.5880	7.5480	2.0720	2.0700	2.0740	3.6525	3.6657	3.6393
LV Winding (Star)								
Winding Voltage (V)			Winding Current (A)			Winding Resistance (Ω)		
			a-n	n-b	c-n	a-n	n-b	c-n
0.0230	0.0229	0.0235	5.0797	5.0797	5.0797	0.0045	0.0045	0.0046

Appendix table 2 - Transformer T1 winding resistance measurements

T1								
MV Winding (Delta)								
Tap 1								
Winding Voltage (V)			Winding Current (A)			Winding Resistance (Ω)		
			A-C	A-B	C-B	A-C	A-B	C-B
18.5000	18.5000	18.4000	2.0000	2.0000	2.0000	6.1667	6.1667	6.1333

Tap 2								
Winding Voltage (V)			Winding Current (A)			Winding Resistance (Ω)		
			A-C	A-B	C-B	A-C	A-B	C-B
17.6500	17.7100	17.6200	2.0000	2.0000	2.0000	5.8833	5.9033	5.8733
Tap 3								
Winding Voltage (V)			Winding Current (A)			Winding Resistance (Ω)		
			A-C	A-B	C-B	A-C	A-B	C-B
17.0600	17.1600	16.9900	2.0000	2.0000	2.0000	5.6867	5.7200	5.6633
Tap 4								
Winding Voltage (V)			Winding Current (A)			Winding Resistance (Ω)		
			A-C	A-B	C-B	A-C	A-B	C-B
16.5500	16.6300	16.4300	2.0000	2.0000	2.0000	5.5167	5.5433	5.4767
Tap 5								
Winding Voltage (V)			Winding Current (A)			Winding Resistance (Ω)		
			A-C	A-B	C-B	A-C	A-B	C-B
15.8000	16.1600	15.9000	2.0000	2.0000	2.0000	5.2667	5.3867	5.3000
LV Winding (Star)								
Winding Voltage (V)			Winding Current (A)			Winding Resistance (Ω)		
			a-n	n-b	c-n	a-n	n-b	c-n
0.0218	0.0219	0.0223	5.0518	5.0518	5.0518	0.0043	0.0043	0.0044

Appendix table 3 - Transformer T3 (TuT) winding resistance measurements

	T3 Winding Resistance Test								
	Phase A			Phase B			Phase C		
Winding	mV	mA	m Ω	mV	mA	m Ω	mV	mA	m Ω
Outer	66.25	558.00	118.7276	67.50	553.00	122.0615	65.50	548.00	119.5255
Middle	55.54	558.00	99.5341	54.30	551.00	98.5481	53.50	547.00	97.8062
Inner	42.68	558.00	76.4875	42.10	551.00	76.4065	41.66	548.00	76.0219

Appendix table 4 - Temperature of tank at 0% unbalance with 0 to 30 Adc in neutral of TuT

0% unbalance (balanced) - Ambient temperature of 23°C			
Current in neutral of TuT (Adc)	Tank Temperature 1 (°C)	Tank Temperature 2 (°C)	Winding Temperature (°C)
0	23.375	23.625	23.750
1	23.375	23.625	23.750
2	23.375	23.625	23.813
3	23.375	23.625	23.813
4	23.438	23.625	23.813
5	23.500	23.625	23.813
6	23.500	23.625	23.813
7	23.500	23.625	23.813
8	23.500	23.625	23.938
9	23.500	23.625	24.000
10	23.500	23.625	24.063
11	23.500	23.625	24.063
12	23.563	23.625	24.125
13	23.563	23.625	24.188
14	23.563	23.625	24.250
15	23.625	23.625	24.313
16	23.625	23.625	24.375
17	23.625	23.688	24.438
18	23.625	23.688	24.500
19	23.625	23.688	24.563
20	23.688	23.688	24.625
21	23.688	23.688	24.813
22	23.750	23.750	24.813
23	23.750	23.750	24.813
24	23.750	23.750	24.813
25	23.750	23.750	24.813
26	23.750	23.750	24.813
27	23.750	23.750	24.813
28	23.750	23.750	24.813
29	23.750	23.750	24.813
30	23.750	23.750	24.813

Appendix table 5 - Temperature of tank at 4.5% unbalance with 0 to 30 Adc in neutral of TuT

4.5% unbalance - Ambient temperature of 22°C			
Current in neutral of TuT (Adc)	Tank Temperature 1 (°C)	Tank Temperature 2 (°C)	Winding Temperature (°C)
0	23.375	23.625	23.813
1	23.438	23.625	23.813
2	23.438	23.625	23.813
3	23.438	23.625	23.813
4	23.438	23.625	23.813
5	23.500	23.625	23.813
6	23.500	23.625	23.813
7	23.500	23.625	23.813
8	23.500	23.625	23.938
9	23.500	23.625	24.000
10	23.500	23.625	24.063
11	23.500	23.625	24.063
12	23.563	23.625	24.125
13	23.563	23.625	24.188
14	23.563	23.625	24.250
15	23.625	23.625	24.313
16	23.625	23.625	24.375
17	23.625	23.688	24.438
18	23.625	23.688	24.500
19	23.625	23.688	24.563
20	23.688	23.688	24.625
21	23.688	23.688	24.813
22	23.688	23.688	24.813
23	23.688	23.688	24.813
24	23.688	23.750	24.813
25	23.750	23.750	25.063
26	23.750	23.750	25.063
27	23.750	23.750	25.063
28	23.750	23.750	25.063
29	23.750	23.813	25.063
30	23.750	23.813	25.063

Appendix table 6 - Temperature of tank at 18.7% unbalance with 0 to 30 Adc in neutral

18.7% unbalance - Ambient temperature of 22°C			
Current in neutral of TuT (Adc)	Tank Temperature 1 (°C)	Tank Temperature 2 (°C)	Winding Temperature (°C)
0	23.375	23.625	24.063
1	23.438	23.625	24.063
2	23.438	23.625	24.063
3	23.438	23.625	24.063
4	23.438	23.625	24.063
5	23.500	23.625	24.063
6	23.500	23.563	24.063
7	23.500	23.563	24.063
8	23.500	23.625	24.063
9	23.500	23.625	24.000
10	23.500	23.625	24.063
11	23.500	23.625	24.063
12	23.563	23.625	24.125
13	23.563	23.625	24.188
14	23.563	23.688	24.250
15	23.625	23.688	24.313
16	23.625	23.688	24.375
17	23.625	23.750	24.438
18	23.625	23.750	24.500
19	23.625	23.750	24.563
20	23.688	23.813	24.625
21	23.688	23.813	24.813
22	23.750	23.875	25.063
23	23.750	23.938	25.188
24	23.813	23.938	25.313
25	23.938	24.375	25.813
26	24.000	24.375	26.000
27	24.063	24.375	26.125
28	24.563	24.688	26.125
29	24.563	24.688	26.125
30	24.563	24.688	26.125